



**SAIMM PYROMETALLURGY**  
**INTERNATIONAL CONFERENCE 2026**  
Foundations of Competitiveness and Sustainability

**26-28 MAY 2026**  
CSIR INTERNATIONAL CONVENTION  
CENTRE, PRETORIA, SOUTH AFRICA



**Proceedings Book**

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**SAIMM**  
THE SOUTHERN AFRICAN INSTITUTE  
OF MINING AND METALLURGY

THE SOUTHERN AFRICAN INSTITUTE OF MINING AND METALLURGY  
JOHANNESBURG 2026

# **SAIMM PYROMETALLURGY**

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# FOREWORD

*What will South African pyrometallurgy be a hundred years from now?*

*What foundations must we lay now to get there?*

*Are we willing to make the choices that will change the future of our industry – and our country?*

These are the questions behind this conference — and behind these proceedings.

South Africa possesses the geological endowment, the people, and a century of built industrial capability to grow beyond pyrometallurgy, to explore and tame the vast and daunting frontier of high-temperature science and technology. The minerals beneath the Bushveld Complex — chromite, platinum group metals, vanadium, titanium — and the manganese of the Kalahari Manganese Field occur in concentrations found nowhere else on earth. They are the feedstocks of traditional smelting and, increasingly, the critical minerals of the energy transition: PGMs for hydrogen fuel cells, vanadium for grid-scale energy storage, titanium and manganese for the advanced materials a changing world demands.

This conference is an opportunity to look back at where we came from, acknowledge where we are now, and most importantly, look into the future and start choosing who and what we want to be. We invite our international colleagues — from 14 countries — to shape this future with us. Pyrometallurgy is in our soil, in our people, and in our story.

A century ago, Hendrik van der Bijl laid three foundations for a minerals-based industrial economy: affordable electricity through the Electricity Supply Commission, domestic steel through the Iron and Steel Corporation, and development capital through the Industrial Development Corporation. On those foundations rose the ferroalloy smelters of the 1960s, Mintek's DC arc furnace technology — developed here and licensed to the world — the ilmenite smelters of Namakwa Sands and KZN Sands, and the platinum smelting complexes of the Bushveld. Van der Bijl built with vision, with principle, and without self-enrichment. He planted trees under whose shade he would never sit. Politics change. Principles endure.

Those foundations served this country for a hundred years. Today South Africa's pyrometallurgical industry is no longer competitive or sustainable. Electricity costs have increased more than ninefold since 2008. Infrastructure constraints limit what we can move and produce. The global production landscape has shifted decisively — China now supplies approximately 60% of the world's ferrochrome, a position built through sustained state investment, and colleagues across Europe, Asia, and the Americas face variants of the same competitive pressure. These facts are the springboard for what comes next. Foundations can be laid again — and laying them will require choices that are bound to challenge accepted assumptions and norms, both within our industry and beyond it. This conference is a first foundation stone.

For the first time, the SAIMM brings Pyrometallurgy, Furnace Tapping, and Refractories together under a single banner — three symposia in one gathering. This is a deliberate consolidation of disciplines that share operating pressures, that depend on each other technically, and that are stronger for working in the same room. Ex Unitate Vires. The conference structure carries a message to our industry and our country: unity is strength. This time, all of us.

The programme reflects a community that is already choosing. One hundred and four papers from 14 countries. Four plenaries, fifteen keynotes, ten invited presentations, and sixteen student papers span the full arc from seasoned industrial practice to the next generation's fresh contribution. The PyroFuZA workshop — the Future of Pyrometallurgy in South Africa — opens the conference week with the forward-looking question that matters most: who do we choose to become?

Among the international colleagues gathered here are partners whose own industries face overlapping pressures. They bring perspectives that no single national community could generate alone.

The next generation is here too — and their presence carries a question about whether this industry will build the conditions that retain its best young minds or continue to export them. Van der Bijl laid foundations for competitiveness and sustainability that carried this industry for a century. We have the endowment, the people, and the knowledge to lay new foundations, built on competitive energy, principled leadership, strategic partnerships, and the courage to invest in a future whose full benefit our children's children will inherit. These proceedings can be the first planting.

**Let us choose well ... together.**

**Dr Johan Zietsman**  
**Conference Chairman**

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# KPI Trees as guide and reference tool for improvement

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Escalating costs and demanding market conditions compel high performance levels from limited resources. Under these conditions we are often handed blind goals without tangible roadmaps on how to achieve these, while working to gain as much benefit as possible from our efforts. This paper argues that key performance indicator (KPI) trees are central in navigating these conditions. KPI shines in highlighting opportunities and pitfalls when challenged with multiple efforts and untangling the how-to of a blind goal. A well-constructed KPI tree highlights the drivers, their impacts on the outcome, inter-dependence between drivers, and improvement opportunities each driver holds. It sets the basis for cost-benefit analyses and has a central role in cross-checking the benefits from several projects: checking unrealistic commitments in the planning phase and measuring success in the implementation phase. By understanding the basis for KPI targets, the KPI tree can also be used to challenge perceived targets. This paper demonstrates these concepts with three examples: how a KPI tree can be used to keep commitments honest, untangle blind goals into tangible targets, and break through perceived limitations.

## INTRODUCTION

Competitive markets and high production costs have always prompted and sustained improvement initiatives. In recent years though – and for the foreseeable future – escalating competition from world markets and soaring costs are multiplying the pressure to improve on survive-or-perish levels. Out of necessity we are dealing with ‘blind’ targets, such as: ‘lower the unit cost by 10%’. Or, investing in multiple concurrent improvement projects, each promising improvement and, while we desperately hope for all these improvements to fully realise, we know in the back of our minds that the room-to-improve pie is finite and smaller than the sum of all promises. How do we spend our resources wisely? And more daunting – will achieving theoretical best be good enough to survive in the current environment?

A key performance indicator (KPI) tree is an established tool, having been in use since at least the early 2000s – long before 2011 as claimed by a few internet sources; though its role as permanent advisor to business decisions is surprisingly under-utilised. A well-constructed KPI tree can serve as an indispensable reference tool to keep us honest in forecasting benefits from ideas, help translate ‘blind’ goals into tangible targets, while challenging our perceived ceilings. This paper aims to illustrate with examples how a KPI tree can serve as a sounding board in avoiding these traps.

An internet search for KPI trees delivers an abundance of resources (three of which are listed in the references (Ante, *et al.*, 2018; Bumba, *et al.*, 2023; Ionescu, *et al.*, 2024)). Different criteria and characteristics are given for KPI trees which are often customised to the application. From my experience KPI trees as applied in the context of this paper have the following characteristics:

- They are simple visual representations linking driver KPIs via intermediate KPIs to outcome KPIs.
- Each level of KPIs is linked to the next via sound mathematics and consistent application of engineering units. KPI trees include all parameters needed to be a complete mathematical representation of the outcome KPI, without any double accounting.
- A KPI tree branches out until the driver KPIs are within the control of the audience.
- Each KPI has two numbers: one is the target performance, and the second is the actual performance. The target performance is often based on a theoretical or demonstrated best, while the actual performance is derived from analysis of actual data over a given period.
- The gaps between these two sets of numbers are the potential for improvement. The reasons behind these gaps are one of the most significant outcomes of a KPI tree analysis. Getting to these reasons usually requires in-depth data analysis.
- KPI trees are not financial models; they disregard complexities and are purposely kept minimal and simple.

## KPI TREES FOR TiO<sub>2</sub> SLAG PRODUCTION

A KPI tree for TiO<sub>2</sub> slag production from ilmenite smelting is given in Figure 1. This example has slag production as outcome, but it can easily be expanded to have the revenue from slag as outcome by adding slag pricing and cost to level of slag production and production time. Driver KPIs are highlighted with yellow colouring. The mathematical relations linking the drivers to the outcome are listed in equations 1 to 5. The tree can be read from left to right or right to left:

$$\text{Annual TiO}_2 \text{ Slag Production } \left(\frac{t}{a}\right) = \text{TiO}_2 \text{ Slag Production } \left(\frac{t}{h}\right) \times \text{Production Time } \left(\frac{h}{a}\right) \quad [1]$$

$$\text{TiO}_2 \text{ Slag Production } \left(\frac{t}{h}\right) = \text{Ilmenite Feedrate } \left(\frac{t}{h}\right) \times \text{Slag Yield (\% of ilm)} \quad [2]$$

$$\text{Ilmenite Feedrate } \left(\frac{t}{h}\right) = \frac{\text{Power Input (MW)} - \text{Heat Losses (MW)}}{\text{Theoretical Energy } \left(\frac{MWh}{t \text{ ilm}}\right)} \quad [3]$$

$$\text{Power Input (MW)} = \text{Power Capacity (MW)} \times \text{On stream rate (\% of capacity)} \quad [4]$$

$$\text{Production Time} = 8760 \left(\frac{h}{a}\right) \times \text{On stream time (\% of time)} \quad [5]$$

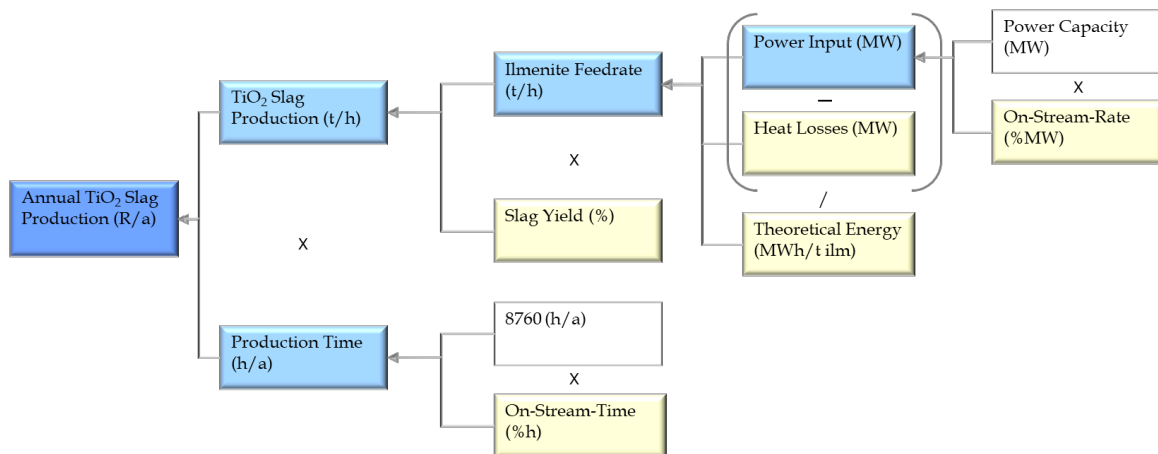


Figure 1. KPI tree with drivers determining the revenue from TiO<sub>2</sub> slag production.

The KPI tree in Figure 1 is making the inherent assumption that the annual hours cannot be changed (hence the white colouring), and similarly that the furnace power rating is also a given – an assumption which is revisited later in the paper. The message is obvious: by improving the driver KPIs, the annual slag outcome will increase by default. The next logical questions are: what is the current performance of each driver KPI, what should it be, and, what needs to be done to bring it up to par? The answers will be revealed from in-depth plant data analyses and a fundamental understanding of the ilmenite smelting process.

#### ***On-Stream-Rate and On-Stream-Time***

On-stream-rate (OSR) is the average run-time power input, divided by a reference power rating. This reference power rating may be the design capacity, or a best demonstrated value. Either may apply provided the same number is used consistently. On-stream-time (OST) is simply the ratio of power-on time to calendar time.

The actual OST and OSR performance – and detractors preventing target OSR and OST – will be clear from downtime and slow time logs. Accurate capturing of the reasons for downtime and slow time is essential for high integrity results.

#### ***Heat Losses, Theoretical Energy and Slag Yield***

All three these parameters relate back to the mass and energy balance of the process. Visualise a space within the physical boundaries of the furnace vessel along the roof, walls and hearth: this is the boundary of the mass and energy balance, and the boundaries of the heat loss as a driver KPI. Heat losses therefore incorporate roof, shell and hearth cooling, including cooling of internal copper blocks, but excluding cooling external to this boundary. Heat losses occur irrespective of power input – albeit at lower rates with prolonged downtimes. Hence, following the construction of the KPI tree in Figure 1, the heat loss number is the total heat loss averaged over the OST. This number will be slightly higher than the full-power instantaneous heat loss number because it includes a component of heat losses incurred during downtimes.

The theoretical energy is the energy needed to convert 1 ton of ilmenite to  $\text{TiO}_2$  slag of the desired grade. For ilmenite smelting the theoretical energy is around 1 MW/t ilmenite. Its precise number depends on, amongst others, the ilmenite and anthracite qualities, and slag  $\text{TiO}_2$ .

Slag yield is the ratio of slag tapped to ilmenite feed. It depends on the ilmenite and slag qualities. Like the theoretical energy, slag yield is fixed once the ilmenite quality and slag  $\text{TiO}_2$  are set. Where the theoretical energy has limited room for improvement by eliminating inefficiencies like air ingress, and moisture in the anthracite, slag yield is almost fully determined by ilmenite and slag qualities.

#### ***Other Performance Indicators***

While this paper focusses on KPI tree parameters, it must be said that there are other performance indicators outside of the KPI tree which carry useful information. IPR, the ilmenite to power ratio, is an example. The IPR is a valuable measure of the thermal efficiency of the furnace and it is driven by the same driver KPIs shown in Figure 1: to improve the IPR the operator needs to run the furnace at maximum power input, smelt with high grade raw materials to keep the theoretical energy low, and keep heat losses to an optimum.

#### ***Driver KPI Sensitivities***

While the KPI tree is a user-friendly visualisation of how driver KPIs feed the outcome, the relative importance or impact of each driver is not visualised with a tree. This need can be met by translating the mathematical relationships of the KPI tree into a waterfall graph such as given in Figure 2. For illustrative purposes, assume a 40 MW furnace with theoretical and best demonstrated driver KPI numbers as in Table I. To evaluate the sensitivity of the annual slag production to each driver KPI, the impact of a 5% decrease in each driver KPI is quantified in Figure 2. The directions of the impacts are as expected: lower OSR, OST and slag yields decrease the annual slag production, while lower heat losses and theoretical energies increase slag production. Interestingly the OSR and theoretical energy changes

the outcome with 5.8% and 5.3% respectively, which are higher than the 5% change in KPI. The annual slag production is the least sensitive to a 5% change in heat losses, while slag yield and OST show changes in the outcome equivalent to the change in KPI. It needs to be noted though that in this example 5 MW heat loss is a low 12.5% of the power input capacity. For a higher heat loss load, heat losses will be more sensitive to a 5% change. It is significant that the combined impact of the changes is a decrease of 14.9%, which is larger than the sum of the individual impacts of -9.7%.

Table I. Illustrative theoretical and demonstrated best driver KPIs for a hypothetical 40 MW ilmenite smelter

|                  | Power capacity (MW) | On-stream-rate (%) | Heat losses (MW) | Theoretical energy (MWh/t ilm) | Slag yield (%) | On-stream-time (%) |
|------------------|---------------------|--------------------|------------------|--------------------------------|----------------|--------------------|
| Theoretical best | 40                  | 95.0               | 5.0              | 1.030                          | 50.3           | 92.0               |
| Sensitivity test | 40                  | 90.3               | 4.8              | 0.979                          | 47.8           | 87.4               |

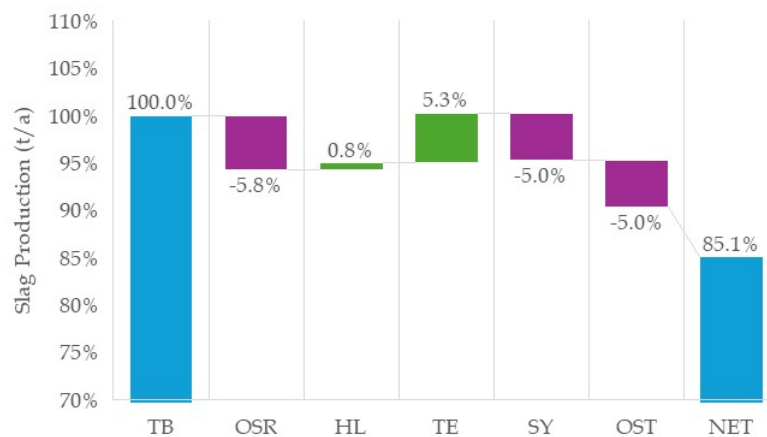


Figure 2. A waterfall graph quantifying the impact of a 5% decrease in each of the driver KPIs. (TB = theoretical best; OSR = on-stream-rate; HL = heat losses; TE = theoretical energy; SY = slag yield; OST = on-stream-time; NET = combined impact of the 5% reductions in each driver KPI).

## EXAMPLES FROM ILMENITE SMELTING

The same hypothetical 40 MW ilmenite smelter is taken as basis for the following examples, with actual – equally hypothetical driver KPI performances in Table II. The waterfall graph in Figure 3 visualises the contribution of each driver KPI in decreasing the annual slag production from theoretical best to actual. Any effort to improve the annual slag production must be rooted in one or more of the KPI drivers. More specifically, the effort must address a reason – proven with data – which causes the lower performance of the KPI.



Table II. Theoretical and best demonstrated driver KPIs for a hypothetical 40 MW ilmenite smelter, compared to hypothetically actual performance numbers

|                    | Power capacity (MW) | On-stream-rate (%) | Heat losses (MW) | Theoretical energy (MWh/t ilm) | Slag yield (%) | On-stream-time (%) |
|--------------------|---------------------|--------------------|------------------|--------------------------------|----------------|--------------------|
| Theoretical best   | 40                  | 95.0               | 5.0              | 1.030                          | 50.3           | 92.0               |
| Actual performance | 40                  | 92.0               | 5.8              | 1.040                          | 49.5           | 90.0               |

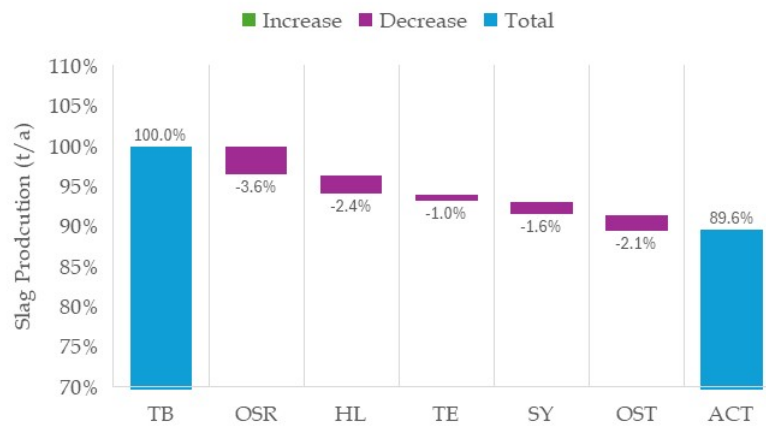


Figure 3. A waterfall graph visualising the impact of actual performance in bringing down the performance numbers from theoretical best to actual. Based on the numbers in Table II. (TB = theoretical best; OSR = on-stream-rate; HL = heat losses; TE = theoretical energy; SY = slag yield; OST = on-stream-time; ACT = combined impact of the actuals).

### Keep promises honest

It is standard practice to motivate the spending of resources by amongst others the estimated benefit which can be realised from spending these resources. This is where the KPI tree serves as valuable reference. Firstly, each project must address a specific detractor shown from data analysis as a reason for not achieving theoretical best. Secondly, the benefit from one, or the cumulative benefit from multiple projects, cannot be more than the gap between actual and theoretical best. Unless a fundamental aspect of the theoretical best is addressed via the project – this will be returned to later in the paper.

Take OST of the hypothetical 40 MW ilmenite smelter as an example: as per the theoretical best in Table II the best it can do is 92%. OST is also unique in that it has a hard ceiling: there are only 8760 hours in a year. OST itself can be broken up further into a next lower level of driver KPIs as visualised in Figure 4 with the driver KPIs the additioning intervals of electrodes, additioning time, planned shuts, and unplanned downtime. Additioning intervals in turn depend on electrode consumption (kg/MWh), power input (MWh), and electrode lengths (m). Assume for argument's sake data analyses of the theoretical best and actual performance numbers to these lower-level driver KPIs yield the numbers in Table III. This view offers the following insights:

- Improvement efforts should focus on the gaps in electrode additioning time and the causes for unplanned downtime.
- Estimated benefits can at best be as large as the gap, and no bigger. In the example, an initiative to reduce the electrode additioning time cannot promise an improvement any larger than 12 minutes per addition, unless the assumptions underlying the theoretical best of 35 minutes are consciously shifted as part of the effort. OST has a finite limit: there are only 24 hours in a day

and hence the improvement potential is finite. This sanity check is crucial especially when multiple efforts are addressing the same driver KPI.

- Interactions between driver KPIs need to be considered. For example: any effort which increases the OST will lead to higher cumulative annual energy input, leading to higher annual electrode consumption in kg/a even though the kg/MWh number does not change. For the same adding interval of one addition every 12 run-hours, more additions per annum will occur. To keep the promised benefit realistic, the increase in OST needs to be tempered with the increased number of additions.
- The unintended consequence of the improvement needs to be addressed. Decreasing the duration of additions brings with it a decrease in opportunities to conduct parallel planned or unplanned downtime activities. To realise the promised increase in uptime, these activities must be equally shortened, find existing alternative downtime timeslots, or be eliminated.

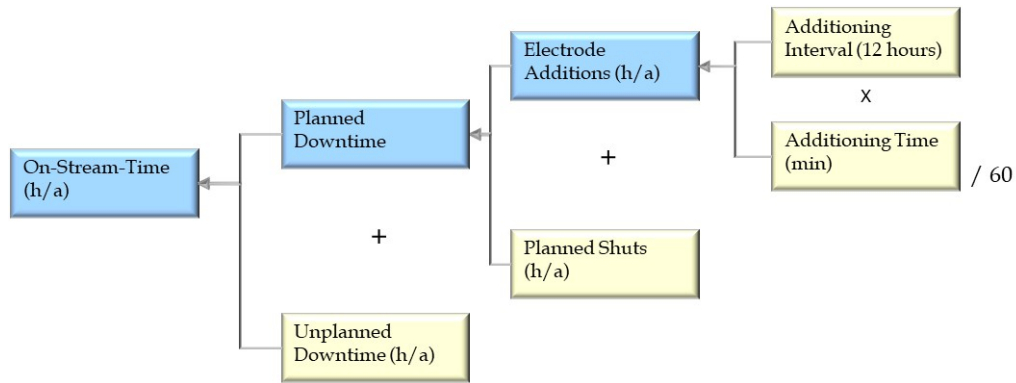


Figure 4. KPI tree breaking OST up into a lower level of driver KPIs.

Table III. Theoretical best and actual performance values of KPIs for the OST of a hypothetical 40 MW ilmenite smelter. Driver KPIs are in bold.

|                             |                    | Theoretical best | Fictive actual performance | Performance gaps |
|-----------------------------|--------------------|------------------|----------------------------|------------------|
| On-stream-time              | %                  | 92.0             | 90.0                       | 2.0%             |
| Electrode addition interval | Hours per run time | <b>12</b>        | <b>12</b>                  | -                |
| Downtime with each addition | Min each           | <b>35</b>        | <b>47</b>                  | <b>12</b>        |
| Number of additions         | Per annum          | 671.6            | 657.0                      |                  |
| Adding downtime             | h/a                | 391.8            | 514.7                      | 122.9            |
| Planned shuts               | h/week             | <b>4.0</b>       | <b>4.0</b>                 | -                |
| Unplanned downtime          | h/week             | <b>2.0</b>       | <b>3.0</b>                 | <b>1.0</b>       |
| Total downtime              | h/a                | 703.8            | 878.7                      | 174.9            |

#### Translate 'blind' goals to tangible targets

Hard times can compel 'blind' goals, such as realising a year-on-year energy improvement of 5%. Firstly, the engineering units of the goal needs clarification: 5% of total energy input can easily be realised by reducing the annual slag production with an equivalent amount. But this surely would not be the intent. Assume therefore a more refined goal is set by specifying the energy improvement as 5% of specific energy consumption, SEC as MWh/t TiO<sub>2</sub> slag.

Working with target and actual values of the driver KPIs in Figure 1 proves to be an insightful exercise to appreciate what is needed to realise this objective for the first year of the year-on-year counter for the hypothetical 40 MW smelter. The actual SEC is 2.49 MWh/t slag. A 5% improvement brings it to 2.37 MWh/t slag – 0.01 points different from the theoretical best case and therefore for all practical purposes

the goal requires a shift from actual performance to theoretical best, within one year. Clearly the realism of these targets can be questioned. To advance the argument further, assume this is possible, the following year would need an SEC of 2.24 MWh/t slag. Achieving this by pulling individual levers will require a 60 MW furnace (at 5 MW heat loss), or lowering the heat losses to 3.2 MW (which is a low 8% of the total power input), or a theoretical energy of 0.975 MWh/t ilm, or increasing the slag yield to 53.2%. Both theoretical energy and slag yield can potentially be improved to these levels by a prompt improvement in ilmenite and reductant qualities. Given the integral nature of many mine-smelter operators, this is not achievable within a year, and certainly not as a year-on-year initiative (though it does highlight the impact of raw material resource selection). A more realistic approach is to improve each driver KPI with smaller amounts as illustrated from the last row in Table IV. Though each of these small improvements is closer to realism than one large individual shift, they are still better than the theoretical best and achieving them will still require creativity. The conclusion is obvious though: at some point we will need to challenge the theoretical best.

Table IV. Example numbers showing how small improvements in several driver KPIs could realise a 5% improvement in specific energy consumption.

|                       | Power capacity (MW) | On-stream-rate (%) | Heat losses (MW) | Theoretical energy (MWh/t ilm) | Slag yield (%) | On-stream-time (%) | SEC (MWh/t slag) |
|-----------------------|---------------------|--------------------|------------------|--------------------------------|----------------|--------------------|------------------|
| Actual performance    | 40                  | 92.0               | 5.8              | 1.040                          | 49.5           | 90.0               | 2.49             |
| Theoretical best      | 40                  | 95.0               | 5.0              | 1.030                          | 50.3           | 92.0               | 2.36             |
| 5% Improvement in SEC | 40                  | -                  | 4.5              | 1.020                          | 52.0           | -                  | 2.24             |

### Challenge perceived ceilings

*'The reasonable man adapts himself to the world; the unreasonable man persists in trying to adapt the world to himself. Therefore, all progress depends on the unreasonable man.'*

– George Bernard Shaw

In the preceding examples the underlying assumption was that the theoretical best was determined by design aspects such as furnace power rating, electrode configuration, and raw material qualities – all parameters which admittedly are largely fixed. Our subconscious approach to the theoretical best is the question: what is possible? In the current business environment, we need to change the question to 'what is needed to achieve the impossible Lala-land?' The following paragraphs describe examples with the proviso – in line with an unforgiving economic setting – of low to no capital expenditure. This proviso therefore excludes the options of ilmenite pre-heating and pre-reduction, both of which will reduce the theoretical energy significantly.

#### On-stream-rate

In principle the on-stream-rate can be increased via two approaches: shortening low-power operation when recovering from downtimes and continuing to address detractors preventing consistent runs at maximum capacity, or abandoning the belief that the design power rating is a limitation. In casting away this belief, the OSR in the KPI tree is in principle allowed to exceed 100% while the power capacity stays unchanged. Transformers are often installed with excess capacity and provided there are no other downstream bottlenecks, this can be a straightforward solution to increase OSR. When this luxury is unavailable, the electrical operating window can be questioned by consulting an exact electrical power-voltage-current diagram specific to the equipment and application. For instance, by lowering the operating resistance with 0.5 mΩ the power output of the hypothetical 40 MW furnace can be increased

to 41 MW with operating voltage still within design limits, and providing that the current limitations can accommodate the increase in operating current. In KPI driver terms this would be equivalent to 40 MW at an OSR of 97.2%. Any seasoned ilmenite smelter would outright dismiss the idea. But if open to follow through with the thought we might find a way: yes, lowering resistance would lead to x, but could we counteract it with y? This example is not claiming a trouble-free solution but simply argues that we can challenge perceived ceilings when armed with detailed equipment knowledge and a deep process understanding.

#### *Heat Losses, Theoretical Energy and Slag Yield*

In similar unreasonable fashion we can continue to explore:

- Roof heat losses which are the major contributor to total heat losses. Following heat transfer principles, what parameter(s) can be levered to minimise roof heat losses while still protecting the integrity of the mechanical structure? From a practical perspective how could the roof assembly be changed to do this? What unintended impacts are foreseen and how can these be dealt with? To investigate these questions effectively, exact heat loss instrumentation, supported by a sound understanding of heat transfer principles, are needed.
- The annual slag production is as sensitive to the theoretical energy as it is to OST and OSR (Figure 2). Yet, while we focus on OST and OSR, we accept and even ignore the theoretical energy. Admittedly theoretical energy is a relatively in-flexible number determined largely by raw material qualities with ilmenite grade often fixed by the integrated mine-smelter value chain. Nevertheless, ilmenites and reductants do carry inefficiencies which must be quantified. The theoretical energy itself merits breakdown which mathematically quantifies the contribution of ilmenite impurities, ash and moisture in the reductant, and air ingress amongst others. This can be achieved with a detailed ilmenite smelting mass and energy balance. Ilmenite quality is especially significant because it affects two driver KPIs: theoretical energy and slag yield to deliver a double impact on slag production – either way.

#### *On-stream-time*

Table III quantifies a set of realistic driver KPIs to deliver a theoretical best OST of 92.0%. In an unreasonable world, targets for the electrode additioning interval of every 20 run-time hours, each lasting only 25 minutes could be set. And while we are at it, the four hours planned downtime could be kept, but with a decrease in the unplanned downtime to one hour per week. The potential OST from each of these improvements is given in Table V. To achieve these targets a detailed breakdown of the activities making up the electrode additions, the unplanned downtime, and the electrode additioning intervals will need detailing. On the face of it, little more than a paper exercise. But here is a real-life example: as mentioned the electrode additioning interval depends on electrode consumption, power input and electrode dimensions. On the Tronox smelters it was a change in electrode dimensions which realised an electrode interval of 20 run-time hours. One driver KPI down, two more to go. The psychological impact of this improvement is undeniable: once a perceived ceiling is broken, the psychological effort needed to break the next is less and this attitude has the potential to jump-start more similar initiatives.

*Table V. Unreasonable world targets and their impact on OST*

|                                                       | OST, % |
|-------------------------------------------------------|--------|
| Theoretical best                                      | 92.0   |
| Electrode additioning every 20 run-hours              | 93.8   |
| Electrode additioning 25 min each                     | 93.7   |
| Combination of 20 min additioning, every 20 run-hours | 94.9   |
| Only one unplanned downtime per week                  | 92.4   |
| Cumulative impact                                     | 95.1   |

A waterfall graph based on the KPI tree drivers in Figure 1 and Figure 4, expanded to include the Lala-land targets described in this section are shown in Figure 5. In Table V the unreasonable world targets are calculated backwards from theoretical best to illustrate the impact of each driver KPI compared to

theoretical best. The probability of achieving all these Lala-land targets is not high. And undeniably none of these challenges will be achieved without significant effort, expert knowledge, a deep understanding of the equipment and process, or without risk. The purpose of Figure 5 is not to state the possible, but to facilitate challenging the ceilings.

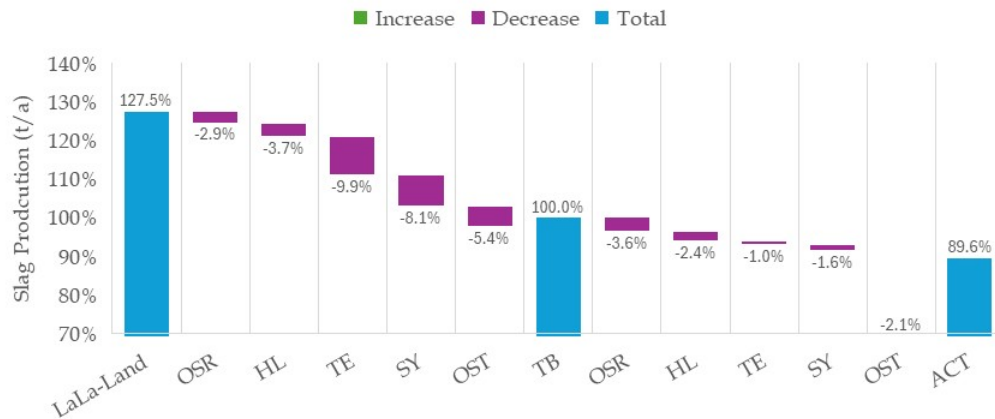


Figure 5. A waterfall graph working backwards from theoretical best to Lala-land. (TB = theoretical best; OSR = on-stream-time; HL = heat losses; TE = theoretical energy; SY = slag yield; OST = on-stream-time; ACT = combined impact)

## IN CONCLUSION

KPI trees are visualisations which demonstrate the mathematical links from driver KPIs through intermediate KPIs to outcome KPI. In combination with waterfall graphs, KPI trees are an essential tool to focus improvement effort, translate blind goals to tangible targets, and keep improvement promises realistic. Even when used in an unreasonable context, the mathematical prerequisite of a KPI tree must be upheld: the mathematical path from the driver KPI to the intermediate KPI must be correct, clear and achievable.

In the construction of a KPI tree, double accounting is avoided, though interaction between KPIs will remain and must be considered in target setting and estimating improvements. At the same time, actual improvements often lead to unintended losses. To realise the promised benefits, these losses must be addressed in the benefit estimation and the project scope.

Unreasonable KPI trees built on Lala-land numbers could successfully and consistently lead to higher than theoretical best numbers in an environment where open mindsets prevail, and the workforce includes experts with knowledge and understanding of the equipment, process, and relevant scientific concepts. These are essential traits to maximise the success rate of unreasonable-ness.

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# Advances in pyrometallurgical piloting

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The global metallurgical industry faces many challenges, providing opportunities for engineering innovation with exciting new projects, whether it involves extracting critical minerals, promoting circularity, shifting to green metals production or simply improving efficiency to increase profitability. During the development and commercialisation of new processes and technologies, it is essential to assess the readiness and maturity of each application and mitigate any significant risks before implementation at commercial scale. Pilot testing can often be an effective tool for de-risking new technologies and ensuring successful project outcomes, when well-executed. A key outcome of piloting should be metallurgical understanding; however, it is also important that the pilot programme is designed to provide information required to enable the scale-up and design of practical, reliable, and high-performing equipment.

This test work campaign was a portion of a maturation plan for a pyrometallurgical technology. The paper covers from problem definition to the design of the pilot furnace and monitoring systems, through to test execution and data analysis. The test work setup employed advanced monitoring techniques, including real-time off-gas analysis and high-temperature bath video, to gain novel insights into process behaviour. The paper also demonstrates how a computational model, developed in parallel with the test work and validated against experimental results, is a powerful tool to support process and equipment scale-up.

**Keywords:** pilot testing, scale-up, technology readiness, test work, modelling

## INTRODUCTION

As a result of aggressive carbon emission reduction targets, the need to secure critical minerals supply, and the pressures to improve circularity, the metallurgical industry is seeing – at a pace faster than ever – the development and commercialisation of new process flowsheets, new technologies or applications. Often, the need to be ‘first-to-market’ drives accelerated project timelines, and the time necessary for adequate research, test work, modelling and comprehensive process studies is simply not available. This leads to increased commercialisation risks, notably to key performance indicators affecting the commercial-scale business case.

To minimise such risks, a technology readiness assessment is a useful instrument to critically analyse elements of the flowsheet. As part of the assessment, a technology maturation plan is developed to reduce and manage the specific risks associated with a new technology implementation. Maturation plan development should consider the timelines of the commercialisation schedule, to generate practical answers that can be used as the design/engineering progresses, often in parallel. A maturation plan can involve a diverse range of activities, such as benchmarking, test work, modelling, piloting, and/or study work. The scope, scale and details of such work are tailored to ensure key technology uncertainties and risks are addressed in line with project budgets and timelines (Rodd *et al.*, 2024).



In this confidential example, the key aspects of interest pertained to:

- Reductive smelting in an electric furnace of a feed material, reductant and fluxes – producing gas, slag and metal;
- Mass and heat transfer kinetics of feed added to molten slag bath;
- Reduction efficiency and propensity for foaming;
- Deportment of elements of interest between metal and slag, under representative pO<sub>2</sub> conditions.

Given the fundamental nature of the items to be addressed by the maturation plan involved:

- Comparison with benchmarking data and/or empirical models;
- Thermodynamics modelling, complementing the understanding of slag chemistry, slag properties and observed deviation from equilibrium conditions.
- Computation modelling, first replicating pilot-scale and scaled-up geometries;
- Pilot-scale smelting test work campaigns, targeted at investigating the impact of different variables.

The combination of these activities was powerful in gaining the necessary understanding of the phenomena at play, to support a successful scale-up and rapid ramp up to full production. This paper focuses on the computational modelling and test work portion of the maturation plan.

### **Computational Model**

Computational modelling, including computational fluid dynamics (CFD), offers an excellent opportunity to simulate new or difficult-to-visualise or measure processes and to provide engineering design and optimisation opportunities. The application of CFD to electric smelting furnaces is particularly challenging due to the combination of multiple physical phenomena and inherent challenges in obtaining validation data at the high process temperatures. The physical phenomena include fluid flows, material properties (variable with temperature and composition), electromagnetics, magnetohydrodynamics, melting and solidification, buoyancy and particle behaviour.

The use of CFD in the pyrometallurgical smelting environment has advanced to the extent needed to describe the slag heat transfer and fluid flow within a furnace including the slag interaction with the feed, matte/metal, electrodes/arc, crucible and freeboard. Predicting furnace bath heat transfer and fluid flow coupled with feed interaction (melting, reactions, gas generation etc.) when increasing the furnace size from test-scale to commercial-scale can provide insight into the temperature distribution within the slag, potential freeze lining and build up thicknesses, and other key performance indicators such as throughput. This is in line with the Harada *et al* (2018) who found that well-executed, continuous pilot campaigns can produce industrially meaningful data, but scale effects (thermal efficiency, flow patterns, entrainment, energy consumption) must be explicitly understood – especially when using small furnaces.

The model can highlight, for example, the effect of electromagnetic stirring of the slag bath in different furnace scales and configurations. Harada *et al* (2018) studied the effect of DC arcs and Lorentz-driven flow fields in the slag and metal bath for DC furnaces. In a small DC pilot furnace, the electromagnetic stirring due to Lorentz forces is in the opposite direction to the buoyancy-driven flow, which minimises bath stirring significantly as shown in Figure 1. In a larger circular, 3-electrode AC furnace, however, the Lorentz forces drive a circular pattern of flow as seen in Figure 2. These two figures highlight both the importance of a correct understanding of the fundamental phenomena and their impact in different furnace geometries. The location and quantum of feed addition to such slag bath, given the stirring and bath temperature gradients, can impact smelting rate and related reactions (for example, when a particle gets added/carried towards a cold zone vs towards a hot slag zone).

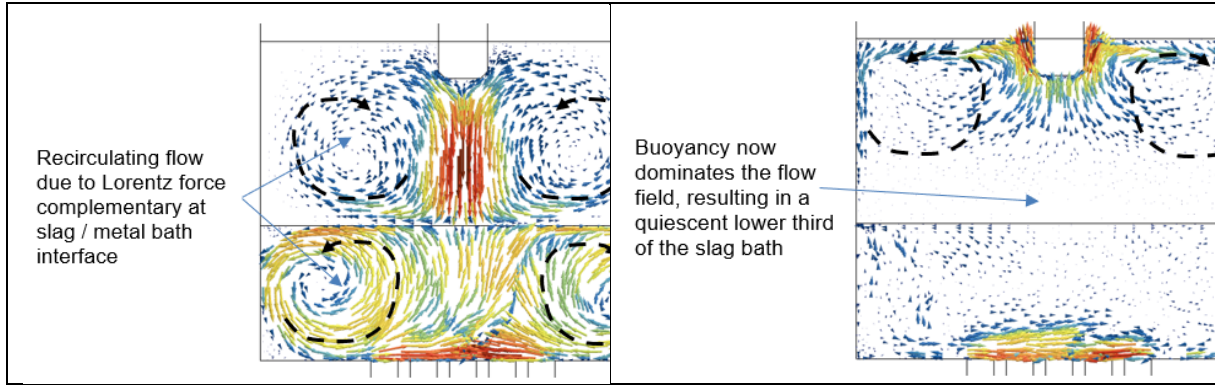


Figure 1: Effect of Lorentz forces only (left) and both Lorentz and buoyancy (right) on slag and metal bath flows.

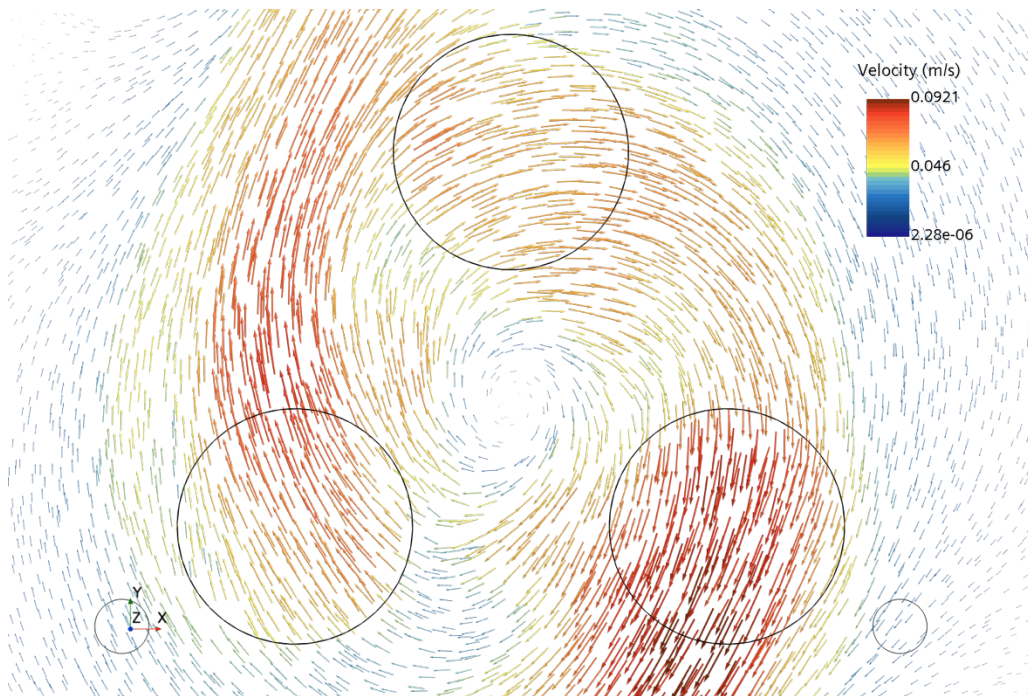


Figure 2: Slag velocity due to Lorentz forces on horizontal plane beneath electrode tips in a circular AC furnace.

One of the more challenging aspects of furnace simulations is to incorporate the plasma arc without having to resolve the small length and time scales associated with this phenomenon, without resorting to zero- or one-dimensional approaches that oversimplify reality. To support the pilot work, a hybrid approach was developed that provides realistic arc behaviour under the influence of its own and external electromagnetic fields but allows for a manageable computational effort in both steady and unsteady simulations. Figure 3 illustrates both the simulated arc and the material properties used in its creation.

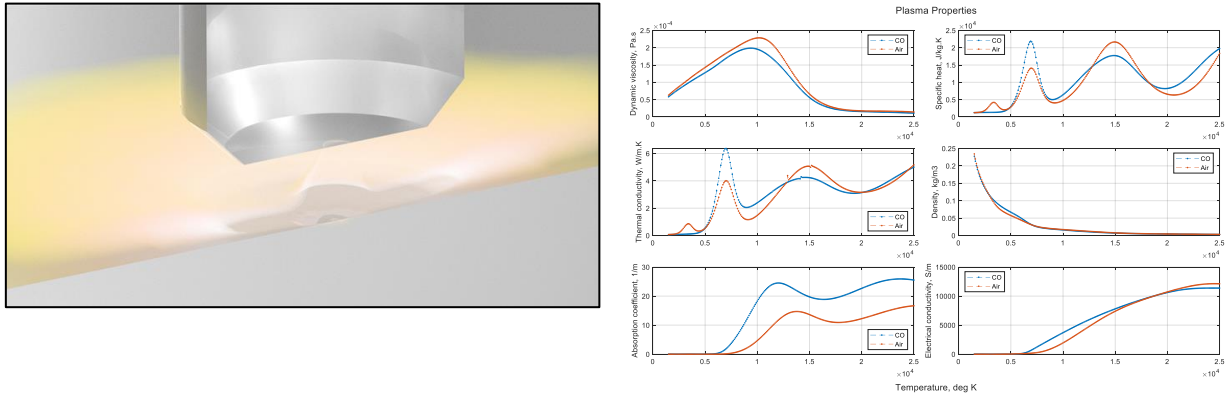


Figure 3. (a) Fully coupled magnetohydrodynamics applied to bath and plasma arc and (b) Detailed arc plasma property definitions.

Once the model features are built, the impact of different furnace geometries, fluid properties, feed port locations etc., on flow and velocity gradients, thermal gradients and other phenomena of interest can be investigated.

### Pilot Testing

As part of this maturation plan, pilot testing was conducted to understand specific process phenomena and to validate the CFD. To ensure the test work delivers maximum value and high-quality data, careful consideration must be given to all aspects of the programme, including material selection and preparation, equipment design and instrumentation, the training and operating procedures of the team, and processing and interpretation of the data obtained.

### Raw Material Procurement, Preparation, and Sampling

Raw material selection followed from the maturation plan and test objectives. The particle-size distributions for different tests were also selected, and the feed system was designed accordingly. A vibratory feeder with a loss-in-weight bin was selected to handle a wide range of particle sizes and allow for continuous measurement and control of the feed rate.

Careful material preparation, sampling and analysis were identified as a critical foundation for campaign success. Materials should not be allowed to segregate, and sampling must follow best practices to justify meaningful conclusions. To achieve this, each raw material was blended, split, and sampled using rotary splitters exclusively, and care was taken to ensure that suitably large sample sizes were always used for analysis. The laboratory analysis of each type of raw material was selected to meet the required objectives. A rotary splitter was not available at the smelting site, so all materials were split into small, sealed bags and the batches for test work were prepared from these to minimise segregation. As required, materials were dried and/or crushed and screened to the desired size.

### Furnace Design

#### Scale and Configuration

The maturation plan and test objectives were used to determine the ideal scale for the test work. While a larger test furnace may provide seemingly impressive results, bigger is not necessarily better; it depends on what the objectives of the test work are. Testing at larger scale limits the number of operating points that can be tested due to the time taken to reach steady state, increases the amount of raw materials required, and raises the cost. For truly novel processes, the raw materials themselves may also be under development and only available in limited supply, so this is an important consideration in sizing a test furnace. It is often necessary to use different scales of testing (Geldenhuys and Jones, 2011) to achieve the full objectives of a comprehensive maturation plan.

After evaluation of key phenomena at play and possible geometries, the following objectives could be achieved with a 50 kW DC furnace as indicated in Figure 4:

- Open-bath operating mode
- Impact of variable feed materials granulometry and composition
- Impact of variable slag chemistry
- Visualisation of feed behaviour for validation of feed models
- 

The furnace was carefully instrumented to obtain sufficient data to validate the model so that it could be used to extrapolate behaviour reliably to larger scales. In future, the model will be used to evaluate commercial-scale geometries with a much higher degree of certainty.

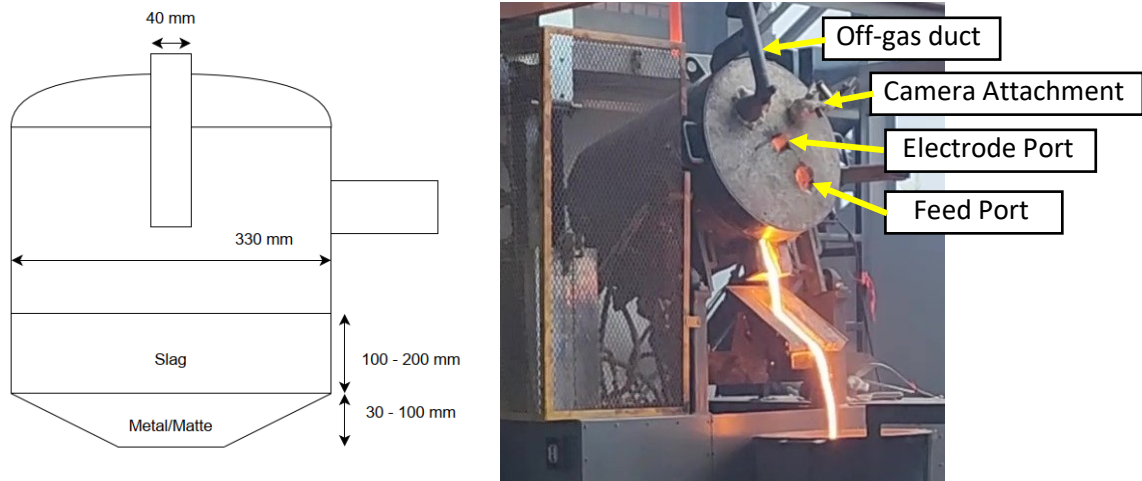


Figure 4: Furnace geometry (left) and roof arrangement (right).

#### *Tapping*

The pilot furnace used in this study was designed to be a tilting furnace that is emptied solely by tilting (Figure 4) and does not have a taphole. This suited the needs of the process well, without requiring the complication of tapping. Some processes, such as submerged arc smelting (eg., FeMn) may require a taphole to remove material without disturbing the accumulated bed significantly. Lancing a taphole on a furnace this size can result in significant refractory wear, so a specially sized drill is recommended where possible, and particular care in tapping practices must be exercised to ensure safe operations and minimal refractory damage.

#### *Cooling*

The cooling strategy was developed to safely promote vessel integrity, while allowing for monitoring of heat losses. Water cooling can introduce safety risks and may over-cool pilot furnaces, creating non-representative thermal profiles by increasing heat-losses. Relying on radiative cooling alone can make it difficult to quantify heat losses, or result in overheating of nearby equipment. Air cooling was selected for the sidewalls, and heat fluxes were calculated by measuring air temperatures and flow, as well as estimating radiative losses using shell temperatures measured with the thermal camera.

#### *Refractories*

Refractory life on a test furnace is inherently challenging due to temperature cycling, varying slag compositions and process conditions, and the proximity of the arc to the sidewall. Various refractories were tested because they each have different advantages. Although refractory selection was primarily focused on the pilot furnace needs, the campaigns also provide valuable learnings that can be applied to commercial applications. Because refractory dissolution has a proportionally greater effect at small pilot scale, even modest wear can alter bath chemistry and affect results. This also limited the refractory choice in this case.

Refractory selection for a pilot-scale furnace should consider lining type (brick versus castable), chemical compatibility, and overall campaign practicality. Castable can be easier to install at small scale



and offer flexibility in fitting tight geometries, but it requires proper curing and firing to remove moisture and achieve full strength. Brick linings, by contrast, are pre-fired and immediately ready for use, though they may be more difficult to install within small furnace dimensions.

Refractory material selection must first and foremost suit the expected slag chemistry and process conditions. Thermodynamic modelling was used to narrow down the suitable refractory phases and cup tests (shown in Figure 5) were conducted to assess the real impact of different slags on the refractory. Slag #1 has a low liquidus and evidently sufficiently low viscosity to penetrate the brick pores completely at the cup test temperature of 1600°C. Slag #3 precipitates an MgO-based solid as the primary phase (Figure 6) and therefore showed the least refractory penetration.



Figure 5. Cup test results for MgO bricks with different slags.

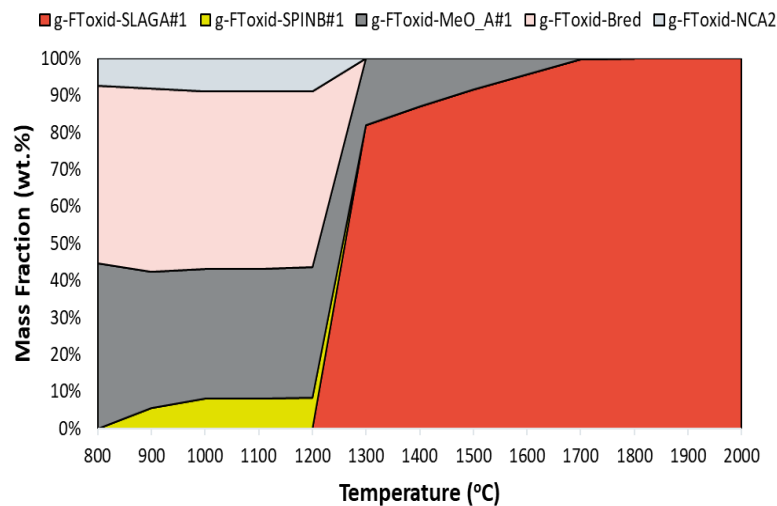


Figure 6: FactSage phase distribution of Slag #3.

### Off-gas

Fume and dust can present a serious health hazard to those operating the plant. It is industry practice to ensure that any hazardous materials generated by the process and borne by the off-gas are captured and treated in an off-gas abatement system. This was no different in the case of this piloting facility.

A key factor driving the desired metallurgical processes within the furnace was maintaining a low-oxygen atmosphere. High freeboard oxygen content potentially interferes with the reduction process in the bath. It may also cause electrodes to wear prematurely as the graphite reacts with oxygen at high

temperatures. Oxygen from outside the furnace can enter the freeboard through any openings in the furnace structure, namely the feed port, the camera port, the tapping spout, and the off-gas off-take. This can be attributed to the ambient air's density being significantly higher than that in the furnace freeboard. However, since the smelting process in the bath does generate carbon monoxide during the reduction process, a positive pressure within the freeboard reduces ambient ingress through differences in densities.

It is this same gas generation that poses a hazardous risk to the operators of the plant, forcing fumes, dust and carbon monoxide out of the furnace openings. Therefore, the off-gas is extracted to a baghouse filter using a set of induced draught fans. The extraction creates a vacuum within the furnace, which then increases the amount of ambient air pulled in, compromising the reducing atmosphere within the furnace. Therefore, a fine balance must be struck whereby the off-gas generated by the furnace is matched exactly by the extraction off the furnace. The extracted off-gas is also flammable, often containing high volumes of carbon monoxide and even hydrogen (raw material dependent). The carbon monoxide is also highly toxic and requires full combustion or dilution to below hazardous limits. The off-gas system is equipped with a dilution damper that allows the operator to perform both steps in the right conditions.

The off-gas ducting downstream of the furnace off-take is equipped with instrumentation for measuring off-gas parameters as indicated in Figure 7. Specifically, flow rate (via differential pressure measurements and temperatures) along with a continuous gas analyser allows for accurate measurements to be taken at high to low resolutions (1 second to 10 minute intervals). The gas analyser measures oxygen, hydrogen, carbon monoxide, and carbon dioxide using infrared analysis on a continuously extracted gas sample. The sample is passed through a gas conditioning system prior to analysis. As a standard and for simpler data management, 10 second intervals have been used. The continuous measurements have allowed the generation of off-gas flow and temperature profiles that provide insight into the furnace process and allow for the completion of mass and energy balances.

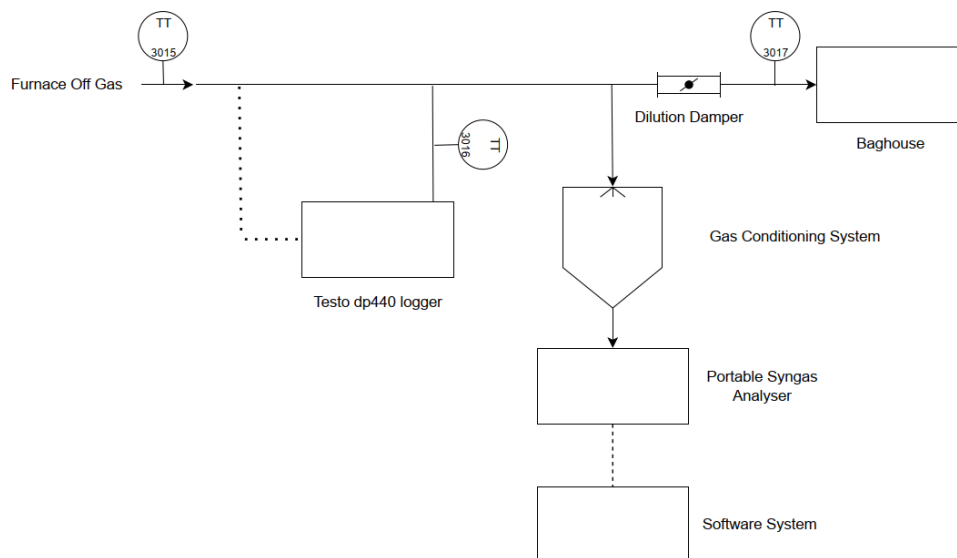


Figure 7: Off-gas monitoring.

With the pilot plant being a relatively small operation, and the low amount of gas generated during the process, off-gas velocities are not very high. Typical velocities range from 2 – 8 m/s depending on generation and extraction volumes. The operator must regularly observe the furnace to ensure that off-gas is not being expelled into the immediate area, while also monitoring the off-gas analysis to ensure that there isn't excessive air ingress into the furnace. A difficulty arises when off-gas production from the furnace is low, leading to lower off-gas velocities in the take-off. Velocities lower than 2 m/s have

an increased margin of error. This is counteracted with the addition of an inert gas. Nitrogen is purged into the furnace through the tapping spout as well as the furnace feed port. Not only does this reduce the amount of air ingress, it also assists in maintaining a higher off-gas flow rate to allow for better measurement. The operator must also adjust this flow rate periodically, along with the suction from the furnace and dilution. On larger pilot plants, this task is achieved with less effort by automating the process based on furnace freeboard pressure measurements. It was found on the smaller pilot plant that operators were able to adjust control variables more quickly by using visual feedback provided by the surveillance camera.

### *Measurement and Monitoring*

Accurate comprehensive monitoring serves to support safety and operation during tests, as well as calculations and interpretation after the test. The monitoring devices were selected to meet the maturation programme objectives. Wherever possible, data was logged continuously through the programmable logic controller (PLC).

Figure 8 shows the temperature transmitters (TT) that are connected to the PLC logging, including six thermocouples in the refractory sidewall. There are also flow transmitters (FT) on the cooling water and air lines, and N2 purge, as well as a flow indicator (FI) on the cooling N2 to the camera. Refractory and shell temperatures were monitored closely, particularly during heat-up and early operation, when the thermal response of the lining is highly transient and temperature gradients can be steep. This period can drive rapid changes in heat loss and bath stability, and it can materially affect interpretation of early test data and energy-balance closure.

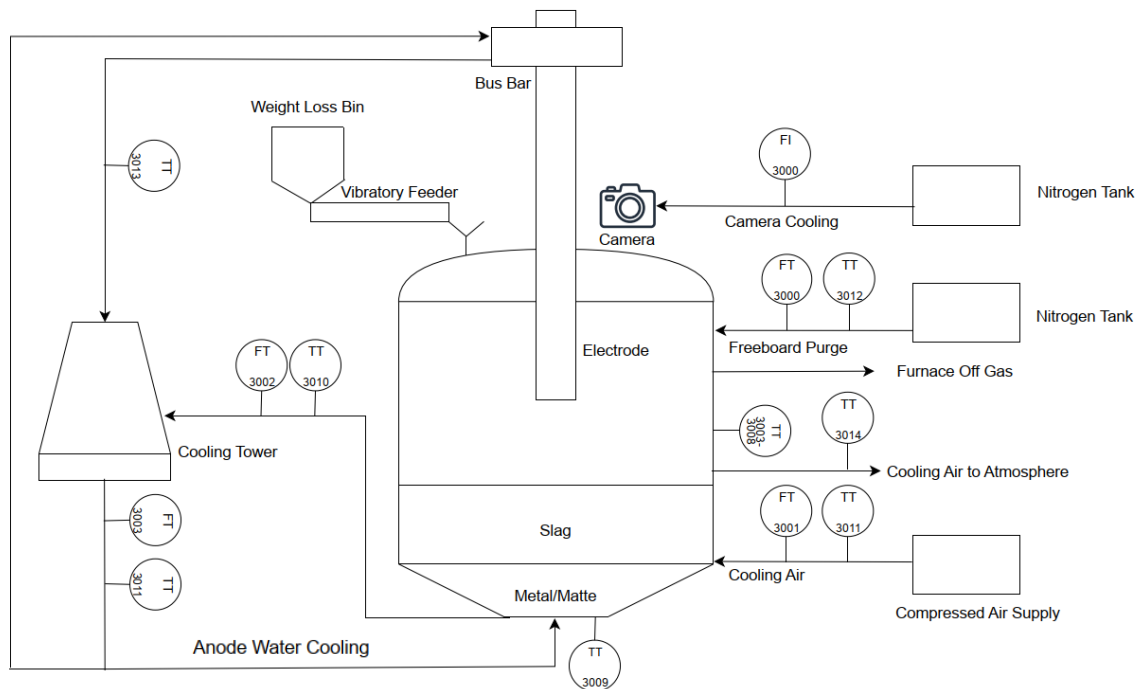


Figure 8: Furnace instrumentation.

Continuous digital data recording is valuable to understand transient behaviour, while real-time displays with alarm functions support operator awareness by highlighting deviations from target or safe operating limits. It is also essential to log and record manual data, photos, camera footage, observations, and shift reports with great care as this provides important context for interpreting the results. Such information is equally important as the information collected via the control system.

### *Sampling devices*

Regular sampling of molten slag and metal during the test was identified as a key requirement to meet

the campaign objectives. Sampling devices are designed for industrial applications and can cause significant disturbances to such a pilot furnace bath. It was also important for the samplers to be rapid and reliable to prevent extended furnace downtimes and air ingress.

Slag sampling was done by simply using the slag obtained on the sounding bar. This sample was carefully collected on a clean steel tray to avoid contamination.

Metal sampling is more challenging. Some success was obtained with a suctioned glass tube; however, this proved to be unreliable and often required repeat sampling. Commercially available sampling devices were investigated including vacuum tube samplers and lateral entry samplers. Both produce a coin-shaped sample that is suitable for spectrographic ('spark') analysis that saves on laboratory costs and enables faster turnaround times. Thinner samples are preferable because they freeze more rapidly between the chill plates, minimising the risk of graphite precipitation, particularly in high-carbon metals; however, if the slag temperature is too low and the viscosity too high, a thicker sample may be required. Both 6 mm and 12 mm samplers were tested in this campaign. A lateral entry sampler was selected due to its simplicity and ease of use. The standard design was modified in conjunction with the supplier to enable sampling from a very shallow metal bath to maximise the sampling window in a test.

#### *Dip Thermocouple*

Dip thermocouples are used to measure the bath temperature and are typically connected to a digital readout capable of storing data for post-test analysis. Measurement is performed by attaching a sacrificial thermocouple tip to the end of a lance and briefly immersing it into the molten bath. Selecting the appropriate thermocouple type is critical, as each type operates within a specific temperature range and produces distinct millivolt outputs based on its alloy junction of its hot junction legs. For example, Type B thermocouples can measure temperatures up to approximately 1820°C, whereas Type S is generally limited to about 1760°C; Types S, R, and B differ primarily in the platinum-rhodium composition of each leg, resulting in different calibration curves. Selecting the wrong tip type will yield incorrect readings because the generated millivolt signal will not correspond to the intended calibration. The lance itself must be designed with sufficient reach to allow safe immersion into the furnace while remaining manoeuvrable for the operator. Sacrificial thermocouple tips are commonly made of cardboard, which burns away after immersion and therefore cannot remain in the bath for extended periods; non-splashing versions with specialised coatings are also available to minimise agitation of the molten bath during measurement. During this campaign various configurations and coatings were tested to ensure optimal accuracy with minimal process disturbance.

#### *Thermal camera*

An external thermal camera was used to regularly monitor furnace shell temperatures. From a safety perspective, it enables rapid identification of localised hot spots, allowing corrective actions before damage occurs. In addition, thermal imaging data was used to support overall furnace energy-balance calculations and validation of thermal models.

#### *High temperature bath video camera*

Videography inside furnaces is not new, examples include Borodin et al (2016) who used a camera to detect brightness changes that signalled arcing abnormalities and assisted in furnace control. Capturing accurate video at the scale of a small test introduced some unique challenges and required a specialised design. In this case, it was important to capture video of the feed behaviour on the bath surface to validate feed behaviour models; thus the camera was designed to support this objective. A visual camera was selected over a thermal camera because it was expected to provide better visibility of feed particle behaviour. The camera enabled continuous observation of the bath surface, slag behaviour, feed dynamics, and arc conditions as seen in Figure 9.



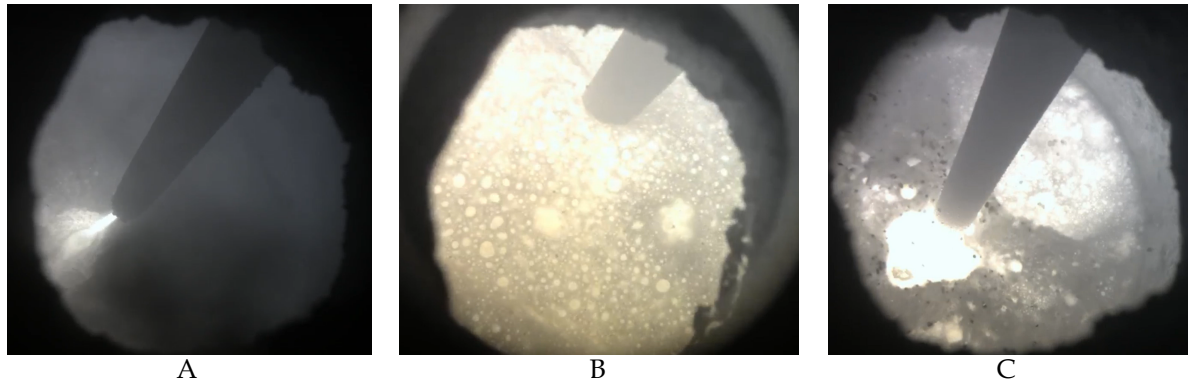


Figure 9. Furnace bath camera images. (A) Lens filter optimised to capture arc. (B) Foaming of slag bath. (C) Molten bath with feed material floating on top.

Because the camera operates above a furnace, it must withstand intense radiant heat and remain functional for extended periods. Nitrogen was selected as the cooling fluid to prevent the risk of introducing oxygen to the furnace. Water cooling was avoided to minimise safety concerns. The camera mount was designed to be removable so that operators can quickly withdraw it during unexpected events such as foaming, sustained flames, arc flaring, or tapping operations. This rapid-removal capability both protects equipment and preserves operator safety.

Various filter lenses were tested during camera operation. Appropriate filtering prevents sensor saturation, protects the optics from excessive infrared radiation, and controls how much visible light reaches the sensor. Proper filtering ensures the recorded imagery is both usable and safe for the camera hardware, especially under bright arc conditions.

The camera provides several key insights beyond general bath observation. It allows operators to identify the onset and severity of foaming and fuming events, observe arc deflection patterns, and distinguish between cold start-up conditions and fully molten steady-state behaviour, information that informs decisions on when to introduce feed. It also revealed the influence of nitrogen purging on bath stability, allowing adjustments when excessive agitation was observed.

### Team Development

Effective pilot-scale execution relies on coordinated input from a multidisciplinary team, including equipment operators, facility owners, engineers, and personnel responsible for material preparation, handling, and downstream processing. Clearly defined roles, contact lists, and escalation pathways for safety or technical issues help maintain accountability and reduce ambiguity during periods of intense activity. Standard operating procedures (SOPs) therefore form the backbone of repeatable operation, defining the required steps before, during, and after test work. Well-documented SOPs reduce dependence on individual experience and preserve institutional knowledge through shift handovers or personnel changes.

Like commercial scale operations, engaging an interdisciplinary team, including electrical, mechanical, controls and automation, refractory, safety, and process engineering, ensures that pilot findings (e.g., sensor placement, cooling strategy, refractory wear patterns, feeding dynamics) are relevant and transferable to full scale. Short daily stand-ups before and after runs, and a weekly technical review help surface cross-functional issues early and coordinate corrective actions.

Although some metallurgical labs operate on a ‘turnkey’ basis with limited day to day inputs, Hatch’s approach to this testing was to develop an integrated team of Hatch and test facility operations personnel. Hatch completed all detailed planning and had continuous site supervision and oversight during the campaign. Pilot facility optimisations were implemented based on experience, and

continuous improvements were made throughout the campaign. This combination of Hatch expertise in both testing and commercial scale applications, along with operator experience, provided a significant advantage for the programme.

### Data Processing and Analysis

Given the volume of pilot data, a structured data-management system is critical. Data was stored digitally and in a shared repository, with raw data preserved separately from processed datasets. Raw data should contain timestamps, instrument IDs, and software versions where applicable. A concise data dictionary (signal names, units, sampling rates, calibration intervals) prevents misinterpretation downstream. Similarly, comprehensive documentation should be maintained throughout the campaign: observations, deviations, anomalies, and informal learnings (e.g., foaming onset, gas purge sensitivity, camera-observed phenomena of note). Any lessons learned should be captured in a log alongside formal reports to retain small but actionable insights.

It is also important to make the data accessible and meaningful across sources. Measurements like dip-thermocouple readings, refractory wall temperatures, off-gas composition, and power input are most valuable when they can be interpreted together, in context, rather than as separate datasets. A custom data visualisation tool was developed for this purpose during this test campaign. The goal of the visualisation tool is to align diverse variables against a common reference; in this case, time. Furnace conditions evolve clearly over time, from start-up to steady state to cool-down, and key events (e.g., feed changes, foaming, tapping) can be logged with timestamps to support cause-and-effect interpretation.

The implementation pairs time-series plots with synchronised furnace video and an event log, as shown in Figure 10. Users can scroll through the timeline to focus on a period of interest and instantly view the corresponding footage while overlaying or stacking relevant variables (e.g., power, refractory temperatures, off-gas composition). This significantly speeds up analysis by automatically aligning data, which makes patterns easier to spot. The same framework can also support live monitoring, giving both on-site and remote team members a shared view of evolving conditions during a test.

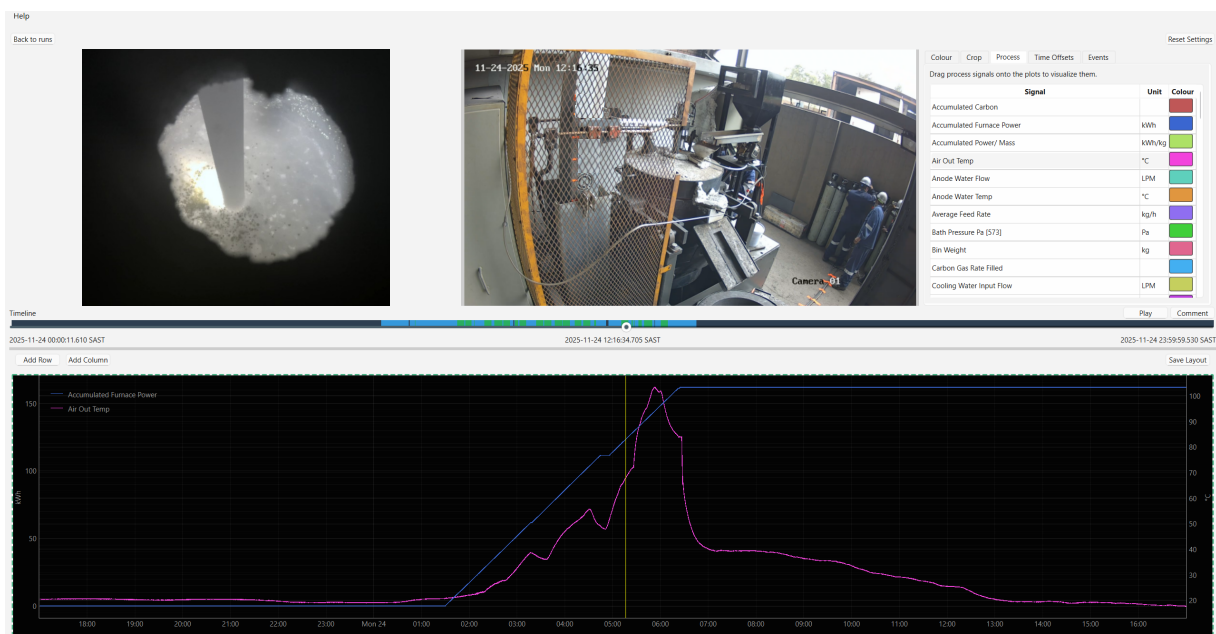


Figure 10. Data visualisation tool.

## CONCLUSION

Although the test campaign is ongoing at the time of writing, the information gained from compiling global expertise into this test implementation represents a significant learning. During procurement it was uncertain whether the equipment would perform well enough to meet the challenging requirements, particularly the bath camera and lateral-entry metal samplers. The data is on track to provide meaningful validation of the CFD model so that it can be used with more confidence to model the next scale of testing. This type of integrated maturation plan combining well-executed and -monitored test work with detailed CFD modelling is expected to serve as a framework to significantly improve technology development timelines, while simultaneously decreasing project and technology ramp-up risk.

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# Beyond the numbers: Material property insights as the hidden foundation of furnace performance

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South African pyrometallurgical producers, including those in chromium, manganese, and platinum group metal (PGM) production, are facing increasing pressure from rising electricity costs, electricity-supply constraints, shortened furnace campaign lives, and tightening economic and environmental regulations. To remain economically viable under these external constraints, the industry is compelled to move beyond conservative, experience-based design margins toward more precise furnace operation supported by physically credible modelling. However, the value of digital modelling tools is frequently limited by uncertainty in input parameters, specifically, material properties. As the hidden foundation of reactor performance, properties such as slag viscosity and thermal conductivity govern critical heat and mass transport phenomena. Their values often carry significant uncertainty, which propagates through simulations and contributes to uncertainty in furnace behaviour, performance, and profitability. This paper examines how uncertainty in high-temperature material property descriptions influences pyrometallurgical furnace modelling. Material properties are first linked to process behaviour through transport arguments and dimensionless-number analysis. A representative multiphysics furnace case, based on direct reduced iron smelting, is then used to illustrate how uncertainty in selected inputs can affect heat-transfer interpretation, furnace sizing, and throughput estimates. The paper further discusses two complementary approaches for improving material property descriptions: physically constrained bounds for slag thermal conductivity, informed by slag structure and heat-transport considerations, and *auxi-mpp*, an open-source Python library that supports more consistent implementation of slag, alloy, and gas property models in process and multiphysics workflows. Together, these examples illustrate how physically grounded material property descriptions can reduce ambiguity in model inputs and support more consistent engineering interpretation.

## INTRODUCTION

The South African pyrometallurgical industry remains a cornerstone of the national economy and a critical node in the global supply chain for chromium, manganese, and platinum group metals (PGMs) (Basson, 2007). However, the sector currently faces a period of unprecedented pressure. Electricity prices for industrial users have risen sharply over the past decade (Mahony and Baartman, 2018; Minerals Council South Africa, 2025), while international CO<sub>2</sub> regulations, including the European Carbon Border Adjustment Mechanism continue to tighten (European Commission, 2026), significantly constraining the economic operating window of energy-intensive smelting and refining operations. In this context, optimising energy efficiency and the pursuit of sustainable operation are no longer matters of strategic preference, but central operational requirements. As a result, available design and operating margins have narrowed. Reliance on experience and conservative operating margins to accommodate parameter uncertainty and process variability is giving way to a demand for deeper scientific understanding and more physically credible models. This shift is particularly critical for high-temperature furnace systems involving interacting slag, alloy, matte, and gas phases.

Globally, as the industry accelerates decarbonisation of steelmaking and other metal-production processes (International Energy Agency (IEA), 2025), the traditional decades-long timeline for developing and implementing new processes is no longer practical. Within this transition, computational modelling, including coupled multi-physics simulations (CFD/FEM/DEM) (Bezuidenhout, Eksteen and Bradshaw, 2009; Reynolds, 2013), thermodynamic equilibrium modelling based on Gibbs free energy minimisation with CALPHAD databases (Bale *et al.*, 2002), kinetics and transport models, and steady-state or dynamic flowsheet models (Jung and Van Ende, 2020), is increasingly used to accelerate development cycles, and to reduce scale-up and start-up risks for emerging technologies (Sohn, 2014).

As the metallurgical industry moves toward lower-CO<sub>2</sub> production, the physical properties of materials emerge as a hidden foundation. This is because the capability of high-fidelity models is ultimately constrained by the quality of their input parameters. For example, when constructing complex multiphysics models, advances in mesh refinement and numerical solvers can enhance computational performance. However, even highly sophisticated models remain fundamentally limited by the accuracy and representativeness of their input data.

In high-temperature metallurgical reactors, typically operating above 1500°C with multiple phases, process behaviour and product quality are determined by the coupled effects of thermodynamic and physical material properties (Pistorius, 2007; Nelson and Hundermark, 2014). Thermodynamic properties of solutions, such as heat capacity, Gibbs energy, and enthalpy of mixing, govern reaction pathways and equilibrium extents of reaction, whereas physical properties (Mills, Yuan and Jones, 2011; Muire, Zaaïman and Zietsman, 2024), including viscosity, electrical and thermal conductivity, density, surface tension, and diffusion coefficients, control reaction rates, process stability, and spatial distributions within a reactor. Together, these physicochemical properties determine key phenomena, including dissolution and refining rates, bath mixing and homogenisation, phase separation efficiency, refractory erosion and wear, tap-stream stability, and ultimately product quality. Crucially, neither the physical nor the chemical properties are constant; they vary with temperature, composition, and oxygen potential. As a result, any meaningful computational description of furnace behaviour requires material properties to be described with sufficient fidelity to capture their intrinsically coupled and variable nature under operating conditions.

Despite this critical reliance on materials data, the industry currently faces a substantial knowledge gap. Traditional empirically derived databases struggle to cover the novel regimes introduced by emerging decarbonisation routes, particularly hydrogen-based direct reduction of iron ore followed by melting in electrical furnaces (Vogl, Åhman and Nilsson, 2018; Fei *et al.*, 2024). Consequently, furnace designers may need to rely on wider uncertainty bounds or generic estimates, which can propagate uncertainty into engineering interpretation and scale-up decisions. The same concern extends to established South African operations in chromium, manganese, and PGM production, where published physical property data for process-specific slag compositions remains limited (Muir, Zaaïman and Zietsman, 2024). This paper discusses the role of material property uncertainty in pyrometallurgical furnace modelling. It links selected properties to furnace behaviour through transport phenomena and dimensionless analysis, uses a representative multiphysics furnace case to illustrate uncertainty propagation, and then introduces two routes for improving material property descriptions through constrained thermal conductivity bounds and more consistent implementation of property models.

## FROM MATERIAL PROPERTIES TO PROCESS PHENOMENA

### Material properties and furnace behaviour

The performance of metallurgical furnaces is not determined by any single design parameter or operating action; rather, it arises from the interplay of material properties and coupled physical phenomena such as heat transfer, electromagnetics, fluid flow, and chemical reactions (Zietsman and Pistorius, 2006). Crucially, the primary challenge in engineering practice is not merely identifying what a specific property affects but understanding how property uncertainty amplifies changes in furnace



performance through these intricately coupled physical processes. This amplification ultimately manifests as variability in arc behaviour, energy distribution, freeze-lining stability, slag-alloy separation efficiency, refractory lining life, and safety risks.

Density ( $\rho$ ), for example, changes with composition and temperature directly governs buoyancy forces and slag-alloy separation rate. It influences the closure of mass balances and inventory accounting, as well as the tendencies for droplets and particles to settle, float, or remain entrained in the molten slag bath.

Viscosity ( $\mu$ ), the core parameter influencing flow resistance and shear dissipation, dictates tapping flow stability, and the slag-alloy separation efficiency, and in conjunction with surface tension shapes the generation and stability of foaming slag, which strongly influences energy efficiency and the feasible and stable operating window.

Slag electrical conductivity ( $\sigma_e$ ) contributes to the electrical behaviour of electric furnaces through its effect on slag-bath resistance, voltage-current response, and power distribution. These effects are relevant to immersed-electrode, brush-arc, and open-arc operation, where changes in slag conductivity may alter the balance between resistive heating, arc behaviour, and bath heating. Because  $\sigma_e$  varies with temperature and composition, uncertainty in this property may affect assumptions used in electrical-system design and process modelling. If this uncertainty is not adequately represented, the resulting design may have reduced flexibility across changes in power input, electrode operating mode, slag composition, and bath temperature.

Simultaneously, uncertainty in slag thermal conductivity ( $\kappa$ ) represents one of the most subtle risks in freeze lining design, with furnace sidewall calculations showing its influence on heat transfer coefficients and freeze lining thickness (Joubert and Kotzé, 2020). If the frozen layer's thermal conductivity is underestimated (i.e., actual  $\kappa$  is higher than the design value), the layer may become excessively thick, reducing the active furnace volume and distorting power distribution. This can, in extreme cases, induce mechanical risks to the electrodes and force abnormal shutdowns. Conversely, if thermal conductivity is overestimated (i.e., actual  $\kappa$  is lower than the design value), the freeze layer may fail to form or remain dangerously thin. Under power fluctuations, bath level variations, and restarts after power outages, this fragile layer will then be prone to localised melting or spalling, exposing copper coolers or water-cooled elements to the high-temperature melt, potentially increasing the risk of localised failure. Beyond these few critical properties, others are also strongly coupled with furnace behaviour and performance. These are summarised in Table I.

These property descriptions have direct relevance to South African pyrometallurgical practice. In PGM smelting, slag viscosity and electrical conductivity govern matte-slag separation efficiency, operating stability, and metal losses (Nell, 2004; Jones, 2005; Zhang, Sun and Jahanshahi, 2009). In ferrochrome production, electrical conductivity data for process-specific slag compositions has not been systematically updated since the mid-1990s (Holappa and Xiao, 2004), and no published thermal conductivity data exists for ferrochrome slags (Muire, Zaaïman and Zietsman, 2024). In manganese alloy production, slag viscosity and thermal properties remain critical for interpreting reduction behaviour, energy distribution, and containment performance (Steenkamp, Tangstad and Pistorius, 2011). These gaps are directly relevant to the uncertainty challenge discussed.

Table I: Example of key material properties and their representative process behaviour in pyrometallurgy.

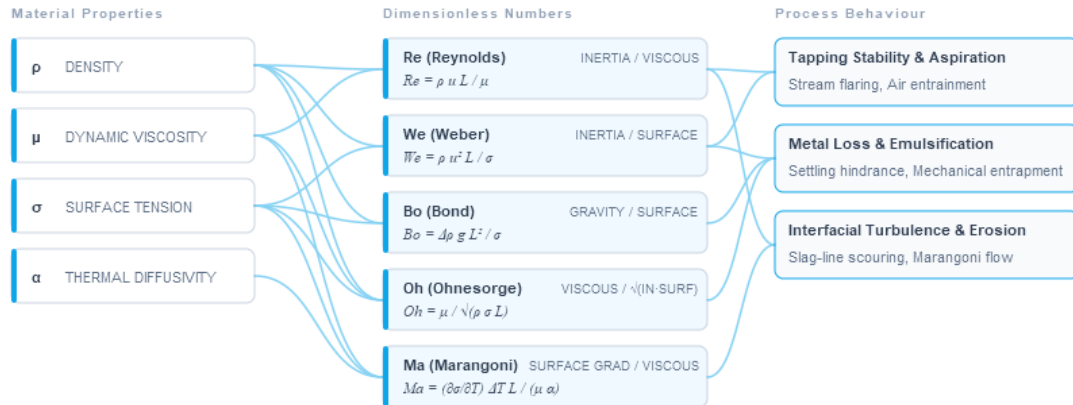
| Property                                             | Example industrial impacts                                                                                                                                                                                                                                                                                                                   |
|------------------------------------------------------|----------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------|
| Density ( $\rho$ )                                   | Slag-alloy separation tendency, closure of mass balances and inventory accounting, flotation/sedimentation behaviour of droplets and particles in the bath.                                                                                                                                                                                  |
| Volumetric thermal expansion coefficient ( $\beta$ ) | Flow velocities and patterns in the baths, heat transfer rates to feed heaps and slag freeze linings, furnace productivity and safe process containment, refractory stress and thermal-shock risk during power interruptions and ramping, changes in bath convection and stratification tendency, stability of freeze lining and accretions. |
| Viscosity ( $\mu$ )                                  | Tapping flow stability, flow patterns and heat transfer, slag-alloy separation efficiency and entrainment tendency, foaming behaviour and foam stability.                                                                                                                                                                                    |
| Electric conductivity ( $\sigma_e$ )                 | Electric system design, furnace productivity and operability, electrode and arc stability in electric furnaces, effective power penetration and distribution in the bath, avoidance of persistent cold zones.                                                                                                                                |
| Thermal conductivity ( $\kappa$ )                    | Furnace productivity and operability, heat-load estimation to cooling elements and shell, formation and control of freeze lining/accretions, heat-loss accounting and energy-efficiency optimisation, mitigation of local overheating and burn-through risk.                                                                                 |
| Specific heat capacity ( $C_p$ )                     | Thermal inertia and resilience to power volatility, melting and sensible-heat energy accounting, temperature drift and stabilisation time during operating mode changes.                                                                                                                                                                     |
| Diffusion coefficient ( $D$ )                        | Dissolution rate of solids into slag, rate limitation of slag-alloy interfacial refining and impurity transfer, inclusion chemistry adjustment and removal kinetics.                                                                                                                                                                         |
| Surface tension / interfacial tension ( $\gamma$ )   | Entrainment and emulsification tendency, foaming propensity and defoaming response, tapping stream stability and spitting/splashing risk, slag wetting/infiltration tendency into refractories.                                                                                                                                              |

### Influencing process behaviour with material properties and dimensionless numbers

Dimensionless numbers offer a useful link between material properties and process behaviour in pyrometallurgy (Chattopadhyay and Isac, 2012). They are ratios that combine material properties, operating conditions, and characteristic length or time scales into compact groups, allowing momentum, heat, and mass transport to be compared and scaled across different systems. However, because these numbers are calculated directly from material property inputs, uncertainty in those inputs will propagate into the calculated dimensionless groups and, consequently, into the characterisation of process performance, as illustrated in Figure 1. In the figure, the central blocks group representative dimensionless numbers according to the dominant transport domain and provide a framework for linking material property variation to the process behaviours shown on the right. The connecting lines indicate dominant pathways of interaction, rather than a complete dependency structure. The figure therefore serves as a conceptual framework for the discussion that follows.

In practical pyrometallurgical modelling, this propagation is most often manifested through dimensionless groups used to characterise fluid flow, heat transfer, and mass transfer. Examples include the Reynolds number for flow behaviour, the Prandtl and Nusselt numbers for heat transfer, and the Schmidt and Sherwood numbers for mass transfer. Each of these groups depends on specific physical properties, including density, viscosity, thermal conductivity, heat capacity, surface tension, and diffusivity. As a result, deviations in material property values seldom affect only one aspect of process behaviour. For example, a change in slag viscosity may alter the Reynolds number and hence the flow regime, while also affecting the interpretation of heat-transfer behaviour through related dimensionless groups. Since these ratios are often nonlinear, even modest uncertainty in property values can propagate through numerical models and produce significant differences in outcomes such as freeze-lining stability, bath mixing, interfacial reaction rates, furnace productivity and efficiency, and ultimately profitability. As such, dimensionless numbers provide a practical means of tracing how uncertainty in material property descriptions can influence furnace-relevant behaviour and its engineering interpretation, reinforcing the need for accurate, temperature- and composition-dependent property data in physically credible models.

## 1. Momentum transfer



## 2. Heat transfer



## 3. Mass transfer

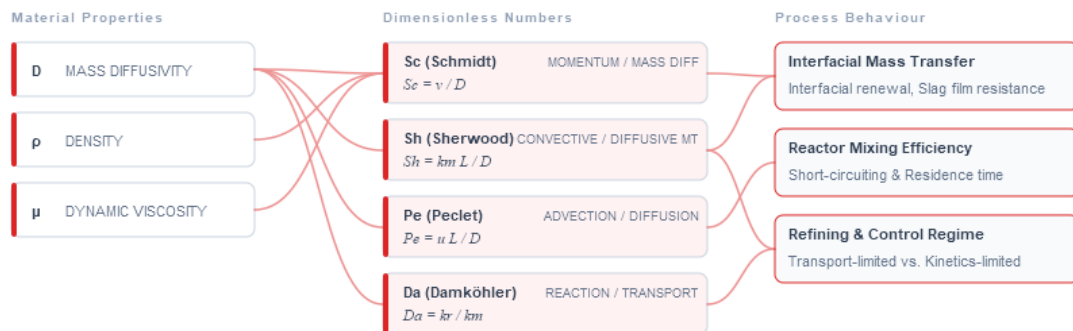


Figure 1. Mapping of propagation paths from material property roots to process behaviour. Material properties are listed on the left, and the central blocks summarise representative dimensionless numbers that act as interpretive bridges between material property variation and transport behaviour in the furnace. The right-hand blocks provide examples of process consequences associated with these transport regimes.

### Uncertainty propagation in multiphysics simulation

To illustrate how uncertainty in material property descriptions can influence model outputs, a representative direct reduced iron (DRI) smelting furnace case was used for a one-factor-at-a-time sensitivity analysis. Each parameter in was varied independently across its stated range while all other parameters were held at baseline values. Table II summarises the input ranges used to represent practical uncertainty in selected operating conditions and material properties for this case. The ranges were selected as practical bounds for sensitivity screening, informed where possible by published slag property data, engineering estimates, and industrial furnace property ranges reported for comparable high-temperature systems.(Verein Deutscher Eisenhüttenleute, 1995; Mills, Yuan and Jones, 2011; Nelson and Hundermark, 2014) Notably, key slag properties span wide intervals due to the limited availability of measured data for novel DRI slags, with slag thermal conductivity allowed to vary from 0.2 to 2.25 W m<sup>-1</sup> K<sup>-1</sup> and slag viscosity from 0.01 to 0.5 Pa s.

The response shown in Figure 2 was evaluated relative to the baseline case. Within the ranges considered, slag thermal conductivity produced the largest throughput spread, approximately 140% relative to the baseline. While secondary to thermal conductivity, slag viscosity forms a critical second tier alongside process temperatures, contributing an additional spread of roughly 80%. These results indicate that uncertainty in key material property inputs can materially affect furnace-sizing assumptions, operating-window interpretation, and throughput estimates. Because furnace design is commonly based on a target production rate, uncertainty in property data can be reflected directly in design inputs and may contribute to over- or under-sizing of critical systems under plant operating conditions. Although the DRI smelting case reflects work conducted in an international context, the methodology and the role of material property uncertainty applies equally to South African furnace systems, where comparable or greater property uncertainty exists for process-specific slag compositions.

Table II: Representative parameter ranges and uncertainty levels used for sensitivity analysis in the DRI smelting furnace multiphysics simulation.

| Parameter                  | Unit                              | Minimum  | Maximum  | Uncertainty |
|----------------------------|-----------------------------------|----------|----------|-------------|
| Slag thermal expansion     | K <sup>-1</sup>                   | 5.00E-05 | 1.00E-04 | high        |
| Alloy thermal expansion    | K <sup>-1</sup>                   | 5.00E-05 | 1.00E-04 | high        |
| Slag thermal conductivity  | W m <sup>-1</sup> K <sup>-1</sup> | 0.2      | 2.25     | high        |
| Alloy thermal conductivity | W m <sup>-1</sup> K <sup>-1</sup> | 16       | 34       | high        |
| Slag viscosity             | Pa s                              | 0.01     | 0.5      | high        |
| Alloy viscosity            | Pa s                              | 0.007    | 0.014    | high        |
| Feed heap angle of repose  | °                                 | 10       | 20       | medium      |
| Feed thermal conductivity  | W m <sup>-1</sup> K <sup>-1</sup> | 3        | 30       | high        |

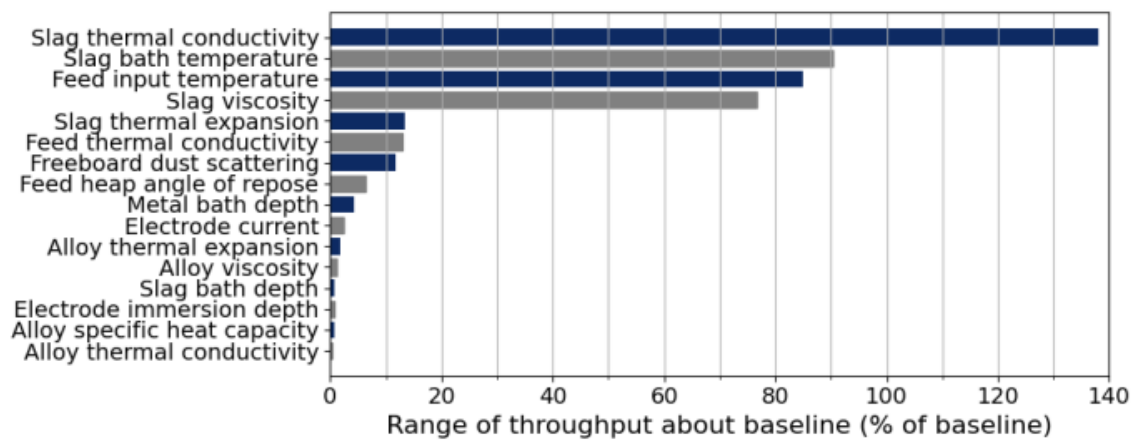


Figure 2. Sensitivity of modelled throughput to material property inputs in the representative DRI smelting furnace case.

## REDUCING MATERIAL PROPERTY UNCERTAINTY FOR FURNACE MODELLING \*\*\*\*\*

Since high-temperature material property descriptions play an important role in furnace modelling, reducing uncertainty in these descriptions remains a key consideration. In the present work, thermal conductivity is used as one illustrative example. Atomistic considerations of slag structure and heat transport are used to motivate a physically informed framework for estimating thermal conductivity. As illustrated in Figure 3 (a), thermal energy propagation in slag is strongly dependent on network connectivity. Heat flows efficiently across covalent bridging oxygen (BO) bonds, whereas more ionic non-bridging oxygen (NBO) bonds associated with network modifiers (e.g., Ca, Na) induce stronger scattering events. This dampens heat transfer and reduces thermal conductivity.

This is particularly relevant because measurements of slag thermal conductivity at high temperatures remain subject to substantial uncertainty. For instance, reported values may depend on the measurement technique, sample state, radiative contribution, electrical leakage, and interpretation of the measured thermal response. Laser-flash and transient hot-wire measurements have been reported to differ substantially (Hasegawa et al., 2012; Kang, Lee and Morita, 2014; Hashimoto et al., 2025). To address this ambiguity, a theoretical framework is proposed to estimate lower and upper bounds for thermal conductivity. The framework correlates silicate network connectivity with heat transport efficiency and covers the  $K_2O$ - $Na_2O$ - $CaO$ - $MgO$ - $Al_2O_3$ - $SiO_2$  system. Notably, unlike conventional empirical approaches that require extensive parameter fitting for specific slag compositions, the developed model is grounded in fundamental physical characteristics and independent of such multicomponent data fitting. Figure 3(b) presents illustrative lower and upper thermal conductivity bounds for selected ternary subsystems at 1500 °C in the molten state. For the  $CaO$ - $Al_2O_3$ - $SiO_2$  system, the overall trend in thermal conductivity is qualitatively consistent with experimentally reported behaviour. The results further reflect the network polymerisation associated with charge compensation, including an increase in bridging oxygen and the appearance of a conductivity maximum. The figure also suggests cation-dependent differences in charge-compensation behaviour across the representative systems shown. By narrowing the physically plausible range of thermal-conductivity values, this approach can reduce the burden of multiphysics computation and improve the interpretability of simulation results used in furnace-related design studies and risk assessment.

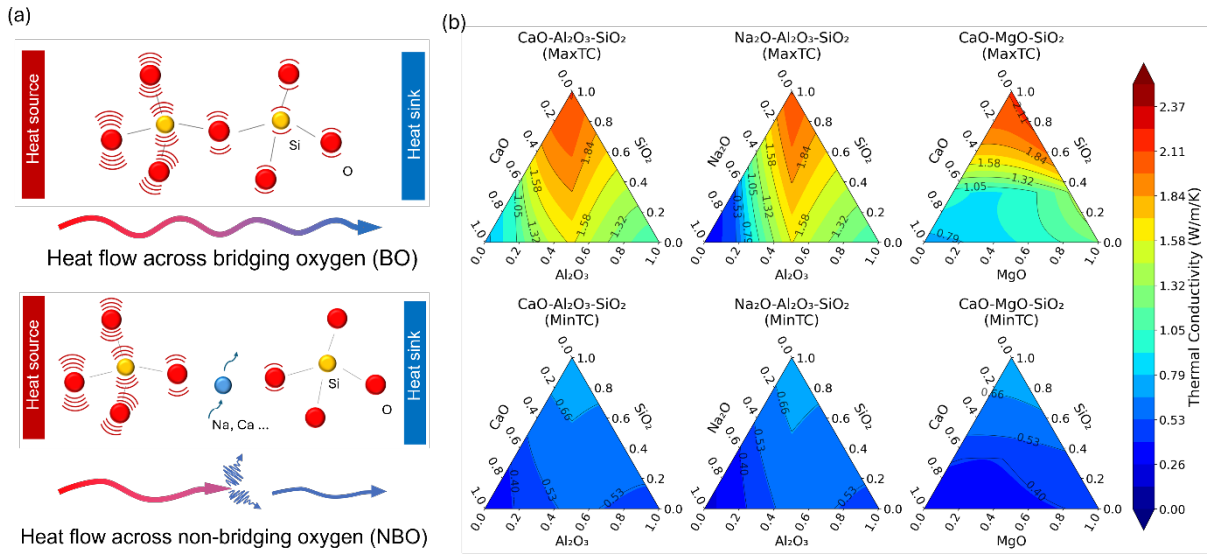


Figure 3. (a) Schematic illustration of the proposed mechanistic link between silicate-network connectivity and heat transport in molten slag. Bridging-oxygen-rich networks are associated with more efficient heat propagation, whereas increasing non-bridging oxygen content is associated with reduced thermal transport efficiency. (b) Calculated lower and upper thermal-conductivity bounds at 1500 °C in the molten state for selected representative ternary subsystems within the  $K_2O$ - $Na_2O$ - $CaO$ - $MgO$ - $Al_2O_3$ - $SiO_2$  oxide system. The examples are illustrative of the broader compositional space considered in the framework.

In addition to constraining property ranges, uncertainty can also be reduced in practice by improving consistency and traceability in property-model implementation. As shown in Figure 4, *auxi-mpp* is an open-source Python package developed to describe physical properties of high-temperature materials, including density, viscosity, electrical conductivity, and thermal conductivity, for molten slags, alloys, and gases, as functions of temperature, pressure, and composition. (Koning, Muire, Zhu, Zietsman and Nortje, 2026) The *auxi-mpp* framework is intended to support more consistent material property evaluation by packaging property models within common Python classes that include calculation methods, units, references, parameters, and scope information. For slag models, binary and multicomponent calculations can also be linked, where applicable, to thermochemical equilibrium calculations through a user-defined equilibrium slag function that provides equilibrium composition and bond-fraction information. This provides a structured route for coupling property calculations with process and multiphysics models. Its open-source nature also supports transparency, traceability, and external scrutiny of model implementation. However, it is also worth noting that some applications of *auxi-mpp* may still depend on licensed thermochemical inputs, particularly for certain slag-property models, including data from the Modified Quasichemical Model (MQM) accessed through ChemApp.

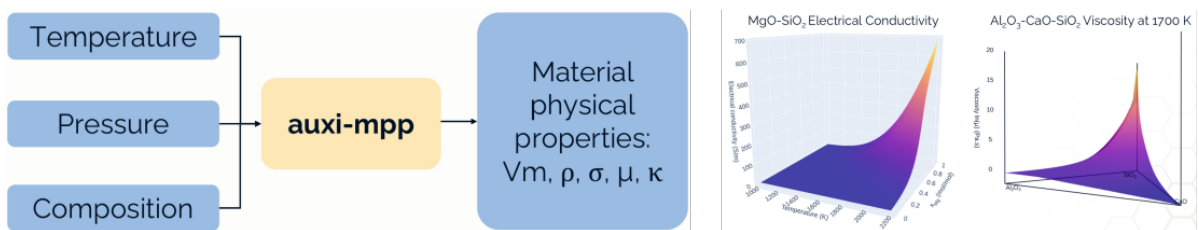


Figure 4. Schematic overview of the *auxi-mpp* framework for high-temperature material property evaluation. The figure shows the principal inputs to the framework (temperature, pressure, and composition), together with representative property outputs and illustrative examples of model predictions.

## CONCLUSION

In high-temperature furnace modelling, material properties are often treated as inputs to the calculation, but in the furnace itself they are also part of the physics being modelled. This paper has discussed the potential impact of material property uncertainty on furnace modelling by linking selected properties to transport phenomena, dimensionless relationships, and furnace behaviour. Through a representative uncertainty-propagation example, it has further illustrated how uncertain material property inputs can affect the interpretation of heat transfer, throughput estimates, and furnace-design calculations. To support more reliable furnace modelling through improved material property descriptions, two approaches have been presented in this work. The first constrains physically plausible property ranges, as illustrated by the thermal conductivity framework, while the second improves the consistency of property model implementation, as introduced through auxi-mpp. Taken together, these approaches make material property inputs more physically accountable and strengthen the link between the physical furnace system and the calculations used to represent it. In this sense, material property descriptions are a hidden foundation of credible furnace modelling and engineering interpretation.

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# Operational strategies for capacity increase and steady-state operation at the Northam Platinum Zondereinde

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This paper describes the operational strategies that enabled Northam Platinum's Zondereinde smelter to grow from a single 10 MW furnace operation running five days a week to a two-furnace operation producing in excess of one million ounces of platinum group metals (PGMs) per annum. This transformation was achieved during a period of pricing and exchange rate volatility, loadshedding, and significant changes in concentrate mineralogy as the Bushveld Complex shifted from Merensky to UG2-dominant mining. Rather than relying on a single transformative intervention, the capacity increase was realised through the sustained application of incremental operational improvements across four areas: targeted workforce development focused on control room operators, the use of mobile messaging technology for real-time operational discipline, systematic concentrate blending to manage changing feed characteristics, and the decoupling of slag handling from furnace operations. Matte fall was stabilised at approximately 5%, compared with 10% or more at comparable operations. The paper discusses the practical lessons learned during this process, including the importance of investing in information quality, the value of testing blending theories through controlled furnace trials, and the role of infrastructure improvements in sustaining operational gains. The experience demonstrates that consistent, focused operational discipline – applied daily over an extended period – can deliver step-change improvements in smelter performance.

**Keywords:** PGM smelting, furnace operations, concentrate blending, operational discipline, matte fall, Northam Platinum

## INTRODUCTION

The South African PGM smelting industry has undergone significant change over the past two decades. Smelters originally designed around Merensky reef concentrates have had to adapt to increasing proportions of UG2 concentrate, which presents distinct challenges including higher chromite content, lower sulphide grades, and altered matte fall characteristics (Hundermark *et al.*, 2011; Jacobs, 2013). At the same time, operations have had to navigate electricity supply constraints, pricing volatility, and exchange rate uncertainty.

Northam Platinum's Zondereinde smelter provides a case study in how these challenges were addressed through operational innovation rather than capital-intensive technology changes. Approximately 10 to 15 years ago, the smelter consisted of a single furnace operating at 10 MW, running five days a week. A dryer plant ran as and when needed to maintain silo levels, and the workforce operated within a conventional shift structure without active engagement in operational decision-making.

Today, the Zondereinde smelter operates two furnaces and has more than tripled its output. The operation is consistently ranked on the lower end of the cost curve for delivering PGMs to market, it grew from 400,000 ounces and during the 2025 financial year exceeded one million ounces of PGM production (Northam Platinum, 2025).

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This transformation was not the result of a single defining intervention. It was the product of sustained, incremental improvements applied across workforce development, information management, concentrate blending, furnace management practices, and supporting infrastructure. As Clear (2018) observes: "It is so easy to overestimate the importance of one defining moment and underestimate the value of making small improvements on a daily basis." This paper describes the principal strategies that contributed to the Zondereinde smelter's capacity increase and the lessons learned during their implementation.

## **BACKGROUND**

### **The Zondereinde smelter**

The Zondereinde smelter forms part of Northam Platinum's processing division. The original installation comprised a single 15 MW electric furnace, which was operated at 10 MW. A second 20 MW furnace, designed by Tenova Pyromet, was commissioned in 2017/2018, bringing total installed capacity to approximately 35 MW. The smelter processes concentrates from the Zondereinde, Booysendal, and Eland mines, as well as from third parties, supplemented by recycling and re-mining streams.

### **Industry context**

The shift from Merensky to UG2-dominant mining across the Bushveld Complex has had profound implications for PGM smelting operations. UG2 concentrates are characterised by higher chromium oxide content, which increases the risk of chromite spinel formation in the furnace and complicates slag management (Hundermark *et al.*, 2011). The easy-to-float, predictable matte fall associated with Merensky concentrates has given way to more variable and generally lower matte fall with UG2 feeds. Furthermore, the addition of recycling and re-mining streams to furnace feed has made steady-state operation a continuous balancing exercise.

These challenges are compounded by external factors including electricity supply constraints (loadshedding), commodity price cycles, and exchange rate volatility, all of which demand operational resilience and cost discipline.

## **WORKFORCE DEVELOPMENT AND OPERATIONAL DISCIPLINE**

### **Targeted training of control room operators**

The commissioning of the second dryer and furnace exposed vulnerabilities in the workforce. It became evident that upskilling the entire workforce simultaneously was not feasible. Instead, the decision was taken to focus training resources almost exclusively on the 8 to 12 control room operators – the individuals whose decisions directly governed furnace performance.

This was an intensive period of approximately one year during which these operators were trained around the clock. The guiding principle was that the ability to make correct operational decisions depended on the quality of information available to the control room. Each operator needed to understand what every parameter meant, why it mattered, and what actions were appropriate for the range of scenarios encountered during each shift.

Two key lessons emerged from this phase. First, when human capital is limited, it is essential to deploy people where their skills deliver the greatest return. This required supervisory personnel and management to understand where each individual's strengths and weaknesses lay. Second, it required an abandonment of rigid title-based responsibilities. Each available skill and aptitude was directed to where it served the collective goals most effectively.

The results were tangible. Personnel who had previously been passive – following procedures without

understanding their purpose — developed into operators who could assess a situation, analyse the data, and take appropriate action. This growth in capability and confidence among the core group of operators subsequently catalysed the upskilling of their wider colleagues.

### **Targeted training of control room operators**

The smelter had maintained a strict policy prohibiting mobile phones on the plant floor, with only senior supervisory personnel granted access. In an effort to improve information flow and direct more attention to problem areas, this policy was selectively relaxed. Additional permits were granted to shift supervisors and plant leaders, enabling the establishment of WhatsApp messaging groups for operational communication.

The initial experience was chaotic — an undifferentiated production group where everything was posted and shared, followed by the inevitable proliferation of splinter groups for engineering, human resources, incident reporting, and other functions. Despite this, mobile messaging proved to be one of the most effective management tools adopted during the transformation. It improved the transfer of learning between shifts, enabled the enforcement of specific operational disciplines, and provided the ability to monitor plant status at a glance — something that was previously not possible.

A specific example illustrates the impact. The closure of electrode column lids had been a persistent discipline problem that resulted in frequent electrode breaks. By requiring supervisors and plant leaders to photograph closed lids during every shift and share the images via WhatsApp, compliance was enforced and the problem was resolved virtually overnight. The effectiveness of this approach is straightforward: when an individual knows that photographic evidence of compliance is expected, discipline follows.

The adoption of mobile messaging is estimated to have accelerated the smelter's overall operational improvement by approximately two years.

## **CONCENTRATE BLENDING STRATEGY**

The implementation of strict protocols for concentrate storage, sampling, and blending has been one of the most consequential changes at the Zondereinde smelter.

The nature of the run-of-mine material processed by the concentrators has changed dramatically over the past decade and will in all likelihood continue to change. The transition from Merensky to high-chrome UG2 concentrates, together with the increasing role of recycling and re-mining streams, has made furnace feed management a critical operational discipline.

In response, the smelter developed a multitude of blend recipes that are enforced strictly. The development of these blends was an iterative process: multiple trials were conducted on the furnaces using different blends and setpoints, and the results were used to refine both the blend formulations and the associated operating parameters. This willingness to test theories in practice — running controlled trials on production furnaces — yielded insights that could not have been obtained through theoretical analysis alone. In particular, the process developed a deep understanding of the individual chemistries of slag and matte inside the furnace, a capability that has proved valuable for both metallurgical and non-metallurgical members of the team.

The blending programme is specific to the Zondereinde operation and its particular combination of concentrate sources. There are no universal shortcuts; each processing facility must develop its own blending protocols through systematic investigation and trial.

The outcomes of this programme have been significant. The smelter has achieved consistent and predictable matte fall, together with low slag and matte temperatures and long furnace campaign life cycles. Matte fall at Zondereinde averages approximately 5%, compared with 10% or more at many

comparable PGM smelting operations. While lower matte fall introduces its own operational challenges, the predictability achieved through disciplined blending provides a stable foundation for furnace management.

## **FURNACE MANAGEMENT PRACTICES**

### **Sounding discipline and bath level management**

Building on the same approach used to enforce electrode lid closure, a rigorous sounding discipline was implemented. Bath levels are measured by means of two-hourly soundings, which are recorded and shared via WhatsApp. This practice ensures accurate monitoring of furnace bath levels and enables management within a 5 cm band – tighter than the 10 cm band that had been the previous norm.

The sounding bar inspections also yielded information beyond bath level measurements. Through careful evaluation of the sounding bar, furnace personnel began to gather additional operational intelligence on furnace conditions, enabling a degree of fine-tuning that was not possible prior to the adoption of mobile messaging for real-time information sharing.

### **Decoupling of slag handling**

The single largest change in furnace operations was the decoupling of the slag handling process from the furnace. This decision was made in response to significant furnace downtimes attributable to slag handling.

The conventional approach – water granulation of tapped slag – was eliminated entirely. Instead, slag is tapped to the ground and allowed to cool naturally, with water cannons employed where necessary to manage the cooling process.

The benefits were immediate. Plant availability increased substantially as the single largest contributor to furnace downtime was removed. Over time, additional benefits became apparent, including reductions in energy consumption and water usage.

## **SUPPORTING INFRASTRUCTURE**

An unanticipated contribution to the smelter's transformation came from a plant-wide concreting project. As part of the operation's environmental responsibilities, a programme was initiated to reduce rainwater runoff from the processing plant. This began with a meticulous sampling campaign involving thousands of grid samples at various depths across all unconcreted surfaces.

The results of the sampling campaign revealed that the excavated contaminated material had sufficient value to fund the entire concreting project from the proceeds of its disposal. What had been an environmental liability became the means to finance a significant infrastructure improvement.

Beyond the environmental and financial outcomes, the concreting project had a notable effect on employee morale. The visible transformation of the plant – from muddy, deteriorating surfaces to clean, paved working areas – provided a tangible and daily reminder of the progress being made.

## **DISCUSSION**

The Zondereinde smelter's transformation was not the result of a single technological breakthrough or capital investment, but rather the cumulative effect of sustained operational discipline applied across multiple fronts simultaneously.



The workforce development programme demonstrated that, when resources are limited, targeting training at the point of greatest operational leverage – the control room – can deliver disproportionate returns. The adoption of mobile messaging technology provided the mechanism to enforce discipline and transfer knowledge across shifts, addressing a problem that conventional management structures had been unable to resolve. The concentrate blending programme established the foundation for predictable furnace behaviour in the face of changing feed mineralogy. And the decision to decouple slag handling from the furnace removed the single largest source of unplanned downtime.

These interventions are not individually novel. What distinguished the Zondereinde experience was the commitment to implementing all of them systematically and sustaining them over a period of more than a decade. The compounding effect of consistent daily improvement proved to be more powerful than any individual intervention.

It is important to note that not all aspects of this journey were planned. Mistakes were made, and many of the most valuable insights emerged from the willingness to test ideas in practice and learn from the results. The blending programme in particular evolved through a process that was, at times, uncomfortable – a "war of attrition" in which theories were developed, tested, and refined through direct furnace trials.

## CONCLUSIONS

The Northam Platinum Zondereinde smelter has grown from a single 10 MW furnace to a two-furnace operation producing in excess of one million ounces of PGMs per annum, while achieving a position on the lower end of the industry cost curve. This was accomplished through the sustained application of incremental operational improvements, principally:

1. Focused workforce development targeting the 8 to 12 control room operators whose decisions directly govern furnace performance.
2. The adoption of mobile messaging technology as a tool for enforcing operational discipline, transferring knowledge across shifts, and enabling real-time plant monitoring.
3. Systematic concentrate blending to manage the transition from Merensky to UG2-dominant feed, achieving consistent matte fall at approximately 5%.
4. Decoupling of slag handling from furnace operations, removing the single largest contributor to furnace downtime.
5. Infrastructure improvements that delivered environmental compliance, financial returns, and improved employee morale.

The Zondereinde experience demonstrates that there is no such thing as an overnight success in smelter operations. Step-change performance improvements are achievable, but they are built on the foundation of consistent, focused action applied daily over an extended period.

## ACKNOWLEDGMENTS

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## Martin van der Merwe

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Martin van der Merwe has more than 36 years experience in primarily the PGM industry, in positions of engineering management and process management. He is presently the Departmental Manager for the Smelter, Precious Metals Recycling Facility and Slag Plant at the Northam Platinum Zondereinde Processing Plant.

Martin's career history demonstrates his successful integration of electrical engineering expertise with metallurgical process knowledge, culminating in industry-best operation of furnaces in terms of crucible longevity, operational control and electrical efficiency, particularly with problematic high-chrome, low matte-fall feed sources.

Martin's exceptional approach to operational discipline has stood him in good stead during his career, principles that he will share with the audience during his presentation.

# Risk-based prioritisation of spare parts for smelting furnaces

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This paper discusses a method for prioritising and scheduling requirements for the purchase of critical spares for a smelting furnace, to ease cash flow pressures within a capital-constrained environment. Spare parts requirements are assessed based on their criticality, considering furnace failure modes, risk ratings and equipment supply lead times. Examples from an application of this method for a platinum group metals furnace are provided as a case study.

Compared to isolated failure of a single part, multi-part localised failure of a sub-system is more common in smelting furnaces; thus, a spares prioritisation approach that focuses on entire sub-assemblies becomes more suitable for smelting furnaces than traditional part-by-part methods. Additionally, budget constraints, time constraints and lack of sufficient data prompted the use of a simplified method, presented in this paper, rather than going into the full depth and detail of methods such as FMECA which can be unnecessarily onerous. This simplified method does however necessitate consultation with highly experienced subject matter experts and leverages historical records and benchmarking of typical smelting furnace failure incidents.

**Keywords:** risk-based spares prioritisation, spares criticality

## INTRODUCTION

Hatch, as the original equipment manufacturer (OEM) for a local platinum group metals (PGM) smelting furnace, supplied a list of critical spares to a client, which required significant expenditure to purchase the recommended spares. Facing budget constraints and poor commodity prices, purchasing the recommended spares in one lump-sum capital investment was not deemed practical during the pre-startup phases of the project. Additionally, it was necessary to budget for planned maintenance spares during upcoming planned outages. Hatch developed a plan to stage spares purchases to ease cash flow pressures while minimising ongoing risk.

This situation is one faced by many furnace operators today. Operational and planned maintenance spares represent a significant expense and unless properly managed, can impact a company's financial viability. Soft and/or unpredictable commodity prices negatively affect cash flow. However, in this case there was of course no appetite from the operator to defer spares purchases to prioritise cash flow, as this increases operational risk and can result in large unplanned operational losses due to unavailability of spares. A strategy was needed to manage the spares carefully, requiring close collaboration with all relevant stakeholders to ensure operational goals were met within an acceptable risk envelope.

If critical operational spares are not on site when a failure occurs, unplanned downtime is incurred as the operation waits to procure the spare equipment and often results in unnecessary costs to expedite the delivery. In the past, many incidents occurred, and significant costs were incurred in expediting a critical spare that was not available.

The associated unplanned downtime also had consequences for the furnace integrity: process variability including sudden power drops, additional shutdowns and thermal cycles, and inability to tap introduced stress on the equipment, negatively affecting campaign life, and increasing the risk of failure.

On the other hand, holding excess spares also has consequences, such as excessive holding costs and tying up capital in inventory. Finally, there is the potential risk of waste as some material – such as refractory – has a shelf life, or the risk of obsolescence if designs change. For the above reasons, there was a clear need for an informed method to balance the necessity of having critical spares on hand to reduce operational risk with the cost of procuring and holding spares.

### **Method of spares prioritisation**

Hatch has developed a spares prioritisation method that is streamlined for smelting operations and considers the criteria relevant to making these decisions – operational risk (damage to equipment, safety, asset availability) vs commercial considerations (cost, lead time, supplier reliability, available storage). To illustrate how this method can be practically applied, a case study will be explored in which this spares prioritisation method was used for a new furnace of Hatch design.

There are several established methods typically used to govern prioritisation of the ordering of spares, such as the ABC method (Partovi & Burton, 1993), the VED method (Botter & Fortuin, 2000), a Failure Mode, Effects, and Criticality Analysis (FMECA), or various Analytical Hierarchy Process-related (AHP) methods (Saaty, 2008). Due to the complexity of equipment involved in operating a smelter and competing operational and business factors, methods that focus on the failure of individual parts do not correctly capture the risk profile of the equipment, such as the ABC method, which focuses on the annual cost of purchasing the spare part (Partovi & Burton, 1993), or the VED method, which focuses on the severity of losses due to the non-availability of the equipment (Botter & Fortuin, 2000). Another group of methods considered were those relating to the AHP analyses; these typically involve using the judgement of experts ranking parts against each other in pairs, to establish an overall hierarchy (Saaty, 2008). However, failures in furnaces tend to be multi-part, localised failures of a sub-system which then damages various other parts around the failure area. Failures of any single part tend to damage neighbouring parts and can compromise surrounding sub-systems as well. Thus, it would be difficult to accurately compare the individual parts in pairs, as required in an AHP analysis. Additionally, only focusing on a single criterion (e.g., operational risk) is insufficient, and a more holistic approach is needed for furnace operators to balance risk, cost, and lead time. An FMECA could have been done but would not have been practical given the client's time constraints. FMECA is an extremely time-consuming method and is often too rigorous for the given application, leading to simplified methods often being adopted (Kahouadji & Belkaid, 2025). Additionally, the smelter in question was brand new and distinct from other equipment previously operated at the site, so there was not sufficient data on the plant's historical spares consumption specific enough to this piece of equipment to carry out an FMECA.

A semi-quantitative method, where numerical values were assigned to qualitative risk factors offered a middle-ground approach between qualitative and quantitative risk assessments (Aven, 2016), and was found to be suitable to meet the client's schedule, as well as flexible enough to consider entire sub-systems of the furnace, as was noted above to be necessary. This method was used to analyse the furnace through a 'failure modes' approach of spares prioritisation, where the various sub-systems are designated by looking for the closest high-risk part, and the likely extent of damage caused if that part were to fail. The most logical definition for the sub-systems in this case were the furnace bays; the sections of the crucible separated by vertical structural members (the buckstays). The power delivery system and hearth were also considered as sub-systems. The bays, as subsystems and the associated failure modes, are shown in Figure 1, as a simplified mark-up of the furnace in plan-view. As sub-systems, the bays and their failure modes were then grouped according to similarity of the closest high-risk part and the commonality of parts in each area, as shown in Figure 1. For example, the central bays on the matte endwall (centred on the matte tapblock) all have the same highest risk part (matte tapblock), and the equipment used to replace one damaged central bay on the matte endwall could be

used to replace any of the other bays in that area; hence they were grouped into one sub-system under 'Failure Mode 1'.

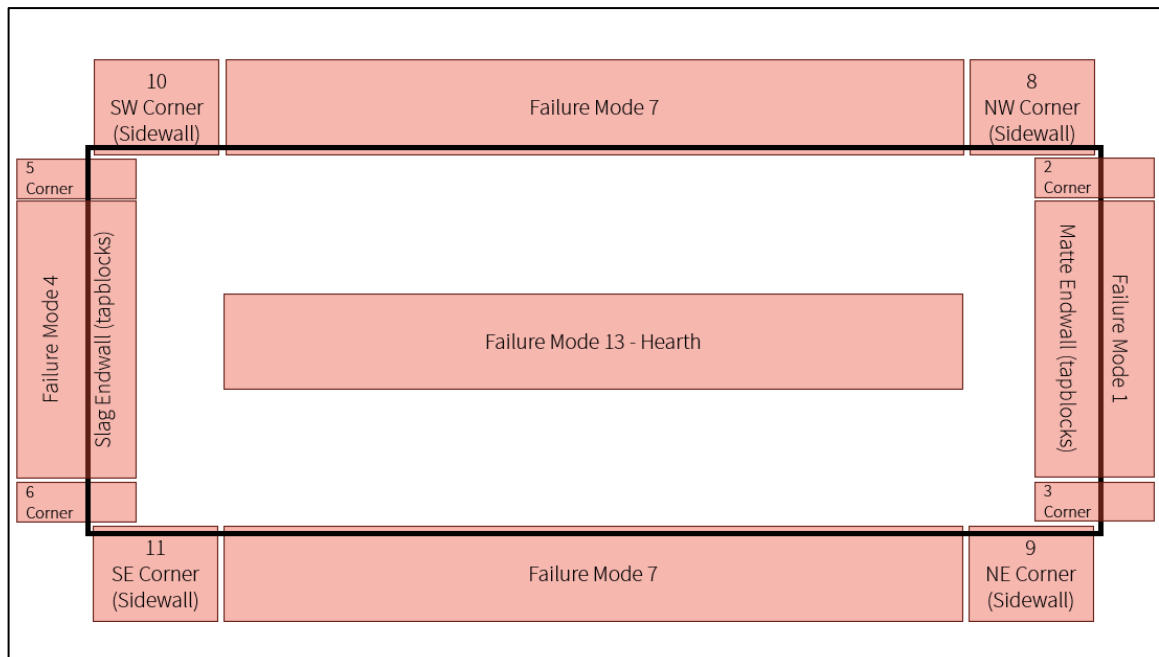


Figure 1 - Breakdown of Failure Modes at Furnace Crucible (Furnace Plan View, Simplified).

The corner bays are related, as they would also be affected by a failure of the tapblock as in Failure Mode 1, but a separate failure designation is required due to having different parts in the sub-system. This is shown in Figure 2 showing Failure Mode 1 and 2 on a simplified schematic view of the matte endwall, with the tapblocks and plate coolers shown.

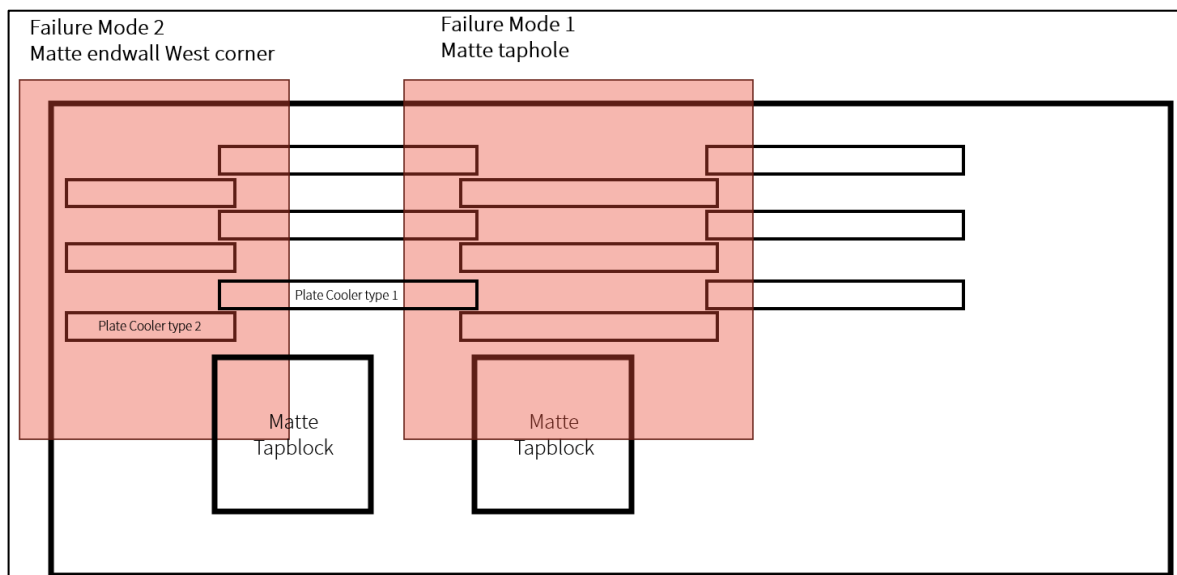


Figure 2 - Detail View of Failure Mode Grouping at Matte Endwall (Furnace Front View, Simplified).

Critical operational spares were clearly needed on site to recover quickly from failure events. However, there was also consideration for spares needed during planned maintenance events; parts which are

needed for major maintenance intervention such as rebuilds. Note that these were not analysed in this method – only critical operational spares were included.

By applying insights as the OEM, drawing on benchmarks, and consulting subject-matter experts (SMEs), the developed methodology was used to drive value for the furnace operator by managing cash flow at an acceptable risk level. However, ongoing collaboration and equipment monitoring is required as the furnace ages. Regular re-evaluations of risks and review of the furnace operator's inventory are needed to ensure the furnace operator continues to have the necessary spares available to run the operation as smoothly as possible.

#### ***Failure modes and risk ratings***

The designation of different sub-systems was guided by SME experience, benchmarks, and feedback from previous significant incidents or near misses. In most cases, failure in a specific bay would be limited to that bay, but for other failure modes, such as a hearth failure or power delivery system failure, the damage could cascade to other areas – such as a molten material run-out damaging the crucible directly, but also structural elements such as buckstays or furnace platforms. Hence these areas were separated out as sub-systems.

Hatch generated a list of equipment, with quantities, for the repair of each sub-system in the case of the associated failure mode occurring. Each failure mode was rated based on the likelihood of it occurring and the severity of the consequences if it does occur (in terms of impact on operation, safety, and financial loss). See Figure 3 for the specific likelihood and consequence ratings, in the form of a Risk Matrix, where consequence defines the severity of the incident, and likelihood defines the probability of the event occurring. The separate likelihood and consequence ratings (both scored out of 5) for each failure mode were added to arrive at an overall risk rating that incorporates both concepts. The final risk rating of each failure mode is included below in Table II, according to the ratings described in Figure 3 (listed in descending order of risk rating).

*Table I - Final Risk Ratings for Assessed Failure Modes*

| No. | Failure Mode              | Likelihood   | Consequence      | Risk Rating |
|-----|---------------------------|--------------|------------------|-------------|
| 1.  | Matte tap-hole failure    | P-4 Likely   | C-4 Major        | 8           |
| 2.  | Transformer failure       | P-3 Possible | C-5 Catastrophic | 8           |
| 3.  | Slag taphole failure      | P-4 Likely   | C-3 Moderate     | 7           |
| 4.  | Sidewall failure          | P-3 Possible | C-3 Moderate     | 6           |
| 5.  | Hearth failure            | P-1 Rare     | C-5 Catastrophic | 6           |
| 6.  | Matte endwall West corner | P-2 Unlikely | C-3 Moderate     | 5           |
| 7.  | Matte endwall East corner | P-2 Unlikely | C-3 Moderate     | 5           |
| 8.  | Slag endwall West corner  | P-2 Unlikely | C-3 Moderate     | 5           |
| 9.  | Slag Endwall East corner  | P-2 Unlikely | C-3 Moderate     | 5           |
| 10. | Sidewall Northwest corner | P-2 Unlikely | C-3 Moderate     | 5           |
| 11. | Sidewall Northeast corner | P-2 Unlikely | C-3 Moderate     | 5           |
| 12. | Sidewall Southwest corner | P-2 Unlikely | C-3 Moderate     | 5           |
| 13. | Sidewall Southeast corner | P-2 Unlikely | C-3 Moderate     | 5           |

Lead times for the parts were taken into consideration when assessing the consequence of a failure. Where the sub-system requires long lead time parts, the consequence of not having them on site is higher as procurement will take longer and/or be more expensive, as well as increasing unnecessary furnace downtime.

|                                      |                                                                          | Likelihood/Frequency                                                                                     |                                                                                                 |                                                                                |                                                                            |                                                                                                            |                   |   |  |
|--------------------------------------|--------------------------------------------------------------------------|----------------------------------------------------------------------------------------------------------|-------------------------------------------------------------------------------------------------|--------------------------------------------------------------------------------|----------------------------------------------------------------------------|------------------------------------------------------------------------------------------------------------|-------------------|---|--|
| Rating                               |                                                                          | P=5<br>Almost Certain                                                                                    | P=4<br>Likely                                                                                   | P=3<br>Possible                                                                | P=2<br>Unlikely                                                            | P=1<br>Rare                                                                                                |                   |   |  |
| Description and Indicative Frequency |                                                                          | The event is expected to occur in most circumstances. Likely to occur frequently - More than 1 per year. | The event will probably occur in most circumstances. Likely to occur several times, 1 per year. | The event should occur at some time. Likely to occur sometimes, 1 per 5 years. | The event could occur at some time. Unlikely but possible, 1 per 10 years. | The event may occur only in exceptional circumstances. Assumed it may not be experienced, 1 per 100 years. |                   |   |  |
|                                      |                                                                          |                                                                                                          |                                                                                                 |                                                                                |                                                                            |                                                                                                            |                   |   |  |
|                                      |                                                                          | Consequence/Severity                                                                                     |                                                                                                 |                                                                                |                                                                            |                                                                                                            | Health and Safety |   |  |
| C=5<br>Catastrophic                  | Furnace Operation is halted for more than 4 Weeks                        | Plant                                                                                                    | Greater than \$10 Million Loss                                                                  |                                                                                |                                                                            | 5                                                                                                          | 10                | 9 |  |
| C=4<br>Major                         | Furnace Operation is halted for more than 2 weeks but less than 4 weeks. |                                                                                                          | \$1 Million to \$10 Million Loss                                                                |                                                                                |                                                                            | 4                                                                                                          | 9                 | 8 |  |
| C=3<br>Moderate                      | Furnace operation is halted for 1 week or less                           |                                                                                                          | \$100 Thousand to \$1 Million Loss                                                              |                                                                                |                                                                            | 3                                                                                                          | 8                 | 7 |  |
| C=2<br>Low/Minor                     | Furnace Operation is halted for 24 hours or less                         |                                                                                                          | \$10 Thousand to \$100 Thousand Loss                                                            |                                                                                |                                                                            | 2                                                                                                          | 7                 | 6 |  |
| C=1<br>Insignificant                 | Furnace operation continues but at a reduced efficiency                  |                                                                                                          | Less than \$10 Thousand Loss                                                                    |                                                                                |                                                                            | 1                                                                                                          | 6                 | 5 |  |
|                                      |                                                                          |                                                                                                          |                                                                                                 |                                                                                |                                                                            |                                                                                                            |                   |   |  |

### ***Operational wear***

One failure mode that was analysed differently from the other modes presented was failure due to regular operational wear. As a failure mode, operational wear must be treated differently to the planned maintenance spares needed for major rebuild interventions or the critical spares needed for unexpected failure events. Operational wear happens during normal operation; it is expected, but associated maintenance often cannot wait until major repairs. For this failure mode, each sub-system was analysed, the consumable spares were noted, and their average wear rates were assessed.

For this failure mode, it was not appropriate to rank failure in terms of likelihood of failure. To prioritise the re-ordering of operational spares, the relevant systems were still split up into sub-systems as above. Then, for each of these sub-systems, the following criteria were compared to prioritise the order frequency of the spares:

1. Failure predictability:

This criterion describes whether the sub-system in question is easily accessible to be inspected and if wear characteristics are visible or measurable. If it is possible to track the ongoing wear over time, the necessary replacement can be ordered when failure is known to be imminent. If the wear characteristics cannot be practically observed, then the exact time of failure cannot be accurately predicted, which introduces additional risk of not having the part on site ahead of the expected life-end of the component. Note that appropriate monitoring practises can reduce the risk here by reducing uncertainty of when the part will fail; this rating assumes that if wear characteristics can be measured, they will be to a reasonable degree. This requires the appropriate priority to be given to relevant monitoring tasks.

2. Expected campaign life:

This criterion represents the life expectancy of the part, which informs when the replacement is needed. This information can be based on historical procurement information (if available) or based on benchmarks for similar parts at other operations. While there may be some uncertainty in this assessment, predictions can be refined as new information becomes available as the equipment ages.

3. Expected lead time:

Generally, the lead time can be based on data from the original ordering of the equipment; however different market and global conditions can impact the actual delivery time. Supplier reliability can also be a factor.

### ***Spares prioritisation***

Once all the failure modes were assigned risk ratings, a detailed critical spares list and re-order schedule was generated. In the case study for this method, the operational team was consulted following the development of the lists of parts to order for the highest priority failure modes (Priority 1, or P1 items), and some changes were implemented based on feedback:

1. Some high-cost parts that were deemed relatively lower risk by the operator were removed.
2. The required quantity of certain parts was increased to match the furnace operator's experience with the extent of damaged equipment due to a certain failure mode.
3. Some key parts from a low-risk failure mode were added, as the furnace operators nonetheless felt they merited inclusion in the initial (P1) critical spares list.

This process then continued for our case study, to generate lists for the next levels of priority, onto P2, P3, P4, etc. Depending on the maturity of the operation, once the most critical spares have been identified, the risk assessment itself can be given as a deliverable for the site team to generate the next spare orders and re-orders independently. This was done for the case study in question.

Another consideration to reduce the overall cost was part interchangeability between sub-systems. If, for example, part A is required to repair damage via multiple failure modes, e.g., Failure Mode 1 and Failure Mode 2, then the failure mode that requires the largest quantity of part A is used to drive the overall quantities ordered. This further helps manage the cost of spares, however, it can leave gaps in the case of successive failures that require the same component to repair; e.g., if Failure Mode 1 occurs, and we use 50% of the part A stock to repair the damage, and then Failure Mode 2 happens, there may



be insufficient stock of part A to repair Failure Mode 2. Thus, sufficiently rapid replacement of all spares used is crucial. This strategy was not selected by the furnace operator in the case study, in order to maintain a conservative approach to managing risk.

#### *Iterative risk assessment*

With this risk rating being given to each of the failure modes (and its associated parts list), the next step of the process was to review the outcomes of the risk assessment by looking at the change in the risk ratings for each failure mode when the appropriate spares become immediately available, as having the spares on hand reduces the consequence of the failure mode occurring.

This adjusted risk rating helped quantify the risk depending on whether the spares were available or not, to drive procurement decisions. It also allowed the operator to quantify the total risk exposure with the spares that were currently available. For example, if the failure mode for the 'matte tap-hole' sub-system occurs, there will certainly be a period of power-off in the immediate wake of the incident to repair any damages and clean up the area. However, if the necessary spares are readily available, the repairs can commence as soon as the area is safe to work in with no delay. This reduces the consequence of the failure mode, as the impact of lost production is minimised; no production time is lost in waiting for required spares to arrive on site and the need for any costly expediting of required equipment is negated. Therefore, the overall risk rating associated with the matte tap-hole failure mode can be reduced when the required spares are available on site. The benefit of each spares provision was assessed through review of residual risk, and the decrease in risk was used to justify the expenditure.

In the case study, after the risk assessment was revisited, the next list of critical spares was generated in parallel with assistance in procuring the P1 spares. The P2 list was delivered to the operation while manufacture and shipping of the P1 items were underway, to help the operation plan spares orders in advance efficiently and ensure the required budget could be made available at the time when needed. As the furnace continued operation, some risk ratings were adjusted based on feedback from site and Hatch team observations regarding performance. While the specific timelines and content for each of these lists was very operational-specific, the timeline used in this case study is summarised below as an example:

*Table II: Spares Ordering Timeline from Case Study*

| Spares Priority List | Recommended Timeline                                                                                                                                    |
|----------------------|---------------------------------------------------------------------------------------------------------------------------------------------------------|
| P1                   | Aim to have on site upon start-up                                                                                                                       |
| P2                   | Have on site within 6 months of operation                                                                                                               |
| P3                   | Have on site within 1 year after start-up (informed by continuous monitoring)                                                                           |
| P4                   | Prior to the first major planned maintenance event (e.g. endwall repair). Informed by equipment that could realistically be replaced during the repair. |

In this case, at the time when the subsequent lists after P1 were being ordered, some spares stock from start-up and from the original P1 order were fully consumed, so the risk assessment was reviewed at the operation's request, as the re-ordering spares when they are being consumed is critical to maintaining sufficient stock in storage. With the prioritisation of spares done as an outcome of the risk assessment – with necessary quantities listed – it was simple to implement re-ordering rules for the operation's commercial team to ensure the recommended spares are always available on site. The higher the priority rating, the more critical it is to have the spare available; the replenishment of stock levels of that equipment was prioritised.

Providing the recommended spares list ahead of when they must be ordered also allows the operation to account for the budget needed for re-ordering spares. While the use of critical spares cannot be accurately forecasted, a general prediction was made based on the likelihood assigned to the failure mode they are associated with – i.e., if the likelihood of the failure mode occurring is rated as a 3, it is

expected to happen once in a five year period, then it can be assumed the spares will be consumed at some point within the stated period.

### ***Revalidation***

For this case study the methodology applied helped the furnace operator plan the spares budget, as it provided a timeline for when critical, operational, and planned maintenance spares should be ordered and informed when re-ordering should happen according to use. As further time passes, operational feedback will be used as an input to refine the spares ordering methodology to inform the required critical spares. This can be done reactively or proactively. For critical operational spares:

1. A reactive method would be employed when there is an observable high usage rate of a spare part. For example: if it was hypothetically observed that slag inserts are being consumed faster than is typically expected, this information can be fed back to the procurement team to ensure there are sufficient spares available when needed while the operational practice causing this is being corrected.
2. A proactive method would be, as a hypothetical example, monitoring for operational practices which are known to cause accelerated wear or damage to components, and then increasing the priority of the purchase of this spare, to ensure it will be available when needed on account of the expected reduction in lifespan. If the operation is struggling with poor tapping practices, while they are in the process of putting measures in place to correct this the priority rating on tapblocks can be increased, as it is known that the risk of damaging the tapblock is elevated.

For planned maintenance spares:

1. A reactive method would be, hypothetically, realising necessary spares are not available when the schedule time to perform a regular maintenance activity arrives, and ordering the parts at that time, delaying maintenance until parts are available.
2. A proactive method would be considering the long-term maintenance plan, checking it against available spares in stores and planned usage rates, and ordering any required parts in advance of maintenance procedures so they arrive before the planned execution date. These usage rates can be updated as parts are consumed. As an additional synergy, those responsible for maintenance planning can also be informed that a tapblock replacement may be necessary imminently and can be prepared to execute on this task. Further assistance with maintenance planning or predicting maintenance timelines can be provided as necessary.

Additionally, a monitoring programme with the OEM was implemented in this case study. The data from this monitoring programme was an extremely useful input to developing the priority lists in this case. The condition information and ongoing observations collected through this service were used to refine exact quantities of spare parts in later orders, to save capital where possible and manage any areas where due to specific site conditions, risk of failure may be elevated. As part of the service, recommendations were also made to make minor improvements to operating practices to reduce the wear and tear on in-service equipment, increasing its campaign life and reducing the need for spares. Recommendations were also made for minor design changes to future spare parts to facilitate better performance for continuous improvement. Further operational feedback will be used to refine the strategy as it becomes available over time.

### ***Major maintenance intervention planning***

As the operation progresses, the need for planned maintenance spares for major maintenance interventions, such as the replacement refractory and copper coolers needed for an endwall rebuild, becomes a higher priority. Unused critical spares may be consumed during a rebuild if deemed appropriate, in addition to the planned maintenance spares ordered specifically for the repair.

To make the decision to use critical spares during a major maintenance intervention, a risk assessment should be performed regarding the likelihood of the critical spares being consumed in the lead-up to the planned maintenance event, as well as the potential unavailability of those consumed spares following start-up immediately after the rebuild. Moreover, the availability of critical spares can also provide more flexibility in the scope of the rebuild. These critical spares being kept in stores can be used in the planned maintenance event as spares for potentially unexpected scope – for example, when an

endwall rebuild is executed, tapblocks will be pulled out and inspected. If more replacements than expected are discovered to be necessary, having some in stock as critical spares will allow for this replacement to happen smoothly with minimal schedule impact. This allows the operation to get the best possible result out of their planned maintenance. This was not necessarily relevant for the case study in question, as when this spares prioritisation process was done the furnace was new and a major maintenance intervention was not yet in the detailed planning stage.

## CONCLUSION

While critical spares procurement and availability can be a significant cost for smelter operations, proper planning and analysis can ensure that both the risk incurred by the operation and the capital expenditure is acceptable for the overall goals of the operation. The method that Hatch developed for the specific challenges faced by furnace operators when ordering critical spares addresses these competing factors of risk management and budgetary constraints. Using our subject matter expertise, a series of sub-systems and failure modes were devised and ranked according to risk of failure. The ordering of spares was then prioritised to manage the risk. The lists of specific parts required for each sub-system's failure mode were developed based on benchmarking and operational experience, and an ordering schedule for recommended critical spares was produced, with potential for ongoing improvement as further operational and equipment usage information becomes available with time.

In the case study presented, this method of prioritising critical spares was successfully implemented to assist the furnace operator with managing their operational risk within the budget available. Ongoing condition monitoring support also served an essential role in informing on the condition of sub-systems to inform equipment condition and associated risk, and thus the criticality of the associated spares. The ordering of critical spares continues with the method described.

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## **Milan von Delft**

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Milan von Delft is a Mechanical Engineer with experience in furnace design, quality assurance, asset performance management, and spare parts supply. He currently serves as an Intermediate Mechanical Engineer at Hatch Africa, supporting PyroTech industries with the Asset Performance Management (APM) group, through condition monitoring via site inspections and data analysis, as well as the supply of spare parts. Previous work has included equipment design of various packages for Pyrotech clients, primarily PGM furnaces, quality assurance of said equipment, technical advisory to construction and commissioning teams, and operational data analysis. His current work focuses on spare parts management, coordination between design and execution teams, client engagement, and equipment design.

# Developing process safety management key performance indicators for smelting operations

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This paper presents an approach to developing Process Safety Management (PSM) key performance indicators (KPIs) with a focus on pyrometallurgical operations. The paper presents an adaptation of mature petrochemical PSM practices, along with other proven PSM methodologies, to the platinum group metals (PGM) smelting environment. The KPIs are tailored to smelting operations, with the objective of advancing PSM dialogue to the benefit of safety in the sector. The methodology begins with a review of established PSM systems and metrics applied in the oil and gas industries as guided by American Petroleum Industry Recommend Practice (API RP) 754 to identify possible extrapolation into pyrometallurgical operations. This is compared with more established mining risk management processes such as Operational Risk Management to focus on Process Safety Unwanted Events around molten metal processes, to identify both existing and new process safety leading indicators. These established pyrometallurgical risks include challenges such as molten material containment, refractory degradation, fuel system failures and corrosive or flammable gas handling systems. The paper concludes by proposing a KPI dashboard to represent leading and lagging indicators for generic benchmarking of process safety performance to support vigilance and continuous improvement. By doing so, pyrometallurgical operations can supplement a reactive stance centred on personal safety with a proactive culture that anticipates and prevents process safety failures, thereby safeguarding people, assets, business, and the environment.

## INTRODUCTION

Process Safety Management (PSM) is a system that focuses on the prevention, preparedness, mitigation, response and restoration from catastrophic releases of chemicals or energy associated with a facility, (CCPS, 2010). These facilities include chemical, mining and mineral processing facilities which handle hazardous chemicals and energy systems as part of their normal day to day operations. Globally, PSM was introduced following a series of process safety accidents; to control the risks associated with hazardous materials that can lead to catastrophic events. Examples include the Bhopal tragedy of 1984, Texas City refinery fire in 2005 and the Deepwater Horizon environmental event of 2010. An initial process safety standard was issued by the United States Occupational Safety and Health Administration in 1992 to regulate and improve the safety performance of businesses operating with these risks (OHS1910.911, 1992). Several other legislative and industry entities across the world have generated further regulations and guidelines to prevent fire, explosion and release of hazardous materials. In all these guidelines, the monitoring and communication of the PSM system performance is a fundamental requirement. Such monitoring is done through the use of carefully formulated key performance indicators (KPIs).

The introduction of PSM across the mining value chain is relatively novel and offers an opportunity to manage high consequence, low frequency events associated with the release of energy from fires, explosions and hazardous materials.

Safety data has typically emphasised lagging occupational safety indicators such as lost time injury frequency rates (LTIFR) and total recordable case frequency (TRCF). While these metrics are important, they fail to adequately elevate process safety risk around catastrophic incidents. Specifically, the smelting industry, despite its high-temperature processes, hazardous materials, and complex operations, has fallen outside the scope of traditional PSM. In the pyrometallurgy space, in which smelting operations are one of the key processes, the handling of molten materials and metals presents an explosion risk when such materials come into uncontrolled contact with water, which is often present as part of the associated cooling systems. Several catastrophic incidents have been reported in literature, for example, Ferguson and Zsomboky (2017) list over 20 serious electric arc furnace accidents caused by steam explosions during the period 1994 to 2014. In all these incidents there were fatalities and serious injuries to personnel, highlighting the forever urgent and challenging goal of ensuring safe and reliable operations. PSM offers an opportunity to manage such high consequence, low frequency events and thus the development of possible PSM KPIs as applicable to the pyrometallurgical industry is the focus of this paper. The subsequent sections present the relevant information and all the pertinent literature that informed this work together with the methodological approach followed to develop the PSM KPIs.

### Valterra Platinum and the need for PSM

Valterra Platinum, formerly Anglo American Platinum, is the largest primary producer of platinum in the world. Headquartered in Johannesburg, South Africa, the company operates three smelting operations in the country: the Waterval, Mortimer and Polokwane smelters. Waterval and Mortimer Smelters are located on the western limb of the Bushveld Complex, and Polokwane Smelter is located on the eastern limb (Hundermark *et al.*, 2011). Further to this, there is the Unki primary smelting facility in Zimbabwe and a converting process in Rustenburg adjacent to the Waterval Smelter. Figure 1 shows the high-level value chain for Valtterra Platinum, which has four key steps: mining, concentrating, pyrometallurgy and refining.

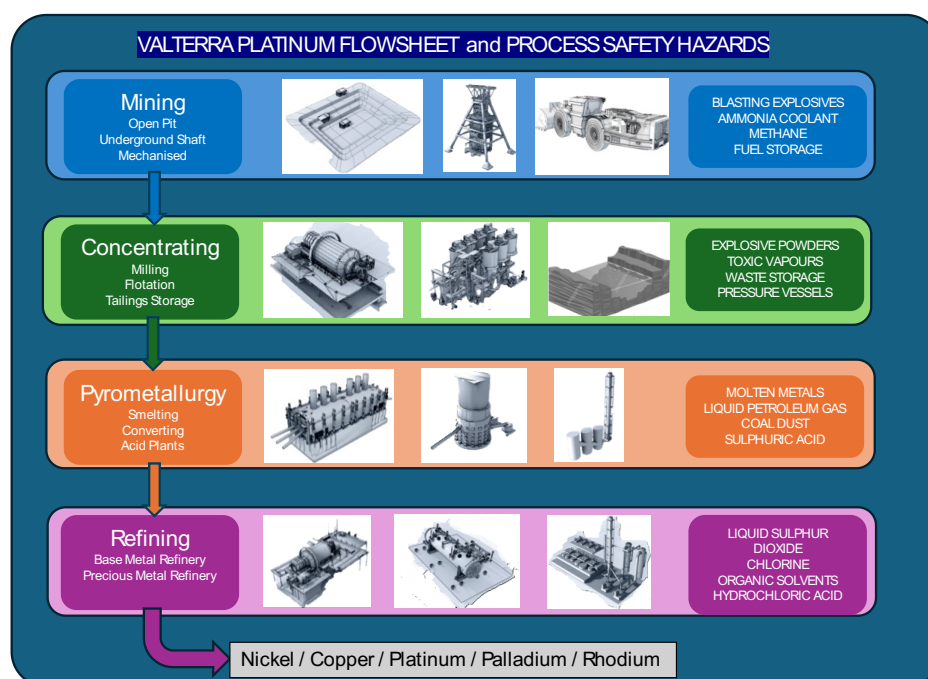


Figure 1. Valtterra Platinum value chain and associated risks

The four primary smelters and central converting facility fall under the pyrometallurgy step. As part of the last step in the value chain, the company operates a Base Metals Refinery and a Precious Metals Refinery, both located in Rustenburg in the North West province of South Africa.

The pyrometallurgical processes at Valterra Platinum have previously been described by others (Jacobs (2006), Sima and Legoabe (2006) and Hundermark *et al.*, (2006)) and are thus not discussed in detail in this paper. In brief, the smelting process uses electrical energy to melt the concentrate for further value aggregation through molten liquid density partitioning between the matte and slag phases. The product (furnace matte) is moved to the converting operation that uses oxidative reactions to further upgrade the value stream and remove iron and sulfur associated with the primary furnace mattes. The production process concludes with the refining operations which separate the pay metals into discrete saleable quality commodities.

The hazards associated with each step in the value chain (Figure 1) have been well managed by the company over the years. Despite this, there have been incidents, both internally and within other mining businesses, associated with these and other hazards in the business. Further to this, there are numerous potential losses of primary containment) events that pose extraordinary risk to the safety, business viability, environment, nearby communities and reputation of the business. In an effort to improve overall safety in the company and strengthen barriers for managing the risks associated with the hazards in the value chain, Valterra Platinum has elected to implement PSM as an enhanced risk management measure for preventing unwanted process safety events. The introduction of this PSM activity into the established mining business system in the company has required significant integration efforts to minimise disruption to existing business processes.

It is important to highlight that mining has not generally fallen under process safety regulations and subsequently required extensive activity alignment to the available frameworks. The repeatedly referenced basis of “you can’t manage what you don’t measure” (Swuste, *et al.*, 2016) has similarly required the implementation of a Process Safety KPI system to allow the effective monitoring and control of catastrophic risks. In the rest of this paper, we present an approach which was followed in developing PSM KPIs with a focus on pyrometallurgical operations. The KPIs are tailored to smelting operations, with the objective of advancing PSM dialogue to the benefit of safety in the sector.

## **PROCESS SAFETY MANAGEMENT FRAMEWORKS**

Risk management is a fundamental business process and is a well- entrenched discipline in mining businesses. The quantification of risk is generically achieved through an assessment of the consequence of an unwanted event associated with a hazard in conjunction with the likelihood of that event occurring. This is illustratively summarised in Table I, where the risk scoring system can be standardised as required for a specific business. PSM leverages off this risk process and, in the case of established operations, invariably needs to consider the likelihood of catastrophic events.

Presented in this section are the two integrated systems around process safety utilised within Valterra Platinum. Firstly, the operational risk management (ORM) framework and secondly, the Energy Institute PSM framework as adopted for implementing process safety management. Relevant aspects of each framework are outlined, leading to the development of a Process Safety KPI dashboard from the existing safety incident database to support the retrospective implementation of PSM in a well-established mining company.

Table I: Generic risk matrix (with the red box highlighting priority unwanted event consequences vs likelihoods)

| Event Risk Rating/ Priority (1) |                    |                     |                     |                     |                     |
|---------------------------------|--------------------|---------------------|---------------------|---------------------|---------------------|
| Consequence<br>Likelihood       | 1<br>Insignificant | 2<br>Minor          | 3<br>Moderate       | 4<br>High           | 5<br>Major          |
| 5<br>Almost Certain             | Medium<br>(11)     | Significant<br>(16) | Significant<br>(20) | High<br>(23)        | High<br>(25)        |
| 4<br>Likely                     | Medium<br>(7)      | Medium<br>(12)      | Significant<br>(17) | High<br>(21)        | High<br>(24)        |
| 3<br>Possible                   | Low<br>(4)         | Medium<br>(8)       | Significant<br>(13) | Significant<br>(18) | High<br>(22)        |
| 2<br>Unlikely                   | Low<br>(2)         | Low<br>(5)          | Medium<br>(9)       | Significant<br>(14) | Significant<br>(19) |
| 1<br>Rare                       | Low<br>(1)         | Low<br>(3)          | Medium<br>(6)       | Medium<br>(10)      | Significant<br>(15) |

### Operational risk management process

Valterra Platinum has been a member of the International Council for Mining and Metals (ICMM) and utilises an ORM framework to identify and implement controls for hazards. The ORM process uses a four-layer risk assessment model (Figure 2) together with some of the established risk assessment tools applied across the various application levels.

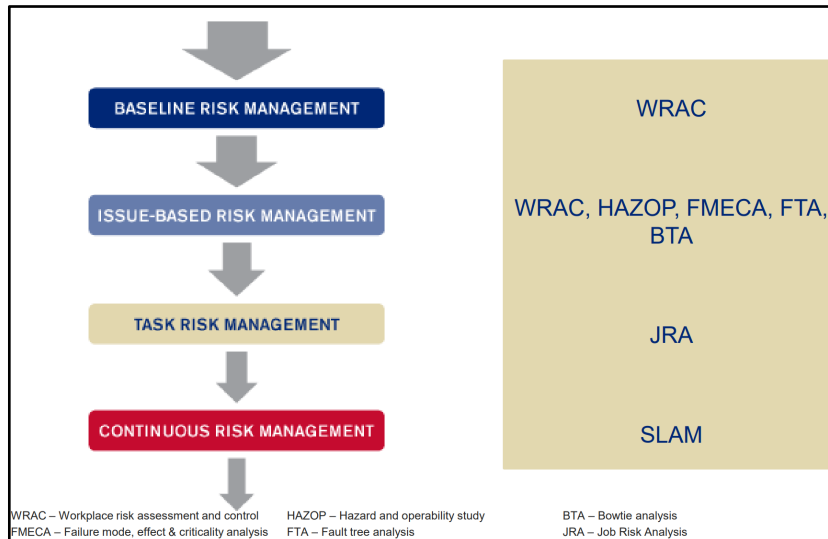


Figure 2: Four-layer risk assessment model and tools.

This flowsheet encapsulates a full risk management process from:

- Layer 1 involving Baseline risk assessment for the initial identification of hazards and priority unwanted events
- Layer 2 considering Issue-based risk assessment of these hazards in operations and the management solutions to achieve acceptable risk tolerance, i.e., the specific, or issue-based risk these hazards pose in operational applications,



- Layer 3 utilising Job Risk Assessments to identify interaction with these hazards in tasks with appropriate mitigation.
- Layer 4 covering the continuous in-situ vigilance to evaluate whether the risk has been properly identified and is being effectively managed during a task. This includes looking for how any hazard exists and is controlled in the operational work areas.

Whilst the generic risk processes within ORM have effectively identified hazards, including process safety hazards, the initial controls developed through issue-based studies have generally been evaluated qualitatively and rarely through the utilisation of detailed quantitative risk reduction work. The application of Quantitative Risk Assessment tools is limited and the risk horizon for general assessments was typically 20 to 30 years as the assessed life of an asset. The introduction of PSM has necessitated more frequent application of rigorous issue-based risk assessment tools such as Layers of Protection Analysis, Failure Mode and Effect Analysis and Predictive Human Error Analysis amongst others to address the technical determination of the likelihood of an event and the effectiveness of controls to prevent and mitigate it. Consequence modelling is also required to quantify catastrophic risk and determine tolerable management. All these tools generate controls that require monitoring and can be aggregated in KPIs. The KPI reviews in turn can lead to prioritised and disciplined management of controls that should rarely be tested but need to be available when required.

The prioritisation of managing these hazards is underpinned by implementing the correct oversight to check the health of the risk controls. Numerous investigations into the mining industry process safety accidents have repeatedly endorsed the need to enhance the system for monitoring leading and lagging indicators to determine the health of process safety and direct responsive actions. The ability to identify and react to weak signals is critical in the prevention of catastrophic process safety events.

### **The Energy Institute Process Safety Management Framework**

The Energy Institute is a chartered professional membership body for the energy industry supporting over 200 organisations worldwide working with energy. Its purpose is to “*develop and disseminate knowledge, skills and good practice towards a safe, secure and sustainable energy system*” (Energy Institute, 2022). In line with its mission, the Energy Institute published the Energy Institute PSM Framework in 2010 and recently revised it in 2022. This framework is derived from the original OSHA1910.911 process safety regulation referenced earlier. The framework sets out what organisations need to do to assure the integrity of their operations, by defining measures that need to be implemented to ensure the required work is done. The framework has four main pillars, namely:

- Process safety leadership
- Risk identification and assessment
- Risk management and
- Review and improvement.

The four pillars are detailed in 20 PSM elements (Table II). Details of each element, which contain the expectations that describe what each organisation implementing the framework must achieve to meet the intent of each element, are presented in Energy Institute (2022) and will not be discussed further in this paper. According to the Energy Institute (2022) the 20 elements of the framework work together to create barriers between process safety hazards and the occurrence of major incidents. Any one of the 20 elements, when complete without any deficiencies, will prevent a hazard from developing into a catastrophic accident. By implementing the framework, organisations move from a reactive approach of managing safety based on past incidents, emphasising lagging safety indicators such as LTIFR and TRCF, to a systematic proactive basis of understanding and implementing the required actions and interventions to prevent incidents.

Table II: The 20 elements of the Energy Institute PSM Framework

|                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                           |                                                                                                                                                                                                      |
|---------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------|------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------|
| <b>Process safety leadership</b> <ol style="list-style-type: none"> <li>1. Leadership, commitment and responsibility</li> <li>2. Identification and compliance with legislation and industry standards</li> <li>3. Employee selection, placement and competency and health assurance</li> <li>4. Workforce involvement</li> <li>5. Communication with stakeholders</li> </ol>                                                                                                                                                                                                                                                                                             | <b>Risk identification and assessment</b> <ol style="list-style-type: none"> <li>6. Hazard identification and risk assessment</li> <li>7. Documentation, records and knowledge management</li> </ol> |
| <b>Risk management</b> <ol style="list-style-type: none"> <li>8. Operating manuals and procedures</li> <li>9. Process and operational status monitoring and handover</li> <li>10. Management of operational interfaces</li> <li>11. Standards and practices</li> <li>12. Management of change and project management</li> <li>13. Operational readiness and process start-up</li> <li>14. Emergency preparedness</li> <li>15. Inspection, maintenance and asset integrity</li> <li>16. Management of safety critical devices</li> <li>17. Work control, permit-to-work and task risk assessment</li> <li>18. Contractor and supplier, selection and management</li> </ol> | <b>Review and improvement</b> <ol style="list-style-type: none"> <li>19. Incident reporting and investigation</li> <li>20. Audit, assurance, management review and intervention</li> </ol>           |

The Energy Institute framework, akin to other analogous frameworks (e.g., the Centre for Chemical Process Safety – CCPS Risk Based Process Safety (RBPS) Framework, CCPS 2025), has a broad pillar of review and improvement with element 20 directing the management review and intervention. This element, and the Process Safety KPI dashboard, as was developed as part of this work, is partially informed by element 19 dealing with incident reporting and investigation.

## DEVELOPING NEW KPIs

The process of developing new process safety KPIs began with a literature review on available sources and guidelines. The literature review provided several widely recognised guidelines which are frequently cited (Brazier (2021), Swuste *et al.*, (2016)) and key examples include the following:

- American Petroleum Institute Recommended Guideline (API RP 754)
- Centre for Chemical Process Safety Process Safety Metrics (CCPS, 2010)
- International Association of Oil and Gas Producers (IOGP 456)

These guidelines were analysed with a focus on their relevance to the petroleum industry and how they can be adapted for mining operations. Notably, API RP 754 explicitly states that its principles apply to any industry where a loss of containment could lead to severe harm or damage (American Petroleum Institute, 2021). Many multinational companies also report significant event data aligned with API metrics in their Environmental, Social, and Governance disclosures to guide investors.

The Process Safety Indicator Pyramid (Figure 3a) offers a clear framework for mapping critical process safety KPIs across different focus areas and introduces the concept of leading and lagging indicators. API RP 754 further explains how to classify and populate the different tiers. Figure 3b, sourced from an API fact sheet following the release of the 3rd Edition, provides additional clarity on tier definitions:

- **Tiers 1 and 2** represent actual loss-of-containment events, categorised by the scale of the release using defined thresholds amongst other consequence criteria. These indicators are reactive and offer little in terms of prevention. In extreme cases, they can lead to catastrophic, business-ending incidents with major public consequences – such as Bhopal (1984), Deepwater Horizon (2010), or Texas City (2005).

- **Tier 3** focuses on compromised barriers identified before failure, such as seized pressure relief valves, excessive corrosion, or operations outside safe limits.
- **Tier 4** addresses operational discipline and system performance, including adherence to maintenance schedules for safety-critical equipment, competence in executing process safety procedures, and maintaining accurate, up-to-date process safety information.

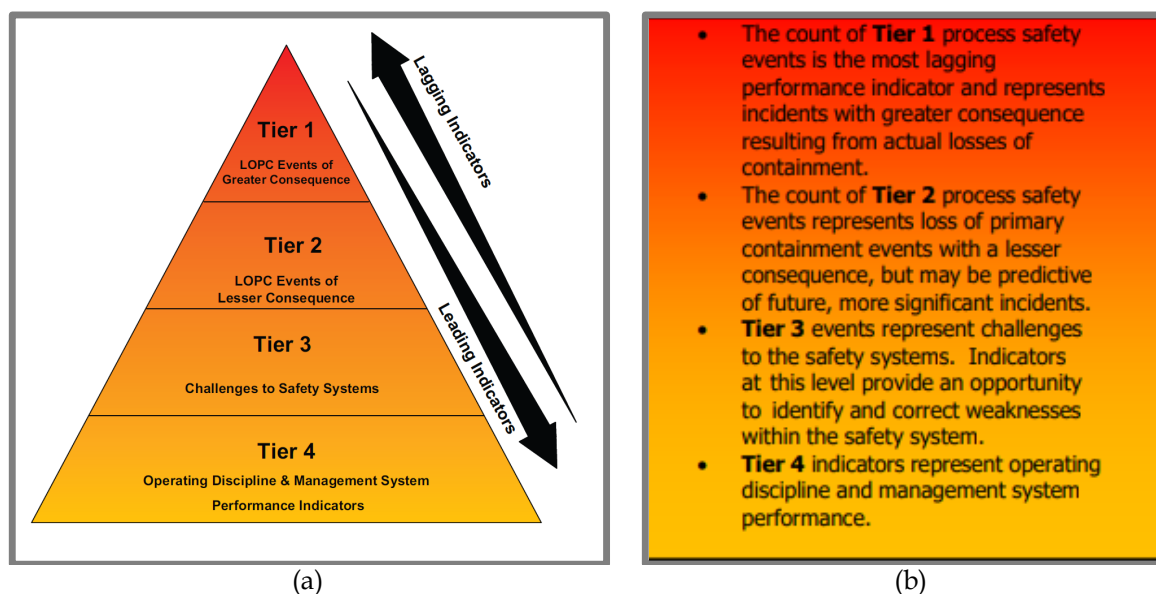


Figure 2: (a) Process Safety Indicator Pyramid (American Petroleum Institute, 2021) and (b) associated description of tiers.

The 4-tier triangle in Figure 3 was subsequently selected as the framework for building a bespoke primary Process Safety KPI dashboard for Valterra Platinum. This was a critical integration activity to prevent the introduction of new terms to describe already-managed processes. A good example of this is where the safety system used at Valterra Platinum has historically tracked events such as High Potential Incidents (HPI) and High Potential Hazards (HPH). It therefore was required to translate these metrics and, as far as possible, any others into the Process Safety Indicator system. The output of mapping the existing consequence rankings at Valterra Platinum, from Table I, against the API tiers, from Figure 3, is summarised in Table III.

Table III: API Tier alignment with Valterra Platinum consequence rankings

| No.    | API-RP754 tiers                                                                                                                                     | Valterra Platinum consequence alignment                                                                                                                                                                                  |
|--------|-----------------------------------------------------------------------------------------------------------------------------------------------------|--------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------|
| Tier 1 | Count of greater consequence events with actual losses of containment.                                                                              | Actual consequence 4 and 5 events – significant losses arise from the release of energy.                                                                                                                                 |
| Tier 2 | Loss of primary containment events with lesser consequence actual consequences.                                                                     | Actual consequence 3 events/ Potential consequence 4 and 5 events - classified as High Potential Incidents.                                                                                                              |
| Tier 3 | Events representing challenges to the safety system. Indicators provide an opportunity to identify and correct weaknesses within the safety system. | Actual consequence 1 and 2 events (with no potential higher rating than 3). Challenges to the safety controls without loss of containment related to potential events of 4 and 5 – classified as High Potential Hazards. |
| Tier 4 | Indicators represent operating discipline and management system performance.                                                                        | Selected indicators e.g.: overdue critical maintenance, missed inspections, late PHA action closure, aged and overdue management of change actions.                                                                      |

Part of the work done in developing the relevant KPIs was to establish potentially missing and unique Valterra Platinum performance indicators to allow prioritising existing risk controls against a process safety indicator dashboard. The additional KPIs were determined by following two approaches:

- **PSM maturity assessment** at Valterra Platinum against the twenty elements of the Energy

Institute PSM Framework.

- **Interrogation of a large historical incident database** to determine the prevalence and nature of past process safety incidents

The work done on the foregoing two areas, together with the results which led to the development of the Process Safety KPI dashboard, are presented in the following section.

## Work done and results

### *Process safety management maturity assessment*

As part of implementing PSM at Valterra Platinum, a maturity self-assessment was conducted using the Energy Institute's 20-element framework. The assessment followed the standard questions provided in the framework. Figure 4 illustrates a histogram showing the median scores for each element across the mining company's operations. Each element was rated on a scale of 1 to 5, where:

- 1 indicates no compliance
- 5 represents full alignment with the framework requirements.

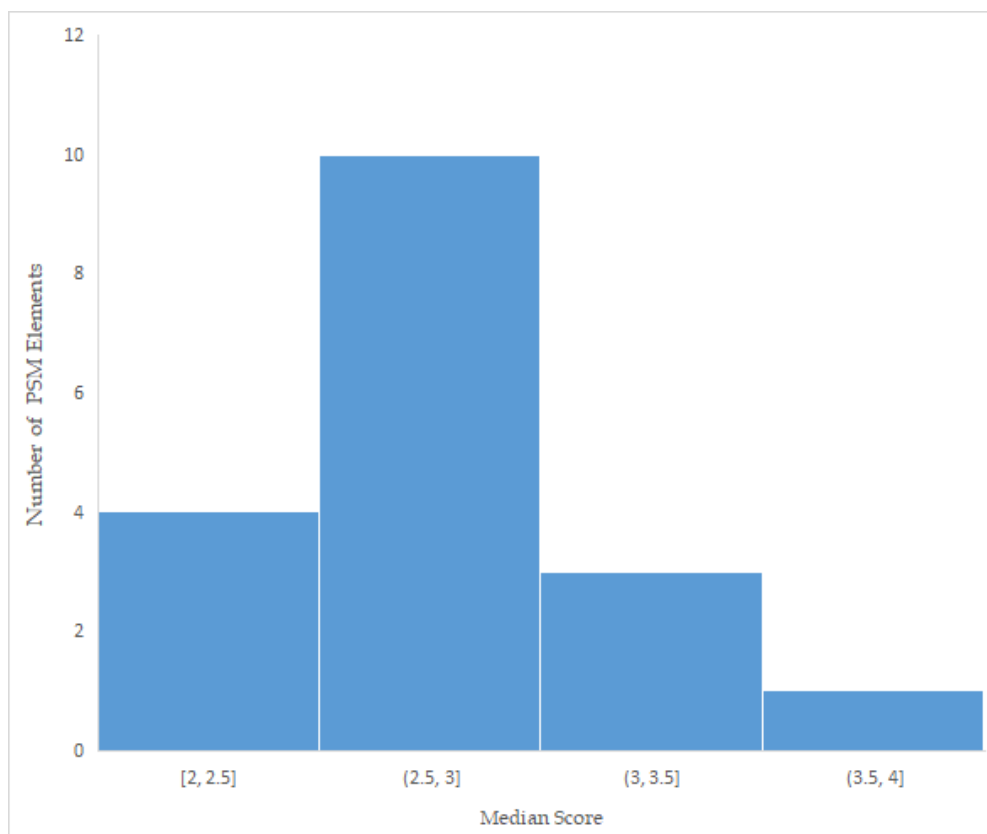


Figure 4: (a) Process safety self-assessment

The results are unsurprising given that PSM is a relatively new initiative within the company. The assessment helped identify areas that need improvement with the company generally scoring above average across the 20 EI PSM elements. It is notable that the ORM process had been effective in risk identification and assessment of Priority Unwanted Events that included many PSM hazards and established a foundation for prioritising risk management. Low scores generally reflect the absence of a long-standing PSM programme and the lack of defined KPIs prior to the work done as part of this paper. To address these gaps, Valterra Platinum has developed closure plans for each operation aimed at improving PSM maturity. Progress against these plans forms part of the new KPIs and is tracked and integrated into the Process Safety Indicator discipline, with compliance assured through regular audits.

### *Historical incident analysis*

Artificial intelligence was used as a tool for searching existing and large text-based safety incident databases for historical incidents to determine process safety trends and priority areas. From the literature reviewed, there was a host of different computational tools, however, Natural Language Processing (NLP) emerged as a field most likely to assist with analysing language and text-based data (Ghazizadeh, 2020). This methodology looks to digitise large text databases into discernible attributes. Specifically for the purposes of this work, NLP was used to generate binary classifications. An example of this machine learning concept is described by Van Otten (2023) for identifying SPAM from email language structure.

The objective of using an AI tool was to review the eight years of electronically stored text records to identify the prevalence, nature and location of process safety incidents. This output would then form the basis of the last step of configuring an aligned Process Safety KPI dashboard.

The analysed safety incident database consisted of 11571 safety incident records accumulated from January 2016 until April 2024, and was processed via the NLP process which consisted of the following five key steps (Techbelli, 2023):

- Data pre-processing covering collection, cleaning and configuring
- Feature engineering
- Model training
- Model validation and
- Results and feedback

The training data set used represented 80% of the available data and was made up of the randomly selected 9256 clean text records. Once the model was established, the remaining 20% of data records in the original event description format without process safety classifications were passed through the model as part of model validation. The final AI model results are tabulated in Table IV and also shown in Figure 5.

*Table IV: Biased model results*

| <b>Metric</b>      | <b>Precision (%)</b>                | <b>Correct identification (%)</b>   |
|--------------------|-------------------------------------|-------------------------------------|
| Non-Process Safety | $\frac{1618}{1707} \times 100 = 95$ | $\frac{1618}{1771} \times 100 = 91$ |
| Process Safety     | $\frac{455}{608} \times 100 = 75$   | $\frac{455}{544} \times 100 = 84$   |

From the table, the final analysis delivered the correct identification of 84% of the process safety incidents from the 2300 odd validation data records. The results show that whilst a higher number of process safety incidents were correctly identified, there was a commensurate increase in the numbers of non-process safety incidents incorrectly identified as process safety (75% precision). This was deemed to be a 'safer' error since the lower numbers of process safety incidents facilitate detailed quality reviews that can be used to correct this bias and further train the model.

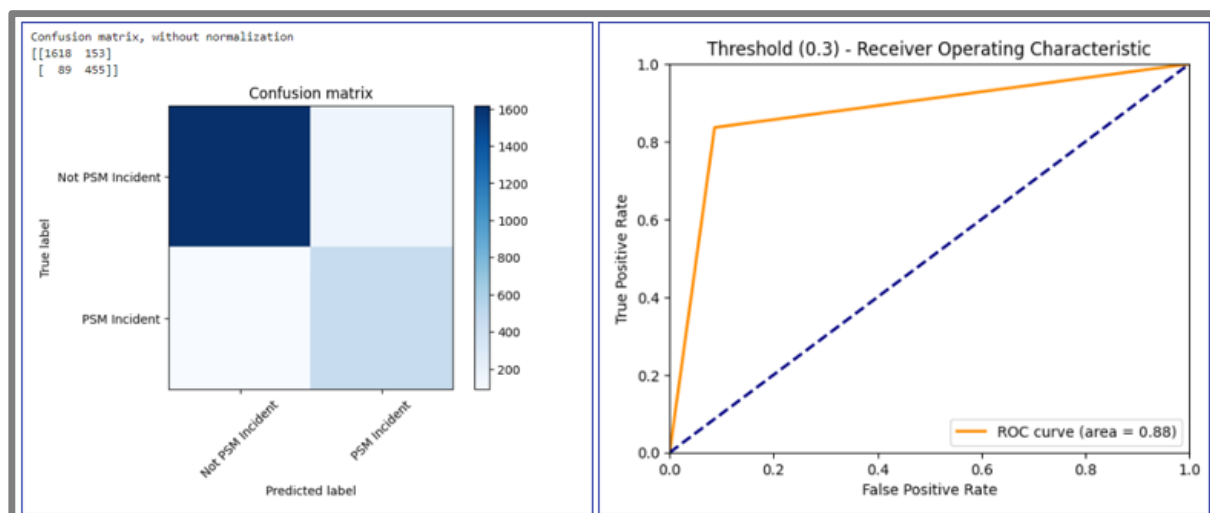


Figure 5 shows the confusion matrix (illustrating the effectiveness of the model in both actual and false positive classifications for each state of process safety or non-process safety), as well as the receiver operating characteristic (ROC) from the NLP model. The area under the curve on the ROC plot shows the combined accuracy for each of the possible classifications at the set threshold (0.3, applied to determine the extent of a match before classifying the incident as process safety or not). The higher this number, the better the model. The bias has been applied to risk incorrect classification of a non-process safety event over failing to identify actual process safety incidents.

The ROC of 0.88 indicates that the lower threshold for matching terms to process safety had a smaller impact on incorrectly identifying non-process safety incidents than it did on correctly identifying process safety events. These results were deemed acceptable with the understanding that the review cycle would then be used to recalibrate the model and continuously improve the algorithm's capacity to discern the process safety incidents more accurately.

The AI data analysis results allowed assessment around the types of hazards involved, the mechanisms of release, the equipment involved and the operational or functional locations of the events. Some examples of these findings are illustrated in the PowerBI® WordCloud plots in Figures 6 and 7. The plot sizes the text descriptor according to its prevalence in the incident data.



Figure 6: Hazards involved in events from incident database.

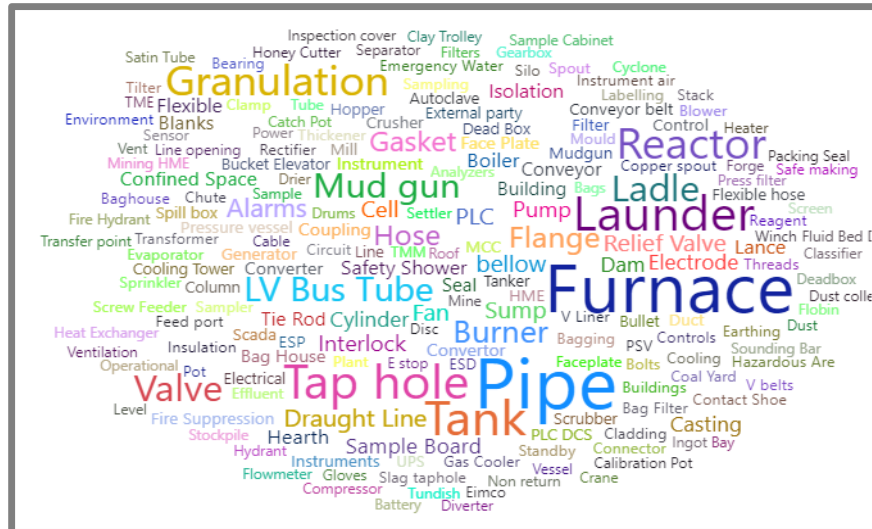


Figure 7: Equipment identified from incident database.

The WordClouds validate the intuitive perspective that the elevated temperature pyrometallurgical processes, represented through terms like molten metals, cooling water, furnace and launder, are amongst the most significant components of the mining company process safety exposure. Figure 6 gives some insight into equipment that could be considered for Process Safety Critical Equipment (PSCE) prioritisation (see below).

It is important to note that a review of historical data only served to identify realised process safety issues in the business. Given that process safety events should ideally be infrequent, this database approach does not on its own identify the full business process safety risk. It was therefore critical to integrate these findings with the baseline risk assessment results of Priority Unwanted Events.

### Proposed KPIs and PSM dashboard

The identification of preliminary KPI metrics was achieved through a review of API RP 754 and supporting documents that list typical leading indicators within mature PSM industries. The initial KPIs were selected based on the incident database in conjunction with the availability and quality of data in the company and the maturity assessment.

Table V lists the specific KPIs which were identified as applicable to smelting operations. Also presented is a brief outline of what each KPIs entails as well as the Tier classification (see Figure 3).

Valterra Platinum uses Microsoft PowerBI® as a performance reporting tool. PowerBI® is effective in accessing the established business data lake to generate the reports and performance across a variety of metrics. This allows the association of IsoMetrix® data (the software platform for storing and configuring the Safety, Health and Environment data) with other data sources such as finance records, maintenance system status, alarm records, training status etc.

PowerBI® also has powerful visualisation tools for presenting metrics which was thus employed for the KPIs developed as part of this work; the initial process safety indicator primary dashboard was generated using this functionality and a sample of the graphic interface is captured in Figure 8.

The format allows the setting of targets and the measurement of performance for those targets. The output does not directly support time-based trending, and this is covered through supporting graphical output. The intent is to trigger deeper investigation of any metrics that highlight attention. Valterra Platinum generally sets annual targets, and this dashboard would represent the current calendar reporting period year to date. Whilst long-term trends are reviewed, the immediate comparison with the previous year is generally used to both contextualise and set immediate annual targets with continuous improvement aspirations.



Table V: List of smelting KPIs

| No. | KPI                              | Tier | Description                                                                                                         |
|-----|----------------------------------|------|---------------------------------------------------------------------------------------------------------------------|
| 1   | Level 4 and 5 consequences       | 1    | Count of actual Level 4 and 5 consequence events with significant losses from the release of energy (See Table I)   |
| 2   | HPIs                             | 2    | High Potential Incidents (with potential Level 4 and 5 consequences)                                                |
| 3   | Level 3 actual                   | 3    | Count of actual incidents with Level 3 consequence                                                                  |
| 4   | Level 1 and 2 actual             | 3    | Count of actual incidents with Level 0,1 and 2 consequences                                                         |
| 5   | HPH level 4 and 5                | 3    | High potential hazards (hazards with potential levels 4 and 5 consequences)                                         |
| 6   | Critical control failures        | 3    | Number of critical control failures (controls whose failure could results in Level 4 or 5 consequences)             |
| 7   | Trip bypasses                    | 3    | Bypassed interlocks and trips from a basic control system or safety instrumented system beyond an allowable period. |
| 8   | Alarm rate                       | 3    | The number of alarms per control room operator per unit time                                                        |
| 9   | Overdue critical control actions | 4    | Overdue actions required to assure the effectiveness of critical controls                                           |
| 10  | Overdue incident actions         | 4    | Overdue actions from incident investigations                                                                        |
| 11  | Ageing MOCs > 2 years            | 4    | Changes (process, equipment, control etc.,) that remain open and incomplete for more than two years                 |

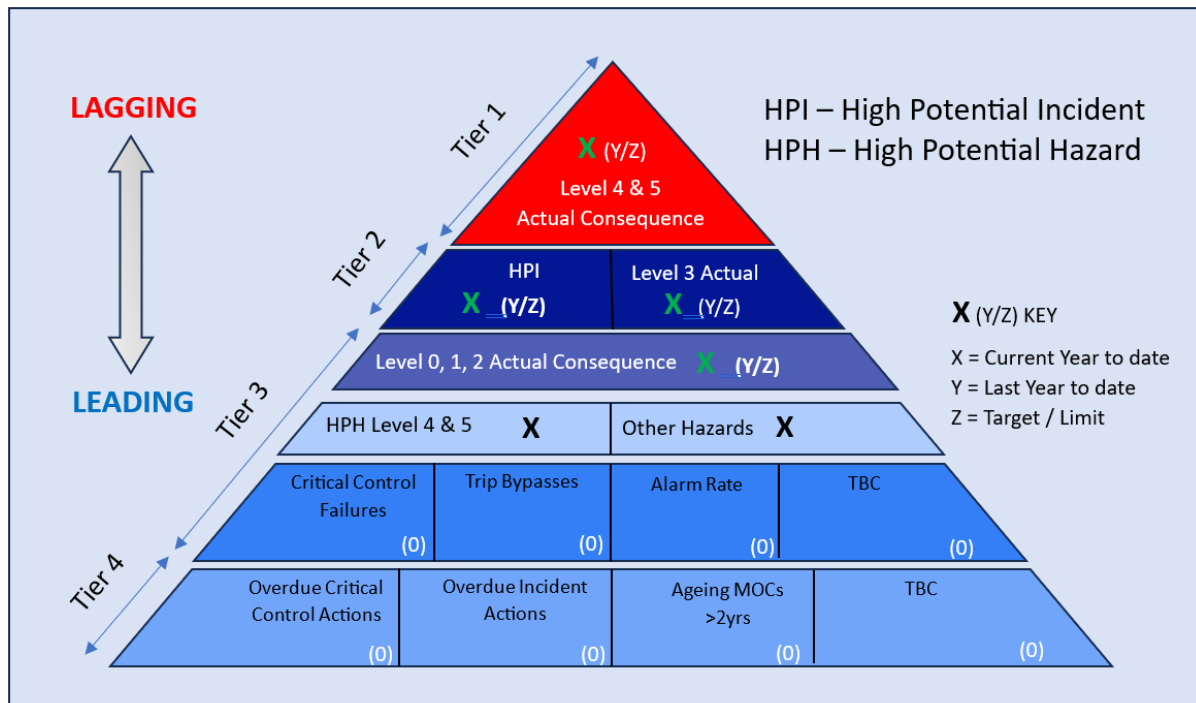


Figure 8: Valterra Platinum process safety dashboard (modified for confidentiality).



#### *Leading indicators and growth opportunities.*

Tier 1 and 2 KPIs as shown in Figure 7 were identified previously as lagging indicators whereas Tier 3 and four KPIs identified as leading indicators. This is because Tier 3 and 4 incidents (and associated KPIs), having relatively lower consequences, are seen as precursors for Tier 1 and 2 incidents (and associated KPIs), having higher consequences. It is understood that analysing and tracking Tier 1 and 2 incidents, as done in this work provides lessons that can be implemented to prevent recurrence. However, owing to the low frequency of such incidents the focus in PSM is thus shifted to Tier 3 and 4 KPIs which reflect challenges and weaknesses in the safety system. Tracking and responding to these leading indicators will therefore minimise the likelihood of the more severe Tier 1 and 2 incidents. The added advantage of responding to the low consequence KPIs is that it reduces the likelihood of having to report Tier 1 and 2 incidents as per API RP 754, for which the threshold for such reporting is considerably lower when compared to the traditional OHS levels.

The need to respond to the weak signals from these low consequence incidents has led to other PSM growth opportunities with the same effect as the higher Tier KPIs shown in Figure 7. Specific examples are PSCE maintenance measures and PSM critical task training. PSCE refers to equipment whose failure could lead to catastrophic consequences (having a ranking of 4 or 5 on the risk matrix in Table I) or whose purpose is the prevention or mitigation of catastrophic consequences. In the pyrometallurgy space such equipment could include tap-hole drilling and plugging equipment, furnace hearth cooling systems, fire safety systems, etc. On the other hand, PSM critical tasks are those operational tasks with an administrative component such as manual furnace tapping and associated tape-hole drilling, which when poorly-executed or missed will significantly compromise process safety. These opportunities have led to the development of further pyrometallurgy KPIs which are tracked as part of the KPI dashboard presented in this study.

The integrity of PSCE is managed via an 8-element process, with the identification of such equipment being the first step. Table V1 lists the 8 elements followed at Valterra Platinum, from which relevant KPIs were developed and tracked on a regular basis to ensure adequate and appropriate management.

*Table VI: PSCE management*

| No. | Element                           | Description                                                                                                                                      |
|-----|-----------------------------------|--------------------------------------------------------------------------------------------------------------------------------------------------|
| 1   | PSCE Identification               | Identification of PSCE based on consequence of failure                                                                                           |
| 2   | Quality Assurance and Control     | Use qualified resources for design, fabrication, construction of asset, following quality control plans during inspection of parts and materials |
| 3   | Asset tactics                     | Develop controls to verify the equipment always meets its performance specifications while mitigating against service degradation                |
| 4   | Training                          | All employees performing maintenance tasks on PSCE must be trained and qualified                                                                 |
| 5   | Defect Management and elimination | Develop a repair and change programme ensuring deficiencies are corrected and changes done in a safe manner for subsequent operation             |
| 6   | Reliability Engineering           | Performance and failure histories are monitored to proactively improve reliability                                                               |
| 7   | Audits                            | Auditing done to determine gaps in PSE management and how to close these                                                                         |
| 8   | Review and improvements           | Continuous improvement due to dynamic nature of process and process safety risks                                                                 |

Like Table VI above, tasks that have been determined as safety critical tasks (SCT) undergo the following analysis:

- Detailed analysis determining task frequency and duration
- Determining high-risk activities
- Developing job risk analysis for such high-risk activities

- Incorporating human factors in the analysis
- Ensuring adequate training and supervision.

Specially formulated KPIs linked to the SCTs were identified as was the case for PSCE to form part of the KPI dashboard. This work was continuing at the time of writing this paper.

## CONCLUSION

The introduction of a PSM framework required considerable integration work within established processes such as ORM. A PSM KPI report has subsequently been achieved through leveraging on existing business frameworks with established PSM industry practices. The following are particularly listed as contributions from the work.

- Re-emphasised the importance of established KPIs – as being critical to focus businesses to deliver prioritised and strategic outcomes.
- Reviewed several PSM guidelines and noted that the establishment of process safety key performance indicators is critical to the implementation of any PSM framework
- Adapted mature petrochemical PSM practices, along with other proven PSM methodologies, to the PGM smelting environment
- Integrated existing company PSM management practices, as well as revising the company's hazard inventory to determine mining industry specific PSM KPIs.

The generation of the API aligned KPI triangle allows standardised reporting for general industry learning and benchmarking. The approach further allows flexibility in the introduction of new KPI indicators as the PSM programme matures within the business.

Some of the limitations from the work done, particularly with the NLP work done as part of the assessment of the company's hazard inventory include the following:

- Low but acceptable NLP precision
- Limited operational validation of the NLP modelling results

Other shortcomings include dependence on legacy ORM categories for some of the KPIs, which was however, important for continuity and integration of PSM into the company. The developed KPI dashboard is already in use at Valterra Platinum. Improvement and growth opportunities are continuing with future work focused on KPI dashboard validation and on expansion of pyrometallurgy-specific KPIs.

The establishment of a Process Safety Key Performance Indicator dashboard has greatly enabled the business's objective of preventing catastrophic events and loss.

## ACKNOWLEDGEMENTS

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### **Mkhokheli Ndlovu**

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Dr. Mkhokheli Ndlovu is a seasoned process engineer with dual qualifications in chemical and metallurgical engineering, and over 18 years of industrial experience spanning the chemicals and minerals processing sectors. He has a strong track record in applying Process Safety principles to operational plants and executing capital projects, particularly during his decade-long tenure at Sasol Group Technology, where he contributed to plant optimization and led various project phases.

Dr. Ndlovu has successfully managed projects from concept development through feasibility, engineering design, construction, commissioning, start-up, and optimization. Notably, between 2017 and 2023, he led the design, construction, commissioning, and start-up of the first Wet Gas Sulphuric Acid (WSA) gas cleaning technology implementation in a PGM processing facility at Valterra Platinum's Polokwane Smelter, serving as Process Lead and Commissioning Manager (OHSA 16.2 Appointee).

Currently, he is a Senior Process Engineer in the Process Safety Management (PSM) department at Valterra Platinum, where he plays a key role in driving the implementation of PSM across the organization.



### **Keith Laurence Hines**

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Mr. Keith Hines is an experienced process engineer with over 25 years in the Southern African mining industry across the commodities of gold, coal, potash, diamonds, base metals and platinum group metals. He has covered a variety of technical, project and operational management roles within these commodities.

He has recently supplemented his qualifications by completing a MSc in Process Safety and Loss Prevention in support of his current role as Senior Manager for Process Safety in Valterra Platinum. Mr. Hines is focused on achieving measurable and sustainable reduction of process safety risk through strong engineering practices and discipline health.

# Demolition techniques applied to the 107 m tall smelter converter stack at Palabora Mining Company

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In June 2025, Draco Group demolished a 107 m converter stack at Palabora Mining Company after a survey confirmed severe instability. Commissioned in 1960, the stack underwent a single significant refurbishment in the late 1980s. By the time the project began, the concrete shell and internal reinforcement of the stack had deteriorated to the point where its stability could no longer be guaranteed. Survey results by Knight Piésold, a company specialising in geotechnical engineering showed the stack leaned 6° with a corresponding 600 mm deflection from the centre line. This exceeded the SANS 10160 serviceability limits by 22%.

Top-down dismantling and crane-assisted removal were assessed and rejected due to the degree of instability and restricted access to the stack. Instead, a controlled toppling strategy was developed, combining precision blasting with structural engineering measurements. More than 400 blast holes were drilled to form a failure plane. A hinge was formed using a diamond rope cut to guide the fall, and additional masonry and steel supports were installed to deal with openings that were left by the ducts entering the stack. Collapse modelling drew on more than 1000 reflectorless survey data points, while thermal drone surveys provided additional information on structural stress. A 300 m exclusion boundary was set around the site and actively monitored. Environmental controls were an integral part of the demolition plan.

The stack was brought down safely within the planned zone, landing with a deviation of less than 20 m. No damage occurred to adjacent infrastructure, and no safety incidents were recorded. Smelter operations resumed within 48 hours, following the reinstatement of the rail lines. Draco Group's achievement as a technical service company sets a new benchmark for specialist demolition across Africa. It showcases how engineering excellence, environmental care, and stakeholder collaboration can align to solve complex problems safely, responsibly, and memorably. The approach provides a clear framework for future high-risk demolitions where conventional dismantling methods are not applicable.

**Keywords:** Demolition, converter stack, controlled blasting, structural instability, environmental safeguards, precision engineering

## INTRODUCTION

Palabora Mining Company (PMC) is situated around 500 km northeast of Johannesburg in the South African province of Limpopo in a town called Phalaborwa, as shown in Figure 1.



Figure 1: Phalaborwa Locality map

The Palabora smelter commenced in 1966 and consisted of a single coal-fired reverberatory furnace (currently changed to double-sided blown type), three Peirce-Smith converters, two anode refining furnaces, a holding/scrap melting furnace and an anode casting wheel, and a sulfuric acid plant, as shown in Figure 2 (Jones and Mackey, 2015). Over the years one of the original structures of the smelter, i.e., a reinforced concrete converter stack, has been deteriorating. Refurbishment was done in the late 1980s enabling it to continue operating safely. Knight Piesold structural assessment done in 2025 revealed that the stack had reached its end of life. Draco Group was appointed to do the converter stack demolition, and this paper will detail how that process was conducted from planning to execution.

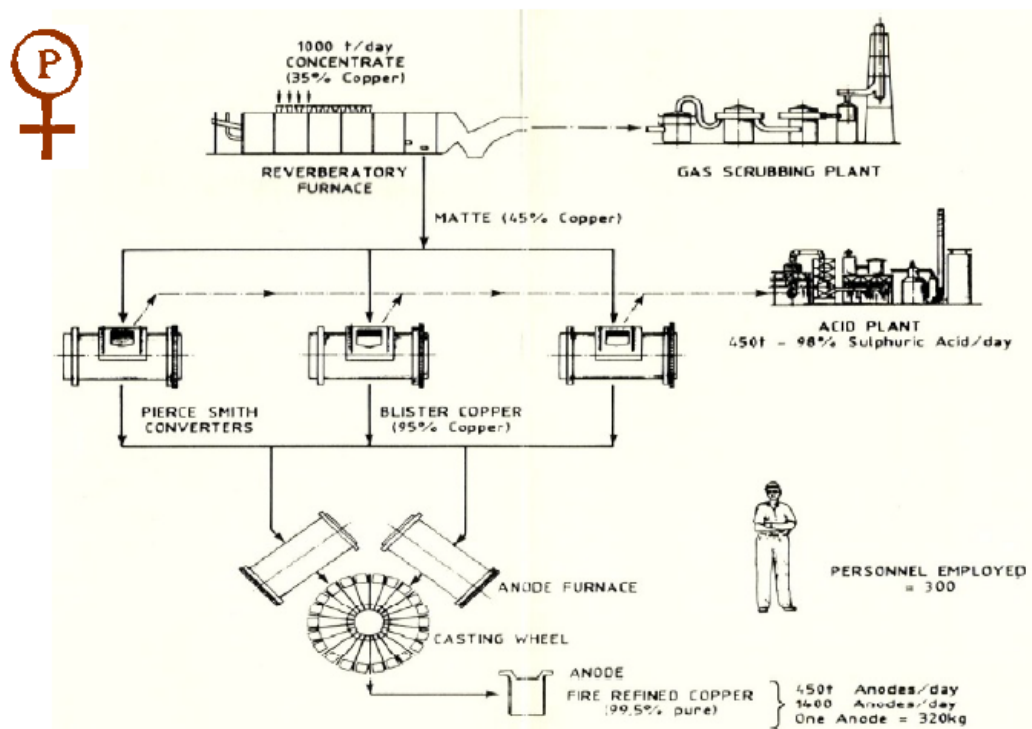


Figure 2: Overview of the Palabora smelter (Jones and Mackey, 2015)

In June 2025, Draco Group demolished a 107m converter stack at PMC after a survey confirmed severe instability. Commissioned in 1960, the stack underwent a single significant refurbishment in the late 1980s. This was the last major intervention before the stack was demolished. By the time the project began, the concrete shell and internal reinforcement of the stack had deteriorated to the point where its stability could no longer be guaranteed. Survey results by Knight Piésold, a company specialising in geotechnical engineering showed the stack leaned 6° with a corresponding 600 mm deflection from the centre line. This exceeded the SANS 10160 serviceability limits by 22%. The location of the stack within an active smelter and close proximity to the Kruger National Park meant that the geotechnical risk of the infrastructure extended beyond industrial operations to environmental protection.

Top-down dismantling and crane-assisted removal were assessed and rejected due to the degree of instability and restricted access to the stack. Instead, a controlled toppling strategy was developed, combining precision blasting with structural engineering measurements. More than 400 blast holes were drilled to form a failure plane. A hinge was formed using a diamond rope cut to guide the fall, and additional masonry and steel supports were installed to deal with openings that were left by the ducts entering the stack. Collapse modelling drew on more than 1000 reflectorless survey data points, while thermal drone surveys provided additional information on structural stress. This was done to verify that the exclusion zone was clear of people and wildlife. As the surrounding infrastructure occupied nearly the entire perimeter, only a narrow 20° corridor could be used as the fall zone. Within this strip lay three active railway lines, which were taken up before demolition and rebuilt within two days after demolition to allow production to continue. A 300 m exclusion boundary was set around the site and actively monitored. Environmental controls were an integral part of the demolition plan. They included wetting of the landing bed prior to demolition to suppress dust, careful containment and transportation of debris to a licensed disposal area, and the separation of reinforcing steel for recycling.

The stack was brought down safely within the planned zone, landing with a deviation of less than 20 m. No damage occurred to adjacent infrastructure, and no safety incidents were recorded. Smelter operations resumed within 48-hours, following the reinstatement of the rail lines. Draco Group's achievement as a technical service company sets a new benchmark for specialist demolition across Africa. It showcases how engineering excellence, environmental care, and stakeholder collaboration can align to solve complex problems safely, responsibly, and memorably. At its core, the project was about more than toppling a stack; it was about protecting lives, safeguarding critical infrastructure, and leaving a legacy of world-class execution in extremely challenging environments. The approach provides a clear framework for future high-risk demolitions where conventional dismantling methods are not applicable.

### Literature review

The first European to record the copper occurrence at Loolekop copper orebody (now mined by PMC) was Carl Mauch, while reconnaissance mapping of a portion of the Transvaal and Rhodesia was done in 1868 – 1871 (Kuschke and Tonking, 1971). The first mining operations carried out by Europeans in the vicinity of Loolekop was for phosphates in 1930 and vermiculite in 1936 (Beale, 1985; Lombard *et al.*, 1964). It was on 22 August 1956 when PMC was incorporated for the purpose of acquiring certain options and prospecting rights in the Loolekop area (Lombard *et al.*, 1964). An extensive drilling programme took place during 1957 – 1962 which confirmed the existence of over 300 million tonnes of ore amenable to opencast extraction from a pit with dimensions of 1500 m by 900 m on surface and final depth of 370 m at a finished pit slope of 45° (Steenkamp, 1979; Kuschke and Tonking, 1971). Shaft sinking started in October 1960 where, in the period of June 1961 to March 1962 a total of 1610 m of development was done on the 122 m level (Kuschke and Tonking, 1971). Results obtained from these investigations formed a reliable basis for the planning of full-scale mining, concentrating, smelting and refining operations that PMC is known for nowadays.

Jones (2017) as well as Jones and Mackey (2015) have summarised the following in relation to copper smelting in the Southern Hemisphere:

- Industrial-scale smelting began in South Africa in the 1850s or 1860s when copper was produced by the O'okiep Copper Company in cupola furnaces

- Messina Copper Mine was producing a high-grade matte by 1914
- Copper smelting started in the Zambian copper belt in 1931, with two reverberatory furnaces and two Peirce-Smith converters at Nkana
- In 1966, PMC started smelting copper in a coal-fired reverberatory furnace and three Peirce-Smith converters
- There are other copper smelters like Kansanshi – Solwezi in Zambia; Konkola Copper Mines Nchanga Smelter – Chingola, Zambia; Mopani Copper – Mufulira, Zambia; Chambishi or China Nonferrous Metal Mining Group – Zambia; Dundee Precious Metals – Tsumeb, Namibia; BCL Nickek-Copper Smelter – Selebi Phikwe, Botswana; Rubamin – Likasi, DRC and Copalco – Gemiston, South Africa.

According to Beale (1985), the purpose of the PMC smelter is to process concentrate and a comparatively small amount from other sources, together with recycles and revert material to produce these three deliverables:

- Good quality anodes at a satisfactory copper recovery,
- Sulfuric acid at a satisfactory sulfur recovery, and
- A waste slag low in copper.

Table 1 Typical figures of PMC smelter operation (Beale, 1985)

| #  | Parameter                   | Value | Units |
|----|-----------------------------|-------|-------|
| 1  | Concentrate treated         | 1030  | t/d   |
| 2  | Copper grade                | 36    | %Cu   |
| 3  | Silica flux consumed        | 165   | t/d   |
| 4  | Oxygen consumed             | 28    | t/d   |
| 5  | Coal consumed               | 280   | t/d   |
| 6  | Reverberatory slag produced | 660   | t/d   |
| 7  | Matte produced              | 805   | t/d   |
| 8  | Scrap copper charged        | 140   | t/d   |
| 9  | Converter slag produced     | 470   | t/d   |
| 10 | Anodes produced - quantity  | 445   | t/d   |
| 11 | Anodes produced - quality   | 99.5  | %Cu   |
| 12 | Smelter copper recovery     | 98    | %     |
| 13 | Sulfuric acid produced      | 355   | t/d   |
| 14 | Sulfur capture              | 75    | %     |

Typical statistics related to smelter operation at Palabora can be seen in Table I (Beale, 1985). It must be noted that these numbers did change somehow, especially after the mine moved from open pit mining to underground mining. Since the smelter operation has also changed from reverberatory furnace to double-sided blown (DSB) type furnace these numbers will also be somehow different.



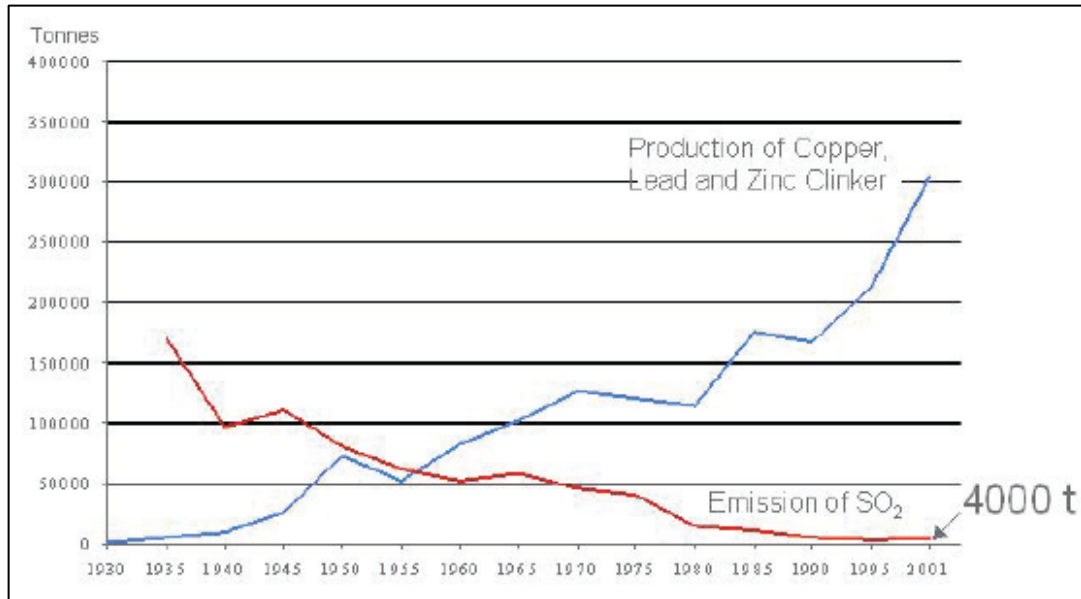


Figure 3: Production and emission history for Boliden Ronnskar smelter in Sweden (Brannstrom and Hedlund, 2013).

About a century ago in the history of copper smelters, it can be seen that concern for environmental issues was practically nonexistent; since that time only the surroundings closest to the smelters were polluted by emissions of sulfur dioxide and dust (Brannstrom and Hedlund, 2013). The early attempts to decrease the pollution in the area around the smelters involved the building of high stacks and increasing the dilution of the process gases; such high stacks made the working environment acceptable (Edlund and Lundquist, 1979). As new environmental regulations were introduced around the world, the copper industry had to initiate things like gas cleaning for dust capture, followed by sulfuric acid plants (Brannstrom and Hedlund, 2013). See Figure 3 for a good example of where production of copper/ lead /zinc increased, but due to improvements made then in the smelter operations, the emissions of SO<sub>2</sub> kept on dropping.

Edge (1979) described Palabora smelter reverberatory furnace as “possibly the most productive single reverberatory furnace operation in the world”. As technology evolved then PMC smelter had to embark on a retrofit project from 2019. This involved changing reverberatory furnace to a DSB type shown in Figure 4. Another change in the smelter operation was on the sulfuric acid plant from single contact to double contact including single absorption to double absorption. The changes done to the PMC smelter should allow it to better comply with the new emissions regulations while maintaining or improving its production parameters as listed in Table I.

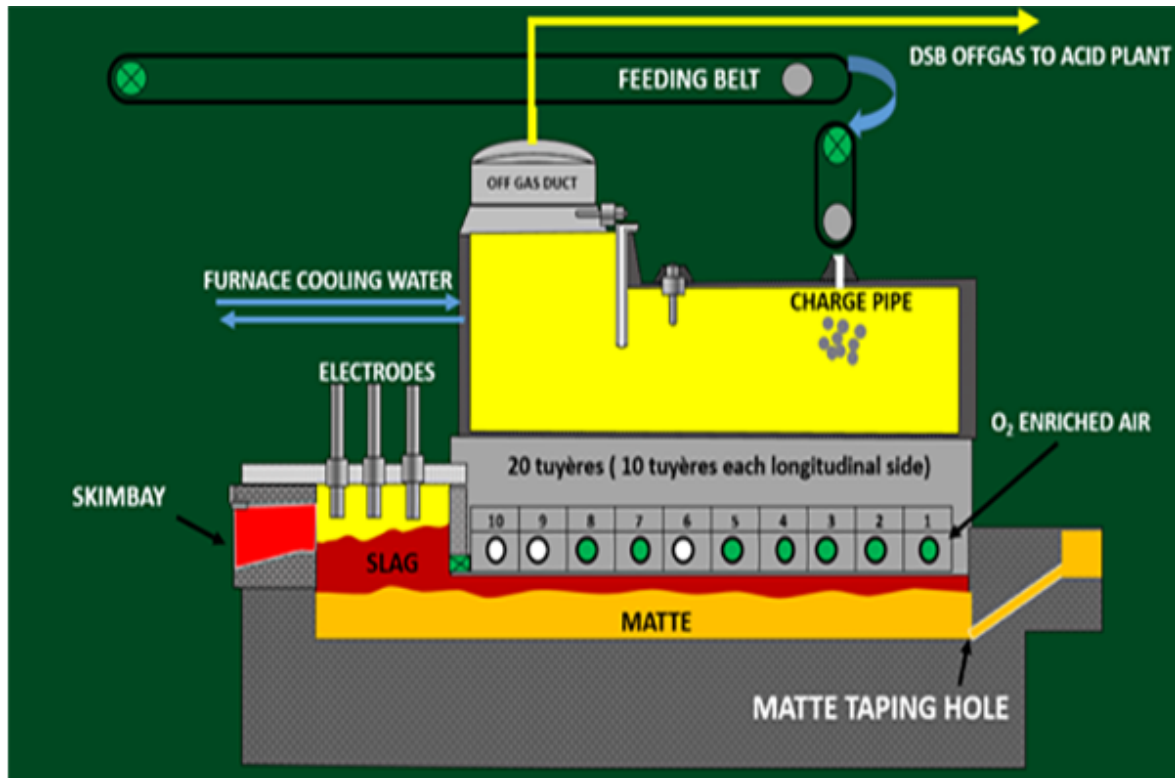


Figure 4: Layout of the PMC smelter double-sided blown furnace ([www.palabora.com](http://www.palabora.com), 2026).

Snodgrass *et al.*, (2024) addressed four critical factors for the smelters being safe, stable, capable and sustainable. Due to ageing one of the PMC smelter's original structures i.e., the converter stack became unsafe, unstable, incapable and unsustainable. When all these factors raised by Snodgrass *et al.* (2024) were in the opposite side then the option left was to go for demolition as explained in the next section.

#### Planning and Execution of Demolition

The PMC converter stack has been one of the smelter structures still standing, since the plant started operating in 1966 as can be seen in Figure 5 (Jones and Mackey, 2015). By early 2025, a structural assessment by consulting engineers Knight Piésold revealed alarming signs of instability. The structure was leaning 6° with a 600 mm off-centre deflection, exceeding national SANS 10160 serviceability limits by 22%. This raised the risk of a sudden collapse. Situated at the heart of PMC's live smelter operation – and within the boundaries of the Kruger National Park – the stack posed significant safety, environmental, and operational risks.



Figure 5: PMC smelter with three stacks: A – acid plant i.e., 75 m; B – converter i.e., 107 m and C – reverberatory i.e., 154 m (Jones and Mackey, 2015).

Asarco Smelter in El Paso, Texas embarked on an exercise to perform explosive demolition of two concrete chimneys where their plan involved factors like explosive demolition approach, potential vibration impacts, potential dust generation evaluations plus control mechanisms, security controls, public traffic controls and stakeholder engagement activities (Texas Custodial Trust, 2013). In the case of the PMC smelter converter stack demolition, the challenge for Draco Group was to bring down the unstable stack with absolute precision, avoiding damage to surrounding infrastructure and ensuring compliance with strict environmental and safety requirements. The project challenges and complexity stemmed from the four overlapping risk factors shown in Table II.

Table II: Project challenges for demolition of PMC smelter converter stack

| # | Challenge identified      | Description and effect of the challenge                                                                                                                                                                                              |
|---|---------------------------|--------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------|
| 1 | Structural instability    | The stacks lean, eccentric loading, and considerable height, combined with its location within an operational plant, rendered conventional demolition methods – such as top-down demolition or the use of scaffolding – impractical. |
| 2 | Tight spatial constraints | The smelter plant surrounded the stack on 340° of its perimeter. Only a narrow 20° corridor could be used as a safe fall zone.                                                                                                       |
| 3 | Operational requirements  | PMC required uninterrupted smelter operations. Three live railway lines located in the designated fall zone had to be dismantled pre-blast and reinstated within just 48 hours.                                                      |
| 4 | Environmental sensitivity | With the project site falling inside the Kruger National Park, compliance with environmental safeguards was paramount, including protection of wildlife and dust suppression measures.                                               |



Figure 6: Converter stack demolition in D and E during June 2025 (<https://dracogroup.co.za>, 2026).

Draco Group’s task was to engineer a solution that addressed all challenges identified while balancing innovation with tried-and-tested demolition methods. The combination engineering solution with innovation made it possible to safely demolish the PMC smelter converter stack as shown in Figure 6. The key innovations applied in the demolition of converter stack are detailed in Table III.

Table III: Draco Group’s Key Innovations Towards PMC Converter Stack Demolition

| # | Key Innovation           | Details of Innovation                                                                                                                                                                                                                                                                                |
|---|--------------------------|------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------|
| 1 | Precision blast design   | Over 400 blast holes were drilled to create a controlled failure plane. This allowed the structure to fall predictably along its intended trajectory.                                                                                                                                                |
| 2 | Diamond rope cutting     | A pre-weakened hinge line was cut into the base of the stack, guiding the toppling direction with surgical precision.                                                                                                                                                                                |
| 3 | Thermal drone technology | Drones equipped with DJI H20T thermal cameras scanned the 300 m exclusion zone for human or wildlife presence before the blast. They also provided crucial data, showing a 6°C thermal variation between the stack’s crown and base – evidence of thermal movement and additional structural stress. |
| 4 | Reinforcement bracing    | Steel and masonry supports were installed to eliminate the risk of the stack collapsing backward into the smelter plant due to a void created by previous ventilation duct removals.                                                                                                                 |
| 5 | Advanced surveying       | Reflectorless survey methods captured more than 1000 data points across the structure. This data fed into collapse modelling and was cross-validated with thermal drone scans.                                                                                                                       |

The Youtube link below reflects on how this task of demolition was executed following the key innovation in Table III.

#### Demolition of Palabora Mining Company in Phalaborwa

It must be noted that this was the first time in South Africa that a concrete converter stack of 107 m height and instability was brought down through a precision toppling method in an active smelting environment. The overall planning before demolition was executed included factors like, but not limited to: safety and risk management; environmental safeguards; stakeholder and community engagement; performance indicators related to time/cost and quality.



### Activities post-demolition

A demolition plan has to make provision that the area where the fall side will take place is prepared and cleared (McAnuff and Cry, 2007). According to Duggan and Selvey (2020) one of the clean-up criteria to be considered is the need for backfill and such might require prior approval. The PMC smelter demolition took into consideration various approaches of the activities post-demolition and those included things like:

- Dust suppression was maintained through water spraying and blast blankets.
- All demolition debris was contained, collected, and removed within 48 h to a mine landfill site 4 km away. Progress and final stages of this activity can be seen in Figure 7.
- Reinforcing steel was separated and sent for recycling.
- Post-blast monitoring confirmed no contamination of soil or water sources, and no disturbance to local wildlife.
- Collaboration with the Palabora Foundation, PMC's CSI arm, ensured local concerns were addressed and community structures protected.
- Local subcontractors were hired and trained for post-blast clean-up and materials handling, creating short-term employment and valuable upskilling opportunities.



Figure 7: Cleaning progress area after converter stack demolition where in F (cleaning ongoing) and G (cleaning completed) (<https://dracogroup.co.za>, 2026).



Figure 8: PMC smelter area after the demolition of the converter stack (<https://earth.google.com>, 2026).

The blast and clean-up were executed within the 48 hour window, allowing PMC to reinstate rail lines and resume production immediately – confirmed in Figure 8. In terms of the quality of work done, this was seen by the stack landing precisely within the designated fall zone, across the three railway lines, with less than 20 m of tolerance while resulting in no damage to surrounding plant.

## CONCLUSION

The work of demolishing the PMC smelter stack was conducted successfully with no safety concerns. This was seen as an exercise in balancing structural engineering, environmental sensitivity, and operational urgency – all underpinned by meticulous planning and innovative thinking. The key highlights of the demolition of the 107 m converter stack can be summarised as:

- The tallest converter stack of its kind in South Africa to be demolished through controlled toppling.
- The first time a structure with such severe instability was safely dropped in a dense operational industrial setting.
- A world-class demonstration of engineering precision, environmental stewardship, and safety excellence.
- Achievement sets a new benchmark for specialist demolition across Africa and the world.

## RECOMMENDATIONS AND FUTURE OUTLOOK

It is recommended that the mining industry should learn from this work on how engineering excellence, environmental care, and stakeholder collaboration can align to solve complex problems safely, responsibly, and memorably. Since the PMC smelter will be operating for many years to come, then more regular structural assessments are needed on the remaining original structures. Lessons learnt from this project are to be carried out for future similar work at PMC and can also be adopted by other mining houses.

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## **Carl Frederick Coetzee**

Section Engineer  
Palabora Mining Company

Growing up in the mining town of Phalaborwa played a significant role in shaping my interest in engineering from an early age. After completing my schooling, I pursued my passion for problem-solving and mechanical systems by enrolling at the North-West University and in 2017, I proudly graduated with a degree in Mechanical Engineering, a milestone that set the foundation for my career.

In 2018 I started my career as a graduate at Palabora Mining Company, being exposed to all the aspects of the business. During the exposure time I found great interest in the more complex construction projects. In 2022 I obtained my Government Certificate of Competency: Mines and Works Mechanical and began working at PMC Smelter as the legally appointed engineer where I continue to contribute to its operations and development.

Over the years, I have had the privilege of working on multiple projects that have tested both my technical expertise and leadership skills. The more noteworthy projects I was involved in includes the construction of a Double Side Blown furnace, waste Head boiler construction, installation of a 3.5MW Turbine Generator and the demolition of a 107m stack, all of which required careful planning, safety management, and teamwork.

I am currently closely involved in the ongoing Retrofit project at the Smelter, an ambitious project aimed at modernizing and improving efficiency in operations and reducing the emissions, heading to a more sustainable future.

My work has also taken me beyond South Africa's borders. I had the opportunity to travel to China, where I gained valuable insights into oxygen plant technology – knowledge that I have been able to bring back and apply within our local Smelter.

Engineering, to me, is not just a career but a calling. Each challenge is an opportunity to learn, innovate, and contribute to the growth of both the industry and my community. Working at Palabora Mining Company, I remain committed to applying my skills to ensure that the smelter continues to thrive while also striving for sustainable practices and zero harm.



# Three-stage approach in rehabilitation work of the smelter hyperbolic cooling tower at Palabora Mining Company

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The smelter power plant at Palabora Mining Company (PMC) was commissioned in 1964. This power plant incorporates a hyperbolic cooling tower that is essential for dissipating heat from the cooling water system. The cooling tower serves key equipment: the turbine generator condenser and the blowers. The structural integrity of the hyperbolic cooling tower is so critical to the continued operation of the smelter that a series of engineering assessments have been carried out to address its progressive deterioration.

A stability study done in 2009 formed the first rehabilitation programme that identified wind-induced stresses on the ageing shell as the main contributor to loss of structural integrity. As a result of this, stiffening rings were installed to enhance resistance against wind shear. The second rehabilitation programme happened in 2017 where further investigation revealed significant deterioration of the concrete support columns that then led to the implementation of a reinforcement design incorporating steel beams. In 2023, a comprehensive structural assessment concluded that all compromised concrete columns required replacement with steel beams. In this latest programme, rehabilitation measures entailed the enlargement of plinths to accommodate steel base plate knuckles, the reinforcement of the lower shell with 100 mm of additional concrete and steel mesh, the thickening and protective coating of the outer wall, and the repair and coating of all internal columns.

Following these interventions, the cooling tower is now fully supported by steel columns, with all weakened concrete supports removed. The three-stage rehabilitation approach programme has restored structural stability and has extended the operational lifespan of the cooling tower beyond the current life-of-mine. Scheduled routine maintenance of the cooling tower will continue, while the involvement of the external parties will be arranged at certain intervals as and when required.

**Keywords:** Hyperbolic cooling tower, rehabilitation, steel beam, knuckles, plinths

## INTRODUCTION

The smelter power plant at Palabora Mining Company (PMC) has been in operation since 1964, with the hyperbolic cooling tower serving as a pivotal component of the heat dissipation system. This tower supports essential equipment, including the turbine generator condenser and blowers, without which the operation of the smelter would be disrupted. Figure 1 shows the typical schematic of a thermal power plant, the evolution of natural draft cooling towers over years and the key components. The smelter currently in use at PMC also has components similar to those in Figure 1; these components are connected to the working of the hyperbolic cooling tower with some slight differences discussed later.

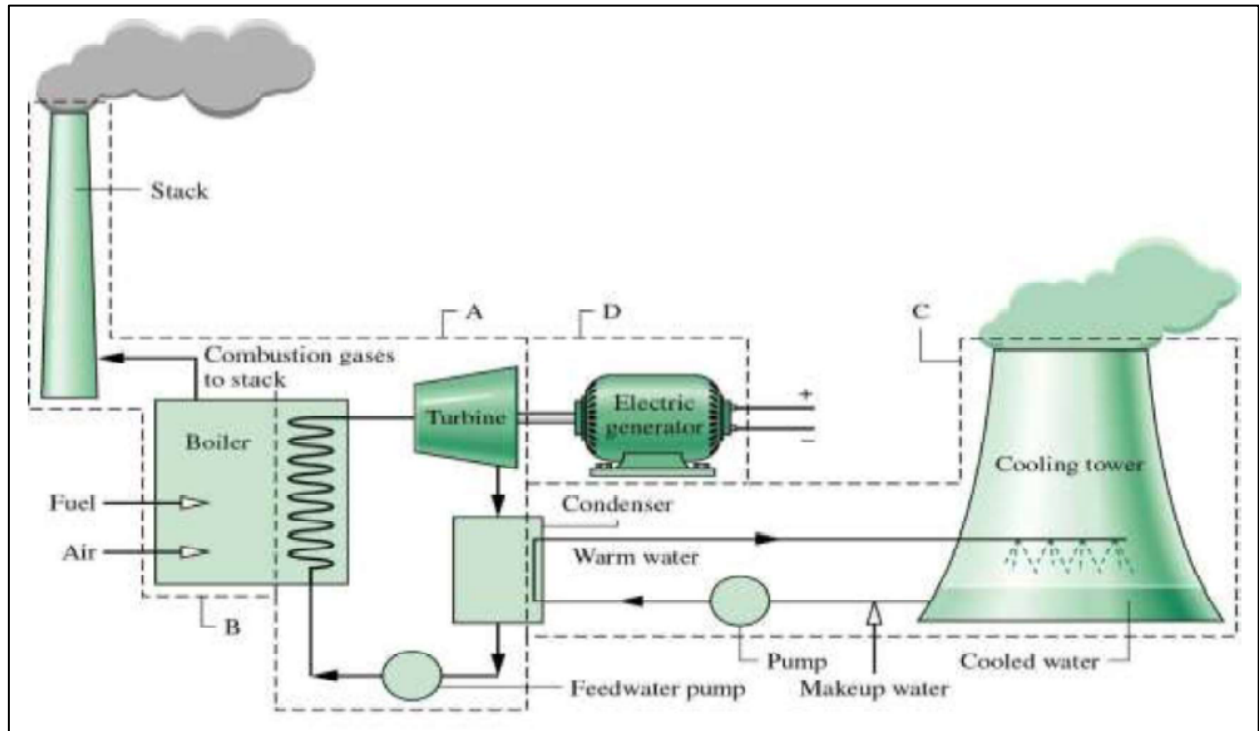


Fig. 1: Typical schematic of a thermal power plant with the cooling tower and other equipment connected to it for operation (Fulzele and Gajbhiye, 2021).

At over 60 years old, the cooling tower is one of the oldest structures of the mine. Structural maintenance over the decades has been necessary to ensure safe operation and prevent unplanned shutdowns. Given its critical role, a series of engineering assessments were conducted to evaluate the condition of the tower and develop targeted rehabilitation strategies to extend its operational life. Following the assessments, the recommendations from the evaluation reports were implemented in three stages, and this paper outlines in detail the approach to rehabilitating the cooling tower.

### Theoretical background

Natural draft cooling towers are large parabolic structures used in power stations, oil refineries and several other thermal installations. Natural draft cooling towers transfer large quantities of low-grade heat from cooling water to the surrounding atmosphere in direct contact or indirect contact arrangement (Amur and Madadnia, 2010; Amritha and Akshara, 2017). In typical thermal power plants, the water from the hyperbolic cooling tower is converted into steam. This steam then drives the turbines from which electric energy is developed. After that, the steam driving the turbines is condensed and recirculated by passing through the boilers where the hot water is again converted into a steam jet (Harish *et al.*, 2022; Asadzadeh and Alam, 2014; Kumar, 2019). At PMC, the design and working arrangement is different. Indeed, the power plant utilises the waste heat from the smelting furnace to heat up the water that is circulated through the tubes of the waste heat boiler. Steam is then generated and used to turn the turbine generator and produce electricity.

In their review of waste heat recovery technologies, Jouhara *et al.*, (2018) indicated that this type of technology adopts a Rankine thermodynamic cycle as demonstrated earlier by Nyemba *et al.*, (2017) in their research work. The adoption of waste heat recovery technologies by smelters ensure compliance with off-gas and heat emissions (Zhang and Ma, 2022). The cooling tower in such a set-up is used mainly to cool the rotating equipment and by heat exchangers to recover the condensate from the turbine as illustrated by Figure 2.

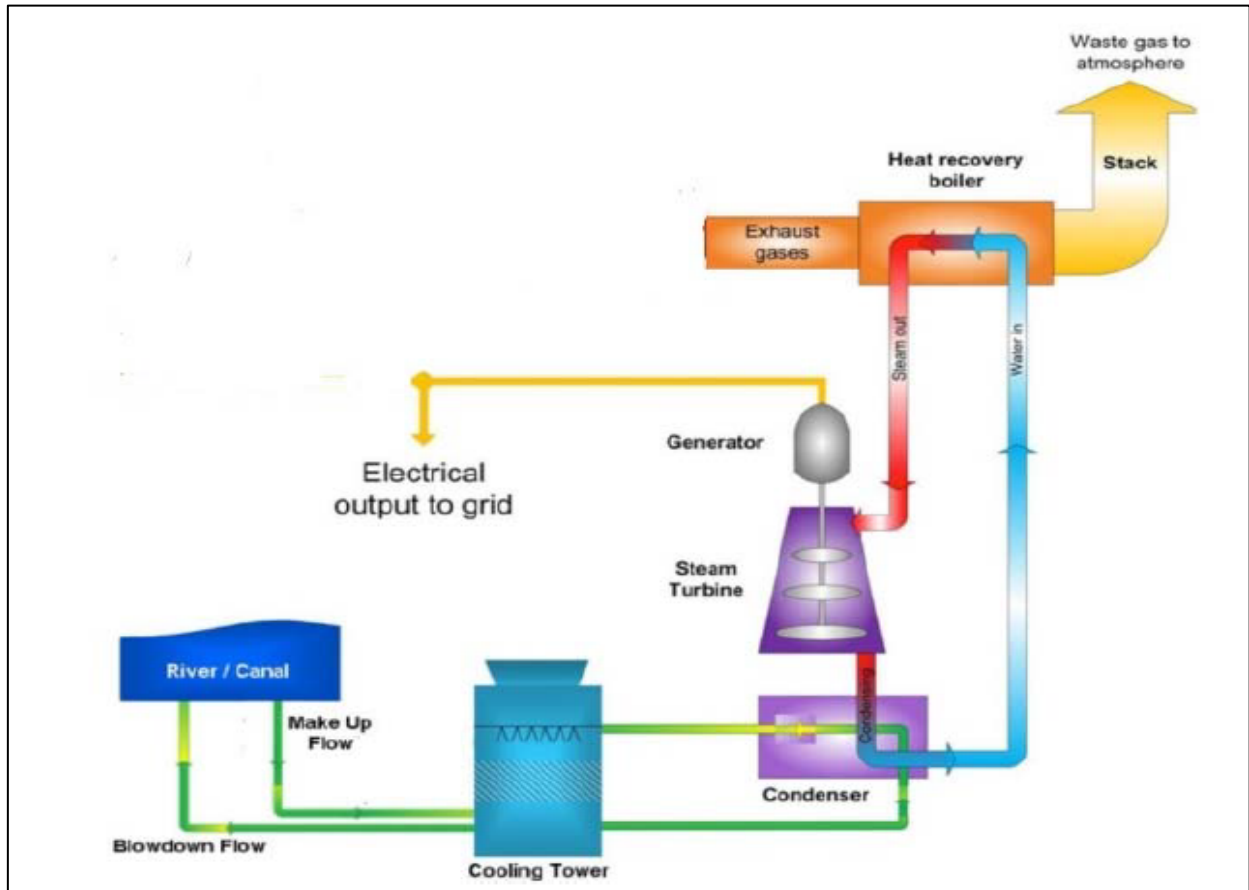


Fig. 2: Rankine cycle in a waste heat recovery plant (Nyemba et al., 2017)

Bamimore et al., (2021) have come up with classification of cooling towers based on draft, airflow, construction, shape and heat transfers as can be seen in Figure 3.

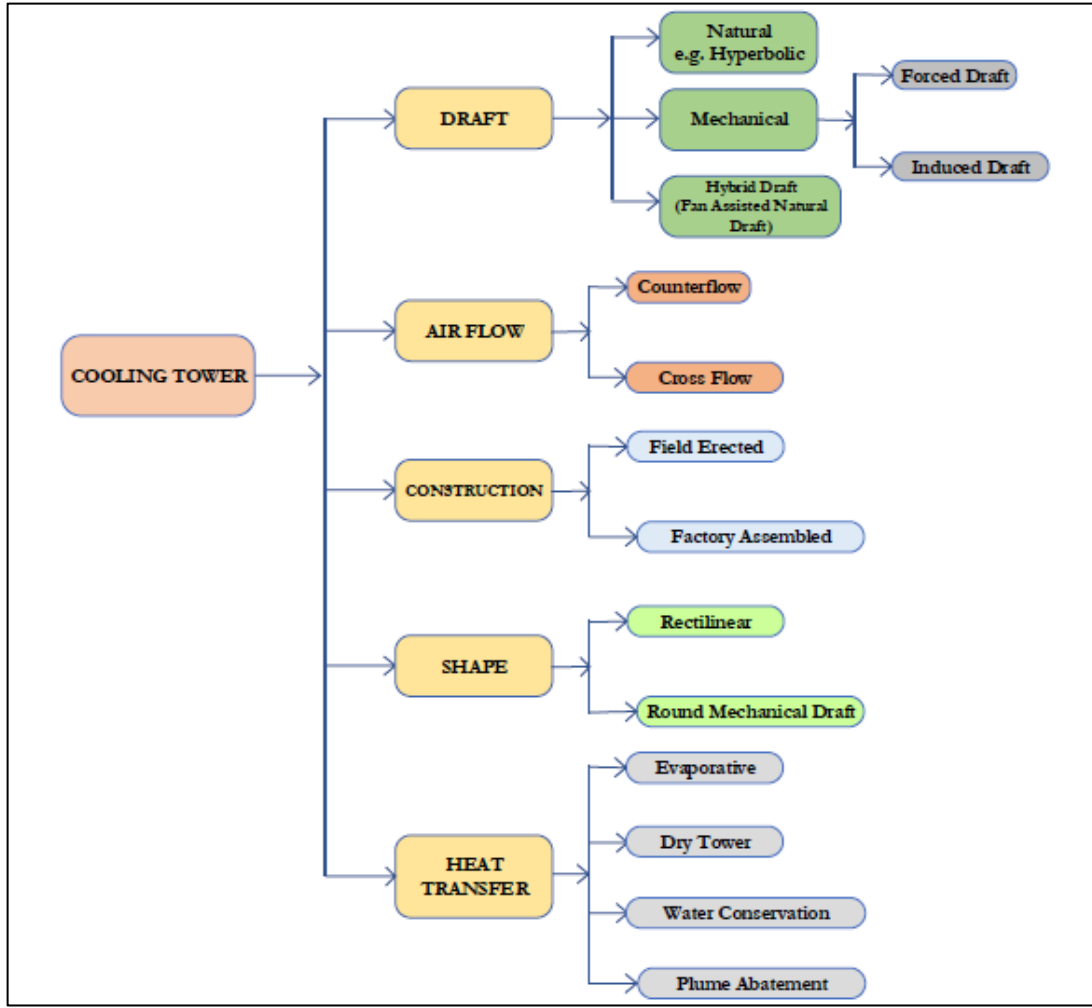


Fig. 3: Classification of cooling towers (Bamimore et al., 2021).

The design of the hyperbolic cooling can be described by Chilton equations (1) and (2) for the size selection (Chen, 1970; Chilton, 1952). The geometry of the shell is described by equation (3).

$$D = \frac{A_b^* (H)^{\frac{1}{2}}}{c^* (C)^{\frac{1}{2}}} \quad (1)$$

where:

$A_b$  is the base area of the tower measured at pond sill

$H$  is the height of the tower measured above sill level

$C$  is the efficiency factor also known as the performance coefficient.

The duty coefficient  $D$  may be worked out from the formula:

$$\frac{W_L}{D} = 90.59 * \frac{\Delta h}{\Delta T} * (\Delta t + 0.3124 * \Delta h)^{\frac{1}{2}} \quad (2)$$

where:

$\Delta h$  is the change in total heat of the air passing through the tower

$\Delta T$  is the change of temperature of the water passing through the tower

$W_L$  is the water load (in lb/h)

$\Delta t$  is the change between the dry bulb air temperature and the aspirated wet bulb air temperature.

The geometry configuration of cooling tower shell is defined by:

$$r = \Delta r + a * \left[ \sqrt{1 + \left[ \frac{(z-125)^2}{b^2} \right]} \right] \quad (3)$$

where:

$r$  is the radius of the tower shell (in m)

$z$  is the height of the tower (in m)

$a, b$  are parameters obtained experimentally during design. Published data exists for such parameters.

Fulzele and Prasad (2020) summarised the natural draft cooling tower operation by showing different parts or components found in this unit as shown in Figure 4.

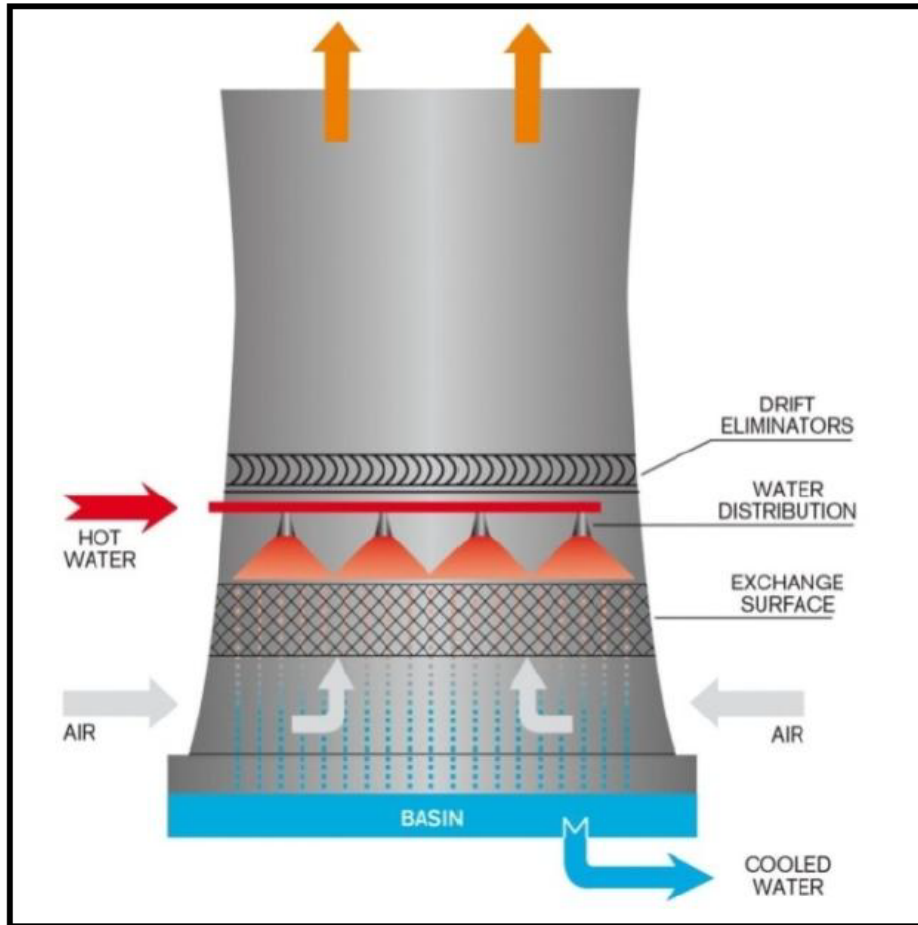


Fig. 4: Schematics of the natural draft cooling tower (Fulzele and Prasad, 2020).

The evaporative cooling process in a cooling tower can be better understood by tracing it in a psychrometric chart shown in Figure 5. Hensley (2009) explained the working of a psychrometric chart for a cooling tower as the change of a kilogram of air (dry weight) as it moves through the tower and contacts a pound of water ( $L/G = 1$ ) as denoted by the solid line. Air enters the tower at condition 1 e.g., 35°C dry bulb and 35% RH, whereupon it begins to gain moisture content and enthalpy (total heat) in an effort to reach equilibrium with water and continues this pursuit of equilibrium until it exits the tower at condition 2.

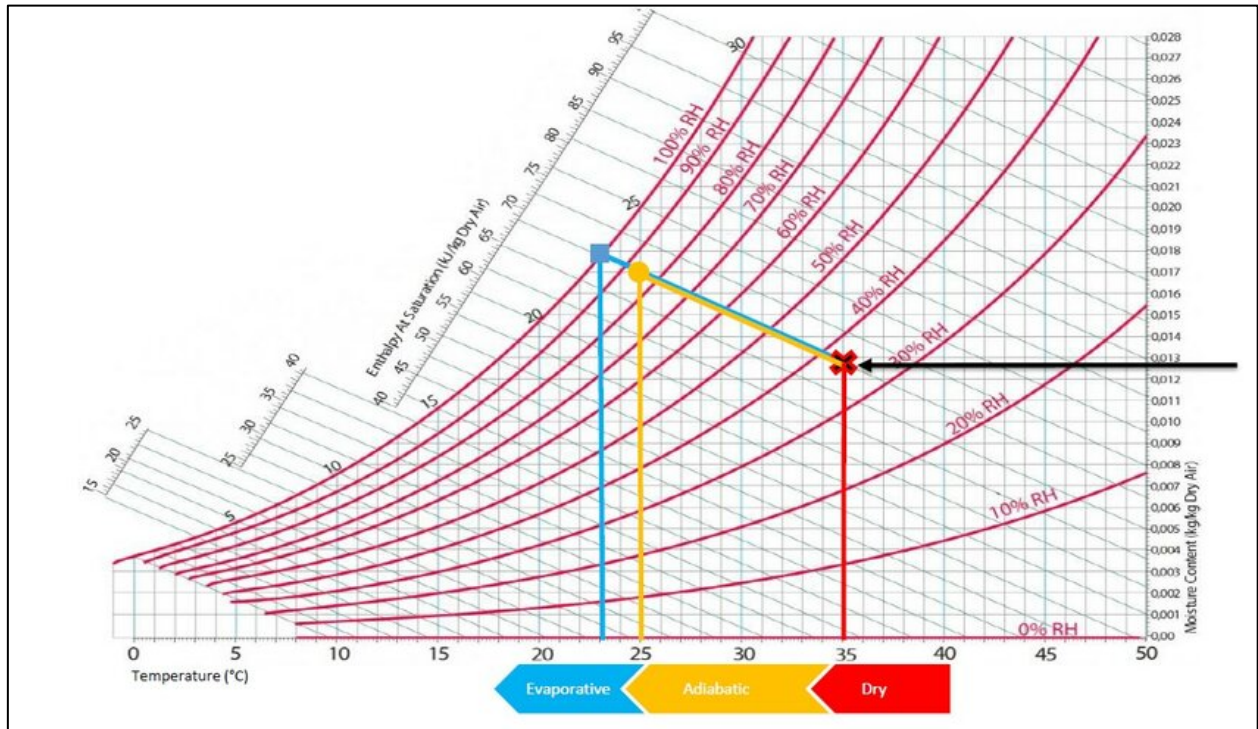


Fig. 5: Psychrometric chart showing boundaries of each cooling type (Taler et al., 2021).

There are factors that affect the performance of cooling towers which can be seen in Table I. These factors are not restricted to the type of cooling tower but are found in almost all the types. What might differ is the impact and frequency of occurrence.

Table I: Factors affecting cooling tower performance (Hensley, 2009)

| # | Factors                           | Comments                                                                                                                                                                                                                                                                      |
|---|-----------------------------------|-------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------|
| 1 | Wet-bulb temperature              | Selection of the design wet-bulb temperature must be made on the basis of conditions existing at the site proposed for a cooling tower and should be that which will result in the optimum cold water temperature at, or near, the time of peak load demand.                  |
| 2 | Dry bulb and or relative humidity | This factor is required where there is a need to know the absolute rate of evaporation at design conditions for any type of tower.                                                                                                                                            |
| 3 | Heat load                         | The appropriate selection of the cooling tower size establishes the equilibrium temperatures at which the tower will reject a given heat load, but the heat load itself is determined by the process being served.                                                            |
| 4 | Cooling range and approach        | Range is the difference between hot water temperature entering the tower and cold-water temperature leaving the tower; whereas approach is the difference between cold water temperature and entering air wet bulb temperature fixed by the size and efficiency of the tower. |
| 5 | Interference                      | This phenomenon happens wherein a portion of the saturated effluent of an upwind tower contaminates the ambient of a downwind tower. Proper cooling tower placement and orientation can minimise the effect of interference.                                                  |
| 6 | Recirculation                     | This happens when some portion of the saturated air leaving the tower is being induced back into the tower air inlets. Potentially,                                                                                                                                           |

|   |                              |                                                                                                                                                                                                                                          |
|---|------------------------------|------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------|
|   |                              | recirculation is related to wind force and wind direction and usually recirculation tends to increase as wind velocity increases.                                                                                                        |
| 7 | Tower siting and orientation | Due to restricted siting areas available in some plants, the plant owner may have to orient towers broadside to prevailing wind and to adjust his design wet-bulb temperature accordingly.                                               |
| 8 | Materials of construction    | Cooling towers may be constructed from the following: wood, metals (hardware), plastics, and concrete.<br>Each of these types of materials used for construction have different effects on the overall performance of the cooling tower. |

Hensley (2009) mentioned that the air quality at any particular site can be the cause of serious adverse effects upon both a cooling tower's longevity of service, and its ability to function thermally; this usually manifests itself in an undesirable, perhaps unexpected change in water quality. External parameters and water quality are factors on which water consumption will depend and not the type of cooling tower as may be thought (Taler *et al.*, 2021). Different researchers have agreed that cooling towers are extremely effective air washers and consequently the quality of the water being circulated over a tower quickly reflects the quality of air with which it is in intimate contact (Gupta, 1976; Kroger, 1996; Hensley, 2009; Guyer, 2018; Harish *et al.*, 2022).

Table II: Factors to consider for maintaining water quality in the cooling tower (Guyer, 2018; Hensley, 2009)

| # | What to consider            | Comments                                                                                                                                                                                                                                                                        |
|---|-----------------------------|---------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------|
| 1 | Blowdown                    | This is the method used for controlling total dissolved solids (TDS) concentrations where a portion of the circulating water flow along with its TDS burden is continuously wasted and replenished with relatively pure make up water.                                          |
| 2 | Scale prevention            | Scale-forming ingredient in cooling water is calcium carbonate, which has a solubility of 15 ppm and is formed by the decomposition of calcium bicarbonate. Treatment companies will advise on the use of selected compounds designed to keep scale-forming solids in solution. |
| 3 | Corrosion control           | The metals utilised in a cooling tower are susceptible to corrosion in varying degrees. Circulating water having corrosion characteristics beyond those anticipated in the tower's design requires treatment but blowdown may also assist to a certain extent.                  |
| 4 | Biological growth control   | Slime and algae may develop in the cooling tower and their presence can interfere with cooling efficiency. Water treatment companies may assist with proprietary compounds for the control of slime and/or algae.                                                               |
| 5 | Foaming and discolouration  | Combination of foaming and discoloured water can result in staining of adjacent structures when foam is entrained in the air stream and discharged out of the fan cylinders. Under those situations, the operation of the fans should be avoided until foaming is controlled.   |
| 6 | Control of foreign material | Suspended materials brought into the system from the air can best be removed by continuous filtration. Oils and fats should be removed from the circulating water by means of a skimmer or by eliminating the source of such contamination.                                     |

Considering the factors mentioned in Table I followed by issues related to water quality in Table II, PMC's smelter hyperbolic cooling tower had to be affected somehow. The number of years that this cooling tower has been operating also justifies the reasons why it would have been greatly affected. The three-staged rehabilitation approach done in the PMC smelter cooling tower will thus be detailed in the next chapters.



## REHABILITATION PROGRAMMES

### First rehabilitation approach

The first formal structural assessment in 2009 focused on the integrity of the cooling tower shell. It identified significant stress due to prevailing south-easterly winds, which threatened shell distortion and potential collapse.

To mitigate this risk, stiffening rings were installed around the concrete shell as shown in Figure 6A. This intervention provided additional resistance to wind-induced shear forces, restoring the structural stability of the shell and allowing continued safe operation of the cooling tower. The use of stiffening rings was supported by Song *et al.*, (2023) as best strengthening measures in steel towers (shown in Figure 6B), and they further suggested three methods i.e., equal spacing method, first order eigenvalue method and first order buckled mode shape model.

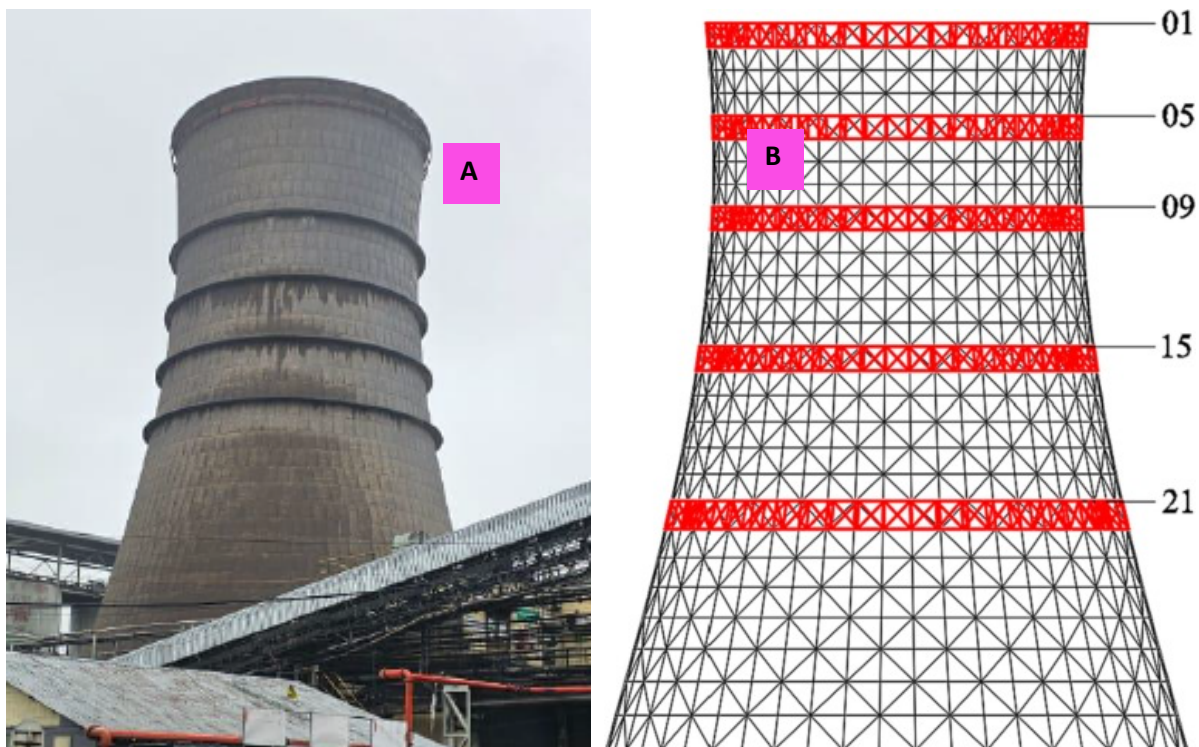


Fig. 6: (A) PMC smelter cooling tower after installation of stiffening rings and (B) effect of stiffening rings (Song *et al.*, 2023).

### Second rehabilitation approach

In 2017, a third-party stability assessment evaluated the condition of the concrete support columns, revealing considerable deterioration. A reinforcement design was developed, incorporating additional steel beams between the existing concrete columns to provide intermediate support as shown in Figure 7.



Fig. 7: Rehabilitation of the PMC smelter cooling tower involving change of concrete beams (C) to steel beams (D).

Execution of this design was completed in 2018, strengthening the tower while maintaining operational reliability. This phase ensured the tower remained safe until a comprehensive rehabilitation could be planned. Hensley (2009) has identified that although concrete and steel cooling tower structures will not burn, they can be rendered useless by significant exposure to very hot fire. This condition will be increased if their contents (like fill, drift eliminators, etc.,) are combustible; when they can be placed in a high-risk category by fire insurance underwriters.

### Third rehabilitation approach

A comprehensive structural assessment in 2023 concluded that all compromised concrete columns required replacement with steel beams to ensure long-term stability. Rehabilitation work began in January 2024 and was completed by October 2024, as shown in Figure 8. Key interventions included:

- Replacement of all internal concrete columns with steel beam columns
- Enlargement of plinths to accommodate steel knuckle bases
- Reinforcement of the lower shell skirt with 100 mm of additional concrete and embedded steel mesh
- Refurbishment of internal pond columns
- Thickening, repair, and protective coating of the outer wall
- Repair and coating of all remaining internal concrete columns



Fig. 8: Final rehabilitation involving changing all remaining concrete to steel beams (E), enlargement of plinths to accommodate steel knuckle bases (F) and refurbishment of internal pond columns (G).



Following these measures, the tower now stands fully supported on steel beams, with all weakened concrete elements removed. The recent view of the cooling tower can be seen in Figure 9.



*Fig. 9: View of the PMC smelter hyperbolic cooling tower (H) immediately after rehabilitation in 2024 and (I) from Google Earth (2026).*

### Discussion of the results from the three approaches

The phased, three-stage rehabilitation effectively addressed the critical structural vulnerabilities of the cooling tower. The first stage mitigated wind-induced stresses, the second stage reinforced deteriorating columns, and the third stage replaced all compromised concrete elements with steel supports. This approach demonstrates a strategic progression, where each stage built upon previous interventions, restoring structural integrity and extending the operational lifespan of the tower beyond the current life-of-mine. The rehabilitation also minimised operational downtime, ensuring continuity in copper production.

## CONCLUSION

The systematic three-stage rehabilitation successfully restored the structural stability of the hyperbolic cooling tower at PMC. The tower now stands fully on steel columns, with protective measures applied to all concrete elements. This case illustrates the effectiveness of phased engineering-led rehabilitation in prolonging the life of critical infrastructure. Scheduled routine maintenance, supported by periodic external assessments, will ensure continued safe operation.

## RECOMMENDATIONS AND FUTURE OUTLOOK

To sustain the integrity of the cooling tower over its extended lifespan, the following measures are recommended:

- **Routine maintenance and inspection:** Conduct regular inspections of steel beam connections, protective coatings, and reinforced lower shell.
- **Corrosion monitoring:** Implement a monitoring program to track steel performance and protective layer integrity.
- **Operational monitoring:** Consider structural health monitoring systems, including strain gauges or tilt sensors, for early detection of stress or deformation.
- **Environmental adaptation:** Periodically assess wind loads, rainfall, and other environmental stresses.

- **Material upgrades:** For future rehabilitation cycles, explore high-performance concrete or corrosion-resistant steel for enhanced durability.
- **Knowledge management:** Document lessons learned and maintain an updated rehabilitation database to guide future infrastructure projects.
- **Sustainability integration:** Evaluate potential energy efficiency improvements and integration with green technologies, ensuring alignment with broader corporate sustainability goals.
- **Water treatment:** Continue making use of the water treatment company in order to have water quality checks done professionally.

Implementing these recommendations will ensure that the cooling tower continues to operate reliably as a critical component of smelter operations, supporting safe and uninterrupted copper production beyond the life-of-mine.

## ACKNOWLEDGEMENTS

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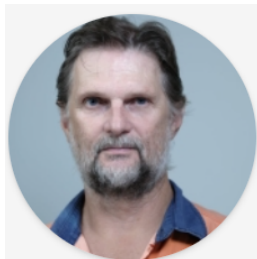
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My project management career began back with Canon SA where I got an extensive knowledge on mechanical engineering. During this tenure I obtained my Diploma in project management with Damline Business School.

To broaden my experience and knowledge I joined Siemens SA (Head office in Germany).

I spent many years in and out of Germany training in house on mechanical, electrical, and instrumentation engineering principles. I finally was included in a global pool of professionals utilised for worldwide Siemens projects and installations.

Later I joined Knight Piesold Consultants based in Phalaborwa where I got extensive experience on mostly civil projects from roads to water dams including tailings dams. I was introduced to PMC's project engineering department which I later joined. At Palabora Mining company, I have spent my last 15 years to date involved in all engineering disciplined projects of all budget sizes. In total I have in the region of 38 years of project management experience within all engineering disciplines.

# Basic operating principles of furnace tapping equipment

E. Grant

Dango and Dienenthal, South Africa

The furnace tap-hole and the related tapping activities are a key component of the successful and safe operation of a smelting operation. By taking advantage of the advancements present in the modern tapping equipment, operator safety, tap-hole lifespan, tapping consistency, and furnace availability can all be optimised. In order to make the most of the equipment available to the operation it is key for all parties involved in the operation, maintenance, and adoption of the equipment to understand both the makeup of the equipment as well as the necessary steps and procedures involved with its operation. By addressing the functionality and operation of the equipment in a 'back to basics approach', this paper aims to assist all parties in the operation and optimisation of their furnace tapping equipment, with a focus on tap-hole management and extension of tap-hole life.

## INTRODUCTION

The aim of this paper is not to give an overarching and exhaustive set of instructions on how an operation should utilise their tapping equipment but rather to address some of the basic points that have been found to be the key to the successful use of this equipment. While the specific details surrounding the usage of this equipment may vary from site to site, the intent behind and the benefit of using this equipment remains largely the same.

The usage of a furnace tapping machine has two primary drivers behind its adoption and continued usage, namely in reducing the exposure of tapping personnel to the dangers associated with furnace tapping operations, while reducing wear to the furnace components by the use of repeatable and predictable opening and closing methods.

### History of furnace tapping

From the start of early industrialisation through to the beginning of the 20<sup>th</sup> century, the furnace tap-hole was opened and closed manually. Opening was achieved by steel bars and hammers to break out the tap-hole, while closing involved pressing small portions of clay or refractory material into the tap-hole. As one can imagine, this was both a slow and damaging process, while also being dangerous for the operators on the tapping floor, exposing them all to the heat, molten materials, fumes and dust associated with furnace tapping (Dienenthal, 2014).

The first example of a claygun was invented around the turn of the century (Vaughen, 1895), and was implemented on a number of furnaces both in America and Europe within the decade. Along with the invention of the oxygen lance (Menne, 1906) around this time the tapping operation was forever changed, allowing for larger furnaces requiring more frequent taps to be developed, as well as allowing blast furnaces to be plugged while blowing, thereby increasing production.

With the adoption of pneumatic and later hydraulic drilling and hammer units the modern combination tapping machine became commonplace. This allows for a faster, safer and more repeatable tap-hole opening and closing operation than was previously achievable.

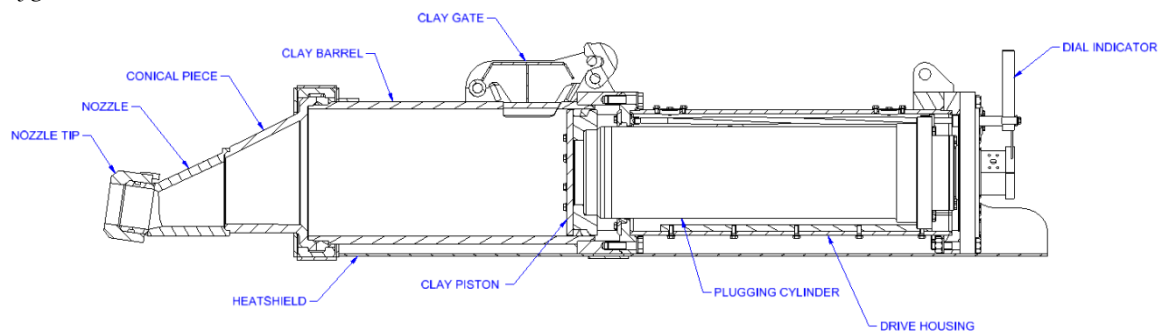
### ***Tapping Procedure***

A typical tapping operation involves the opening of a channel built into the refractory lining of the furnace to allow the molten material within to be removed for further processing, usage or in the case of the slag, to be discarded. Once the molten material has been extracted the channel is once again sealed allowing for further processing to be carried out in the furnace crucible. In order to open the tapping channel a mechanised drill is employed to drill out the contents of the channel and to allow the flow of material; in conjunction with the drill an oxygen lance is often employed to burn away or melt out this passage. To close the passage a clay mixture is forced into the tapping channel which hardens as it bakes and effectively forms a plug against the flow of molten material. To insert the clay into the tapping channel a mechanical claygun is used, which extrudes the clay under pressure into the tapping channel.

### ***Tapping Equipment***

The section below details a typical claygun and drill as might be found on a furnace's tapping floor. The specific details of an operation's tapping equipment may vary as much as the very nature of one operation from the next. However, the general layout and basic composition of the claygun and drill vary only slightly and the components listed can be found on most examples.

#### *Claygun*



*Figure 1 - Sectional view of a typical claygun.*

The claygun is used to force clay into the tap-hole under pressure, the clay displaces the molten material in the tapping channel before hardening and stopping the flow of material from the tap-hole.

The claygun is made up of the following components:

|                    |                                                                                                                                                                                                                                                                            |
|--------------------|----------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------|
| Nozzle Tip         | Consumable portion of the nozzle assembly that comes into contact with the tapface during plugging. The nozzle tip is pressed against the tap face and should form a seal between the tapface and the claygun, preventing clay from bypassing the taphole during plugging. |
| Nozzle and Conical | Connects the nozzle tip to the clay barrel.                                                                                                                                                                                                                                |
| Clay Barrel        | Houses the clay mass for plugging and contains the clay under pressure during plugging operations.                                                                                                                                                                         |
| Clay Gate          | Allows access to the clay barrel for clay loading.                                                                                                                                                                                                                         |
| Plugging Cylinder  | Hydraulic cylinder which drives the clay piston forward to force clay into the tapping channel when plugging.                                                                                                                                                              |
| Drive Housing      | Supports the plugging cylinder and shields the cylinder for excess heat during the plugging operation.                                                                                                                                                                     |



## Drill

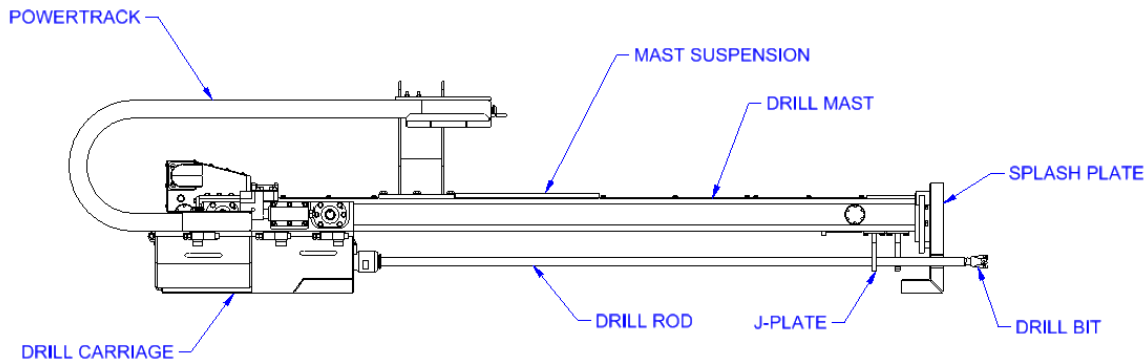


Figure 2 - Typical tap-hole drill unit.

The drill assembly is used to open the tapping channel to allow the flow of molten material from the furnace. By supporting the drilling unit from a ridged and stable manipulation frame, it is possible to set the drill up such that the drill bit and drill rod follows the same path through the tapping channel and into the crucible during each tapping cycle; thus reducing wear on the refractories surrounding the tapping channel, improving furnace performance and minimising furnace downtime.

The drill assembly is made up of the following components:

|                |                                                                                                                                                                                                                                                                                                                                                           |
|----------------|-----------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------|
| Drill Mast     | The drill mast carries the drill carriage, as well as houses the chain drive which actuates the feed forwards and backward of the drill carriage.                                                                                                                                                                                                         |
| Drill Carriage | The drill carriage contains the drill rotator unit. The carriage is fed among the mast by the hydraulic chain drive located in the drill mast. When the carriage travels forward the drill rod is fed into the tapping channel.                                                                                                                           |
| Drill Rotator  | The drill rotator provides the actual rotational energy for the drilling action. Many drill rotators are combined with an internal hammer actuator which improves the drill performance when drilling through the hardened clay.                                                                                                                          |
| Drill Rod      | The drill rod is a consumable section of the drill unit which connects the drill rotator unit to the drill tip. The drill rod is hollow, allowing for purge air to enter the drill bit aiding in waste material extraction from the drilling face. Due to heat exposure the drill rod is consumable and will need to be replaced frequently when damaged. |
| Drill Bit      | The drill bit is responsible for the actual cutting or breaking of the material within the tapping channel. These items are consumable and need to be replaced when damaged or blunt.                                                                                                                                                                     |

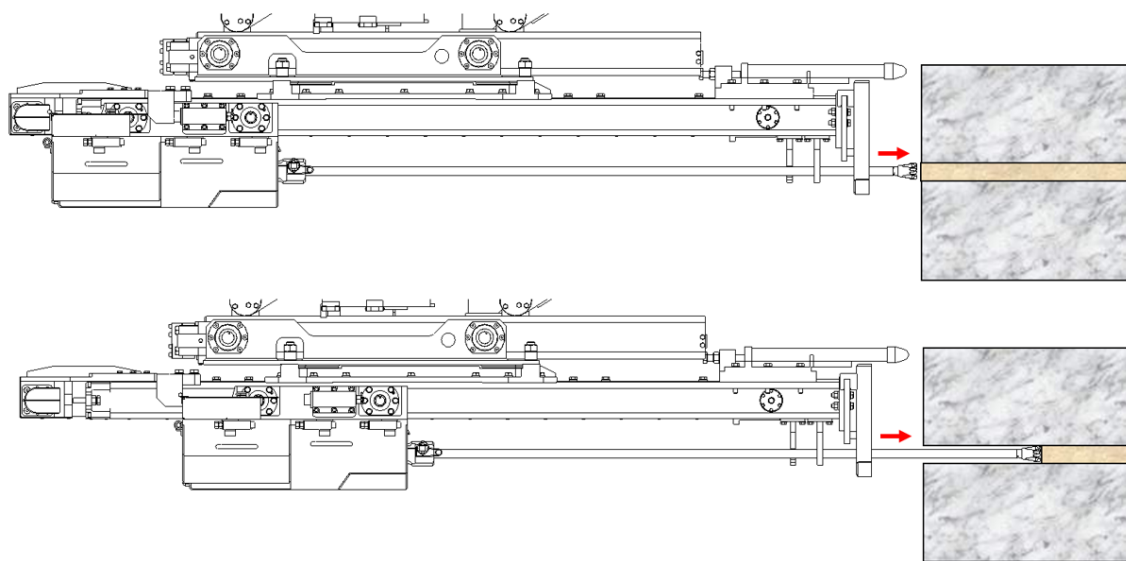


Figure 3 - Drill in operation.

### Lancing

Oxygen lancing works by reacting a hollow steel tube with oxygen gas, combusting the iron which releases a large amount of energy as heat. As the lance pipe is inserted into the tapping channel the excess heat from the combustion reaction serves to melt the contents of the tapping channel (Morales, *et al.*, 2018). This is an effective way to deal with solidified slag or metal contained in the tapping channel which cannot easily be drilled out by the taphole drill.

However, the downside of oxygen lancing is that the operator has very little control over the path that the lance follows once in the tapping channel; the long and slender lance pipe may not always track straight once in the tapping channel. This, combined with the intense amount of heat and the gas volumes generated at the lance tip, can adversely affect the lifespan of the tapping channel refractory bricks (Sutherland & Gous, 2018). This results in reduced tapping performance and increasing the frequency at which the tapping channel must be repaired, thereby increasing furnace downtime. For tapfloor layouts that do not allow the tapping operator to stand directly in front of the tap-hole, lancing at an angle does occur to the detriment of the tap-hole integrity.

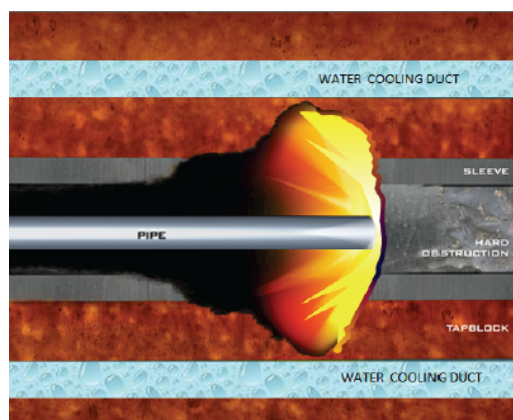


Figure 4 - Effect of lancing on tapping channel walls (Morales, *et al.*, 2018).

## BEST PRACTICES FOR OPTIMAL TAPPING PERFORMANCE

The sequence below lays out what a typical tapping operation should look like. While every furnace and business operation will have its own unique qualities and requirements, the sequence below can be taken as a guide to assist in the proper planning and operation of a typical tapping operation.

Furnace tapping operations are among the most dangerous activities carried out daily while operating a furnace. When dealing with the molten material at elevated temperatures as is present on the tapping floor predictability, reliability and repeatability are keys to ensuring safe operations can be conducted. By fully utilising modern effective tapping equipment the furnace operating team can both increase the predictability of each tapping operation while reducing the human intervention required on the tapping floor while tapping. This reduces the exposure of the tapping operator on the tapping floor as well as reducing the likelihood of an unpredicted event occurring.

### *Pre-tapping Operations*

Before any tapping operation is initiated, it is critical that the operator has full confidence in the readiness of the tapping equipment to carry the operation to completion. Proper checks and set-up at this point will drastically improve the chances of a successful and repeatable tapping cycle. It is important to keep in mind that once the tapping cycle is in motion, every effort should be taken to reduce the number of obstacles or issues that the operator may face as any distractions or equipment failures at this point may have catastrophic consequences.

### *Laundry Inspection*

The operator should carry out a visual inspection of the laundry, the tap-hole and the tapping area. First to ensure that the laundry is clean and ready for the next tapping cycle, that the tapping operation will not need to be interrupted, as well as to verify that the path of the claygun and drill is unobstructed. The laundry should be clear of any buildup in the immediate vicinity of the taphole, as this may impede the travel path of the claygun or drill, as well as potentially preventing the claygun from achieving a proper seal onto the tapface.

### *Machine Inspection*

A visual inspection of the machine should be carried out, inspecting for signs of leaks, damage or loose fasteners. If any deficiencies are noted, the correct remedies should be carried out before the next tapping cycle is initiated.

### *Verifying drill alignment*

The drill alignment should be verified to ensure that when the drill operation is initiated the drill bit will enter the tapping channel in line with the channel at the correct elevation. Proper drill alignment ensures that minimal wear is exerted on the tapping channel. A properly adjusted and set drill will repeatedly enter the tapping channel in the same place and follow the same path when drilling open – this results in a significant reduction in tap-hole wear as the tapblock is not damaged when drilling open the tapping channel, as would be the case when oxygen lancing is employed as the sole method of opening.

### *Verify Claygun Press-on and alignment*

The claygun should be manipulated into position against the tap-hole, once again checking the alignment on the claygun nozzle to the tap-hole. The nozzle should be positioned such that the tap-hole is completely covered by clay portion of the nozzle. The nozzle alignment should be set such that the nozzle forms a seal around the tap-hole against the tapface. A proper seal between the nozzle and the tapface ensures that when the plugging action is carried out the maximum amount of clay is forced into the tap-hole, a poor seal here will result in the loss of clay, reducing the injected volume as well as reducing the pressure at which the clay is injected.

### *Ensure Drill bit and rod is in good condition*

The drill rod and drill bit should be inspected for wear and tear. The performance of the drill unit as a whole is dependent on the ability of the drill tip to break through the plugged tapping channel; the drill bit should be replaced readily and regularly as it wears out. A fresh drill tip will allow for faster

drilling times and will be better able to deal with any obstructions in the tapping channel, resulting in more consistent and reliable tap-hole opening sequences.

The drill tip should be inspected to verify that the purge air holes in the drill tip are open and clear; if the purge air holes have become blocked with slag or metal the purge air will not function, resulting in the drill not operating effectively.

#### *Verify Clay volume and condition*

The operator should ensure that the claygun is filled with a sufficient volume of clay to execute a plugging operation, as well as sufficient excess to repeat the cycle in the event of an emergency.

The condition of the clay in the nozzle and conical should be inspected to verify that the clay has not cured in the nozzle. This can be carried out by activating the plugging cylinder and allowing a small portion of clay to be extruded from the nozzle. This will allow for an assessment of the clay condition in the nozzle, as well as to give the operator an accurate reading of the clay volume in the clay barrel; this action effectively primes the claygun preparing it for the next plugging operation. The operator should leave the clay piston extended and in position after priming the gun; the clay piston should not be retracted as this will result in an inaccurate reading of the clay extruded when the plugging operation is executed.

Ideally the volume in the clay barrel at this point should be sufficient to carry out two plugging operations. When a proper plug of the taphole is not achieved, and material flow from the tap-hole is not stopped, it is useful to have sufficient clay to attempt a second plug immediately after the first.

If it is necessary to add more clay to the clay barrel, this should be done in accordance with the site- and machine-specific safety procedures. When loading clay, the operator should load as much clay as possible into the barrel, before activating the clay piston and 'priming' the clay barrel. This involves driving the clay piston forward until all the air is driven out of the clay, and a small amount of clay is extruded from the nozzle of the clay gun. This process can then be repeated as necessary until the clay barrel is sufficiently full.

The operator should ensure that the clay loaded into the claygun is free from as much foreign matter as possible, including removing any plastic wrapping, cardboard packaging, sticks and any other contaminants from the clay mass before loading. The inclusion of such items in the clay mass results in a non-homogeneous mixture which may result in an inconsistent plug, reducing the claygun performance and resulting in increased tap-hole wear.

It is critical that when the last priming action is carried out, the operator should not retract the clay piston, as this will result in an inaccurate volume reading of the clay extruded during the plugging operation.

If a 'dinner plate' gasket or other type of additional nozzle seal is used on the claygun nozzle tip, this should now be installed in preparation for the plugging operation.

#### *Tapping Operation*

Once all the preparation work has been complete and the equipment standing ready for operation, the tapping process can be carried out. All the above-mentioned work can be carried out in advance, ensuring that the equipment is ready and waiting when the time comes for the furnace to be tapped.

When the furnace is ready to be tapped, and the operator is ready, a last quick visible inspection should be carried out by the operator, just to ensure that both the environment and the equipment have not changed during the elapsed period. If there are any signs that the situation has changed and the tapping cycle may be impeded, the operator should delay the start of the tapping cycle until the situation has been resolved.

Once the tap-hole drill has been activated and the tap-hole is opened, there is little room for interruptions or variations to the process. All care should be taken to reduce the number of distractions

that the tapping operators are faced with while the tap-hole is open. Proper setup and preparation are the key to a successful tapping sequence.

#### *Tap-hole opening (Drilling)*

When the operator is ready to proceed, the tap-hole drill can be activated to begin the tap-hole opening procedure. Provided that a complete clay plugging of the tap-hole has been achieved during the proceeding plugging operation the drill unit should have no difficulty in drilling through the solidified clay mass, opening the flow through the tap-hole.

A properly setup and equipped drilling unit should be able to drill through the closed tap-hole without stalling or binding. The operator should use the full capabilities of the drill for the duration of the drilling operation and should not only rely on the hammer action or purge air function once the drill is unable to advance further. Doing this will only result in increased wear on the drill tip and increase the cycle time required to open the tap-hole. Once drilling is initiated the operator should aim to complete the drilling sequence as quickly and smoothly as is possible.

When drilling it is critical that there is sufficient purge air volume flowing through the drill tip, as this will ensure that the drill waste is evacuated from the drilling face and extracted from the tapping channel. The purge air also acts to cool the drill tip, increasing the lifespan of the part and improving drilling performance.

In the event that there is slag or metal inclusions in the taphole, the drill will likely be unable to advance further down the tapping channel preventing the drill from initiating the flow of molten material from the furnace. In this case, it will only then be required that an oxygen lancing pipe is used to complete the opening sequence of the taphole.

When utilising an adequately sized and equipped tap-hole drill, coupled with an effective claygun and tap-hole plugging procedure, it should be possible to open the tap-hole to the flow of molten material without the use of excessive oxygen lancing. Oxygen lancing to open the flow of material should only be necessary when an incomplete plug is achieved, or when the furnace is suffering from upset conditions (increased freeze linings, low bath temperatures, increased liquidus temperatures, or similar process conditions affecting the properties of the molten material).

#### *Tap-hole Closure (Plugging)*

The basic principle behind the use of clay mass to close the tapping channel is that unlike the furnace slag or metal/matte that flows from the tap-hole during tapping, when cooled and solidified the tap-hole clay can readily be drilled through by mechanical means, allowing for a much less invasive tap-hole opening method to be utilised. By extruding clay under high pressure into the tap-hole and through the tapping channel, the molten material is forced back out of the tapping channel resulting in a clean and homogeneous clay plug forming in the tapping channel. This both simplifies the tap-hole opening process, as well as acting to repair and reinforce the tap-hole by effectively rebuilding the tapping channel from within.

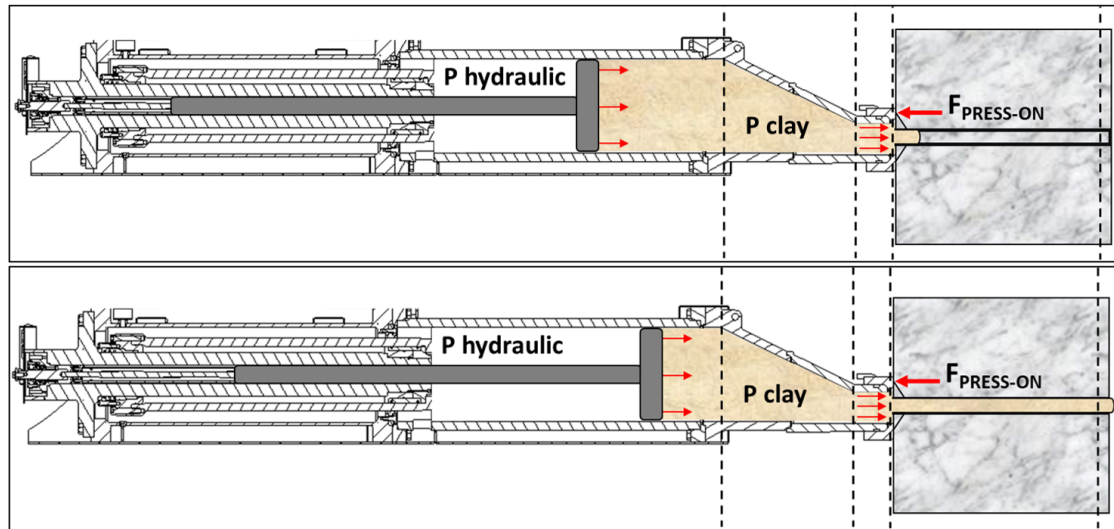


Figure 5 - Claygun plugging action.

The volume of clay extruded into the taphole should be such that it can fill the tapping channel, as well as form an excess lump on the inside of the tapping channel. This 'mushroom plug' acts to better seal the tapping channel, as well as to ensure that when drilling, the drill can pass directly from clay mass into the molten bath passing through minimal frozen product. The excess clay mass extruded into the tapping channel serves to build up the inner portion of the tapping channel where the refractory meets the bath, reducing wear to the refractory and improving tap-hole life. A good rule of thumb is to set the target volume of clay to be 15% higher than the volume required to fill the tapping channel.

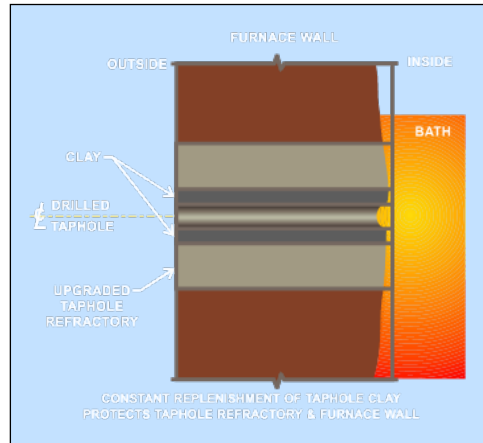


Figure 6 - Tap-hole cross section with replenished clay mass (Credit – TMT).

Once the operational setpoint has been reached and the tap-hole is to be closed, the claygun should be presented to the tap-hole as quickly and smoothly as is possible, without stopping or retracting the device whenever possible. Once the claygun nozzle is in firm contact with the tapface and 'press-on' has been achieved, clay should be injected rapidly and to the full volume required without pausing. The idea being that the clay should be injected such that a complete plug can be achieved before the initial portion of the plug is allowed to set. Should the plugging motion be stopped and attempted to be restarted the clay mass in the tap-hole would have begun to set and will prevent the clay from being injected further into the channel. Once the desired clay volume has been injected into the tapping channel, the plugging action can be stopped. At this point the operator should keep the claygun pressed against the tapface for a short period to ensure that the clay mass has sufficient time to set and harden

in the tapping channel. Once the clay has sufficiently hardened to withstand the internal pressure of the furnace material, the claygun can be retracted and returned to the parked position.

In operations where a water-cooled tapblock is employed, or the furnace is drained from a tap-hole before closing, it may be beneficial to run the drill into the tapping channel before plugging the tap-hole with clay. This assists in removing some of the frozen material from the tapping channel while the material is still hot and malleable, reducing the chance of metal or slag inclusions in the clay mass that may impact the drilling performance.

### **Post Tapping**

Once the tap-hole has been closed and the tapping machines are returned to the park position, the tapping operator can begin to once again prepare for the next tapping cycle to begin.

#### *Machine Inspection*

The machine should be inspected for damages, and any operations issue, defects or variations should be noted and recorded. Should a major defect be noted it should be corrected immediately to prevent any interruption to the following tapping cycle. The condition of the drill bit and drill rod should be inspected and replaced if necessary. The claygun clay volume should be verified and the claygun should be refilled if necessary.

#### *Logging of data*

All relevant tapping parameters should be logged after each tapping cycle. These parameters can be used by the furnace operating team to do condition monitoring of the furnace tap-holes and general health of the furnace. As well as helping to identify if the correct tapping procedures, methods and materials are being utilised for the specific furnace and furnace conditions present.

## **CONCLUSIONS**

Proper application of tapping equipment can dramatically improve furnace performance and furnace uptimes, reduce tapping times, improve maintenance cycles and increase operator safety.

The key to successful, repeatable and safe furnace tapping is not only in having the correct equipment to carry out the task but in the proper application of the tool. Without the care and dedication of the furnace operating crews a successful tapping operation would not be possible.

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I am a young designer currently employed by DDSA. I have grown up in the furnace and smelting industry in South Africa, with work exposure for Samancor Chrome. I have since worked for a project engineering company specialising in the furnace industry, and gained exposure to several smelting operations across the country.

# Advancing sustainable ironmaking: design, modelling, and TRL development of a containerised hydrogen plasma smelting reduction furnace

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The global transition toward low-carbon iron and steel production has accelerated the development of hydrogen-based reduction technologies. Hydrogen Plasma Smelting Reduction (HPSR) offers a carbon-free smelting pathway capable of treating fine, low-grade, or pre-reduced iron ores without the need for carbon reductants. This paper presents the integrated design, modelling overview, and Technology Readiness Level (TRL) advancement of a 100 kVA containerised HPSR test unit engineered to simulate near-industrial smelting conditions on a TRL 5-6 pilot scale.

The furnace is designed for continuous feeding at 18–20 kg/h, with 60–80 kg batch tapping capability, enabling realistic assessment of slag formation, melt dynamics, hydrogen utilisation, and reduction behaviour. A dynamic mass- and energy-balance simulation framework was developed to guide furnace design parameters, predict transient system responses, and assess the influence of feed distribution, pre-reduction degree, slag chemistry, and off-gas recycling strategies.

Model results demonstrate strong alignment with published experimental trends and reveal that hydrogen efficiency is highly sensitive to ore metallisation, feed location, and the use of recycled off-gas. The containerised test unit also incorporates ATEX-compliant hydrogen safety systems, automated electrode control, and flexible feed systems (hollow-electrode, peripheral arc feeding, or side-wall slag feeding).

The integrated design-modelling approach advances HPSR from laboratory-scale (TRL 4) towards bench- and pilot-scale validation (TRL 5–6). The unit will provide critical empirical data for validating reduction kinetics, quantifying hydrogen consumption, and studying scalable process behaviour under industrially-relevant operating conditions. The outcomes support the broader move toward sustainable pyrometallurgy and green steelmaking.

**Keywords:** hydrogen plasma, smelting reduction, decarbonisation, dynamic modelling, TRL development, ironmaking

## INTRODUCTION

Hydrogen-based ironmaking has emerged as one of the most promising pathways for deep decarbonisation of the steel sector, which contributes ~7% of global anthropogenic CO<sub>2</sub> emissions (Zhang, *et al.*, 2023).

Currently, the global iron and steel industry is undergoing a transition phase with a strong focus on shifting away from the traditional Blast Furnace–Basic Oxygen Furnace (BF-BOF) route, and more towards direct reduction-based routes. The direct reduction routes rely on shaft furnaces and natural gas to achieve a highly metallised direct reduced iron (DRI), allowing for a carbon emission reduction between 20-50% based on the smelting route used (Xue, *et al.*, 2024). While shaft-based hydrogen-direct reduction is progressing toward industrial adoption, Hydrogen Plasma Smelting Reduction (HPSR) offers a more flexible pyrometallurgical route capable of treating fine ores, eliminating pelletisation, and achieving rapid high-temperature reduction driven by atomic (H) and ionic hydrogen (H<sup>+</sup>) species.

Despite decades of laboratory-scale studies, the transition from batch crucible experiments to continuous, industrially relevant conditions remains limited. Major challenges include arc stability under hydrogen atmospheres, mass-transfer limitations within the slag, unknown energy transfer pathways between the arc and molten bath, and limited validation of reduction kinetics models under continuous operation (Satritama, *et al.*, 2024).

This paper aims to address the engineering design of a 100 kVA containerised, open-arc HPSE furnace capable of continuous feeding and tapping to closer mimic industrial operations. Additionally, the dynamic process model developed on thermodynamics and zonal mass transfer is developed to simulate the time-dependent nature of HPSR under various operating scenarios.

This paper synthesises design insights and modelling results to progress the TRL of HPSR technology and motivate its potential viability as a low-carbon smelting route.

## BACKGROUND

### *Hydrogen plasma smelting reduction*

HPSR relies on the formation of hydrogen plasma—comprising atomic hydrogen and ionic hydrogen—in a transferred DC arc between a graphite electrode and a molten slag bath. These species exhibit significantly higher reduction potentials than molecular hydrogen, making them capable of rapid FeO reduction under appropriate plasma conditions. At typical thermal plasma temperatures (3000–10 000 K), atomic hydrogen dominates the reactive species, while the concentration of ionised hydrogen remains relatively low, particularly near the slag surface where temperatures decrease.

Figure 1 shows the Ellingham diagram for hydrogen and various other oxides typically found within the HPSR system. The diagram effectively describes the primary reactions taking place, showing the theoretical potential for reduction between hydrogen in its various states (H<sub>2</sub>, H, H<sup>+</sup>). The reduction reactions are thermodynamically possible if the line containing hydrogen sits below the oxide line. The line which represents hydrogen plasma has been omitted from Figure 1, given that the position on the y-axis is far lower than the other oxides. Hydrogen plasma has a Gibbs free energy of -6485 kJ at 0°C and -2895.4 kJ at 5000°C (Roine, 2025). This provides context to Figure 1 that hydrogen plasma, thermodynamically, has the highest potential to reduce all oxides considered. It is important to note that as the temperature increases the reduction potential of hydrogen decreases but carbon has the opposite behaviour as its reduction potential continues to increase as temperature increases.

The formation of hydrogen plasma and the plasma composition is a function of the temperature of the plasma. The temperature is a function of the energy supplied to the system via the electrical arc. Figure 2 illustrates how the composition of the plasma varies for two plasma gases namely pure hydrogen and a 50% hydrogen-argon mixture. The use of a carrier gas, such as argon, will affect the amount of energy required to reach the atomic and plasma states as indicated in the secondary y-axis.

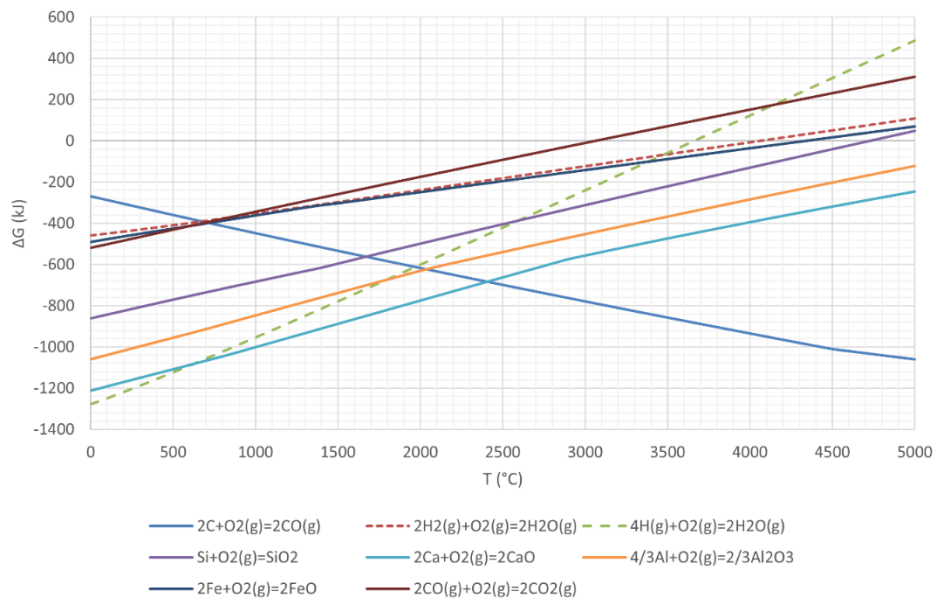


Figure 1– Ellingham diagram for various metal oxides using HSC Reactions database (Roine, 2025).

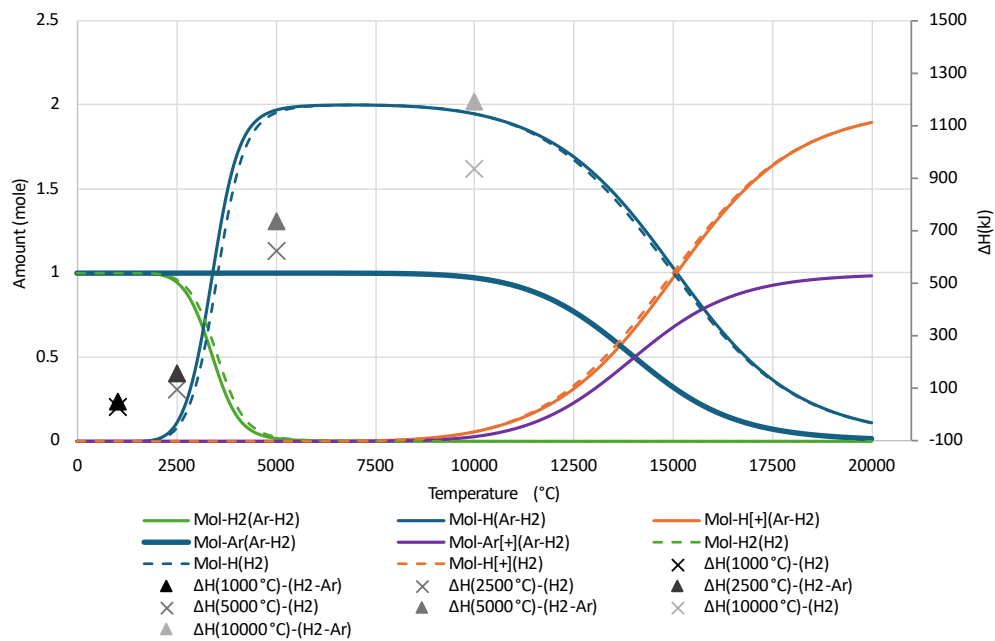


Figure 2– Molar composition of hydrogen plasma for a hydrogen only (H<sub>2</sub>) and 50% argon-hydrogen mixture (Ar-H<sub>2</sub>) as a function of temperature and the change in enthalpy plotted on the secondary y-axis. Calculated using FactSage™ 8.4 (Bale et al., 2016).

Studies by Kamiya *et al.*, (1984) proposed that the reduction kinetics of HPSR is governed by the exposure of hydrogen species to FeO-bearing slag. The work suggests that the reaction mechanism is comprised of seven distinct stages, with the rate limiting step the mass transfer of FeO to the reaction interface. It can thus be inferred that given the high reduction rates of FeO using hydrogen species, the

rate limiting step is not the introduction of hydrogen to the reaction interface. However, this may only be the case given the scale of the laboratory tests, where the reaction interface relative to the slag bath surface area is high and may become a rate limiting factor when upscaling HPSR, as the relative size of the reaction interface to the entire slag bath area decreases. This hypothesis is supported by the work of Behera *et al.*, (2019) who suggests that the rate of reduction is closely linked to the geometric properties of the bath.

Experimental studies have shown that the degree of reduction (DOR) as high as 90-100% has been achieved at laboratory scale [(Souza Filho *et al.*, 2021; Zarl *et al.*, 2022). These studies have assessed the fundamentals of HPSR considering the effects of reducing gas mixture (hydrogen-to-argon ratio)], the effect of hydrogen flowrate, and some investigation on slag properties, particularly slag basicity, and how this influences the reduction kinetics of HPSR (Ernst *et al.*, 2022; Ernst *et al.*, 2023; Naseri Seftjani *et al.*, 2020). The reduction kinetics show strong dependence on arc length and gas compositions as these properties influence the heat and mass transfer of the reaction taking place.

More recently, a batch-operated pilot plant was utilised to smelt batches of 100 kg of various feed materials (Adami *et al.*, 2026). The results indicated that a major difference between lab scale and pilot scale work was the rate of oxygen removal. The authors note that the reactor is unoptimised and that higher oxygen removal rates may be achieved. Additionally, the energy applied to the mass of material charged is significantly different, as the pilot scale required 2 kW/kg charge compared to 50 kW/kg charge at the laboratory scale. The ratio of surface area to bath height is identified as a key parameter influencing the mass transfer within the system, influencing the degree of reduction (Adami *et al.*, 2026).

To date, the work presented above focuses on the use of static crucibles, testing smaller lab-scale sizes typically (<100 g) excluding the newly published work of Adami *et al.*, (2026). These tests are mainly run in a batch type operation. The authors are of the opinion that future work should focus on the continuous operation of HPSR, more closely resembling that of an industrial process common to the iron and steel industry. The increase in scale from lab scale tests will help assess process parameters such as mass and heat transfer characteristics of the HPSR process which are not clearly seen in lab scale units used for the fundamental study of HPSR and reaction kinetics. The bench scale reactor development will provide for useful comparison to the work being done at pilot scale.

### ***Need for scalable validation***

Progressing HPSR beyond Technology Readiness Level (TRL) 4 requires the development of test platforms that capture the operational, thermal, and chemical complexity of industrial smelting systems. Laboratory-scale experiments are effective for determining kinetics, reaction mechanisms, and thermodynamic limits; however, they are less suited to investigating mass and energy transfer, process stability, and operational constraints that emerge in larger systems. It is therefore the authors' position that intermediate-scale test facilities are required to bridge this gap. Continuous operation, realistic feeding strategies, and representative melt volumes introduce important phenomena such as arc instability and transient mass transfer conditions. These factors cannot be adequately reproduced in small-scale systems but are critical for evaluating industrial feasibility.

The 100 kVA containerised test furnace was therefore conceived to enable the systematic investigation of operating phenomena that are essential for industrialisation but inaccessible at laboratory scale. Chief among these is continuous feeding, which introduces realistic thermal cycling, furnace swelling, slag surface renewal, and time-dependent FeO dissolution patterns. Alongside this, intermittent tapping of molten metal and slag is required to reproduce industrial thermal momentum, maintain a representative metal heel, and assess separation behaviour in a genuine two-phase bath.

A central set of unanswered questions relates to arc stability in hydrogen-rich atmospheres. Hydrogen arcs differ markedly from argon-based or nitrogen-based arcs initially proposed for pyrometallurgical furnaces when DC furnaces were developed, envisioning the need for hollow electrodes (Jones *et al.*, 1993; Jones *et al.*, 2011). Hydrogen arcs exhibit higher electrical resistivity, more pronounced voltage fluctuations, and elevated arc velocities (Reynolds, *et al.*, 2024). Extensive work on the modelling of

various electrical arcs for various furnaces has taken place and confirm the strong dependence of arc lengths, voltage, and current interaction based on the prevailing gasses present around the arc as well as the slag system (Reynolds and Jones, 2004; Haraldsson *et al.*, 2025). These behaviours influence heat transfer, focal-spot formation, and the stability of the plasma zone. Establishing viable voltage–current operating windows and identifying hydrogen concentration limits is therefore critical for derisking scale-up. This process is critical to understanding the power supply requirements required to make HPSR work at scale.

Similarly, feed methodology has emerged as a decisive factor in modelling and experimental observations. Feeding through a hollow electrode, into the periphery of the arc, or directly into the slag bath each result in different reduction zones, hydrogen–slag interaction times, and FeO mass-transfer pathways; since these mechanisms heavily influence reductant utilisation and specific energy requirement (SER). The SER is the furnace power input less furnace heat losses divided by the total feed or total metal produced. The effects on process parameters can only be quantified within a furnace that possesses sufficient slag depth, surface area, and melt volume to behave like a scaled-down industrial system.

Beyond performance considerations, inherent process robustness remains largely uncharacterised. Traditional electric smelting furnaces benefit from carbon as a chemical buffer; in contrast, HPSR intentionally excludes carbon, producing an ultra-high-purity iron melt, Fe wt % > 99.5 (Ernst *et al.*, 2022; Adami *et al.*, 2026), with a high liquidus temperature of 1536°C (Bale *et al.*, 2016). This may increase susceptibility to superheating, thermal instability, or arc-to-bath coupling losses under fluctuating power input. Other unknowns include refractory compatibility with high-FeO slags without the presence of a carbonaceous reductant and the long-term behaviour of electrodes under hydrogen-rich conditions.

Collectively, these uncertainties can be grouped into the high-level categories illustrated in Figure 3 which summarises the key feasibility, scalability, efficiency, safety, cost, and environmental questions that must be resolved before HPSR can be considered for industrial deployment. The 100 kVA test unit is therefore designed not only to generate metallurgical results, but to act as a structured validation platform through which these open questions can be experimentally interrogated, modelled, and progressively closed out. This systematic approach is essential to advancing HPSR toward TRL 5–6 and establishing a realistic basis for future pilot- or demonstration-scale facilities.

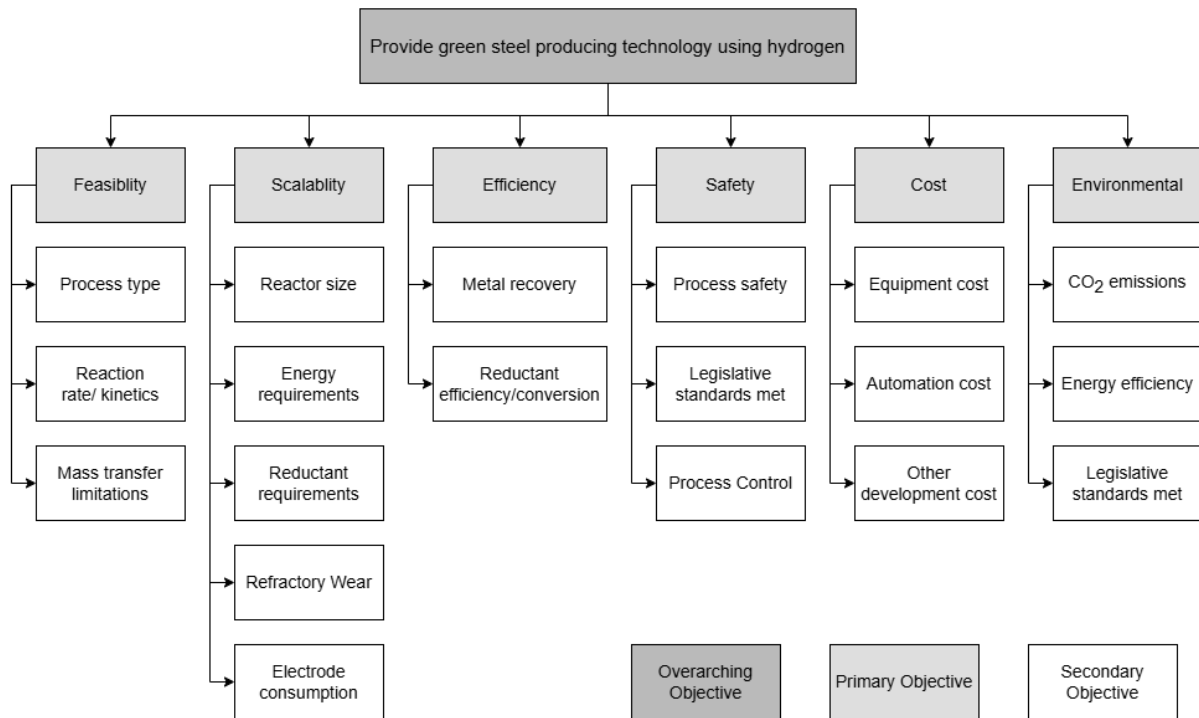


Figure 3 – Breakdown of unknowns for upscaling HPSR.

These requirements motivated the design of the 100 kVA containerised HPSR furnace. The materials and experimental techniques employed in this analysis closely resemble those outlined by (Davies, *et al.*, 2022; Kleynhans, *et al.*, 2023).

## DESIGN OF THE 100 kVA CONTAINERISED HPSR TEST UNIT

### *Overall design philosophy*

A structured and logical progression was used during the design phase of the project. The process flow is shown in Figure 4. A detailed description of the design process is given Wickham *et al.*, (2026a) and can be referred to for additional reading on the design procedure followed.



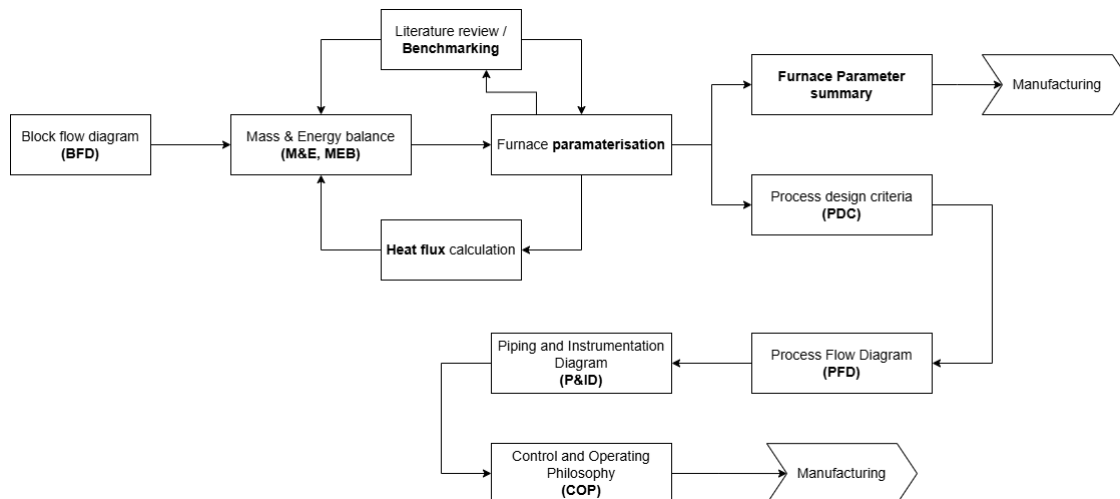


Figure 4 – Structured workflow for process design followed during design phase.

Following the detailed literature review and process design discussions in Wickham *et al.*, (2026a), the following needs were designed for as primary objectives:

- Operate at 100 kVA with variable current and voltage ranges suitable for hydrogen arcs.
- Process 18–20 kg/h of pre reduced iron ore resulting in a metal/slag tap mass of 60–80 kg.
- Support multiple feed geometries: hollow electrode, peripheral arc zone, or side wall feed.
- Operate with pure H<sub>2</sub>, H<sub>2</sub>-Ar mixtures, synthetic syngas (H<sub>2</sub>-CO), or H<sub>2</sub>-N<sub>2</sub> mixtures representing the possibility of preliminary studies of using ammonia as a hydrogen carrier.
- Integrate full hydrogen safety controls (ATEX) due to explosive gas mixtures.

#### ***Furnace vessel, refractory, and thermal design***

A cylindrical refractory-lined vessel with a single graphite electrode is used. Magnesia-chrome bricks provide resistance to FeO-rich slags, while a metal heel is maintained to stabilise thermal behaviour and improve melting efficiency. Thermal modelling predicts heat losses of ~70–75% at this scale, consistent with other 100 kVA open-arc test campaigns (Wickham *et al.*, 2026).

#### ***Gas and feeding systems***

Given the requirement to produce highly excited hydrogen molecules, it was selected to stay with the hollow-electrode configuration for gas injection directly into the arc attachment zone. The unit is designed to handle a flow of up to 1.5 kg/h hydrogen. Given the uncertainty in maximum hydrogen concentrations achievable, argon gas is available to act as a stabilising/carrier gas. Alternative carrier gasses such as nitrogen and carbon monoxide have been accounted for in process design.

The solid raw materials will be fed via screw feeders into the furnace. These allow for high accuracy, low throughput feeding of a large particle size distributions. DC furnaces are known for the capability of handling fine feed material; thus, a wide range of feed materials can be processed within the furnace. The typical feed sizing expected to be processed is +1 mm - 6 mm, however larger particles can be fed if necessary. Finer feed material is expected to improve the reduction rate, given improved heat transfer properties and improved melting kinetics exposing the FeO to the slag reaction interface. The hollow electrode feeder is designed for feed rates up to 10 kg/h, and the peripheral feeder is designed for up to 50 kg/h. The allowable feed rate is calculated via the mass and energy balance (M&E), first by defining the maximum allowable power, secondly the desired reducing gas mixture and stoichiometric requirements, and lastly the slag phase design. The slag design is based on work presented by Naseri Seftjani *et al.*, 2020 and is discussed in detail by Wickham *et al.*, 2026a. The feed rate of the pre-reduced material is calculated as a function of these three variables.

Figure 5 shows the proposed design of the furnace vessel along with the reducing gas - and solid material feed systems. Figure 6 shows the furnace vessel in greater detail, indicating the furnace roof configuration and expected metal and slag heights.

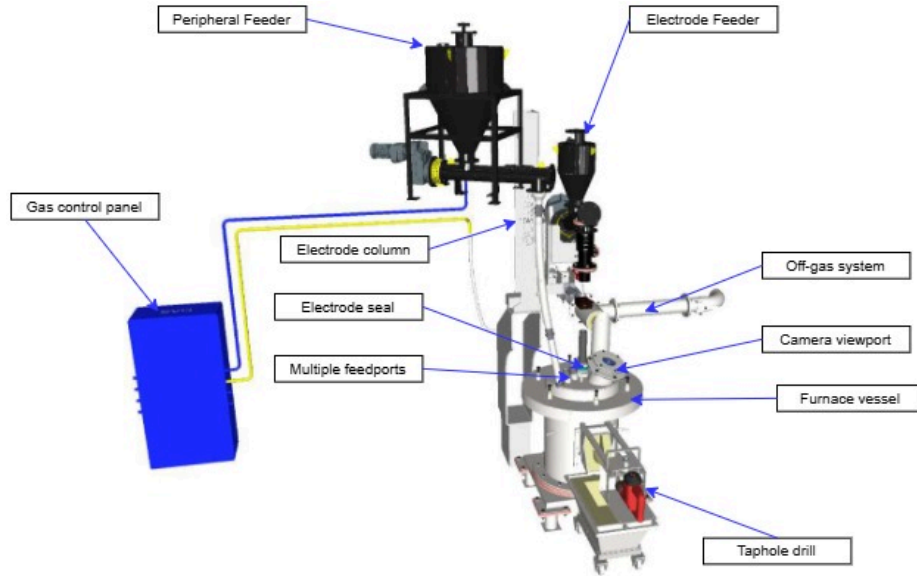


Figure 5 - 3D render of container vessel, screw feeder system, and gas feed system.

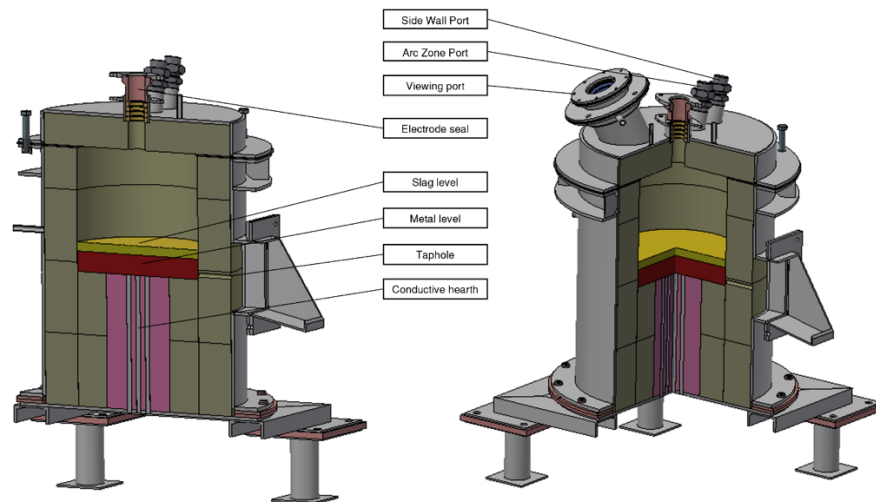


Figure 6 - 3D render of furnace vessel showing expected metal and slag heights as well as key design components.

### **Instrumentation and control**

Given the highly dynamic nature of electrical arcs, particularly those under hydrogen conditions, a specific set of features are required to ensure stable operations (Reynolds, *et al.*, 2024). First, high-speed electrode reticulation to ensure the electrical arc is maintained. Second, high-speed data logging, to ensure relevant electrical and operational data is recorded for further investigation. Thirdly, the recording and trending of furnace vessel temperatures and furnace freeboard temperatures for heat loss calculations and M&E validation. Lastly, remote PLC-based operation of the test furnace has been designed, ensuring operator safety by reducing direct exposure to high-risk areas. Additionally, hydrogen concentration sensors have been installed in various potential accumulation points to ensure

safe operation and reduce explosion risk.

### Containerising the systems

It was decided to encapsulate the system into containers to address three critical aspects, i.) transport to/from various test sites where demonstration is required, ii.) reduced operational footprint compared to elaborate workshops/testing bays, and iii.) allows for intermittent testing while results are being analysed and test plans optimised. Figure 7 shows the proposed containerised design of the 100 kVA test unit.

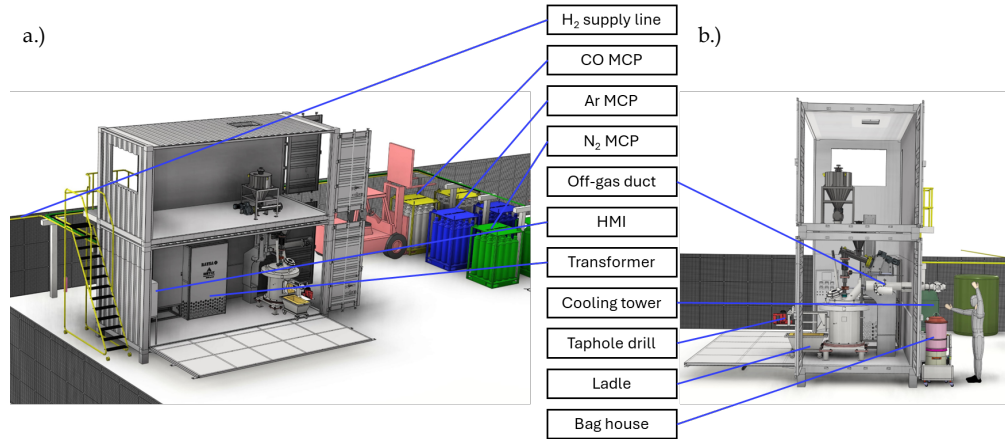


Figure 1 - Design of containerised HPSR showing primary components a.) view of container at site with connection to multiple gas cylinder packs, and b.) side view of the container system with a mannequin for scale.

### Status of test unit

In January 2026, the electrical commissioning and factory acceptance test for the test unit was completed. Here a ~60 kg batch of slag forming fluxes, metal shavings, DRI and steel shot were melted; compositions of the raw materials are summarised in Table I. A starting batch of 12 kg of shavings is added to the furnace prior to the commencement of the commissioning melt. This material is used to form a metal heel during the furnace warm-up/ramp-up phase; this mass allows for 10-15 mm of molten material in the heel of the furnace. During furnace warm-up/ramp-up the power started at 15 kW and increased every 30 minutes with 5 kW. After two hours the warm-up was completed and the refractory temperature of around 150-170°C achieved. The furnace now operates in a stable condition with a stable electrical arc and around 30 kW, hereafter the power is systematically increased until the power setpoint of 45 kW is achieved and material feeding can commence.

The hot commissioning of the reducing gas feed system was completed in March 2026 at the Hydrogen Infrastructure South Africa (HySA) in Potchefstroom, at the North-West University. The completion of the first hydrogen reduced smelting campaign is planned for May 2026.

Table I: Raw material compositions of materials used in commissioning melt. Percentages given is mass %

| Component                      | Burnt lime | Quartz | Steel shavings | DRI  | Steel shot |
|--------------------------------|------------|--------|----------------|------|------------|
| Fe                             |            |        | 99.63          | 89.8 | 97.27      |
| FeO                            | 0.29       | 0.39   |                | 5.08 |            |
| Al <sub>2</sub> O <sub>3</sub> | 0.20       | 1.06   |                | 0.36 |            |
| Na <sub>2</sub> O              | 0.15       | 0.22   |                |      |            |
| K <sub>2</sub> O               | 0.19       | 0.12   |                |      |            |
| CaO                            | 70.7       |        |                | 0.16 |            |
| MgO                            | 2.73       | 0.20   |                | 0.12 |            |

|                  |      |      |      |      |
|------------------|------|------|------|------|
| SiO <sub>2</sub> | 0.79 | 97.7 | 4.39 |      |
| MnO              | 0.77 |      |      |      |
| C                | 1.25 |      |      | 1.0  |
| Si               |      |      |      | 0.75 |
| Mn               |      | 0.13 |      | 0.90 |
| P                |      | 0.01 |      | 0.04 |
| S                |      | 0.01 |      | 0.04 |
| Cr               |      | 0.03 |      |      |
| Cu               |      |      | 0.22 |      |

### Modelling approach

A zonal dynamic model was developed using the Effective Equilibrium Reaction Zone (EERZ) methodology and thermochemical data from HSC Chemistry. This method has seen extensive use for various pyrometallurgical processes (Van Ende, 2022; Oterdoom, *et al.*, 2025). The furnace was divided into five zones as described below and illustrated in Figure 8:

1. Plasma Zone (PZ)
2. Slag/Metal Zone (SMZ)
3. Lance Zone (LZ)
4. Metal Hearth Zone (MHZ)
5. Freeboard Zone (FZ)

The model considers an industrial-scale plant with a target metal production of ~1 million tonne per annum as proposed by (Wickham, *et al.*, 2025).

The following primary assumptions are made in the model development:

1. **Feed and Pre-reduction Basis**  
The model assumes an 80% metallised DRI feed, reflecting realistic outputs from fluidised-bed direct reduction technologies and enabling integration with HPSR for improved hydrogen and energy efficiency.
2. **Furnace Operating Conditions**  
Reaction zones are assigned fixed temperature ranges, with power input adjusted to maintain these ranges. Target product temperatures are ~1600°C (metal), ~1680°C (slag), and ~1500°C (off-gas).
3. **Reduction and Gas Chemistry Assumptions**  
A stoichiometric H<sub>2</sub>/FeO ratio of 2.5 is used for all simulations. Expected hydrogen utilisation is 20–40%, and the model is calibrated to a 95% DOR (Souza Filho, *et al.*, 2021; Zarl, *et al.*, 2022).
4. **Slag and fluxing Assumptions**  
Burnt lime is added to achieve 26–30 wt% CaO, with FeO in slag assumed to fall within a practical calibration range of 10–25 wt%.
5. **Material & Energy Balance Framework**  
All reaction zones are solved simultaneously using a 1-minute time step and treated as well-mixed (CSTR-type) regions. Activity coefficients are calculated using FactSage 8.3 and adjusted to match equilibrium trends in temperature and oxygen partial pressure (Bale *et al.*, 2016).
6. **Heat Losses and Electrode Consumption**  
Total furnace heat losses are assumed to be 15–21% of power input. Electrode consumption is set at 5 kg/MWh, accounting for possible reverse water-gas shift reactions at high temperatures.
7. **Process Losses and Feeding Assumptions**  
Approximately 3% of solids are lost to off-gas entrainment. The model uses fixed feed compositions and a total simulation time aligned with industrial tap-to-tap cycles.
8. **Model Calibration and Scenario Consistency**

Mass-transfer coefficients and reaction extents are calibrated against DOR from literature (Souza Filho, *et al.*, 2021; Zarl, *et al.*, 2022) and kept constant across all scenarios. Slag foaming, refractory reactions, and air ingress are excluded from the model.

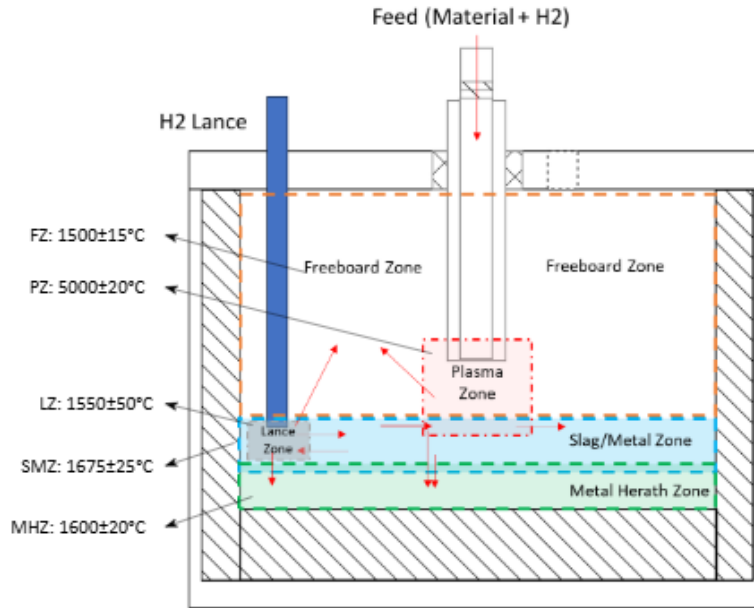


Figure 8 – Various zones used to describe the HPSR furnace with expected operating temperature ranges (Wickham *et al.*, 2026b).

The modelling done here is for the technical feasibility study and comparison of HPSR to other steel-making routes. The above assumptions outline the key modelling aspects, and further detailed model development and model results have been published by Wickham *et al.* (2026b).

#### Simulation scenarios

The model examined the effects of altering the following process variables, which are typical parameters which may be controlled/fluctuate significantly during operation:

1. Feed metallisation (70%, 80%, 90%)
2. Feed distribution (electrode vs side-wall)
3. Hydrogen lance addition
4. Off-gas recycling
5. Intermittent vs. continuous feeding.

The model is developed around the evaluation of a few specific key performance indicators (KPIs). These KPIs include the specific energy requirement (SER), hydrogen utilisation, and degree of reduction as a primary means of comparison.

## RESULTS AND DISCUSSION

#### Model results

The dynamic outputs for the sensitivity analysis are shown in Figure 9, showing the temperature trend of the various zones within the furnace, controlled during the simulation period. The fluctuations of the temperature are caused by the calculation methodology around the times that the furnace is tapped and the mass of material in the zones is near zero and the power into or out of the zones is non-zero.

Figure 10 presents extensive information on the model's sensitivity, and when interpreting the plots, particular attention should be paid to the slope of the trend lines—their gradients and directions are especially important.

### Key predicted behaviours

From Figure 10 the following trends are observed: The SER varies strongly with degree of pre-reduction. This is likely caused by the increased gas feed required for lower metallised feed material, causing the energy requirement to increase.

Feed location significantly affects hydrogen utilisation and DOR. This is caused by the decreased mass transfer of FeO into the reaction zone which is likely representative of what may be present in actual operations. Upcoming test work will be used to validate these results.

Off-gas recycling reduces fresh H<sub>2</sub> demand by up to ~85% while maintaining similar single-pass utilisation. In theory, this concept has clear benefits, however in practise it may be harder to design a system capable of handling and recycling high off-gas volumes.

Intermittent feeding increases DOR but reduces throughput.

These model insights can be used directly to inform furnace operating windows.

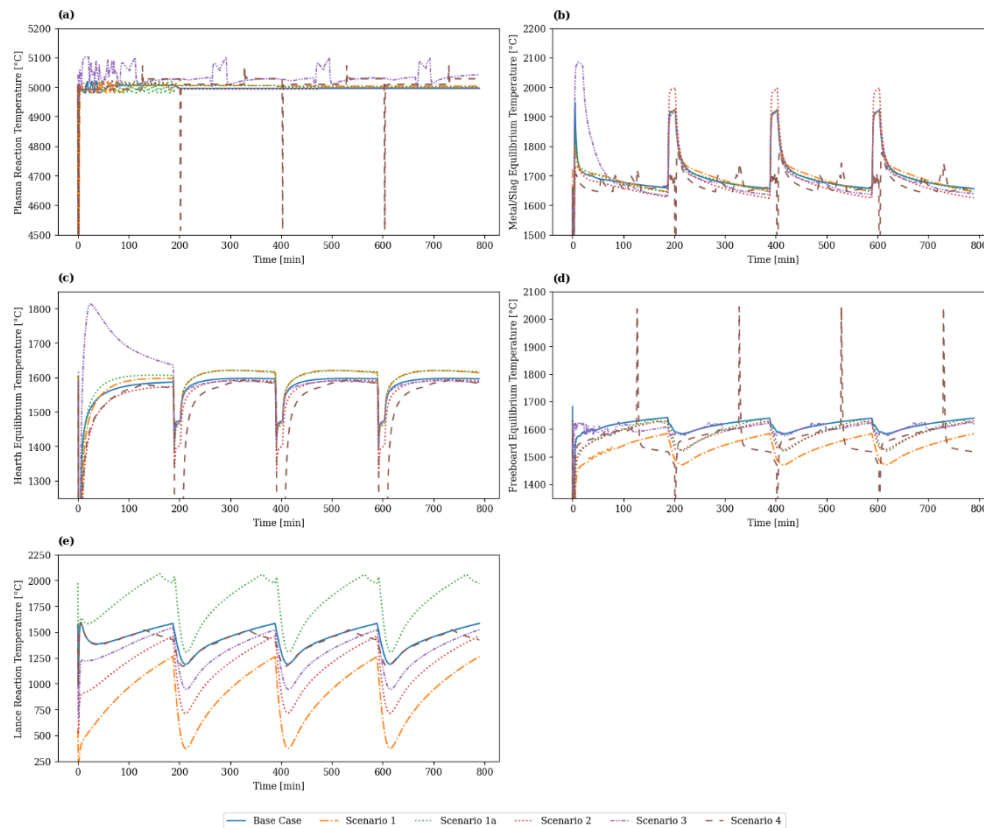


Figure 10 - Sensitivity analysis results showing effects of feed distribution, hydrogen lance addition, feed metallisation and hydrogen feed rate on various process KPIs.

### Sensitivity of hydrogen utilisation

Lower feed metallisation increases single-pass hydrogen utilisation due to higher FeO loading in the slag. Conversely, higher metallised feed reduces H<sub>2</sub> consumption but increases SER because less reduction occurs upstream.

### Influence of feed distribution

Side-wall feeding showed the following main trends: Hydrogen exposure at the arc attachment zone is decreased. This causes a lower degree of reduction to take place, given that the interaction of excited

hydrogen molecules at the reaction interface has less exposure to iron oxide. The lower reduction rate causes an increase in the FeO content in the slag. A slightly higher SER is reported for this method of feeding.

Hollow electrode feed configuration maximises the mixing of material in the slag directly in the reaction interface. This improves the hydrogen utilisation resulting in the lowest off-gas losses of unreacted hydrogen. The most notable consideration for hollow electrode feeding is the significantly increased complexity of the furnace operations. Many industrial DC smelters started with this concept and moved to arc feeding to reduce operational costs of hollow electrodes and smelter complexity (Jones *et al.*, 1993).

#### ***Role of off-gas recycling***

Recycling hydrogen-rich off-gas is the single largest lever for reducing SER and fresh hydrogen demand, consistent with industrial decarbonisation strategies.

#### ***TRL progression***

The combination of dynamic modelling and scaled furnace design allows the HPSR technology to progress from TRL 4 → TRL 5–6, enabling the continuous and reproducible operation at various process parameters. Having a test furnace as well as the framework of the dynamic model, the model can be systematically validated and calibrated under various conditions to determine the heat and mass transfer conditions that can be scaled to larger furnace sizes. The test furnace will also generate representative data for future pilot-plant design.

## **CONCLUSIONS**

1. A 100 kVA containerised HPSR unit was designed to bridge the gap between laboratory-scale experiments and industrial operation.
2. The dynamic modelling framework successfully predicts the time-dependent behaviour of HPSR under various industrially relevant scenarios. This can be calibrated to some extent using data from the 100 kVA test furnace to validate model accuracy.
3. Hydrogen utilisation is strongly influenced by feed metallisation, feed location, and off-gas recycling strategies.
4. The test unit provides a critical platform for validating reduction kinetics, quantifying SER, and understanding arc–bath interaction under the proposed continuous operation.

The integrated design–modelling approach advances HPSR technology toward pilot scale and supports global green steel initiatives.

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# Technical application of process safety in smelting: A qualitative roadmap from HAZID to inherently safer design

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Smelting operations in the pyrometallurgical industry involve high-energy processes, driven by temperatures often exceeding 1000°C, that promote melting and chemical reaction, producing separable molten phases into which elements selectively partition. These conditions make smelting inherently high-hazard, necessitating a rigorous and systematic risk analysis framework that supports both the safe design of new installations and the reliable operation of existing facilities. For existing facilities, a phased implementation pathway is proposed, linking risk-tolerance targets, leading barrier-health indicators and human-factors optimisation of high-risk interfaces such as tapping. Despite these demands, implementing a comprehensive process safety management programme in smelting is often constrained by reliance on legacy occupational health and safety systems that are oriented towards personal-safety outcomes rather than the control of complex, high-temperature major-accident hazards. This paper applies process safety management principles to smelting operations and presents a qualitative toolkit, including smelter-specific hazard identification (HAZID) to define credible low-frequency, high-consequence scenarios and their initiating mechanisms; bowtie analysis to structure controls and interrogate their performance; and inherently safer design to reduce major-accident hazard potential at source. This toolkit drew on a typical smelting flowsheet with a broad hazard envelope based on a focused review of smelter technology. The analysis revealed three concentrated risk clusters: steam explosions from molten material – water contact (the most frequent severe outcome), molten material runouts driven by refractory failure, and flammable furnace atmospheres arising from mismanaged firing and electrode breaks. Bowtie mapping indicated a heavy dependence (60%) on administrative barriers. Mechanical integrity failures of cooling elements, refractories, and off-gas components dominated the initiating and escalation paths. Water, prevalent in cooling and quench systems, was the principal escalation vector. Risk is concentrated at transitional interfaces (tapping, molten material handling, feed changes, and start-up) within tightly coupled operating envelopes that narrow the window for stable control. Residual risk identified through HAZID informed the risk reduction themes. Priority was given to removing or minimising hazards where practicable (via inherently safer design); and where elimination was not feasible, preventive and mitigative measures were defined and strengthened.

## INTRODUCTION

Smelting is a high-temperature pyrometallurgical process used to extract metals from ores. Ubiquitous across mining value chains, these operations are found in both ferrous and non-ferrous industries, including the production of steel, ferroalloys, copper, nickel, cobalt, lithium and rare earths. This places smelting as centrally critical not only to major industrial supply chains, but also to the clean-energy transition (Jones, 2013; Degel, *et al.*, 2015; Azevedo, *et al.*, 2022). High-temperature smelting combines large inventories of molten metal, matte, and slag with water-cooled elements within smelter crucibles and auxiliaries. Notable and inherent hazards include uncontrolled loss of containment of molten material and molten-water interaction, which can trigger violent steam explosions.

These events are fundamentally phase-change driven: intense heat transfer can superheat water and initiate a rapid phase transition, and the associated liquid-to-steam volumetric expansion ( $\approx 1:1700$ ) is sufficient to generate severe mechanical overpressure and blast waves comparable to high explosives (Schubach & Bagster, 1996).

Despite the severity of these process-specific risks, management practices in many operations continue to rely predominantly on conventional occupational health and safety (OHS) frameworks, which were originally built to manage high-frequency, low-consequence occupational hazards, and are therefore not designed to control the complex thermochemical and thermofluid dynamics that drive rare, high-consequence events in modern pyrometallurgical systems. Despite this, OHS practices have matured over the years, with legislation in many jurisdictions. However, despite this maturity in mining, the application of process safety management (PSM) – an alternative framework focused on low-frequency, high-consequence process hazards – remains underdeveloped in smelting operations (Ariawan, 2024; Pieterse, 2022). Key challenges in organisations dominated by legacy OHS systems include:

- Reactive organisational culture: operational norms are shaped by long-standing routines, and risk controls tend to develop only after incidents rather than through systematic hazard identification and mitigation (Karthikeyan, 2013; Shariff, *et al.*, 2016).
- Process safety information gaps: technical documentation is incomplete, inconsistent or outdated, limiting hazard understanding; in parallel, intellectual property and licensing arrangements can restrict access to design and operating data needed for risk control (Valiyev, *et al.*, 2025).
- Ageing infrastructure: progressive degradation, obsolescence, and life-extension operation can erode margins and increase the likelihood of loss-of-containment and other major failures.
- Weak engineered safeguards: reliance on procedures and operator expertise instead of engineered safeguards, leading to dependence on administrative controls - an approach vulnerable to human error. Pieterse (2022) emphasises that improving process safety requires moving beyond administrative controls and integrating robust technical safeguards into operations.

PSM is a systematic and disciplined framework for ensuring the integrity of hazardous processes by identifying, understanding, and controlling the risks associated with chemicals, energy and major-hazard industrial operations. It includes the prevention, preparedness, mitigation, response and recovery from catastrophic releases of hazardous materials or energy (CCPS, 2010; EI, 2022). In this paper, a qualitative process safety management toolkit is presented to support the identification and control of major-accident hazards in smelting operations.

## BACKGROUND

This section presents PSM principles on which the risk management toolkit used in this work and as applied to smelting operations is based. These are:

- PSM frameworks and their integration into operations
- Tools for risk analysis and
- Inherent safety design.

The section ends with a high-level conceptual flowsheet developed to support the hazard identification (HAZID) and subsequent risk analyses conducted in the study.

### Integration of PSM into Operations

PSM frameworks guiding implementation include the Centre for Chemical Process Safety (CCPS) Risk-Based Process Safety Framework (CCPS, 2010) and the Energy Institute (EI) Framework for Process Safety Management (EI, 2022). Both are centred around (i) Leadership Commitment and Culture; (ii) Risk Identification and Assessment; (iii) Risk Management and Control, and (iv) Learning from Incidents and Improvement. Figure 1 shows the CCPS Risk Based Process Safety Framework highlighting the four key areas as well as the associated 20 elements.

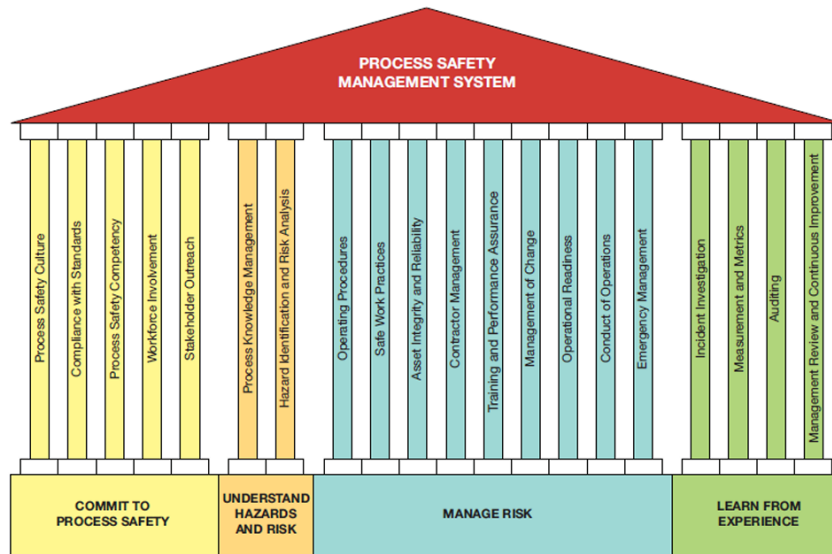


Figure 1: The CCPS's risk-based process safety framework (CCPS, 2010).

Eleven of the 20 elements fall under hazard identification and risk analysis, with nine elements under risk management. This makes it clear that hazard identification and risk analysis are key to PSM integration, as they shape leadership behaviour by informing strategic safety priorities; facilitate safety system development by revealing hazard exposure; guide development of targeted engineering and administrative controls; and allow the calibration of residual risk when considering control effectiveness, informing reliability criteria for critical barriers. Therefore, risk analysis is a key foundational step for integrating PSM into smelting operations.

### Tools for Risk Analysis

A key distinction between OHS and PSM lies in the tolerance of 'unlikely' events. In many smelting facilities lacking PSM, the safety culture remains reactive, focusing primarily on high-frequency personal injuries while overlooking rare but catastrophic process risks. This vulnerability is often aggravated by incomplete engineering documentation and a lack of quality integrity data. While traditional OHS frameworks may deprioritise low-probability scenarios, a PSM approach introduces structured risk analysis tools—ranging from qualitative to quantitative—designed specifically to identify these 'unlikely' hazards and establish the controls necessary to prevent them. Table I presents examples of the three categories of risk analysis methodologies in PSM.

Table I: Risk tools and their feasibility within this study

| Type              | Representative tools                                                                                                                                                                            | Application focus                                                                     | Feasibility of tool under study constraints                                                         |
|-------------------|-------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------|---------------------------------------------------------------------------------------|-----------------------------------------------------------------------------------------------------|
| Qualitative       | Hazard Identification (HAZID), What-if, Hazard and Operability (HAZOP), Failure mode and effect analysis (FMEA), Bowtie (AIChE, n.d.; OSHA, 2000).                                              | Identifies 'unlikely' high-consequence scenarios based on expert judgement.           | Yes, given the low information threshold.                                                           |
| Semi-quantitative | LOPA (CCPS, 2001).                                                                                                                                                                              | Ranks risk and barrier reliability using order-of-magnitude estimates.                | No, given the data-burden requirement.                                                              |
| Quantitative      | Quantitative Risk Assessment (QRA), Fault Tree Analysis (FTA), Event Tree Analysis (ETA), Monte Carlo, consequence modelling (Freeman, 1990; Xu, <i>et al.</i> , 2023; Arendt & Lorenzo, 2010). | Calculates absolute risk levels using statistical probability and physical modelling. | No, given the data burden and accrued uncertainty in analysis, producing misleading prioritisation. |

Prior (2020) showed that for copper smelters, HAZID remains effective under low data-burden conditions, while HAZOP is valuable for classification, yet strongly inductive and therefore data-quality dependent. Broader literature advocates for HAZID and bowtie for PSM integration with Inherently Safer Design (ISD), positioned for long-term risk reduction (Valiyev, *et al.*, 2025; Siddiqui, *et al.*, 2014).

HAZID deductively positions risk, while bowtie translates priority scenarios into clear threat-barrier-consequence models, making barrier dependencies and control effectiveness visually apparent. Residual risk from HAZID should then be seen from an ISD perspective for long-term integration plans.

### Inherently Safer Design

Kletz (1978) indicated that the most effective way to manage risk is to eliminate hazards where possible. This has been developed into four ISD principles: substitute, minimise, moderate and simplify (Hendershot, 2012). PSM frameworks recognise risk analysis methods as a means of controlling risk through passive controls (physical barriers that do not require activation to provide protection), active controls (instrumented or interlocked systems that detect and respond), and procedural safeguards. However, inherent safety operates at the level of process and equipment design. Therefore, risk analysis can provide an avenue for implementing control-centred risk management, whereas ISD considerations often operate at the design level to fundamentally reduce risk. Risk analysis guides ISD by highlighting design gaps based on residual risk, often yielding long-term implementation strategies, whereas control analysis applies controls to defined cause-and-effect scenarios, yielding short- to medium-term implementation strategies. Table II develops the ISD strategy for the smelter implementation roadmap.

Table II: ISD strategy (Hendershot, 2012; CCPS, 2008; Santo, *et al.*, 2023)

| ISD Principle | Technical Description                                              | Risk Analysis gap addressed                                                                                                                   |
|---------------|--------------------------------------------------------------------|-----------------------------------------------------------------------------------------------------------------------------------------------|
| Substitute    | Replace with safer alternatives.                                   | Addresses ageing, outdated infrastructure with state-of-the-art solutions, reducing dependence on safety systems and mitigating obsolescence. |
| Minimise      | Reduce hazardous material/equipment size.                          | Reduces release potential, removes unnecessary equipment, and simplifies logistics.                                                           |
| Moderate      | Use less hazardous conditions (dilution, refrigeration, inerting). | Mitigates severe conditions; derates high-pressure units; reduces dependence on emergency controls.                                           |
| Simplify      | Make operations error-resistant.                                   | Reduces procedural reliance, targets obsolescence, and simplifies maintenance.                                                                |

### Smelter Nest Flowsheet for HAZID and ISD

Typical smelter flowsheets include ferrous and non-ferrous ones. In steelmaking, the blast furnace reduces iron ore mainly with carbon monoxide generated from coke and injected fuels, while heat is generated primarily by combustion of these carbonaceous materials with oxygen in the hot air blast. This route is commonly paired with basic oxygen furnaces, which decarburise hot metal and oxidise key impurities using oxygen. Scrap (and/or Direct Reduced Iron) is typically melted in electric arc furnaces, which are electrically powered and contribute significantly to overall steel production (Charter Steel, 2024). Similarly, non-ferrous smelting, such as in platinum group metals, employs melting, oxidation and reduction. A primary submerged-arc furnace melts sulfur-bearing ores to a metal-sulfide matte under a mildly reducing condition, which is then oxidised in a converter to remove excess sulfur. Recovery is enhanced by slag-cleaning reduction. Converters use oxygen enrichment and may inject hydrocarbons for energy, analogous to flash smelting in copper (Jones, 2013; Mabiza & Mbohwa, 2015; Mackey & Campos, 2001; Nelson, *et al.*, 2019).

Drawing on literature (Bellemans, *et al.*, 2022; Lorig & Charles, 2025; Degel, *et al.*, 2018; Degel, *et al.*, 2015; Fruehan, 1998; Wijenayake & Sohn, 2020; van Stein Callenfels & van Ikelen, 2005; Nelson, *et al.*, 2019; Mabiza & Mbohwa, 2015) a high-level conceptual flowsheet was developed to support the HAZID. Figure presents a technically feasible ‘smelter nest’ configuration with a broad hazard envelope, including submerged-arc electrodes, oxygen-enriched converting with fuel support, coke-based reduction (CO risk), sulfur-bearing off-gas (toxic/corrosive exposure), and high-pressure utilities that can introduce molten-water interaction risk. This combination captures hazards common to both ferrous and non-ferrous operations, making it suitable as a concept-level basis for HAZID and ISD analysis.



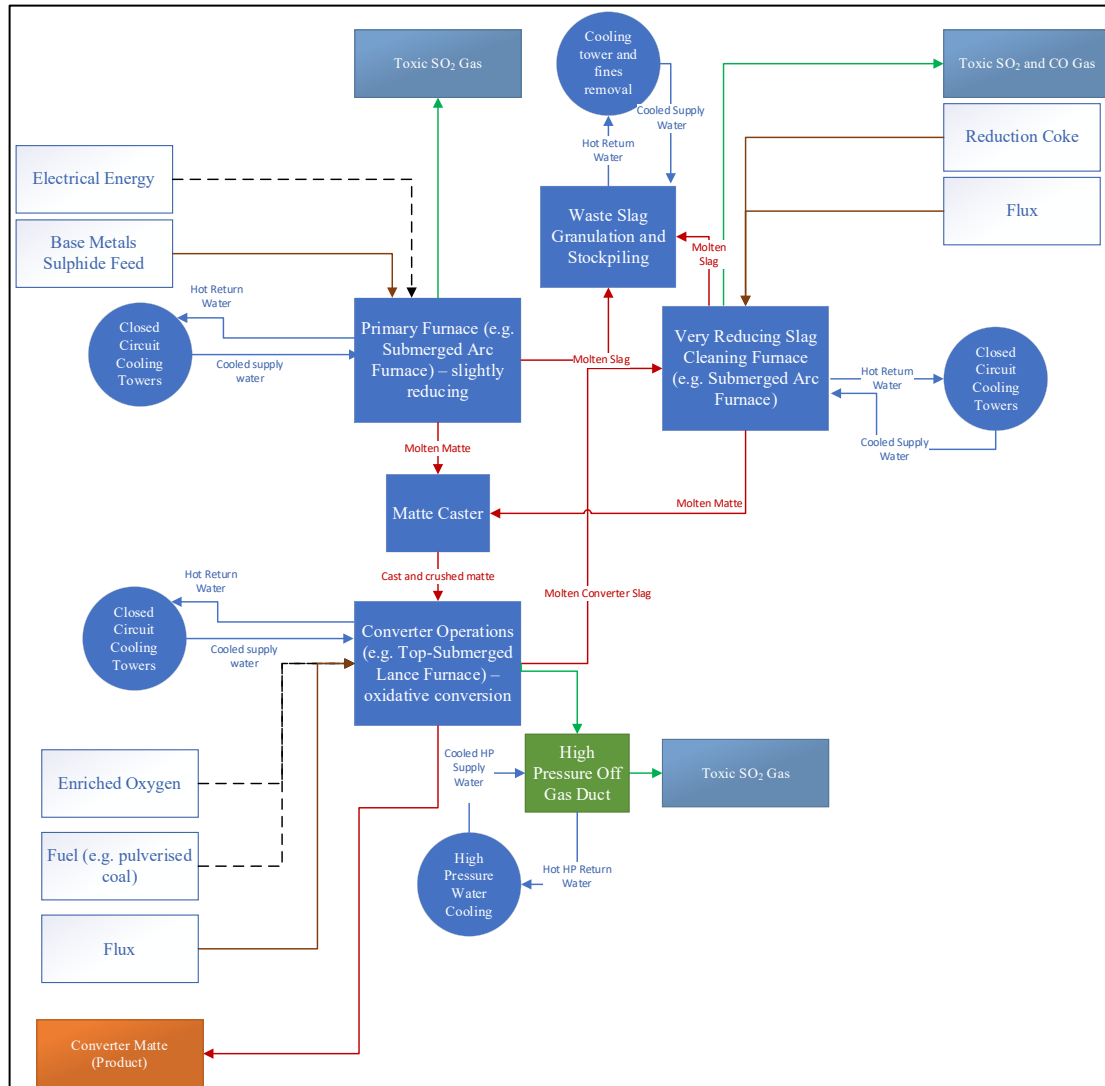


Figure 2: HAZID flowsheet case study.

## METHODOLOGY

The risk analysis tools outlined in the foregoing section were applied to the conceptual smelter flowsheet as developed in the study to understand and assess the hazards and the risks associated with the process. A qualitative, concept-level methodology was formulated and applied to connect HAZID barrier assessment and ISD. Two evidence streams were thus integrated in the analysis : (i) a conceptual smelter flowsheet maximising the hazard envelope across typical ferrous and non-ferrous operations; and (ii) a focused technology review to ground causes, consequences and safeguards in operating realities. This section presents the study methodology and its application.

### HAZID and risk screening

A flowsheet-level HAZID was performed using standard guidewords (Ishola, 2017; ISO, 2016) expanded with smelter-specific hazards such as molten material, molten transportation and furnace positive pressure.

A granular risk matrix or graph was not used in the HAZID; the study is traditionally considered a group exercise that requires multidisciplinary input and user experience to develop risk

characterisation precisely, along with operation-specific risk tolerance to guide the development of further studies. With the study being a desktop screening exercise without the support of design-specific and panel experience to guide consequence and likelihood, a crude qualitative inference was made regarding the impact on People, Assets, and the Environment (graded as low, medium, and high), along with frequency rated as low, medium, or high:

*Table III: Simplified risk classification for HAZID*

| Impact / Frequency                                                  | Low (1)<br>People – injury potential<br>Asset – recoverable damage<br>Environment – non-permanent impact. | Medium (2)<br>People – possible fatality<br>Asset – unit operation level damage<br>Environment – pollution that is recoverable. | High (3)<br>People – multiple fatalities possible.<br>Asset – multiple unit operation damage<br>Environment – permanent damage to the environment |
|---------------------------------------------------------------------|-----------------------------------------------------------------------------------------------------------|---------------------------------------------------------------------------------------------------------------------------------|---------------------------------------------------------------------------------------------------------------------------------------------------|
| Low (1)<br>Technically viable but not heavily cited.                | 1                                                                                                         | 2                                                                                                                               | 3                                                                                                                                                 |
| Medium (2) – Technically viable or plausible from incident data.    | 2                                                                                                         | 4                                                                                                                               | 6                                                                                                                                                 |
| High (3) – Frequently technical citing or a theme in incident data. | 3                                                                                                         | 6                                                                                                                               | 9                                                                                                                                                 |

### Bowtie and Barrier characterisation

The complete HAZID worksheets were first consolidated; duplicated cause and consequences were merged. With this consolidation, top events were defined. Each credible cause was then transcribed as a threat to the left of the bowtie, with safeguards that acted as a credible barrier before the top event was allocated as a preventative barrier. Consequences identified during the HAZID were translated to the right side of the bowtie, and the corresponding mitigative or recovery barriers – those that occur after the loss of control – were positioned accordingly. Escalation factors that influence the effectiveness and sustainability of control were considered and added appropriately to the Bowtie. BowTieXP, a commercially available Bowtie development software application, was used to draft the total diagram.

### Inherently Safer Design screening

Residual risk from the HAZID was the primary gap addressed by the ISD methodology (see Table II).

## RESULTS

The results from the application of the PSM risk analysis toolkit to a smelter are presented in this section. Firstly, the HAZID results are presented, and these are followed by bowties for two selected top events as identified by the HAZID. Control analysis and ISD results conclude the section.

### HAZID and Bowties

The summary presents selected high-risk items identified during the HAZID study undertaken. Additionally, a selection of bowties is shown that prioritise smelter-specific concerns.

*Table IV: HAZID Summary of selected high-risk nine items*

| Guideword / Unwanted event                                      | Causes – integrity or hardware degradation and latent defects                                                                                                                                                                                                                                                | Cause – process or operational upset and external interactions                                                                                                                                                                                                                                                        | Common consequence envelope                                                                                                                                                                                                          |
|-----------------------------------------------------------------|--------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------|-----------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------|--------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------|
| <b>Molten Material</b> / Loss of containment of molten material | 1. Refractory-fabrication & installation defects, cracks or post-machining damage.<br>2. Latent defects or early failure of copper coolers/loss of continuous cooling (over-temperature, bond degradation, cooler voiding/oxidation).<br>3. Uncontrolled furnace shell growth/shrinkage, creating gaps & de- | 1. Improper commissioning/start-up or shutdown (excess heat-up rate, uneven dry-out, premature melt formation).<br>2. Sustained or transient over-temperature due to feed, energy or load excursions; loss of cooling water circulation.<br>3. Poor molten-phase level/interface control, allowing the wrong phase to | 1. Molten splash & radiant-heat burns to personnel.<br>2. Fire/explosion if molten contacts combustibles.<br>3. Steam explosion if molten meets water.<br>4. Severe damage to furnace, auxiliary equipment & long production outage. |

| Guideword/<br>Unwanted event                                                                                                                                             | Causes – integrity or hardware degradation and latent defects                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                  | Cause – process or operational upset and external interactions                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                   | Common consequence envelope                                                                                                                                                                                                                                                                                                                                                  |
|--------------------------------------------------------------------------------------------------------------------------------------------------------------------------|--------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------|------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------|------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------|
| <b>Molten Material</b><br>/<br>Loss of containment of closed-circuit water in a molten environment                                                                       | <p>bonding.</p> <p>4. Mechanical impact on side-walls (rigging or mobile plant); hydration of magnesia brick from stray water.</p> <p>5. Progressive lining loss from aggressive slag chemistry, low-viscosity slag infiltration, high thermal flux or prolonged hot-face corrosion/erosion.</p> <p>1. Fabrication flaws or poor QA of water-cooled panels, staves, jackets and tap-blocks (latent cracks, thin channels).</p> <p>2. Uncontrolled post-manufacture machining of panels/tubes, diminishing the wall thickness.</p> <p>3. De-bonding/voids between coolant tubes &amp; copper/graphite body; fouling or scaling in channels reducing heat transfer.</p> <p>4. Long-term corrosion/erosion of the hot face of copper coolers; erosion of taphole refractories exposing water-cooled blocks.</p> <p>5. Skew drilling/lancing during tapping, piercing water channels.</p> <p>6. High Pressure off-gas-duct panel rupture from stress, corrosion or steam-knock; flashing of cooling medium under low pressure.</p> | <p>erode sensitive zones.</p> <p>4. Abnormal slag foaming (high S, gas over-injection) raises the bath level beyond design limits.</p> <p>1. Operational ingress of water: roof flushing/cleaning, maintenance, draining &amp; refilling, rain- or storm-water entering the roof or tapping the floor.</p> <p>2. Poor water-quality control (O<sub>2</sub>, solids) promoting internal fouling/hot spots in HP ducts.</p> <p>3. Inadequate pressure-control on HP circuits, allowing flashing.</p>                                                               | <p>1. High-energy steam explosion injuring/killing personnel.</p> <p>2. Secondary fire or explosion from structural failure &amp; molten ejection.</p> <p>3. Extensive structural damage to furnace/ducting and protracted downtime.</p>                                                                                                                                     |
| <b>Transportation of molten material</b><br>/<br>Loss of containment of molten material while the metal is in launders or ladles                                         | <p>1. Ladle refractory wear leading to a local hot-spot and burn-through of the steel shell</p> <p>2. Ladle castable that is not fully cured or not preheated, thereby trapping moisture.</p> <p>3. Structural failure of rigging equipment such as slings, lifting beams or crane components</p> <p>4. Blockage or mechanical defect within the launder lining that compromises flow.</p>                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                     | <p>1. Presence of a foreign object on the launder that causes molten-metal splash.</p> <p>2. A-mispositioned ladle that sits outside the designed tapping stream, or misalignment between the launder and the ladle.</p> <p>3. Overfilling of a ladle because the tap is left open too long or the flow is excessive.</p> <p>4. Collision of a vehicle or crane with a ladle that is filled with molten material.</p> <p>5. Improper rigging practice, such as excessive travel speed or poor load control, which may cause the ladle to sway or be dropped.</p> | <p>1. Molten splash and radiant-heat burn to personnel.</p> <p>2. Fire or explosion if molten metal contacts combustible material.</p> <p>3. Steam explosion if molten metal comes into contact with residual water or wet surfaces.</p> <p>4. Major damage to equipment and structures, with extended production interruption.</p>                                          |
| <b>Transportation of molten material</b><br>/<br>Steam explosion occurs during slag-granulation because the quench water flow is insufficient.                           | <p>1. Quench nozzles that are fouled or blocked, thereby reducing the effective wetting surface area.</p> <p>2. Mechanical failure of quench-water hardware such as pumps, pipes, valves or nozzles.</p> <p>3. High water temperature consumes the thermal capacity of water.</p>                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                              | <p>1. Interruption or significant drop in continuous water flow to the granulator.</p> <p>2. Excess thermal load, for example, very high-energy slag, entrained matte or metal pockets, or an elevated tapping rate.</p> <p>3. Poor bath-level control or inadequate crucible-temperature control, which increases slag superheat.</p>                                                                                                                                                                                                                           | <p>1. Steam explosion that injures or kills personnel in the vicinity.</p> <p>2. Shock-waves and projectiles that damage the granulator, the taphole and nearby structures.</p> <p>3. Potential secondary fire or explosion if molten metal is projected outside the containment area.</p> <p>4. Production downtime required for repairs and clean-up.</p>                  |
| <b>Toxic Gas &amp; Oxidants</b><br>/<br>Sudden or progressive release and accumulation of hazardous gases, including carbon monoxide, sulfur dioxide and enriched oxygen | <p>1. Stress-corrosion cracking, metal-loss corrosion or weld failure in off-gas ducts and expansion joints.</p> <p>2. External corrosion, weld defects or fatigue failure in high-purity oxygen pipework; missing or failed flashback arrestors.</p> <p>3. Damaged seals or warped flanges on furnace hoods and uptake sections.</p> <p>4. Duct cooling circuits that leak or block, leading to local overtemperature and shell rupture.</p>                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                  | <p>1. Inadequate or failed building ventilation or induced-draft fans.</p> <p>2. Local overpressure events in ducts caused by blockages downstream.</p> <p>3. Incomplete combustion in the freeboard owing to poor mixing or insufficient oxygen.</p> <p>4. High matte or slag levels that partially seal the uptake, forcing gas through weak points.</p> <p>5. Poor isolation or purging practice during maintenance.</p>                                                                                                                                      | <p>1. Acute poisoning, asphyxiation or respiratory injury to personnel.</p> <p>2. Oxygen-enriched atmospheres that drive intense fires or secondary explosions.</p> <p>3. Corrosion and damage to instruments, cables and structures.</p> <p>4. Long-term health impacts from chronic gas exposure.</p> <p>5. Extended production outage for repair and decontamination.</p> |
| <b>Flammable Substances &amp; combustible dusts</b><br>/<br>Ignition of combustible materials – electrode paste, pulverised coal,                                        | <p>1. Mechanical green-electrode break that exposes raw paste to air; defective welds on electrode-casing extensions; uneven baking that leaves stressed baked electrode zones.</p> <p>2. Burner-throat erosion, cracked lance tips or failed flame sensors that allow pockets of unburned coal in the converter.</p>                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                                          | <p>1. Overfeeding of electrode paste or coal relative to oxygen supply.</p> <p>2. Incorrect coal-to-oxygen ratio or underfeeding of oxygen in the converter.</p> <p>3. Poor housekeeping, allowing dust layers to accumulate on floors, beams and cable trays.</p> <p>4. Disturbance of settled dust layers by maintenance, vehicle movement or</p>                                                                                                                                                                                                              | <p>1. Internal furnace or building fires, dust-cloud deflagrations and secondary explosions.</p> <p>2. Projection of shrapnel and debris causing physical injury.</p> <p>3. Release of toxic combustion products such as carbon monoxide, polyaromatic hydrocarbons and unburned</p>                                                                                         |

| <b>Guideword /<br/>Unwanted event</b>                                                     | <b>Causes – integrity or hardware<br/>degradation and latent defects</b>                                                                                                                                              | <b>Cause – process or operational upset and<br/>external interactions</b>                                                                                                   | <b>Common consequence<br/>envelope</b>                                                                   |
|-------------------------------------------------------------------------------------------|-----------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------|-----------------------------------------------------------------------------------------------------------------------------------------------------------------------------|----------------------------------------------------------------------------------------------------------|
| coal or coke in bins, or accumulated dust – inside vessels or within the furnace building | 3. Blocked vents, corrosion holes or insulation hot-spots in coal or coke bins initiating self-heating.<br>4. Missing or damaged dust-extraction ducting; equipment leaks that release fine solids into the building. | draughts.<br>5. Hot surfaces, welding sparks or frictional heating acting as ignition sources.<br>6. Vibration or impact that dislodges coal dust within bins or conveyors. | coal particulates.<br>4. Structural damage requiring extensive rebuild; significant production downtime. |





## Control Analysis

Controls identified in the bowties were grouped according to the OSHA (2023) hierarchy of controls, which distinguishes between inherently safer measures, engineering controls, and administrative or procedural controls. *Engineering controls* refer to hardware-based barriers such as interlocks, alarms, detection systems, or physical separation that act independently of human intervention. *Administrative controls* include procedures, checklists, operator responses, training, and supervisory actions; generally relying on consistent human performance to remain effective. In some cases, controls were categorised as *mixed* where an engineering element still depended on an operator response or manual reset to function as intended. This framework provides the basis for interpreting the distribution of controls shown in Figure 5. Characterising the controls against the hierarchy of controls indicated a heavy dependence on administrative controls in that  $\approx 60\%$  of controls are administrative. This excluded engineering detection systems that depended on some form of administrative response.

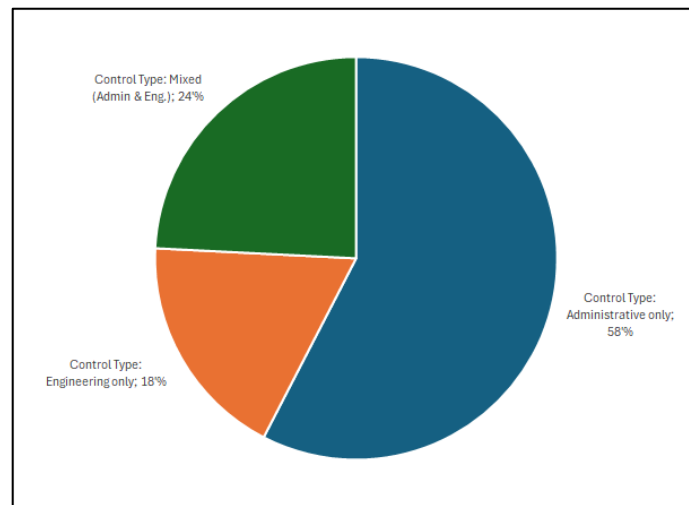


Figure 5: Overall distribution of controls.

Based on the control from the bowtie, Table V shows some short- to medium-term considerations that would be critical to control reliability.

Table I: Engineering focus from Control Analysis

| Theme                                                                    | Engineering focus                                                                                                                                                                                                                                   | Illustrative examples (smelter context)                                                                                                                                                                                                                                                                           |
|--------------------------------------------------------------------------|-----------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------|-------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------|
| Cooling integrity and emergency utilities                                | Verified redundancy and independence (duty/standby), automatic changeover, defined permissives/interlocks, and emergency capacity proven by test. Cooling is treated as a safety-critical service with clear design basis and availability targets. | Dual cooling circuits where practicable; duty/standby pumps with auto-start and failover; flow/pressure/temperature monitoring with trips to reduce furnace energy input; emergency power sized for credible duration; emergency cooling supply philosophy; cooling-water temperature control to prevent boiling. |
| Heat-flux and hot-spot monitoring linked to decisive response            | Continuous thermal trending that enables early recognition of containment degradation, with alarm thresholds tied to response map (including controlled power reduction and shutdown criteria).                                                     | Cooler and furnace wall temperature monitoring; heat-flux proxies derived from energy balance; 'abnormal temperature behaviour' criteria linked to trigger-action and shutdown mapping; automated reporting and review.                                                                                           |
| Leak detection and rapid isolation for water in molten-proximate coolers | Early localisation and rapid isolation before escalation, supported by consistent circuit segmentation and an isolation valve philosophy that is operable under upset conditions.                                                                   | Segmented circuits with zoned isolation; balance/DP logic across zones; remote-operable isolation valves with positive position indication; alarm-to-trip logic for major leaks; tiered 'minor vs major leak' response; pressure testing capability and leak localisation tools.                                  |
| Quality control and assurance for building, repairing, and commissioning | Prevention of latent defects through hold points, acceptance tests and auditable quality records for high-criticality items (refractories, coolers, tapblocks, ladles). Commissioning confirms that safeguards and monitoring function as intended. | Installation assurance packs; refractory and ladle curing controls; pressure/leak tests for cooling circuits; inspection hold points and sign-offs for tapblocks/cooler; post-maintenance functional                                                                                                              |



| Theme                                                                              | Engineering focus                                                                                                                                                                                                                                                                      | Illustrative examples (smelter context)                                                                                                                                                                                                                                                                                                                |
|------------------------------------------------------------------------------------|----------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------|--------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------|
|                                                                                    |                                                                                                                                                                                                                                                                                        | checks of trips, alarms and interlocks; audit trail for deviations and repairs.                                                                                                                                                                                                                                                                        |
| Operating envelope management                                                      | A single operating-envelope package with measurable limits and enforceable bands: molten level, tapping constraints, energy input, chemistry, feed rate vs energy, and feed distribution. Deviations trigger structured actions; persistent deviations trigger constraint or shutdown. | Defined safe bands for level and freeboard; power/energy input constraints with escalation triggers; chemistry limits aligned to stability and integrity outcomes; feed-to-energy ratio limits; feed distribution constraints to avoid local hot-spots and electrode stress; trigger-action plans that define actions, time windows and stop criteria. |
| Furnace integrity: risk-based inspection and maintenance with condition monitoring | Risk-based maintenance and inspection across refractory, coolers, ducts/lines and tapholes, supported by quantified wear/corrosion rates and defined change-out criteria.                                                                                                              | Baseline wall thickness and cooling tolerances; periodic testing; back-calculation for wear rates; corrosion monitoring of off-gas ducting; inspectable taphole/tapblock arrangements with defined inspection frequency; maintenance triggers tied to leading indicators.                                                                              |
| Integrated safe-tapping controls (highest-risk interface)                          | Engineered safeguards reduce dependence on perfect manual execution during tapping by using closed-loop controls, permissives, and hard stops (Nelson & Hundermark, 2016). Human actions remain, but are bounded by engineered constraints.                                            | Closed-loop clay injection rate control; tapblock temperature alarms/trips; controls to prevent skew drilling/lancing; pre-tap permissives (ladle position, launder clear, granulation ready/flow confirmed); tap time and tap count limits; exclusion zoning during tapping.                                                                          |
| Furnace pressure control and off-gas containment                                   | Maintain negative pressure and extraction reliability; detect loss of containment early; design and maintain boundaries to prevent uncontrolled release into occupied areas. Pressure excursions are treated as a major-accident precursor.                                            | Extraction fan redundancy and health monitoring; pressure monitoring with defined response setpoints; duct integrity and corrosion allowances; emergency venting philosophy where justified; controls addressing process drivers of over-pressure.                                                                                                     |
| Flammable atmospheres, dust and ignition control                                   | Prevent formation of flammable conditions, detect early where credible, and interlock ignition sources. Ensure that equipment selection and installation align with hazardous-area classification.                                                                                     | Fuel/oxygen interlocks; combustion and flame-failure logic; off-gas composition/temperature alarms; ignition proxies (duct temperature/pressure) linked to trips; electrode paste and extension management; dust extraction/suppression and housekeeping; hazardous area classification with verified Ex equipment and maintenance regimes.            |
| Toxic gas readiness and occupied-zone protection                                   | Treat toxic releases (CO/SO <sub>2</sub> and related irritants) as a major-accident control problem: detection, ventilation response, alarms, and protection of people through layout, occupancy management and emergency preparedness.                                                | CO/SO <sub>2</sub> monitoring with defined alarm/trips; alarm and evacuation logic; occupancy control during unstable periods; refuge/egress considerations; fit-for-purpose respiratory protection and drills aligned to realistic release scenarios.                                                                                                 |

## Inherently Safer Design

Table VI summarises the highest-leverage inherent design opportunities identified in the assessment, prioritised to reduce major accident hazard exposure.

Table VI: ISD summary

| ISD strategy          | Targeted hazard / top event                                                             | Engineering focus                                                                                                                                                                                                                                                          | Key remarks/trade-offs                                                                                                                                                                                                                                                                                 |
|-----------------------|-----------------------------------------------------------------------------------------|----------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------|--------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------|
| Eliminate/ Substitute | Steam explosion initiated by molten material contact with cooling media                 | Substitute water at molten interfaces with air cooling or inert/ water-based coolants where technically viable (e.g., implanted coolers, tapblocks, selected duct panels).                                                                                                 | Heat-transfer limits and materials compatibility can constrain substitution; some alternative fluids can decompose to toxic species and require decomposition, containment and compatibility assessment - see Konetschnik (2013).                                                                      |
| Minimise              | Steam explosion and high-energy releases following duct/panel cooling failure           | Redesign high-pressure water systems near molten zones as low-pressure circuits, enabled by corrosion-resistant linings and a thermal design that reduces heat-flux demand.                                                                                                | Lower pressure reduces the potential for escalation; it may increase demand for lining consumables and inspection frequency.                                                                                                                                                                           |
| Moderate              | Escalation following loss of cooling or loss of quench capability in molten-granulation | Provide passive or independent emergency cooling capability (gravity tanks, auto-start standalone pumps), with backup capacity sized to credible upset duration. Additionally, reinforced explosion tanks in access-controlled areas and air cooling should be considered. | Backup must be independent of the primary failure modes (power loss, pump failure, blockage) and designed to avoid manual intervention in high-risk zones. Reinforced tanks can contain an explosion, but increased confinement can also intensify overpressure. They therefore require high-integrity |

| ISD strategy         | Targeted hazard / top event                                                                  | Engineering focus                                                                                                                                                                                                       | Key remarks/trade-offs                                                                                                                                                                                                                                                                                                                                                            |
|----------------------|----------------------------------------------------------------------------------------------|-------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------|-----------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------|
|                      |                                                                                              |                                                                                                                                                                                                                         | structural design and appropriately sized pressure-relief.                                                                                                                                                                                                                                                                                                                        |
| Eliminate / Moderate | Molten runout due to refractory failure or shell overheating                                 | Refractory system redesign to reduce liquid penetration and heat transfer (monolithic blocks to reduce joints, backup insulation, controlled freeze-lining where feasible, chemistry-aligned refractory selection).     | Treat as a design requirement supported by transparent modelling and slag-refractory corrosion testing.                                                                                                                                                                                                                                                                           |
| Moderate             | Molten runout with escalation via secondary molten-water interaction                         | Full-inventory secondary containment is designed to be liquid-tight and dry (no standing water; controlled drainage to safe sumps).                                                                                     | Geometry, drainage gradients, maintenance access and exclusion zones must be designed to prevent water pooling and uncontrolled interaction. A full-furnace runout can pool molten material, so drainage should be designed to channel molten into designated runout zones that avoid excessive thermal loading of critical base equipment (e.g., hearth cooling fans and pumps). |
| Moderate             | Instability leading to runout, overheating, accelerated wear, abnormal off-gas conditions    | Design for a controllable operating envelope by ensuring key variables are measurable (molten level/inventory, feed distribution, feed-to-energy ratio, accretions/build-up indicators).                                | Operating stability becomes a design constraint; measurement limitations should be addressed through validated models.                                                                                                                                                                                                                                                            |
| Simplify             | Personnel exposure during manual measurement; poor level control                             | Automate molten level and inventory measurement using non-contact or remotely operated systems appropriate to the furnace type and geometry.                                                                            | Reduces line-of-fire exposure and decision uncertainty during tightly coupled operating conditions.                                                                                                                                                                                                                                                                               |
| Simplify             | Abnormal chemistry driving refractory/cooler wear, foaming and instability                   | Increase molten chemistry control frequency via automated sampling and analysis workflows; assess the feasibility of online analysers where appropriate.                                                                | Sampling and analysis turnaround time must match the required control response window to be effective.                                                                                                                                                                                                                                                                            |
| Moderate             | Escalation following cooling circuit failure during fault finding                            | Redundant, independent cooling circuits for high-criticality water-cooled elements, designed to maintain partial cooling during single-circuit failure.                                                                 | Adds complexity; requires strong instrumentation, isolation philosophy and maintainability by design.                                                                                                                                                                                                                                                                             |
| Simplify             | Steam explosion risk during leak investigation; prolonged isolation causing secondary damage | Remote leak detection and isolation for high-risk circuits (pressure-test capability or flow-to-leak validation per cooler water circuit, acoustic/vibration diagnostics, heat-flux localisation), prioritised by risk. | Reduces the need for intrusive inspection during water leak investigation and shortens isolation duration. Requires disciplined and drilled response training.                                                                                                                                                                                                                    |

### Application in existing facilities

The challenge in applying PSM lies in integrating its principles within the practical constraints of operating assets. Therefore phased implementation requires an approach that recognises organisational readiness and the technical realities of a mature, high-energy processing facility. Key elements include:

- **Leadership recognition and commitment:** Implementation typically starts when leadership recognises the need to explicitly manage major process risk – often driven by regulatory requirements, realised impacts to people, assets or the environment, or recurring incidents and precursor events. Progress depends on resourcing PSM integration, including capacity for risk studies and delivery of the safeguards identified through analysis. During commissioning, tighter controls and behavioural expectations may create short-term disruption; however, these changes generally deliver long-term benefits through improved operational stability, stronger asset integrity, and reduced major unplanned interruptions.
- **Definition of risk-tolerance targets:** Once risks are identified through analysis, clear risk-tolerance targets are required. These targets provide a consistent basis for prioritising actions, assessing residual risk and deciding whether additional risk reduction is necessary.
- **Development of proactive risk awareness and culture:** Sustained PSM requires hazard awareness and a proactive operational culture that supports early identification of abnormal conditions, learning from near misses, and maintaining attention to major-accident risks alongside production

objectives. Training and awareness programmes are essential to embed the behaviours required to ensure administrative controls remain rigorous in practice.

- **Understanding underlying process safety risk:** A credible understanding of process safety risk depends on accurate engineering information. Aligning drawings and documentation to as-built conditions is often necessary to preserve the integrity of process safety studies, but this can pressure technical resources and is therefore typically prioritised based on risk significance.
- **Prioritisation in ageing facilities:** In mature facilities, process safety studies frequently yield numerous findings and recommendations. Effective integration relies on structured triage: prioritising high-impact engineering controls while strengthening the supporting administrative systems that sustain day-to-day control performance.
- **Tracking and monitoring of PSM performance:** Adopting technical performance indicators that provide oversight of control performance, consistent with the themes captured in Table I is key to effective process safety leadership. These indicators provide leading visibility of barrier health, not only lagging incident outcomes.
- **Early project-lifecycle integration for capital injection:** Risk reduction is strengthened by extending PSM thinking upstream into concept selection, feasibility and front-end design, where ISD options can eliminate or reduce hazards at source through process selection, layout, inventory reduction and simplification before detailed design and construction constrain choices.

Below are selected practical technical considerations to guide the integration of control in Table I:

- Upgrading controls often requires rigorous estimation and modelling, which can conflict with the critical path for the timely closure of study actions; without this technical basis, hazards may be overstated, yielding impractical, unimplementable solutions. Leak detection philosophy illustrates this clearly: ‘zero tolerance to leaks’ is rarely feasible, whereas defining leak-rate tolerances and time-to-escalation (the time to conditions that could lead to an explosion) enables practical detection and trigger-action design without over-engineering. In smelting, thermal inertia strains response effectiveness and can amplify secondary consequences. For molten-proximate water systems, safe response commonly requires both reducing/ceasing energy input and isolating water. However, isolating water can itself escalate risk by removing cooling margin and driving widespread integrity loss, increasing the likelihood of molten loss of containment and, further, more severe leaks. Defining leak tolerances is therefore essential to enable controlled isolation without introducing unacceptable asset risk, reserving emergency isolation for exceedance of defined tolerances; developing this technical basis is non-trivial.
- Management of the operating envelope is equally important. Operating-band rules must enable rapid, consistent responses, especially to fail-safe states. Where decision-making regarding shutdown and de-energisation requires off-shift consultation, safe management of upsets is compromised. Operating-band rules and training must empower on-shift personnel to make timely de-energising decisions without complex consultation, while ensuring technical consultation is enabled early for rectification or is systematically introduced before deterioration begins. Organisational maturity and agility should therefore be explicit criteria when assessing operating-band effectiveness.
- Some controls are constrained by technology maturity. Flammable and explosive atmosphere control in furnaces is relevant when combustion is required to energise furnace operation. Preventing the formation of a flammable atmosphere inside the unit depends on ensuring that a flame is present where combustion is expected; if the flame is extinguished, an explosive atmosphere may develop. Where direct flame confirmation in smelter environments is limited, the risk study team must be technically capable of selecting and validating credible proxy indicators (e.g., temperature response across the unit or off-gas composition) and confirming these proxies are appropriate for flame confirmation.
- Safe tapping controls are mostly procedural, so residual risk remains high – especially line-of-fire exposure to molten material. Risk reduction depends on making human actions more reliable through smart alarming and actively managing the factors that drive human error. Human factors and ergonomics are therefore central to the optimisation of safe tapping.
- **Process Safety Critical Equipment:** Legacy OHS systems often under-prioritise rare, high-consequence process hazards, so the reliability of key protective equipment is not managed

explicitly and can be deprioritised relative to production criticality. A risk-based programme is therefore needed to identify the small set of instruments and components that prevent or mitigate major accidents (e.g., furnace moisture detection and cooling thermocouples) and to systematically maintain, test and monitor them to a reliability level consistent with the site's risk tolerance.

## DISCUSSION

The paper converges on a set of concentrated major-accident hazard envelopes in smelting. Across HAZID and bowtie mapping, the highest-risk scenarios centre on loss of molten containment and molten-water interaction, particularly at high-energy interfaces such as tapholes, launders, ladles, and granulation. These events escalate through interacting mechanisms: integrity degradation (refractory and cooler deterioration) interacting with operational upsets and transitional activities (start-up/shutdown, tapping, molten quenching), and can manifest as steam explosions, runouts, and thermal exposure, resulting in fires, structural damage, and business interruption.

Interpretation of the results highlights that smelter risk is strongly shaped by system dependencies. Water is both a necessary utility and a recurrent escalation vector; leak detection and isolation can temporarily reduce cooling while the process remains highly energised, complicating diagnosis and increasing exposure if responses are slow or inaccurate. In parallel, furnace integrity is tightly coupled to maintaining a defined operating envelope: level control (including slag foaming), energy input, feed distribution, and molten chemistry interact through thermal inertia. This means that safe de-energising requires coordinated actions rather than single-parameter correction. Therefore, operating envelopes are coupled and combined with high manual intervention at interfaces (e.g., drilling/lancing, sampling, inspections), demonstrated by the high fraction of administrative controls (60%), in which a person must rationalise interacting hazards and direct system responses. This emphasised the importance of engineered controls, clear trigger-action logic and people-hazard separation.

Accordingly, the paper is structured to address medium- and long-term considerations for risk reduction. The medium-term items strengthen barrier reliability by prioritising redundancy and verification, while the long-term items apply inherently safer design to reduce hazard exposure at source through substitution or minimisation. This is limited by the study's concept-level scope and qualitative grading, which reduce the precision of risk ranking and the consideration of site-specific hazards and controls. The results should therefore be used as a structured basis for prioritisation and would need site-specific engineering data validation and consideration of human factors to be viable.

In operating facilities, this implies that risk studies must be coupled with a phased PSM integration plan that aligns organisational readiness with the constraints of ageing, high-energy assets. Implementation should set explicit risk-tolerance targets, triage findings to prioritise high-impact engineered controls, and track barrier health using technical performance indicators aligned to the engineering focus themes in Table I. When controls depend on trigger-action logic (e.g., leak detection/isolation and operating-band deviations), a defensible technical basis is required (e.g., leak-rate tolerances and time-to-escalation) to avoid impractical control designs that increase risk. Given the heavy reliance on administrative barriers, high-risk interfaces such as tapping also require explicit human factors and ergonomics input (smart alarming, error-proofing, and line-of-fire exposure reduction) and a Process Safety Critical Equipment programme to sustain the reliability of key instruments (e.g., moisture detection and cooling thermocouples).

## CONCLUSION

The paper demonstrates that applying PSM principles to a smelter-specific qualitative toolkit fundamentally changes the way smelter risks are identified, prioritised, and managed. By combining flowsheet-based HAZID, bowtie structuring and inherently safer design thinking, the approach aids more systematic identification of major accident scenarios – particularly molten metal-water

interactions, cooling system failures, loss of containment and furnace over-pressurisation. Translating priority top events into barrier logic provides a clearer, defensible basis for describing what controls must achieve and where performance is most critical, thereby improving both the qualitative assessment and the semi-quantification of risk.

The assessment outputs serve as a prioritisation framework for medium-term barrier strengthening and long-term ISD opportunities that reduce hazard exposure at the source and simplify high-risk interfaces. Because the method is concept-level and qualitatively graded, it should be re-run and refined against site-specific design information, operating conditions and multidisciplinary insights, progressively improving the precision of risk quantification and barrier design criteria. More quantitative tools, with refined engineering data for specific systems, would further enhance the effectiveness of controls that prevent low-probability events and strengthen the prevention and mitigation of PSM events. The results provide direction for prioritising this work for specific implementation.

For practical deployment, the toolkit should be paired with an implementation plan for existing plants that includes leadership commitment and a defined risk tolerance, which together drive the prioritisation of barrier upgrades. Where administrative controls are already mature, residual risk reduction should explicitly include human-factors optimisation of procedural interfaces, such as tapping. In parallel, reliability governance should be implemented via safety-critical equipment through risk-based identification and ring-fenced asset management.

Finally, integrating PSM into smelting operations offers clear advantages over traditional OHS-based approaches by explicitly addressing the high-energy hazards inherent in pyrometallurgical systems. Whereas OHS methodologies are not designed to assess or manage low-probability, high-consequence events, PSM tools such as HAZID, bowties and ISD enable more effective identification, analysis and control of major accident scenarios. This shift toward proactive, engineering-based management improves safety and operational reliability, reduces the likelihood of catastrophic events and aligns smelting operations with modern international best practice.

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# Large-scale dual-electrode dc arc furnace technology for the ferro-alloy industry

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The dual-electrode direct current (DC) arc furnace has emerged as a transformative advancement in ferro-alloy production, offering a scalable and efficient alternative to conventional furnace designs. This technology complements established configurations – including single- and twin-electrode DC, three-electrode AC, and 6-in-line furnaces – by enabling power levels beyond 140 MW. A key innovation is the integration of external electromagnetic arc compensation, which effectively mitigates arc flaring toward furnace sidewalls, enhancing operational stability. Utilising a conventional non-conductive hearth, the system retains proven design benefits while unlocking new performance capabilities. Building on theoretical and pilot-scale validation, the authors successfully converted two industrial-scale FeNi DC furnaces to the dual-electrode configuration. These units operate reliably above 100 MW each, demonstrating the technology's potential for widespread industrial adoption. This development marks a significant leap forward in furnace design, promising improved efficiency, reliability, and scalability for the ferro-alloy industry. The technology is particularly well suited to open-bath smelting operations, offering a viable pathway for the staged conversion of existing submerged-arc furnace infrastructure.

**Keywords:** dual-electrode DC arc furnace, electro-magnetic arc compensation, open-bath smelting

## INTRODUCTION

The ferro-alloy industry is under increasing pressure to improve productivity and operational resilience in the face of constrained electrical power availability, declining raw material quality, and rising electricity costs. In South Africa, many producers operate ageing submerged-arc furnaces (SAFs) that remain structurally sound but are fundamentally limited by the electrical and operational constraints of conventional three-electrode AC technology. These limitations increasingly restrict further scale-up and make alternative furnace operating modes and technologies necessary.

As furnace power levels increase, fully submerged operation becomes progressively more difficult to sustain due to increasing electrical reactance, constraints on burden permeability, and reduced controllability of the electrical arc. Consequently, modern high-power electric smelting furnaces can benefit from adopting open-bath or semi-open-bath operation as a deliberate means of increasing power density, improving electrode stability, and enhancing tolerance to variability in raw material quality. Within this context, furnace technologies capable of stable open-bath operation at elevated power levels are of particular relevance to the South African ferro-alloy industry.

This paper reviews established ferro-alloy furnace technologies and introduces recent developments in dual-electrode DC arc furnace technology. The successful industrial implementation of this technology is discussed, together with its application in open-bath smelting. In addition, practical upgrade pathways are outlined whereby existing submerged-arc furnaces can be converted to either brush arc AC or dual-electrode DC configurations, providing capital-efficient options for increasing production capacity and extending furnace life under South African operating conditions.

### **Review of existing ferro-alloy furnace technologies**

A brief review of existing technical constraints associated with furnace technologies used in ferro-alloy smelting is warranted. With advancements in engineering and process design, these technologies have evolved; however, several limitations remain that impact their efficiency, cost-effectiveness, and adaptability to various ore types. Understanding these constraints is important for guiding development efforts, aiming to optimise furnace performance and enhance overall production capacity within the ferro-alloy industry.

#### ***Three-Electrode Circular Submerged Arc AC Furnace***

SAFs have been the backbone of the ferroalloy industry for decades, having been in use since 1906. They are increasingly being scrutinised for their scalability and energy efficiency. One of the primary challenges facing SAFs is the electrical reactance of the secondary circuit, which imposes a fundamental constraint on the scale of these furnaces (Barker I.J., 2011). These limitations have driven the industry toward alternative operating modes and furnace configurations capable of supporting higher power densities.

#### ***Six-in-line AC Furnace***

The transition from a three-electrode circular furnace to a six-electrode in-line configuration presents a viable approach to overcoming power level limitations. By distancing the end-walls from the first and last electrodes, the design minimises the negative effects of arc-deflection. However, this furnace layout introduces additional design and operational complexity associated with the larger molten bath and extended furnace geometry. Furthermore, the initial capital investment is higher, reflecting the cost of additional electrodes, associated mechanical and electrical infrastructure, and the increased refractory volume required. Although six-electrode in-line furnaces extend the practical power limits of AC technology, the increased furnace footprint, refractory volume, and capital cost reduce their attractiveness relative to other modern alternatives.

#### ***Three-Electrode Circular Brush-Arc AC Furnace***

Brush-arc AC operation represents a practical transition from conventional submerged-arc operation toward controlled open-bath smelting. By deliberately operating with exposed arcs, higher active power input can be achieved due to improved power factor. In an AC furnace, the electro-magnetic forces generated between the opposing current flows in each electrode results in deflection of the electric arcs towards the furnace sidewalls, causing localised heating on sidewall refractory. One method of overcoming the risks of arc deflection in a brush arc furnace is to feed raw material stockpiles in the regions directly behind each electrode. This not only protects the furnace sidewalls but also assists in pre-heating material, resulting in improved furnace efficiency. Importantly, this operating mode can be implemented on existing three-electrode AC furnaces, typically repurposing the existing furnace shell, electrodes and transformers, making it an attractive upgrade option for operators seeking capacity increases without wholesale furnace replacement.

#### ***Single-electrode DC Arc Furnace***

The single electrode DC arc furnace is a significant technological advancement in metallurgical processes, having been in operation for smelting applications since 1984. This technology offers several advantages, such as the ability to utilise un-agglomerated fine materials, which can lead to cost savings and efficiency improvements. The invention and installation of active arc compensation systems have effectively addressed the process challenges imposed by arc-deflection, ensuring a more stable and controlled electrical environment within the furnace. The conductive hearth, acting as the anode, is a

critical component of this configuration, completing the electrical circuit necessary for furnace operation.

While single-electrode DC furnaces offer excellent process stability and raw-material flexibility, their scalability is fundamentally limited by the current-carrying capacity of a single graphite electrode, effectively capping furnace power input levels. Despite this, the single electrode DC arc furnace remains a cornerstone in the field of high-temperature industrial processes, reflecting the ongoing evolution of metallurgical technology.

#### ***Twin-electrode DC Arc Furnace***

The implementation of a twin cathode electrode system in a single DC furnace is an innovative approach to address the limitations of electrode current carrying capacity. This configuration, which necessitates a conductive hearth anode, can effectively centralise the arcs towards the centre of the furnace due to the attractive electromagnetic forces between the electrodes, thereby reducing the risk of sidewall heating. While this system has been successfully employed in the steel-making industry, its adoption in the ferro-alloy sector has been met with challenges due to arc convergence and instability under ferro-alloy smelting conditions. This has prevented sustained high-power operation in this configuration and long-term stable power input above 60 MW has not been demonstrated.

#### ***Section remarks and observations***

The limitations of conventional AC submerged-arc furnaces, together with the scalability constraints of single- and twin-electrode DC configurations, highlight the need for furnace technologies that can operate stably at high power densities in open-bath conditions. Dual-electrode DC arc furnace technology addresses these requirements by combining DC furnace electrical technology with conventional AC furnace electrode and hearth designs.

#### **Dual Electrode DC Arc Furnaces**

Dual-electrode DC arc furnace technology has been developed to address the inherent scalability limitations of conventional single-electrode DC furnaces while avoiding the arc instability associated with twin-electrode cathodic configurations. By employing two independently controlled electrodes operating at opposite polarities within a single furnace shell, the technology enables a substantial increase in furnace power input without exceeding practical electrode current limits.

#### ***Concept and operating principle***

Dual electrode DC arc furnaces make use of conventional DC power supplies, usually consisting of a transformer and two phase shifted converters forming a 12 or 24-pulse configuration as in Figure 1. Operating voltage and current ranges are well within the capability of existing power supply design limits allowing proven rectifier technology to be applied without introducing new electrical risk.

Two electrodes are controlled independently by the furnace control system and, together with the power supply, regulate electrical power input into the furnace. Each electrode is connected to a set of water-cooled copper bustubes by means of flexibles.

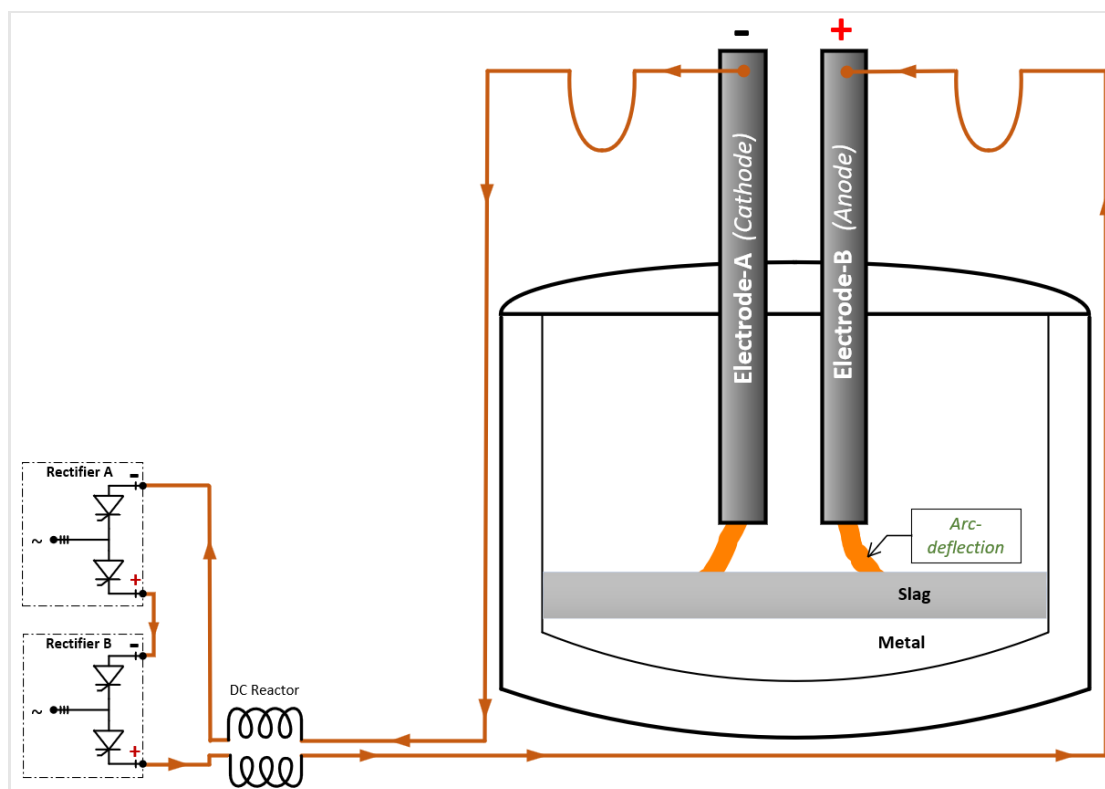


Figure 1. Typical layout of Dual Electrode DC Furnace electrical circuit.

#### **Compatibility with existing furnace infrastructure**

Unlike conventional single- and twin-electrode DC furnaces, dual-electrode DC arc furnaces offer a key advantage in that they no longer require a conductive hearth anode. A conventional AC-type dished refractory hearth can therefore be retained, offering advantages in terms of refractory life, reduced heat loss, and simplified furnace construction. This characteristic is particularly significant in the context of furnace upgrades, as it allows existing submerged-arc furnace shells and hearth designs to be converted to dual-electrode DC operation without extensive mechanical reconstruction.

While the converted furnaces described in this paper make use of graphite electrodes, dual-electrode DC arc furnace technology is not inherently limited to this electrode type. The use of Söderberg electrodes is technically feasible and represents a particularly attractive option for the South African ferro-alloy industry, where Söderberg electrode technology is well established and offers advantages in terms of cost, local availability, and operational familiarity. The elimination of AC skin-effect losses in a DC operation further enhances the suitability of Söderberg electrodes for large-diameter, high-current applications, enabling higher power inputs per electrode when compared with conventional AC submerged-arc furnace operations.

#### **Full scale implementation**

A full-scale industrial implementation of dual-electrode DC arc furnace technology was undertaken through the conversion of two large FeNi DC furnaces previously operating in a twin-electrode configuration. Despite being mechanically sound and originally rated at 80 MW, both furnaces suffered from chronic arc instability and frequent loss-of-arc (LOA) events, resulting in sustained average power inputs of less than 60 MW. These conditions provided a suitable industrial test case for evaluating the effectiveness of the dual-electrode DC configuration under demanding operating conditions.

#### **Conversion methodology from twin to dual**

The conversion involved the polarity reversal of one of the DC converters, from the configuration illustrated in Figure 2 to the configuration depicted in Figure 3. This polarity reversal was achieved by

physically turning all the thyristor capsules around 180° in the two 6-pulse bridges of the one DC converter and re-arranging their thyristor triggering sequences. No structural changes or DC bustube connection changes were necessary, which meant that the changes could be implemented on site within three days.

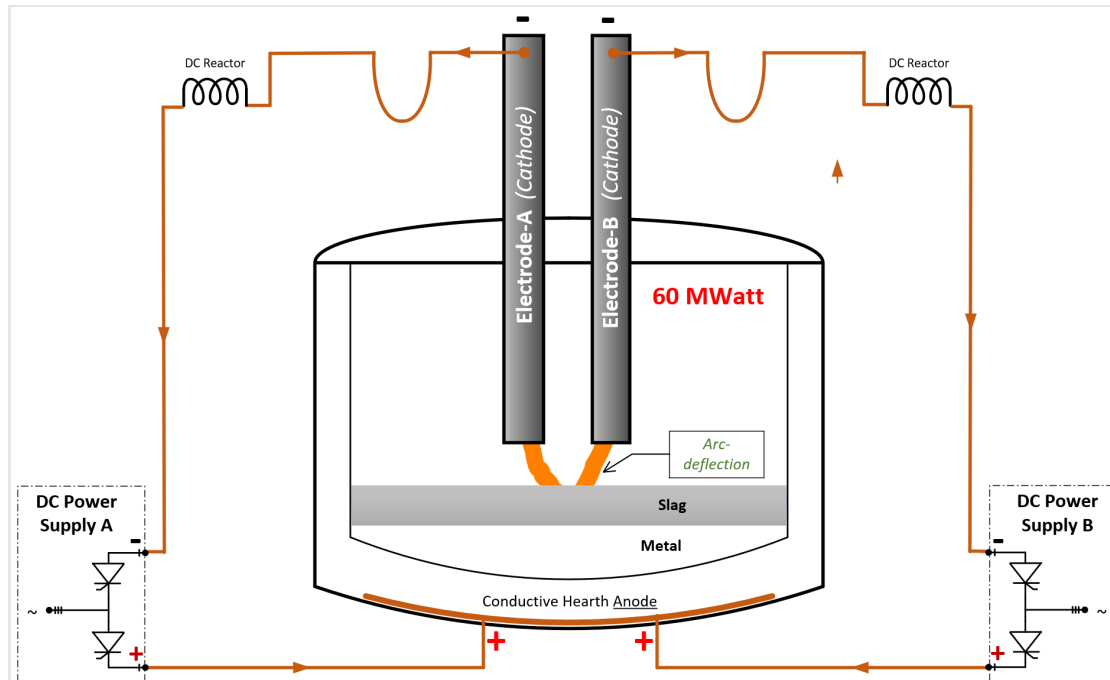


Figure 2. Symbolic layout of KNS Twin-Electrode DC Furnace (prior to conversion).

Twin-electrode configurations, as shown in Figure 2, cause arc convergence due to both mutual attraction between parallel arc currents and the resultant magnetic field of the main DC circuit. The arc-length to electrode spacing becomes a critical factor in determining the threshold of tolerable arc merging and LOA (Burkat & Coulombe, 2015). Conversion of the twin-electrode configuration of Figure 2, to the dual-electrode illustrated in Figure 3, reverses arc convergence to that of divergence by the same mechanisms as explained above.

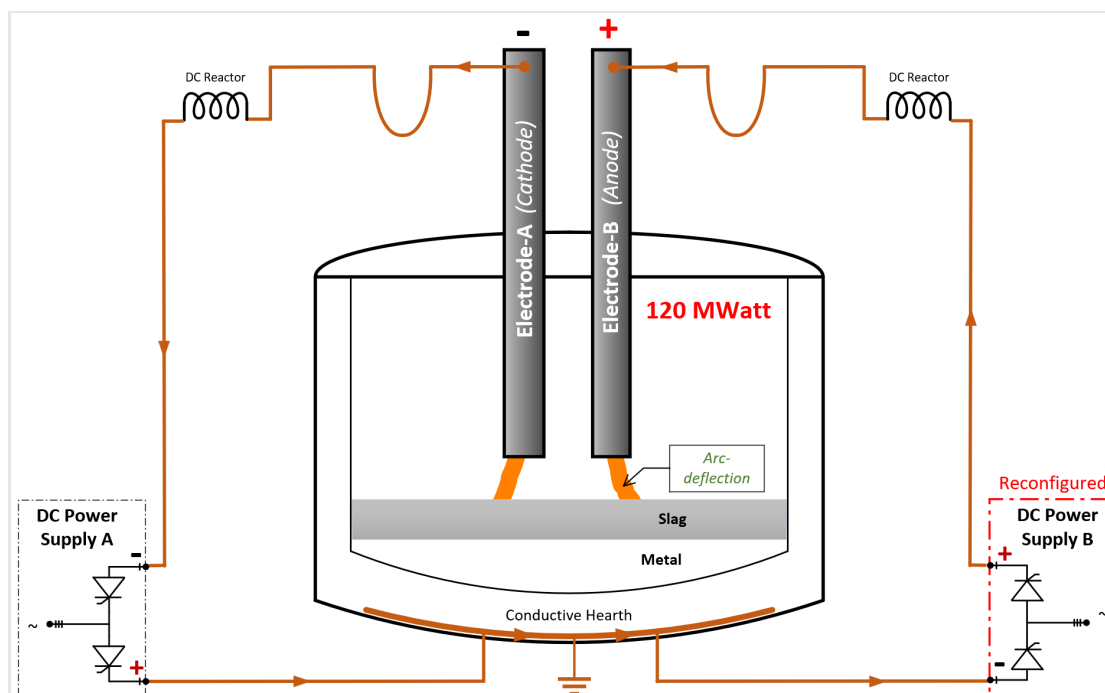


Figure 2. Diagrammatic layout of KNS Dual-Electrode DC Furnace (post-conversion).

### **Risks and mitigation**

A thorough risk analysis was carried out prior to implementation of the dual electrode DC technology, and this process identified two major potential hazards:

#### ***Elevated Alloy Bath Potentials***

The possibility of elevated bath potential was identified, and this could pose a risk to personnel during furnace tapping. If two separate series converters are used as in the case of the converted furnace illustrated in Figure 3, physical grounding of the interconnecting bustube between the two converters to the redundant hearth copper plate and hearth structures establishes a zero-potential for the furnace shell and bottom. Electrodes A and B respectively present full positive and negative voltage with respect to ground and double full voltage between them. With no vertical current in the highly conductive metal bath and existing conductive refractory layer above the hearth copper dish, there will be no elevated alloy bath potential interfering with slag or metal tapping operations. In addition, separate windings of the furnace transformer furnish galvanic isolation for the AC and DC circuits.

#### ***Inter-electrode arcing***

The possibility of inter-electrode arcing was also explored in detail. It would be logical to infer that inter-electrode arcing could be a risk due to the high DC voltage applied across the two electrodes. In addition, the distance between the two electrodes was not increased. As it turns out, the same opposing electro-magnetic forces that prevent top-of-roof as well as bottom-of-roof inter-arcing between AC furnace electrodes are also at work here. The likelihood of this event is therefore extremely small, and to further reduce the risk, a patented GLPS Roof Arc Detection System was installed as a precautionary measure. This system consists of high-sensitivity optical sensors which effect firing-signal inhibition in the DC converters if a furnace roof arc is detected.

#### ***Post-conversion Power Input and Operational stability***

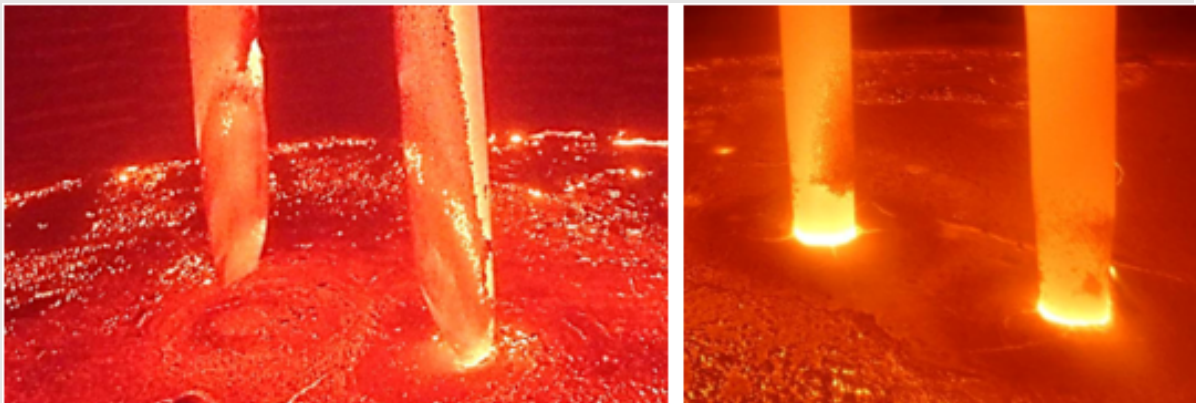
Following conversion to dual-electrode DC operation, both furnaces demonstrated a marked improvement in arc stability and power controllability. Average furnace power input increased substantially, with sustained operating levels exceeding 100 MW. Production capacity more than doubled relative to pre-conversion operation. The previously observed stochastic load variations were



eliminated, resulting in improved interaction with the captive synchronous generators supplying the plant. Furnace availability increased accordingly, confirming that dual-electrode DC operation provides stability comparable to conventional single-electrode DC furnaces at significantly higher power levels.

#### ***Electrode wear***

The two photographs in Figure 4 respectively illustrate the electrode wear patterns for the same furnace before- and after twin-to dual conversion. The large degree of chamfering of the electrode tips in the left-hand picture is ascribed to inter-electrode arcing and arc merging with the arc running up between the electrode inner faces. This phenomenon was the principal problem leading to LOA. Notably, this chamfering disappeared during dual-electrode operation, as can be observed in the right-hand picture.



*Figure 4. Electrode-tip wear pattern before and after conversion from twin-to dual furnace configuration.*

A key concern prior to implementation of the dual-electrode technology was that electrode consumption of the anode would be significantly higher than that of the cathode. The results of the implementation have shown that wear rates in both electrodes are similar. The similarity in wear rates between the anode and cathode further confirms that electrode polarity does not impose a penalty on electrode life in dual-electrode DC operation, alleviating a key concern associated with high-current DC furnace designs.

#### **Arc-deflection Compensation**

A chief consideration in the design of large-capacity dual electrode DC arc furnaces is the mitigation of natural arc deflection towards the furnace sidewalls. In a dual-electrode DC arc furnace, which can be considered analogous to the AC system in some respects, the magnetic interaction between the arcs causes a repulsive force, leading to arc-deflection away from the centreline of the furnace (Reynolds Q.G., 2012). This can result in sustained increases in furnace sidewall heat-loading, to the detriment of sidewall refractory life. Effective control of arc deflection is therefore a prerequisite for stable long-term operation at elevated power levels.

Several methods can be employed to minimise the effects of arc-deflection, and for a dual electrode DC arc furnace a combination of correct furnace dimensioning, anode and cathode bustube routing, and raw material feed positioning is utilised. These items do however only reduce the negative effects of arc deflection, and a patented external arc compensation system is therefore employed to fully neutralise electric arc deflection.

#### ***External Arc-deflection Compensation***

In the dual-electrode DC configuration, the furnace electrical circuit forms a single dominant DC current loop. The magnetic field associated with this loop governs the direction and magnitude of arc deflection and can therefore be intentionally influenced through the introduction of a secondary, controlled magnetic field. A single DC current loop is formed in the dual-furnace configuration, shown by the direction of currents in Figure 5. Applying the Biot-Savart Law around this loop shows that the induced

magnetic flux direction is into the page inside this loop (Hayt & Buck, 2001). By the Lorentz force equation, the presence of this magnetic field will cause the divergence of the arcs by the two opposite directional current flows.

A conductor close to the interconnecting bustube between the two conductors at the bottom in Figure 5 with a controlled current in the opposite direction, will produce a magnetic field out of the page to oppose that in the main DC current. It is feasible in practice to place this conductor – designated the compensating bustube – in closer proximity to the arc regions than that of the interconnecting bustube between the converters. It is therefore possible to neutralise or compensate for the diverging deflection of the two arcs by a controlled compensating current of smaller magnitude than that of the main DC current.

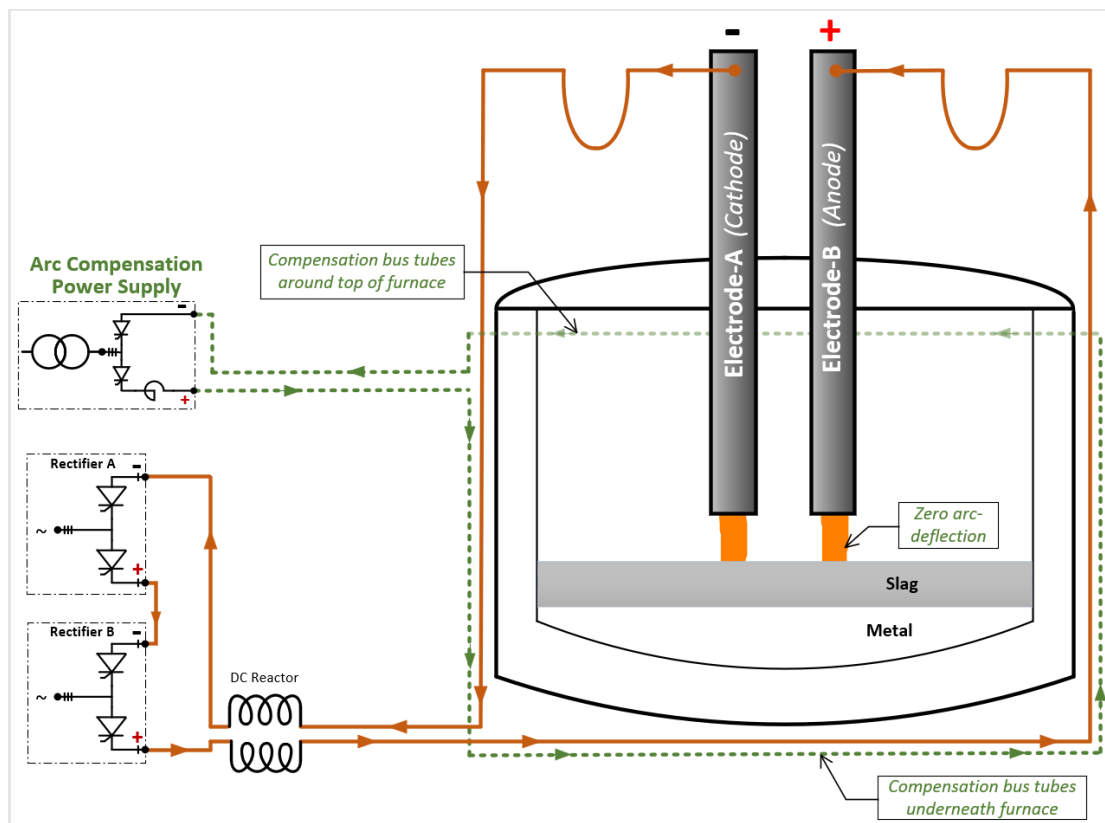


Figure 5. Typical diagrammatic layout of Dual Electrode DC Furnace with Magnetic Arc Compensation.

The compensating current, the design of which must be large enough to force the electrode arcs back to the vertical or even into convergence, is supplied by a separate compensating converter. In the polarity shown it supplements the magnetic field of the interconnecting bustube, but depending on the physical design, a current of reversed direction may be necessary to prevent over-compensation and arc deflecting convergence. When combined with appropriate furnace geometry and operating practices, external electromagnetic arc compensation enables dual-electrode DC arc furnaces to operate reliably at power levels previously unattainable in open-bath ferro-alloy smelting.

## CONCLUSIONS

The continued scale-up of ferro-alloy furnaces is increasingly constrained by the electrical and operational limitations of conventional three-electrode AC submerged-arc furnace technology. As furnace power levels increase and raw material characteristics become more variable, open-bath or

semi-open-bath smelting has emerged as a practical operating mode to achieve higher power densities, improved controllability, and greater tolerance to feed variability.

Dual-electrode DC arc furnace technology addresses the scalability limitations of both single-electrode DC and twin-electrode DC configurations by enabling significantly higher furnace power input while maintaining stable arc behaviour. The use of two independently controlled electrodes operating at opposite polarities eliminates the need for a conductive hearth, allowing conventional AC-type refractory hearth designs to be retained. This characteristic is particularly relevant in the context of furnace upgrades, as it allows the reuse of existing furnace shells and electrode systems, thereby reducing the capital cost relative to a full greenfield installation.

The full-scale industrial implementation described in this paper demonstrates that dual-electrode DC arc furnaces can operate reliably at sustained power levels exceeding 100 MW. Although conversion to dual-electrode DC technology represents a substantial capital investment—particularly in terms of electrical infrastructure and power supply equipment—the ability to retain major mechanical components such as the furnace shell and electrodes significantly improves the overall project economics.

A key enabler for stable high-power open-bath operation is the application of external electromagnetic arc-deflection compensation. By actively counteracting the natural electromagnetic forces that cause arc deflection toward the furnace sidewalls, stable and near-vertical arc behaviour can be maintained across a wide operating range. This enables sustained high-power operation without excessive sidewall heat loading or accelerated refractory wear.

From a South African perspective, the technologies discussed provide a range of practical upgrade pathways for existing ferro-alloy furnaces. Brush arc AC technology represents a viable and relatively lower-capital option for upgrading existing AC submerged-arc furnaces, offering incremental increases in power input and production capacity. Dual-electrode DC arc furnace technology, while requiring higher capital investment, provides a step-change in achievable power density and long-term scalability. Together, these technologies offer South African producer's flexible options to extend furnace life, improve competitiveness, and adapt to increasingly constrained energy and raw-material conditions.

## ACKNOWLEDGMENTS

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## **Ruan Greyling**

Director  
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Ruan Greyling is an Electrical Engineer with 15 years of experience in the mining and metallurgical industry. Ruan specializes in the development and implementation of DC furnace technology and has played a key role in the field implementation of dual-electrode DC furnace systems. His work focuses on advancing innovative electrical solutions to improve efficiency and reliability in high-temperature metallurgical processes.

# Devolatilisation of charcoal and interaction of volatiles with manganese ore

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Charcoal is a potential alternative to metallurgical coke in manganese ferroalloy production due to its lower fossil-based CO<sub>2</sub> emissions; however, its higher reactivity and volatile content may influence carbon consumption and furnace performance. This paper investigates the devolatilisation behaviour of charcoal and the effect of volatile species on pre-reduction of manganese ore.

Experiments were performed using charcoal alone and in mixtures with Comilog and Nchwaning manganese ore. Samples were heated to 800–1200°C under argon while monitoring the offgas composition.

The charcoal released CO<sub>2</sub>, CO, CH<sub>4</sub> and H<sub>2</sub> at increasing temperatures. Consumption of CO and H<sub>2</sub> was observed in the presence of ore, implying that volatile species contributed to manganese ore prereduction. Comilog ore showed stronger gas-solid interactions and caused higher charcoal consumption compared to Nchwaning ore. Above approximately 750°C, increased CO formation indicated the occurrence of the Boudouard reaction, leading to increased carbon consumption.

## INTRODUCTION

Replacing fossil carbon with charcoal in metal production can reduce fossil CO<sub>2</sub> emissions. However, charcoal differs significantly from metallurgical coke in properties relevant to furnace performance, typically showing reduced mechanical strength, higher volatile matter content, and lower ash content (Jayakumari *et al.*, 2023; Ringdalen and Gaertner, 2024; Tangstad and Ringdalen, 2025). Although high volatile content is generally undesirable due to increased load on furnace offgas systems (Surup *et al.*, 2020), these reactive species may also enhance prereduction by interacting with manganese ores (Schanche *et al.*, 2025; Schanche and Tangstad, 2021).

Devolatilisation of different carbon materials has previously been measured up to 600°C and 800°C where CO, CO<sub>2</sub>, H<sub>2</sub> and CH<sub>4</sub> were measured in the offgas (Ringdalen and Gaertner, 2024). Understanding the composition of these volatiles is important, as several studies have been conducted on the effect of CO and H<sub>2</sub> on prereduction of manganese ores (Davies *et al.*, 2023; Ernst *et al.*, 2024; Larssen *et al.*, 2020; Sarkar *et al.*, 2024; Schanche and Tangstad, 2021; Tangstad *et al.*, 2023). In these works, higher temperature, increased reducing gas concentration and presence of H<sub>2</sub> were all found to improve prereduction performance. Initiation of the prereduction reactions may occur at different temperatures depending on the ore and the reducing gas. Notably, reduction reactions with CO and H<sub>2</sub> have been observed at temperatures as low as 400°C (Larssen and Tangstad, 2022; Ringdalen *et al.*, 2021; Schanche *et al.*, 2025). Prereduction with methane has also been studied (Anacleto *et al.*, 2004a, 2004b), where the reaction between methane and MnO in the ore occurs around 990°C (Elliott and Barati, 2018).

To evaluate whether charcoal-derived volatiles can improve manganese ore prereduction, this work investigates the devolatilisation of charcoal and the interaction of the volatiles with manganese ore.

## MATERIALS AND METHODS

### Raw materials

Commercially available charcoal was sourced and designated as CC-16. The charcoal was analysed according to ISO 562:2013 to determine the volatile matter content, fixed carbon, and ash yield. The ash composition was determined by Inductively Coupled Plasma-Optical Emission Spectroscopy (ICP-OES). The charcoal analysis is given in Table I.

Table I: Chemical analysis of charcoal CC-16

| Proximate analysis | Ash*             | FixC*            | Volatiles*                     | H <sub>2</sub> O               |      |      |      |                   |                  |                               |
|--------------------|------------------|------------------|--------------------------------|--------------------------------|------|------|------|-------------------|------------------|-------------------------------|
|                    | 2.56             | 81.91            | 15.53                          | 1.42                           |      |      |      |                   |                  |                               |
| Ash analysis       | SiO <sub>2</sub> | TiO <sub>2</sub> | Al <sub>2</sub> O <sub>3</sub> | Fe <sub>2</sub> O <sub>3</sub> | CaO  | MgO  | MnO  | Na <sub>2</sub> O | K <sub>2</sub> O | P <sub>2</sub> O <sub>5</sub> |
|                    | 28.9             | 0.02             | 0.4                            | 0.6                            | 35.1 | 14.1 | 0.61 | 0.17              | 15.65            | 4.43                          |

\*Dry basis

Two different manganese ores, Nchwaning and Comilog were selected for the current experimental series. Nchwaning ore contains mostly Mn<sub>2</sub>O<sub>3</sub> - type oxides, Fe<sub>2</sub>O<sub>3</sub> and CaCO<sub>3</sub> while Comilog ore mostly consists of MnO<sub>2</sub> - type oxides, Fe(OH)<sub>2</sub> and SiO<sub>2</sub> (Larssen *et al.*, 2020; Schanche and Tangstad, 2021). Both ores contain additional minor phases or gangue minerals, and both ores are utilised in ferroalloy production. Chemical analysis of the ores was done with X-ray fluorescence (XRF) using the fused bead technique. CO<sub>2</sub> was measured by combustion-IR, and the oxygen/manganese ratio was calculated based on titrimetric analysis (ASTM 465-11:2017). The total manganese, total iron, predominant oxides, CO<sub>2</sub> and O/Mn ratio are given in Table II.

TableII: Chemical analyses of Nchwaning and Comilog ores

| Ore       | SiO <sub>2</sub> | Al <sub>2</sub> O <sub>3</sub> | CaO  | MgO   | K <sub>2</sub> O | Mn    | Fe    | CO <sub>2</sub> | O/Mn |
|-----------|------------------|--------------------------------|------|-------|------------------|-------|-------|-----------------|------|
| Nchwaning | 3.19             | 0.35                           | 8.44 | 0.63  | <0.03            | 43.86 | 13.98 | 2.1             | 1.43 |
| Comilog   | 2.82             | 6.66                           | 0.04 | <0.03 | 0.93             | 50.8  | 2.78  | 0.11            | 1.93 |

### Experimental

Experiments were done using the two manganese ores with and without charcoal, in addition to reference experiments on charcoal alone. 250 g charcoal and 1150 g ore was used in the experiments yielding an ore to charcoal ratio of 4.6:1. This ratio was determined by a mass balance calculation for production of high carbon ferromanganese (HC FeMn), assuming 35% MnO in the slag and 20% C consumption by CO<sub>2</sub>. Hence, the ore to carbon mass ratio was similar to industrial production of HC FeMn. Similar mass balances are reported elsewhere (Larssen *et al.*, 2024). The experimental overview is given in Table III.

The target temperatures were selected based on the pre-reduction process in manganese ferroalloy production. The Boudouard reaction ( $2\text{CO} = \text{C} + \text{CO}_2$ ) is active above 800°C (Olsen *et al.*, 2007; Turkova *et al.*, 2014), hence additional consumption of charcoal by CO<sub>2</sub> is limited below this temperature. Previous research indicates that devolatilisation is incomplete at 800°C (Ringdalen and Gaertner, 2024), motivating an additional experiment for each set of conditions at higher temperature. The charcoal experiment was terminated at 1200°C; however, the experiments containing ore were terminated at 1000°C to avoid sintering. All experiments were conducted with a heating rate of 7°C/min.

Table III. Experimental matrix

| Exp No. | Temperature [°C] | Heating rate [°C/min] | Holding time [min] | Carbon | Mn ore         | Particle size [mm] | Gas flow Ar [NL/min] |
|---------|------------------|-----------------------|--------------------|--------|----------------|--------------------|----------------------|
| 1-0     | 800              | 7                     | 0                  | -      | Nchwaning      | 11.2-20            | 7                    |
| 2-0*    | 800              | 7                     | 0                  | CC-016 | Nchwaning (++) | 11.2-20            | 7                    |
| 2-1     | 800              | 7                     | 0                  | CC-016 | Nchwaning      | 11.2-20            | 7                    |
| 3-0     | 1000             | 7                     | 0                  | CC-016 | Nchwaning      | 11.2-20            | 7                    |
| 4-0     | 1000             | 7                     | 0                  | -      | Nchwaning      | 11.2-20            | 7                    |
| 5-0     | 1000             | 7                     | 0                  | -      | Comilog        | 11.2-20            | 7                    |
| 6-0     | 1000             | 7                     | 0                  | CC-016 | Comilog        | 11.2-20            | 7                    |
| 7-0     | 800              | 7                     | 0                  | -      | Comilog        | 11.2-20            | 7                    |
| 8-0     | 800              | 7                     | 0                  | CC-016 | Comilog        | 11.2-20            | 7                    |
| R1      | 1200             | 7                     | 0                  | CC-016 | -              | 11.2-20            | 7                    |
| R2      | 800              | 7                     | 0                  | CC-016 | -              | 11.2-20            | 7                    |

\*experiment with excess ore = undercoked

Experiments were conducted in a resistance furnace using a Kanthal crucible. The crucible is placed in the centre of the furnace, and four thermocouples measure the temperature at different positions in the crucible, as indicated in Figure 1.

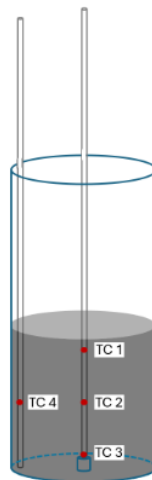


Figure 1. Sketch of the crucible and thermocouple placement

Argon is purged with 7 NL/min through the bottom of the crucible, and the lid has two ports for the installation of thermocouples and offgas extraction during the experiment. The offgas is cooled to remove water and other condensable species prior to gas analysis. The offgas was analysed by gas chromatography (GC) using an Agilent 990 Micro GC. This instrument is equipped with two separate columns and Thermal Conductivity Detectors (TCD). The GC was calibrated with reference gases prior to each experiment.

The samples were weighed before and after the experiments. In the experiments with both ore and carbon, the materials were separated and weighed separately to identify the individual contributions to the weight loss. After the separation of the ore and charcoal, a small amount of inseparable fines

remained that were assessed to contain mostly ore. As such, the weight of these fines was included in the ore weight.

## RESULTS

### Measured temperatures

The crucible is heated from the outside, thus, the highest temperatures are measured close to the edge of the crucible, while the lowest temperatures are recorded by the centre and top thermocouples. As such, there is a significant temperature gradient in the experiments. The edge and centre temperature (TC 4 and TC 2 in Figure 1) from all experiments are shown in Figure 2. The edge temperature shows more consistent temperatures between different charge mixes as it is less affected by exothermal/endothermal reactions in the material compared to the centre temperature measurements. The experiments which were run parallel, but up to different temperatures, closely follow the same temperature trajectory as for example experiments 1-0 and 4-0. The temperature was generally higher in the experiments where only carbon was present compared to experiments with ore and ore-carbon mixtures. There is more than four times the mass of ore compared to carbon in the experiments; hence, more energy needs to be added to achieve the same temperature when ore is present. In experiment 2-0, an excess amount of ore was added, and the temperature in the charge was lower compared to the parallel experiments 2-1 and 3-0.

In the experiments where only ore was present, the temperature at the initial stage is higher compared to the experiments containing ore and charcoal. This difference may be attributed either to the smaller sample size in the only ore experiments, or to the endothermic decomposition reactions occurring in charcoal at low temperatures. In the latter part of the experiment, the ore experiments display a lower temperature compared to the ore + charcoal experiments. Thermal decomposition of oxides and carbonates are endothermic, however reactions between oxygen and reactive species from devolatilisation of charcoal are exothermic and increase the temperature.

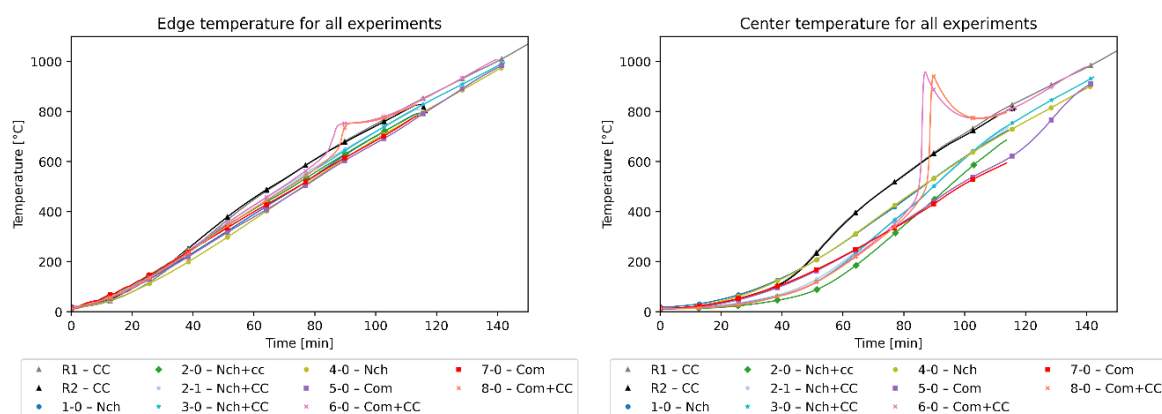


Figure 2. Measured edge and centre temperature (TC 4 and TC 2 in Figure 1) for all experiments.

### Off gas measurements

Hydrogen (Figure 3 and Figure 4) is released in all experiments containing charcoal or ore + charcoal mixes, whereas the ore reference experiments show no detectable hydrogen.  $H_2$  begins to appear around 500°C in the relevant experiments, but the Comilog + charcoal experiments exhibit a slower increase compared to the charcoal reference experiments and Nchwaning + charcoal experiments. This indicates that Comilog ore is more reactive towards hydrogen compared to Nchwaning ore. The charcoal reference experiments follow similar trends, reaching a peak of about 10%  $H_2$  near 800°C.

The three Nchwaning + ore experiments display similar  $H_2$  evolution patterns. Experiments 2-1 and 3-0 have almost identical slopes, until reaching a peak of 6.7% and 5.8%, respectively, at about 800°C. The  $H_2$  content from Experiment 3-0 decreases gradually above 800°C, approaching a concentration of about



% by the end of the experiment. The experiment with excess ore (2-0) follows the same trend, but with lower  $H_2$ , indicating that the excess ore consumes more  $H_2$ .

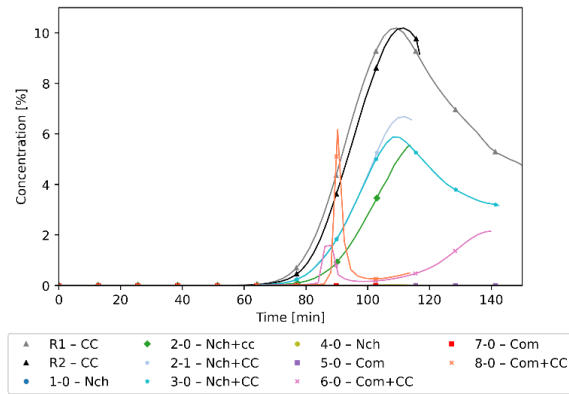


Figure 3.  $H_2$  concentration as a function of time for all experiments.

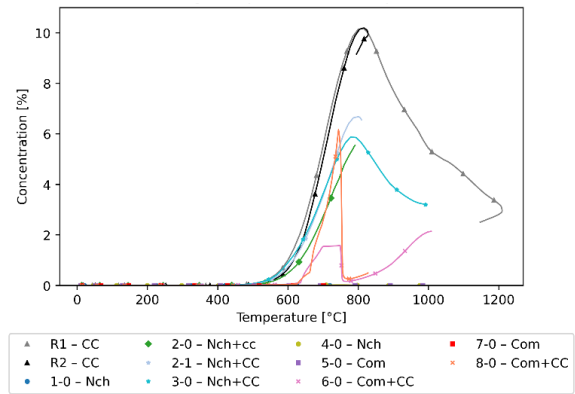


Figure 4.  $H_2$  concentration as a function of edge temperature for all experiments.

The Comilog experiments with charcoal (6-0 and 8-0) show nearly identical slopes until a rapid rise in  $H_2$  concentration around 650°C. Experiment 8-0 peaks at 6.2% near 750°C before rapidly decreasing, while 6-0 appears to plateau at 1.5% for the same temperature span. This discrepancy is likely due to the GC measurements capturing only the start and end of the rapid gas evolution phase. The measured temperatures (Figure 2) indicate a similar behaviour in both experiments; hence it is reasonable to assume that the peak concentrations were similar in both experiments.

After the peak, both experiments show a sharp decline in  $H_2$  concentration to approximately 0.1-0.15% around 800°C, followed by a gradual increase. Experiment 8-0 ends with a concentration of about 0.5% at 800°C, while 6-0 continues increasing towards the end of the experiment. In the latter case, the rise begins to level off towards a peak of approximately 2.1% at 1000°C.

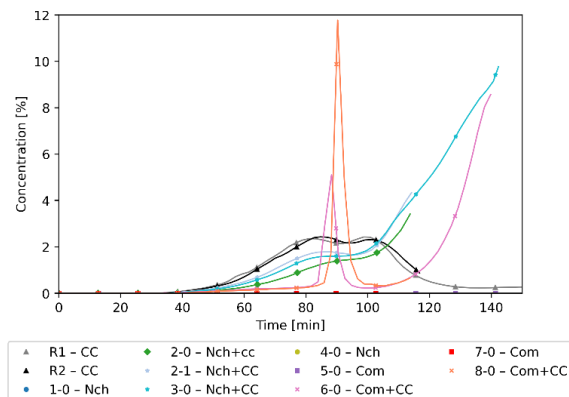


Figure 5. CO concentration as a function of time for all experiments.

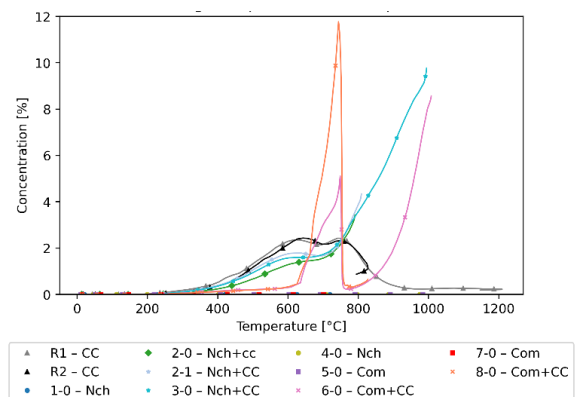


Figure 6. CO concentration as a function of edge temperature for all experiments.

Similarly to hydrogen, carbon monoxide (Figure 5 and Figure 6) only appears in the experiments containing charcoal and ore + charcoal. The experiments with Nchwani and charcoal show that the CO release initiates around 200°C, with a lower CO concentration in the experiment with excess ore – similarly to what was observed with  $H_2$ . The charcoal reference experiments (R1 and R2) behave similarly, and both show an initial rise to approximately 2.4% at 650°C, followed by a slight decrease and a secondary peak of 2.4% around 750°C.

The Comilog and charcoal experiments (6-0 and 8-0), maintains a lower CO concentration compared to the Nchwaning and charcoal experiments. In addition, a rapid rise in CO concentration for the Comilog + charcoal experiments around 650–750°C is observed. Experiment 6-0 peaks at 5%, while 8-0 reaches 11.7%. The lower apparent peak in 6-0 is probably due to the measurement resolution of the GC as discussed previously. Both experiments drop sharply to about 0.3% near 800°C, followed by an increase towards the end of the experiment.

Variations of carbon dioxide concentration is observed across all experiments (Figure 7 and Figure 8). In the experiments with only charcoal or mixtures of ore and charcoal, the concentration begins to increase at approximately 100–150°C, whereas the ore reference experiments exhibit a different behaviour. Both charcoal reference experiments appear to follow a similar trend with one peak of approximately 2% at 400–450°C, followed by a gradual decrease in concentration.

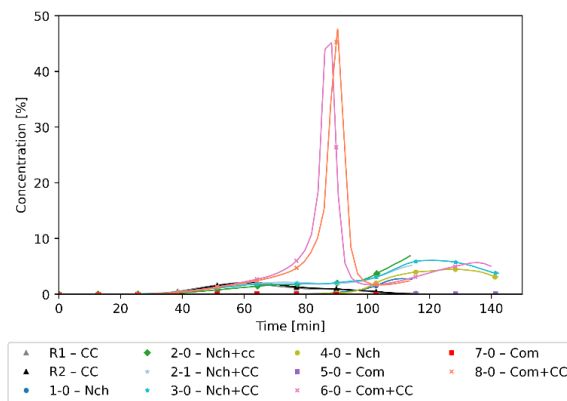


Figure 7. CO<sub>2</sub> concentration as a function of time for all experiments.

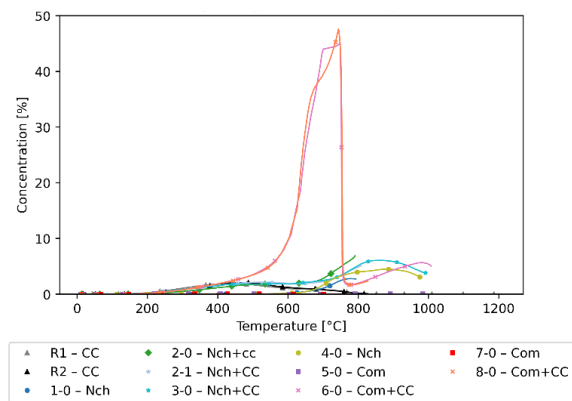


Figure 8. CO<sub>2</sub> concentration as a function of edge temperature for all experiments.

The Nchwaning ore reference experiments follow the same trend, with an increase in concentration from about 500°C. The 800°C experiment reaches a maximum at ca 2.8% as the temperature approaches 800°C. The 1000°C experiment reaches a peak of 4.5% around 900°C before decreasing in concentration towards the end of the experiment. All experiments with Nchwaning ore + charcoal follow a consistent trend, with a gradual increase in concentration to about 2% at approximately 400°C, followed by a peak at 6% around 900°C. The Nchwaning experiment with excess ore (2-0) shows a lower initial rise in CO<sub>2</sub>, but a more pronounced increase in the second peak, ending at almost 7% around 800°C. The increased CO<sub>2</sub> towards the end of experiment 2-0 corresponds to the higher CO consumption observed in Figure 5.

In the Comilog ore experiments, the concentration of CO<sub>2</sub> remains very low throughout and does not exceed 0.1%, as expected due to the absence of carbonates in the ore. In contrast, the CO<sub>2</sub> concentration in experiment 6-0 and 8-0 (Comilog + charcoal) rises sharply around 750°C and reaches 45% and 48%, respectively, corresponding to the previously mentioned rapid gas evolution phase. The concentration drops rapidly to around 2% below 800°C, followed by a gradual increase. Experiment 8-0 ending at 2.4% (800°C) while experiment 6-0 develops a secondary peak approaching 6% as the temperature nears 1000°C. CO<sub>2</sub> is not released from the charcoal at these temperatures, hence the presence of CO<sub>2</sub> indicates the continued reduction of oxides in the ore.

Methane is observed (Figure 9 and Figure 10) in all experiments where charcoal is present. The peak concentration varies between 1.1% as lowest in the Nchwaning + charcoal to 1.7% in the Comilog and charcoal experiments. The methane concentration is generally consistent, all peak concentrations occurring in the same general temperature range between 650°C for charcoal alone and 750°C for Comilog + charcoal. The increased peak temperature may be explained by the relatively larger

temperature gradient when ore is present (Figure 2).

In both charcoal experiments the slope of the curve when the concentration decreases have a bump with slower decrease indicating that the evolution of  $\text{CH}_4$  happens in two steps. This effect may be obscured in the experiments with ore and charcoal due to the high temperature variation in the experiments. The Comilog + charcoal experiments show a faster increase and decrease in concentration than the other experiments and the bumps in the slope are mostly due to the rapid increase in temperature from around 600°C–850°C (edge temperature) and the following higher volume of gas which rapidly empties the crucible and changes the gas composition faster than the GC measures.

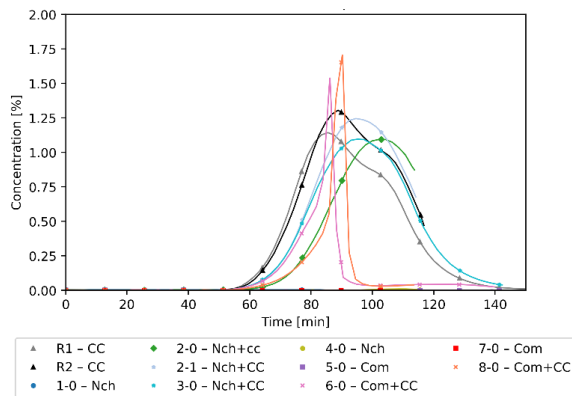


Figure 9.  $\text{CH}_4$  concentration as a function of time for all experiments.

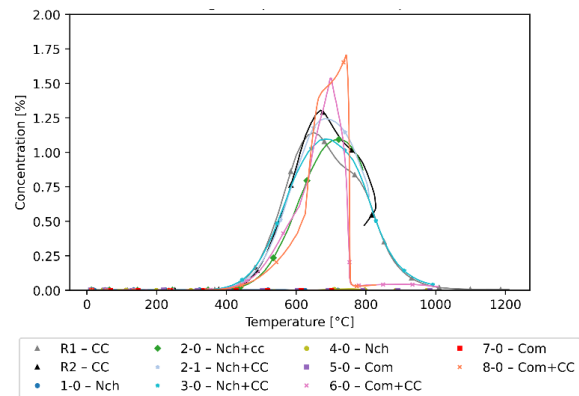


Figure 10.  $\text{CH}_4$  concentration as a function of edge temperature for all experiments.

Figure 11, Figure 12 and Figure 13 show the measured oxygen during the experiments. No oxygen is released during heating of charcoal, but in the reference experiments for Nchwanning and all experiments for Comilog, oxygen can be seen in the offgas measurements. The Nchwanning ore starts to release oxygen when the temperature is around 650°C. The experiment ending at 800°C ends with a concentration of 0.2 %, while the 1000°C experiments end with a concentration of about 2.5% with an increasing trend. The experiments follow a similar trend with temperature. Comilog ore releases more oxygen than the charcoal and Comilog experiments. Comilog ore starts releasing oxygen at around 350–400°C peaking at 18 – 19% at about 700°C. A secondary oxygen emission initiates at around 900°C, increasing until the experiment ends at 1000°C with a concentration of ca 8.7%.

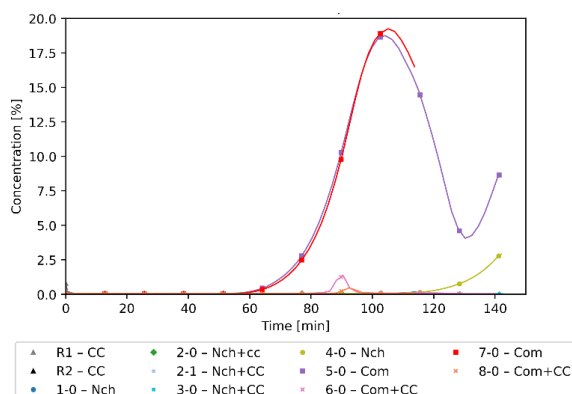


Figure 11.  $\text{O}_2$  concentration as a function of time for all experiments.

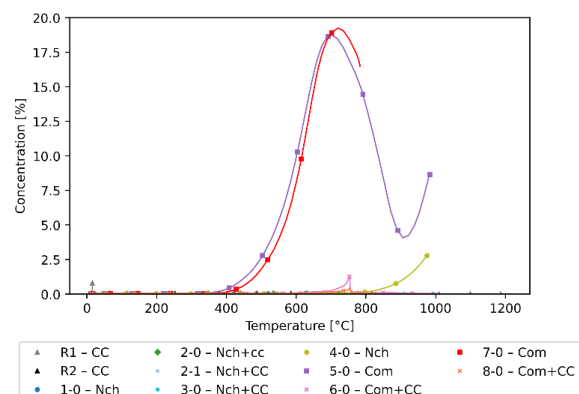


Figure 12.  $\text{O}_2$  concentration as a function of edge temperature for all experiments.

Oxygen is also measured in the experiments with Comilog and charcoal, as seen in Figure 13. Both experiments start releasing oxygen at around 550°C which peaks at 1.35% and 0.45% respectively, at 750°C. The concentration decreases rapidly for both experiments at about 770°C. The oxygen released from the Comilog ore may be caused by the rapid increase in temperature from around 600°C – 850°C (Figure ) which induces a higher gas flow giving the oxygen less time to react with carbon. Experiment 8-0 finishes at 800°C with a concentration close to 0.1%, while 6-0 continues with a slight increase towards 900°C, before decreasing again towards the end of the experiment.

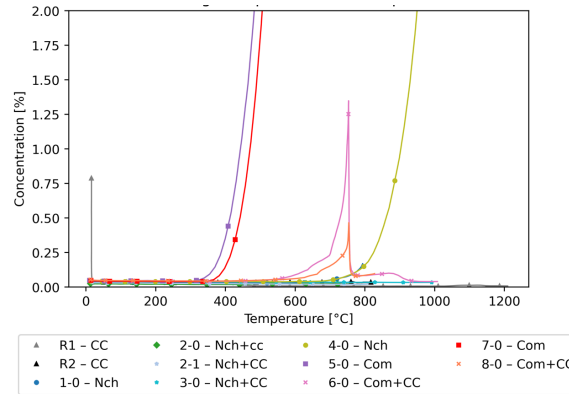


Figure 13: O<sub>2</sub> concentration as a function of edge temperature for all experiments.

The reference experiment on charcoal, showing the concentration of H<sub>2</sub>, O<sub>2</sub>, N<sub>2</sub>, CO, CH<sub>4</sub> and CO<sub>2</sub>, is given in both Figure 14 and Figure 15 with different y-axis range for comparison to the experiments with different ores. It is seen that volatiles are driven off charcoal (CC-16) up to 1000°C and consist of H<sub>2</sub>, CO, CO<sub>2</sub> and CH<sub>4</sub>.

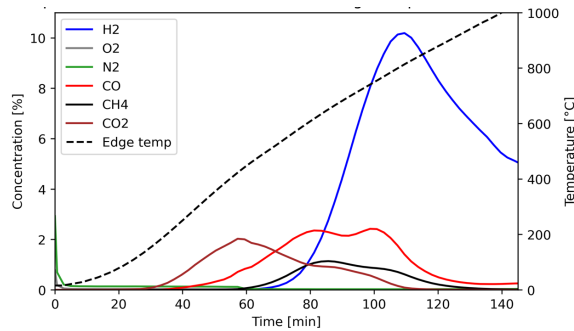


Figure 14. Gas concentrations and edge temperature as a function of time during the heating of charcoal to 1000°C.

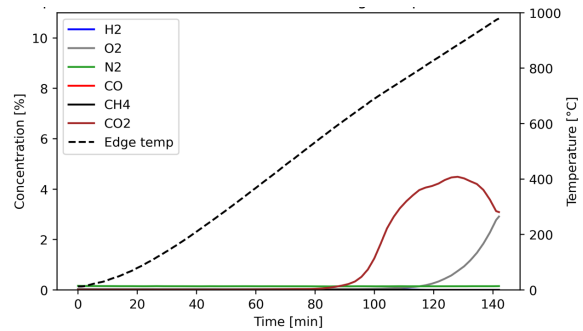


Figure 15. Gas concentration and edge temperature as a function of time during the heating of Nchwaning ore to 1000°C.

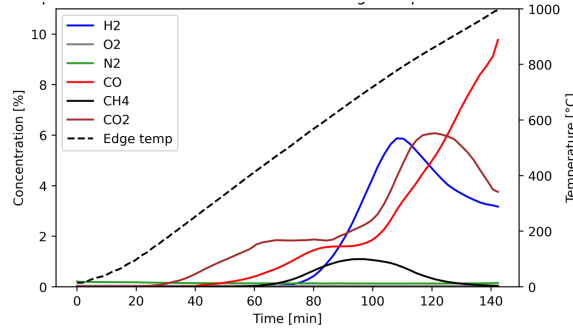


Figure 16. Gas concentration and edge temperature as a function of time during the heating of Nchwaning ore and charcoal to 1000°C.

The experiment with Nchwaning ore (Figure 15) shows that  $\text{CO}_2$  and  $\text{O}_2$  are driven off as the temperature increases.  $\text{CO}_2$  and  $\text{O}_2$  originate from thermal decomposition of carbonates and manganese oxides, respectively. Figure 16 shows the measured volatiles from the Nchwaning + charcoal experiment. The  $\text{CO}_2$  curve in Figure 16 contains the individual contributions of  $\text{CO}_2$  from the charcoal and the ore (Figure 14 and Figure 15). In the latter stage of the experiment, e.g., at 120 minutes, the  $\text{CO}_2$  contribution from charcoal is close to zero (Figure 14) while ore alone yields around 4%  $\text{CO}_2$  (Figure 15) compared to about 6% when the ore and carbon is together. This may partly be explained by CO reacting with the oxides in ore producing  $\text{CO}_2$ , as seen by the reduced CO content in the early stages of the experiment in Figure 16 compared to the contribution from charcoal (Figure 14). However, in the later stages of the experiment, the CO content increases significantly at the same time as the observed  $\text{CO}_2$  increase. In addition to these observations,  $\text{H}_2$  is consumed when ore is present. In previous studies on manganese ore reduction, it has been shown that the water gas shift (WGS) reaction (1), is close to equilibrium at 500°C (Larssen and Tangstad, 2022; Tangstad *et al.*, 2023). Parallel to the WGS-reaction and the reactions between  $\text{H}_2$  and CO with ore, the Boudouard reaction (2) is active above about 800°C (Jayakumari *et al.*, 2023). This indicates a rather complex interaction between the WGS-reaction, Boudouard reaction and oxidation of the gas by reactions with oxides determining the final gas composition in this work. It is however clear from the offgas measurements that there is a higher total content of carbon oxides when the ore and carbon are mixed, indicating excess consumption of carbon, possibly through the Boudouard reaction.



Figure 18 shows the measured offgas during heat treatment of Comilog ore. From around 400°C (60 min) oxygen from decomposition of  $\text{MnO}_2$  is measured in the offgas. A secondary oxygen peak is initiated around 900 °C (110 min) corresponding to the observed decomposition of  $\text{Mn}_2\text{O}_3$  in Nchwaning ore (Figure 15). Comilog ore is known to contain around 5% bound water which decomposes up to 500°C. Water vapour is removed in the condensation trap prior to the gas analysis and will not appear in the gas analysis.

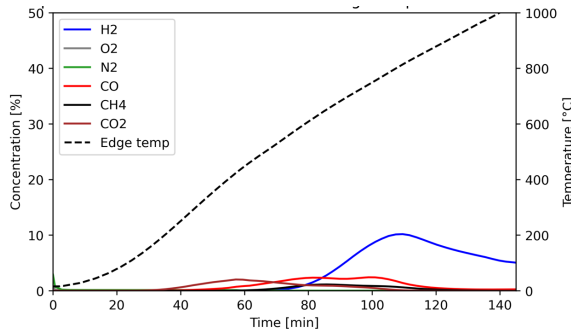


Figure 17. Gas concentrations and edge temperature as a function of time during the heating of charcoal to 1000°C.

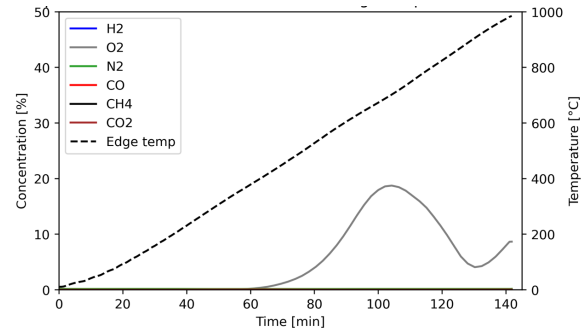


Figure 18. Gas concentration and edge temperature as a function of time during the heating of Comilog ore to 1000°C.

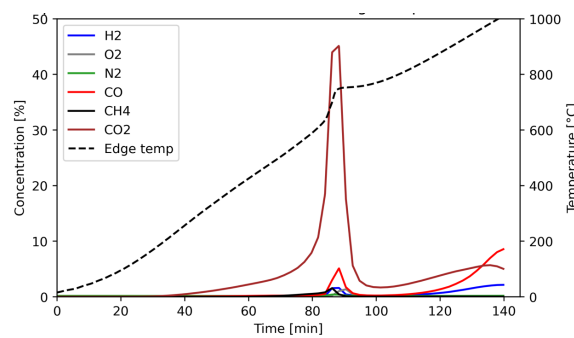


Figure 19. Gas concentration and edge temperature as a function of time during the heating of Comilog ore and charcoal to 1000°C.

In the experiment with Comilog and charcoal, Figure 19, it can be seen that CO is lower in the initial part of the experiment compared to charcoal alone (Figure 17) and the hydrogen content is lower for the duration of the experiment. Comilog ore is more reactive towards CO and H<sub>2</sub>, and has more available oxygen compared to Nchwaning ore (Larssen *et al.*, 2020) which reflects the additional consumption of H<sub>2</sub> and CO in the Comilog experiment with charcoal. Around 90 minutes into the experiment with Comilog and charcoal, rapid chemical reactions between ore and volatile species cause an exothermal peak which rapidly causes a significant temperature increase, which in turn affects the rate of the various chemical reactions. This exothermal peak is common when MnO<sub>2</sub>-type ores are subjected to reactive gases (Larssen *et al.*, 2020; Mukono *et al.*, 2022; Turkova *et al.*, 2014), and is associated with the rapid decomposition and reaction of remaining MnO<sub>2</sub> in the ore. Oxygen from the decomposition may react with the charcoal in addition to the reactive species, contributing to the relatively high CO<sub>2</sub> during the exothermal peak as observed in Figure . In a previous investigation where Comilog ore was reduced non-isothermally in a CO-CO<sub>2</sub> atmosphere without solid carbon present (Ringdalen *et al.*, 2021), oxygen was measured in the offgas when the temperature exceeded 200°C, peaking at an oxygen content of 6.5% when the experiment was terminated at 800°C. Similarly to the experiment with Nchwaning and charcoal, the CO content of the offgas is seen to increase towards the end of the experiment at temperatures where the Boudouard reaction is active.

### Mass change

The weight loss of ore and charcoal from the different experiments are given in Figure 20 and Figure 21, respectively, and increases with experimental temperature for both ore and charcoal. The weight loss of the ore is higher in experiments where charcoal is present (Figure 20). Comilog ore has had higher weight loss compared to Nchwaning ore in all cases, which is as expected, considering the bound water and relatively higher oxygen content of Comilog ore.

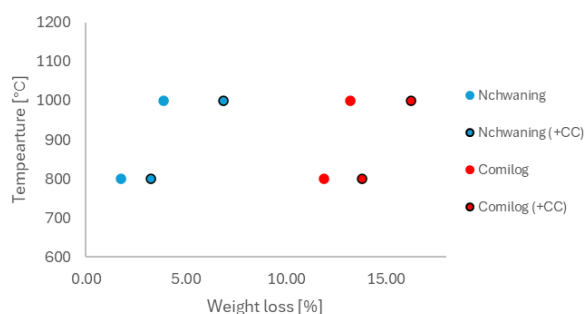


Figure 20. Weight loss of ore (with or without charcoal in charge) paired with end temperature of each experiment.

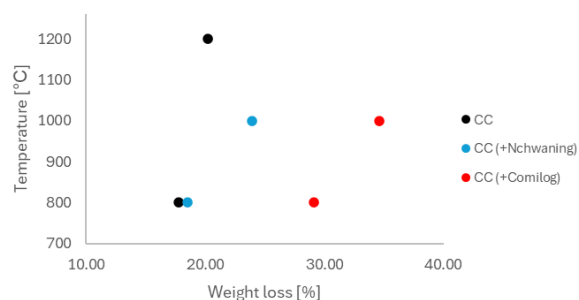


Figure 21. Weight loss of charcoal (with or without ore in charge) paired with end temperature of each experiment.

Charcoal consumption is increased in the presence of either ore type. The charcoal experiences a higher weight loss in the presence of Comilog ore compared to Nchwaning ore. Comilog ore contains significantly more oxygen compared to Nchwaning ore, hence there is a potential for oxygen from decomposition reacting directly with carbon and increased  $\text{CO}_2$  from reduction of oxides reacting with the charcoal via the Boudouard reaction.

## CONCLUSIONS

The offgas from devolatilisation of charcoal was measured with and without the presence of manganese ores.  $\text{CO}_2$  and CO were released from the charcoal as the temperature reached approximately  $150^\circ\text{C}$  and  $200^\circ\text{C}$ , respectively, followed by methane formation around  $400^\circ\text{C}$  and hydrogen evolution at about  $500^\circ\text{C}$ . A good correlation between temperature development and offgas analysis in corresponding regions of different experiments was observed, indicating reproducible results.

The offgas composition was influenced by both charcoal and ore, and interactions between the evolving gases contributed to ore reduction and charcoal consumption. A higher fraction of charcoal was consumed in the presence of Comilog ore compared to Nchwaning ore, although the presence of either ore increased charcoal consumption relative to charcoal alone. Consumption of  $\text{H}_2$  and CO was observed when ore was present, demonstrating that reactive species released during charcoal devolatilisation actively participated in the reduction of manganese oxides. Increased ore addition led to increased consumption of these reactive gases. Comilog ore exhibited higher reactivity towards both CO and  $\text{H}_2$  compared to Nchwaning ore, confirming the higher reactivity of Comilog compared to Nchwaning as reported in previous studies.

At temperatures of approximately  $750^\circ\text{C}$  and above, the Boudouard reaction was observed to occur in the presence of ore, contributing to additional CO formation and further carbon consumption. This indicates that gas-solid reactions between  $\text{CO}_2$ , carbon and ore play an important role in the high-temperature reaction regime.

## ACKNOWLEDGEMENTS

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# Thermal dependence of material properties in furnace tapping models: A FeMn case study

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Multiphysics modelling of furnace tapping in submerged arc furnaces (SAFs) has to date largely assumed that the thermodynamic and physical properties of molten slag and alloy are constant with temperature. This paper presents an exploratory investigation into the validity of this assumption for the case of ferromanganese (FeMn) tapping in the duplex manganese smelting process. A computational multiphysics model incorporating multiphase fluid flow, conjugate heat transfer, and flow through porous media was developed using the OpenFOAM® framework and validated against analytical reduced-order calculations. A temperature-dependent material properties dataset derived from thermochemical simulations and empirical correlations was used, and a sensitivity study was conducted comparing models using constant versus temperature-dependent properties. Results indicate that tapping stream temperatures are insensitive to the assumption of constant properties across all material parameters examined. Phase flowrates are similarly unaffected, with one notable exception: the temperature dependence of slag viscosity was found to dominate the response, accounting for the majority of differences arising from the assumption of temperature-dependent properties in general.

**Keywords:** pyrometallurgy, manganese, furnace tapping, multiphysics modelling

## INTRODUCTION

The pyrometallurgical production of ferroalloys containing chromium, manganese, and silicon is a large industry worldwide. These materials are used in the manufacturing of specialised steels and many other products, and feature on the critical minerals lists of many countries including South Africa.

At present most ferroalloy production is performed using submerged-arc furnaces (SAF), a type of electric furnace with a cylindrical crucible and three vertically mounted self-baking graphite electrodes as described in (Olsen *et al.*, 2007). Alternating current is passed between the electrodes to provide power by resistive heating of the solid charge and molten slag and alloy baths. The charge fed to the furnace builds up to form a porous bed of solid material – the burden – which is continuously consumed by melting and chemical reaction in the hot zones around the electrode tips. Slag and alloy produced by the reactions accumulate in the furnace as a molten bath and must be removed periodically by tapping the furnace. This involves opening a dedicated tap-hole channel in the furnace sidewall, draining a quantity of the molten material through it, and resealing the tap-hole to continue operations. Tapping is one of the most challenging furnace operations for man and machine, and any unexpected inconsistencies in the furnace tapping process can be detrimental to safety as well as causing supply disruptions to downstream processes.

While the furnace tap-hole is open, a number of physical phenomena are important. These include multiphase fluid flow of the immiscible slag and alloy phases, flow through the porous medium of the burden, and heat transfer as the hot molten material moves through the furnace toward the tap-hole.

(Bublik *et al.*, 2021) reviewed modelling approaches for ferroalloy furnace tapping, categorising them into reduced-order models, computational fluid dynamics (CFD), and physical models. Reduced-order models, based on Bernoulli's equation with modifications for particle bed resistance, have seen the most development. Early work incorporated tap-hole channel losses and particle bed effects using the Kozeny-Carman equation. More recent models by (Muller *et al.*, 2015) and (Olsen and Reynolds, 2020) applied these concepts to manganese ferroalloys, with the latter treating slag and metal as separate immiscible phases and employing the Ergun equation for turbulent flow conditions. Early examples of CFD modelling of tapping (Dash *et al.*, 2001) used computational methods to optimise tap-hole geometry to minimise thermal stresses in the blast furnace hearth. Subsequent CFD studies investigated the effects of particle diameter, porosity, and slag viscosity on tapping rates using the Kozeny-Carman formulation (Nishioka *et al.*, 2005). Most CFD work in the field has focused primarily on fluid flow using Volume of Fluid (VOF) methods to track slag-metal interfaces, while multiphysics modelling incorporating heat transfer has been more limited. CFD studies using axisymmetric two-dimensional models (Muller and Steenkamp, 2013) demonstrated that heat transfer coefficients from the tapping stream to tap-hole walls can be very high, and heat transfer rates were likely to be limited by the furnace refractory and cooling systems. (Olsen *et al.*, 2018) performed coupled three-dimensional heat and fluid flow simulations for manganese furnace tap-holes, demonstrating that water-cooled systems are more effective than natural cooling and showing significantly higher tapblock temperatures during tapping compared to non-tapping periods due to convection effects. Beyond ferroalloys, extensive blast furnace work has coupled thermal and fluid analyses. (Cheng *et al.*, 2005) examined three-dimensional iron flow and heat transfer in the blast furnace hearth during tapping, solving the turbulent Navier-Stokes equation coupled with the energy transport equation under conjugate heat transfer and natural convection, while (Ge *et al.*, 2020) investigated temperature distributions in main troughs using coupled energy and momentum equations. These blast furnace studies demonstrated significant thermal stress development near tap-holes and temperature stratification in flowing metal.

A common theme in the majority of furnace tapping modelling to date is that it has been performed with the assumption that the thermodynamic and physical properties of the molten slag and alloy fluids are constant with temperature, thereby partially decoupling the heat transfer and fluid flow components of the models. In this paper the authors conduct an exploratory investigation into the validity of this assumption. Computational multiphysics models of the conjugate fluid flow and heat transfer during furnace tapping are constructed to assess the importance of accounting for the temperature dependence of slag and alloy properties. The coupled multiphysics models are validated against simple analytical and reduced-order calculations to verify that they reproduce the correct flow physics. Temperature-dependent material properties estimated from thermochemical simulation tools are then applied, and results from simulations using constant properties are compared to models using temperature-dependence effects to identify similarities and differences.

## MODEL DESCRIPTION

In the present work the ferromanganese step of the duplex manganese process (Tangstad *et al.*, 2022) has been used as an example case study. The duplex process consists of two SAF units arranged in a sequential flowsheet, with the slag from the ferromanganese (FeMn) furnace used as a feed material into the silicomanganese (SiMn) furnace. This is shown schematically in Figure 1.

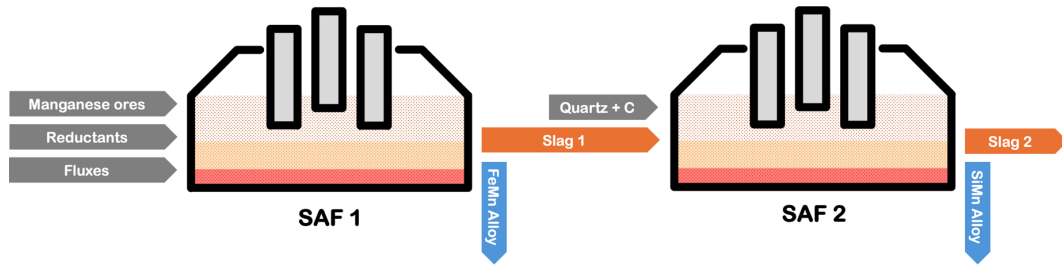


Figure 1: Duplex process for production of FeMn and SiMn.

The FeMn SAF (SAF 1) is operated in semi-batch mode, with continuous choke-fed feeding of raw materials replenishing the burden from above, and intermittent tapping to remove the produced slag and alloy. Per (Tangstad *et al.*, 2022) such furnaces are generally tapped at intervals of two to four hours, and the tap-hole is open for between 15 and 30 minutes during tapping.

### Material properties

Physical properties for representative FeMn alloys and slags in the duplex process were calculated as functions of temperature in (Olsen, 2026), where the methods used included thermochemical calculations as well as analytical and empirical property models. This data set is shown in Figure 2. Although solid-liquid phase transition temperature is not a fixed value for chemically complex materials, indicative values for use in modelling have been identified (*ibid.*) as 1432°C (1705 K) for slag, and 1142°C (1415 K) for alloy. Limited data are available for the temperature dependence of interfacial surface tension between alloy and slag in the FeMn system, and consequently this property was taken to be constant at a value of 0.5 N/m (Bublik *et al.*, 2022).

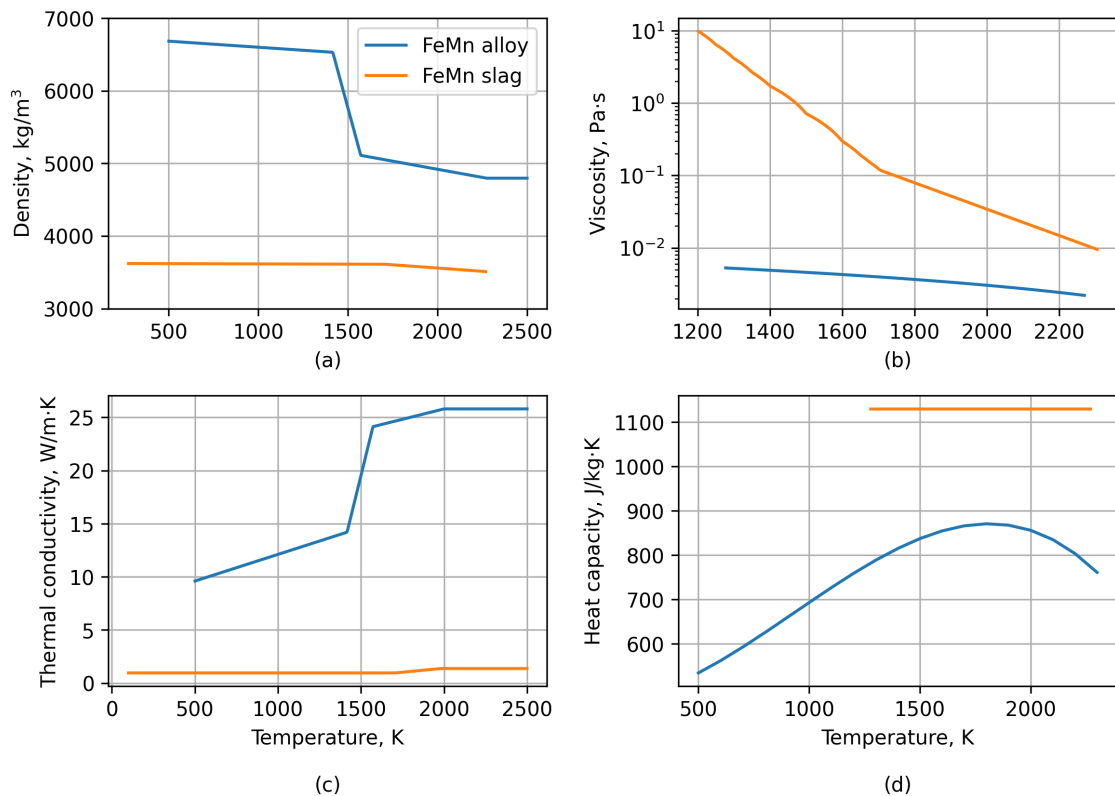


Figure 2: Temperature-dependent material properties for FeMn alloy and slag, (a) densities, (b) viscosities, (c) thermal conductivities, and (d) heat capacities.

Physical properties of the solid burden material forming the porous medium inside the furnace were also assumed to be constant with temperature. Based on values reported in (Ksiazek *et al.*, 2011) the solid raw material's density was specified as 3750 kg/m<sup>3</sup>, its heat capacity was 1000 J/kg.K, and its thermal conductivity was 2.5 W/m.K. Investigation of the impact of temperature-dependent surface tensions and solid material properties is recommended for future studies in this area.

### Computational model

The problem of furnace tapping from FeMn and other SAFs is one of multiphase fluid flow coupled with heat transfer in the presence of porous media. Using VOF formulation for multiphase flow, the governing equations are given in [1]-[3] below. It should be noted that the rate of heat exchange between the solid material of the porous medium and the fluid was assumed to be high in this study, and the two are treated as isothermal with each other at all times – this permits the heat transfer for both media to be described in a single energy balance. The validity of this assumption should however be examined in future work.

$$\begin{aligned} \frac{\partial}{\partial t}(\epsilon \rho \mathbf{u}) + \nabla \cdot (\epsilon \rho \mathbf{u} \otimes \mathbf{u}) + \epsilon \nabla P &= \nabla \cdot \epsilon \boldsymbol{\tau}_{ij} + \epsilon(\rho \mathbf{g} + \mathbf{F}_{st} + \mathbf{F}_D) \\ \frac{\partial \epsilon \rho}{\partial t} + \nabla \cdot (\epsilon \rho \mathbf{u}) &= 0 \\ \frac{\partial \epsilon \alpha_i}{\partial t} + \nabla \cdot (\epsilon \alpha_i \mathbf{u}) &= 0 \end{aligned} \quad [1]$$

$$\begin{aligned} \frac{\partial}{\partial t}[(\epsilon \rho C_p + (1 - \epsilon) \rho_s C_{ps})T] + \nabla \cdot (\epsilon \rho C_p \mathbf{u} T) + \frac{\partial}{\partial t}(\epsilon \rho K) + \nabla \cdot (\epsilon \rho \mathbf{u} K) + \nabla \cdot (\mathbf{u} P) \\ = \nabla \cdot [(\epsilon \kappa + (1 - \epsilon) \kappa_s) \nabla T] + \epsilon(\mathbf{F}_D \cdot \mathbf{u}) \end{aligned} \quad [2]$$

$$\begin{aligned} \mathbf{F}_{st} &= - \sum_i \sum_{j < i} \gamma_{ij} \nabla \cdot \left( \frac{\alpha_i \nabla \alpha_j - \alpha_j \nabla \alpha_i}{|\alpha_i \nabla \alpha_j - \alpha_j \nabla \alpha_i|} \right) (\alpha_i \nabla \alpha_j - \alpha_j \nabla \alpha_i) \\ \mathbf{F}_D &= - \left( \frac{150 \mu (1 - \epsilon)^2}{d_c^2 \epsilon^3} + \frac{1.75 \rho (1 - \epsilon) |\epsilon \mathbf{u}|}{d_c \epsilon^3} \right) \epsilon \mathbf{u} - 0.34 \rho \left( \frac{1 - \epsilon}{\epsilon} \right) \frac{\partial \mathbf{u}}{\partial t} \end{aligned} \quad [3]$$

In the mass and momentum balance [1],  $\epsilon$  is the porosity field,  $\rho$  is material density,  $\mathbf{u}$  is the fluid velocity vector field (actual velocity and not superficial),  $P$  is the pressure field,  $\boldsymbol{\tau}_{ij}$  is the fluid viscous stress tensor,  $\mathbf{g}$  is the gravitational acceleration vector,  $\mathbf{F}_{st}$  is the surface tension force vector,  $\mathbf{F}_D$  is the drag force vector opposing flow through the porous medium, and the  $\alpha_i$  are the fluid phase fraction fields. In the energy balance [2],  $C_p$  is the material heat capacity,  $T$  is the temperature field,  $\kappa$  is the material thermal conductivity, and  $K = \frac{1}{2} |\mathbf{u}|^2$  is the specific kinetic energy in the flow field. In the term expressions [3],  $\gamma_{ij}$  is the interfacial surface tension between phases  $i$  and  $j$ ,  $\mu$  is the fluid viscosity, and  $d_c$  is the effective particle diameter of the particles making up the porous medium. For all material properties the subscript  $s$  denotes the solid particles making up the porous medium, while no subscript denotes the fluid phase.

Due to the complexities of the governing physics and furnace geometries, it is necessary to obtain numerical solutions of [1]-[3]. This was performed with the finite-volume method (FVM) framework implemented in the OpenFOAM® open source computational mechanics platform. Calculations are performed on unstructured meshes using a segregated field solution approach, with iterative pressure-velocity coupling algorithms such as PISO and PIMPLE (Keysight Technologies Inc., 2025). FVM VOF methods with interfacial surface tension force smoothing were used to account for the multiphase nature of the fluid flow, and a novel conservative approach to the formulation of the porosity terms in the governing equations developed by (Oxtoby *et al.*, 2013) was extended to include coupled heat transfer and variable material properties. The solver is open source software and freely available in online code repositories (Oxtoby, 2026).

### Case description and modelling methodology

For the present study, a wedge-shaped portion of the furnace in the vicinity of the tap-hole was modelled. The simplified geometry consists of a plenum/inlet region representing the furnace interior, the tap block face, and the tap-hole channel through which flow occurs during tapping (see Figure 3). Only the fluid region was included in the model – conduction heat transfer effects in the refractory lining and tap block were approximated using lumped heat transfer coefficients at the wall boundaries.

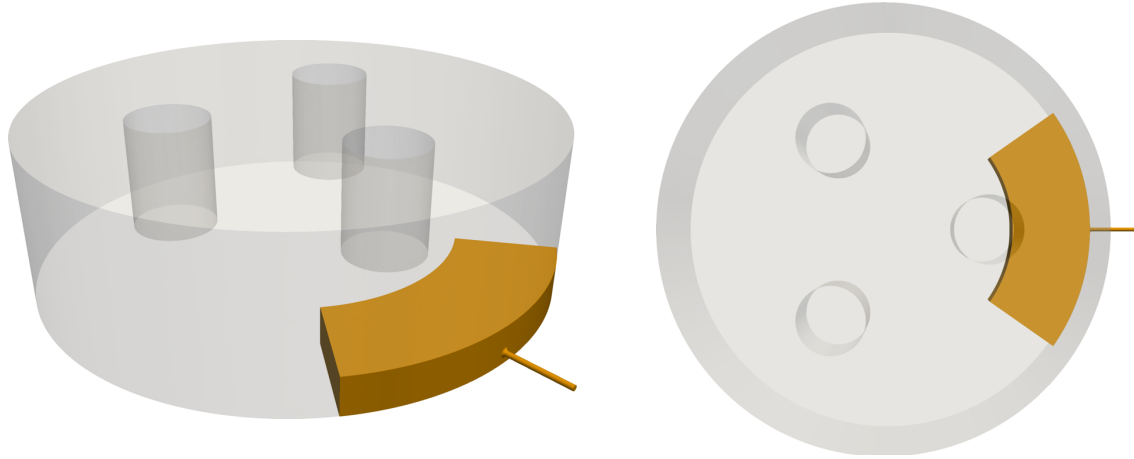


Figure 3: Visualisation of a simplified SAF showing location of region used for computational models.

The model region is shown in more detail in Figure 4. Boundary conditions are specified as follows: the side and top boundaries of the plenum are set as free slip boundaries, to represent far field axisymmetric flow in the furnace and the free-surface phase interface at the top of the slag bath respectively. The back of the plenum closest to the centre of the furnace is set as a free inflow/outflow boundary to allow movement of material from the furnace interior toward the tap-hole, and any inflow is set to a constant temperature representing an average bulk temperature in the active zone between the electrodes. The front of the plenum and tap-hole channel are divided into three sections representing the furnace sidewalls, the hot face of the tap block, and the tap-hole channel itself. These are all specified as no-slip walls with a lumped parameter heat transfer coefficient (HTC) based on the refractory design in each area. The end face of the tap-hole channel represents the outlet and is set either to an insulating wall boundary condition (when the tap-hole is closed) or a pressure-driven outflow boundary condition (when the tap-hole is open).

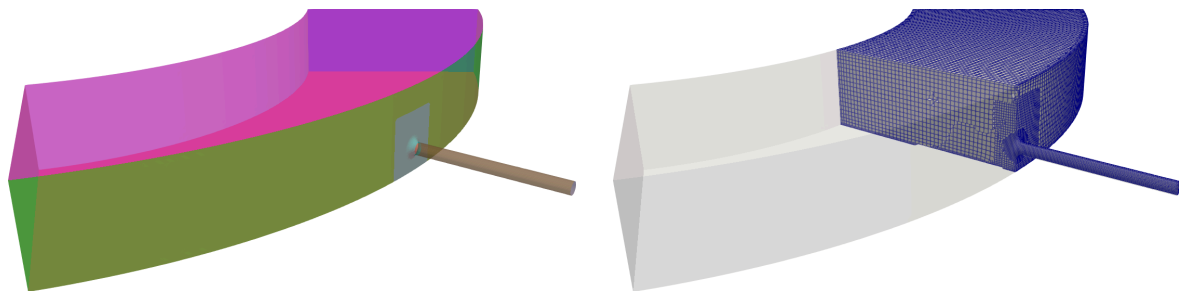


Figure 4: (l) model region showing boundary surfaces, (r) example computational mesh.

Each simulation case is set up with an initial distribution of slag and metal phases, and a predefined porosity field. Examples are shown in

Figure . Cases are executed in two stages: first, steady-state flow and temperature fields due to natural convection circulation inside the furnace are calculated with the tap-hole outlet closed and the tap-hole blocked with material (simulated by setting the porosity in the tap-hole channel to a very low value,

typically 0.1). In the second stage the tap-hole channel outlet is opened, and the channel region is set to a porosity of 1 to allow free flow out of the furnace. This stage is simulated for sufficient time to capture the transient behaviour of the tapping flow after initial opening (typically 300 s). The evolution of the tapping temperatures and flowrates during the second stage is recorded for comparison with other cases.

In all cases, a convolution filter with a window length of 5 s was applied to the temperature and flowrate data calculated in the second stage. This was done in order to reduce high-frequency noise ( $> 10$  Hz) arising from Kelvin-Helmholtz shear instabilities at the phase interface in the converging flow near the tap-hole entrance. Although this noise does represent physically realistic behaviour, it distracts from the mean trends in the tapping flow which are of more interest in the present study.

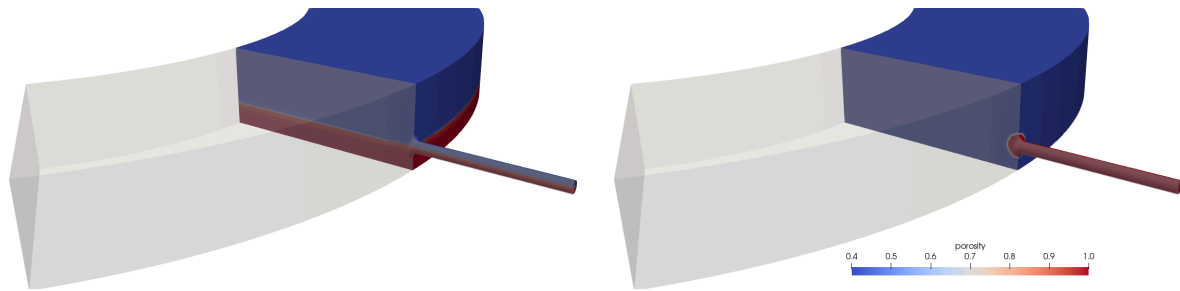


Figure 5: (l) initial slag (blue) and metal (red) phase fields, (r) burden porosity field showing tap-hole entry and channel regions (red) vs bulk burden region (blue).

The full set of parameters selected for the model cases is given in Table . These were intended to be broadly representative of manganese smelting furnaces in general rather than any specific plant, and were estimated with the aid of a range of sources (Muller and Steenkamp, 2013; Nelson and Hundermark, 2016; Olsen *et al.*, 2018; Reynolds *et al.*, 2022; Tangstad *et al.*, 2022).

Table I: Parameters used in CFD model

| Parameter                         | Value                   | Parameter                             | Value                   |
|-----------------------------------|-------------------------|---------------------------------------|-------------------------|
| Furnace diameter                  | 9.68 m                  | Plenum inner diameter <sup>1</sup>    | 6 m                     |
| Tap-hole diameter                 | 0.1 m                   | Plenum wedge angle                    | 70°                     |
| Tap-hole length                   | 1.25 m                  | Tap-hole elevation <sup>2</sup>       | 0.2 m                   |
| Tap-hole wear radius <sup>3</sup> | 0.05 m                  | Tap block height                      | 0.5 m                   |
| Alloy level                       | 0.2 m                   | Slag level (= plenum height)          | 0.7 m                   |
| Sidewall HTC <sup>4</sup>         | 5 W/m <sup>2</sup> K    | Tap block HTC                         | 20 W/m <sup>2</sup> K   |
| Tap-hole channel HTC              | 40 W/m <sup>2</sup> K   | Upper surface HTC                     | 0.25 W/m <sup>2</sup> K |
| Hearth HTC                        | 0.75 W/m <sup>2</sup> K | Ambient temperature                   | 298 K                   |
| Burden porosity                   | 0.4                     | Burden particle diameter              | 0.01 m                  |
| Burden particle sphericity        | 0.6                     | Tap-hole cavity diameter <sup>5</sup> | 0.2 m                   |

<sup>1</sup>Calculated as electrode pitch circle diameter plus electrode diameter

<sup>2</sup>All levels and elevations are referenced to the hearth level (bottom surface of the plenum)

<sup>3</sup>Fillet radius on the perimeter of the tap-hole channel at the entrance, representing wear after use

<sup>4</sup>Heat transfer coefficients estimated using refractory properties and dimensions from sidewall and tap block designs in literature

<sup>5</sup>Size of region in front of tap-hole entrance which is free of particles during tapping

## RESULTS AND DISCUSSION

A set of basic numerical tests using the SAF tapping CFD model was first performed to ensure that its behaviour is representative for the problem at hand. This included a mesh dependence study, and verification of single-phase fluid flow behaviour against analytical models. The inlet temperature boundary conditions in the model were then calibrated to produce a target tapping temperature. Lastly



the calibrated models were used in a sensitivity study to identify whether the impact of temperature-dependent material properties is significant in tapping flows, and if so, which properties have the strongest effect.

### Numerical testing and calibration

The accuracy of results obtained in FVM CFD modelling is dependent in part on the spatial resolution of the mesh on which the numerical approximations are calculated. Solutions on larger meshes consume significantly more time and computational resources, and it is therefore desirable to choose a smaller mesh (coarser resolution) on which errors in the results of interest remain acceptable. For the SAF tapping model, a limited set of cases was executed with mesh resolutions  $\Delta_m$  in the tap-hole region between 0.75 cm and 1.25 cm. All model parameters were as per Table I. The furnace temperature  $T_f$  (the fluid inlet temperature at the back of the plenum) was set to 1833 K, and temperature-dependent properties were used per Figure 2. The results from the dynamic flow simulation during tapping are given in Figure 6.

While the average tapping flowrates for both phases are largely unaffected by mesh resolution, it has a noticeable effect on the predicted steady-state and dynamic behaviour of the temperature field in the furnace. It can be seen from Figure 6(a) and (c) particularly that full mesh independence has not been reached at these resolutions. However, similar trends over time are predicted in all cases for resolutions at 1 cm or better, and the absolute differences in the predictions remain relatively small (of the order of 5-10°C or less). A mesh resolution of 1 cm was selected for the remainder of the study.

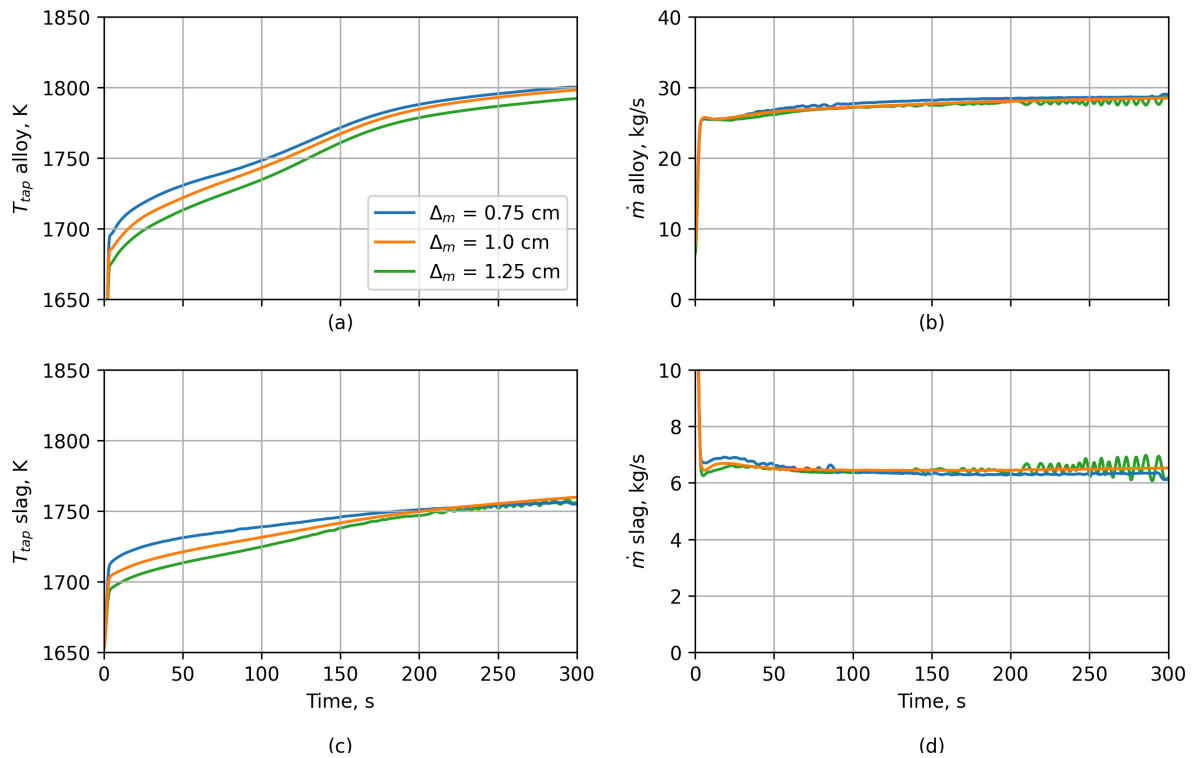


Figure 6: Evolution of (a) alloy tapping temperature, (b) alloy flowrate, (c) slag tapping temperature, and (d) slag flowrate during first 300 s of tapping phase at different mesh resolutions.

For additional numerical testing of CFD models, it is useful to compare the results they predict against analytical or empirical data for similar cases. Although such data are not readily available for the coupled heat transfer component, expressions describing isothermal flow of fluids with constant properties through heterogenous porous beds have been described previously by the authors (Olsen and Reynolds, 2020) and will be used here. These expressions comprise analytical pressure balances for each section of the flow including converging inflow through the porous burden and pipe flow losses

in the tap-hole channel. Constant physical properties for FeMn slag and alloy were interpolated from the data in Figure 2 at a fixed temperature of 1500°C (1773 K), a representative tapping temperature for FeMn SAF processes which has been used elsewhere in modelling work (Muller and Steenkamp, 2013). The results are given in Table II.

Table II: Interpolated FeMn slag and alloy property values at 1773 K

| Property                   | Value                  | Property                  | Value                  |
|----------------------------|------------------------|---------------------------|------------------------|
| Alloy density              | 5020 kg/m <sup>3</sup> | Slag density              | 3599 kg/m <sup>3</sup> |
| Alloy viscosity            | 0.003743 Pa.s          | Slag viscosity            | 0.08886 Pa.s           |
| Alloy heat capacity        | 869.5 J/kg.K           | Slag heat capacity        | 1130 J/kg.K            |
| Alloy thermal conductivity | 24.91 W/m.K            | Slag thermal conductivity | 1.061 W/m.K            |

Steady-state isothermal CFD simulations of fluid flow during tapping of pure slag and pure alloy (single phase systems) were conducted using the properties above together with the parameters in I. A fixed pressure difference between the furnace interior and the tap-hole outlet was imposed to drive the flow. The resulting mass flowrates were converted into average tapping velocities and compared with those calculated using the analytical pressure balance expressions.

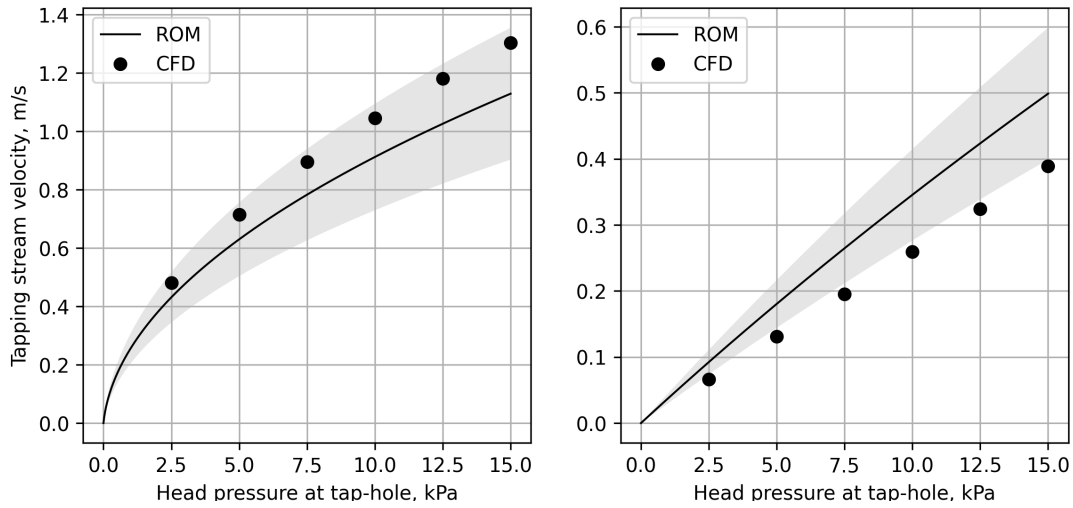


Figure 7: Comparison between CFD model and analytical pressure balance model (ROM), (l) pure alloy tapping, (r) pure slag tapping (grey bands show  $\pm 20\%$  of the analytical values).

The results of the validation comparison are shown in Figure 7, where it can be seen that the CFD simulations are within approximately 20% of the analytically calculated values. Given that the analytical expressions make a number of simplifying approximations concerning the geometry of the problem and symmetry of the flow field which may decrease their accuracy in this case, this is a reasonable level of agreement. Most importantly, the shape of the curves is also correctly reproduced – a parabolic pressure-velocity response in the case of alloy tapping (momentum dominated flow), and a more linear response in the case of slag (viscous drag dominated flow).

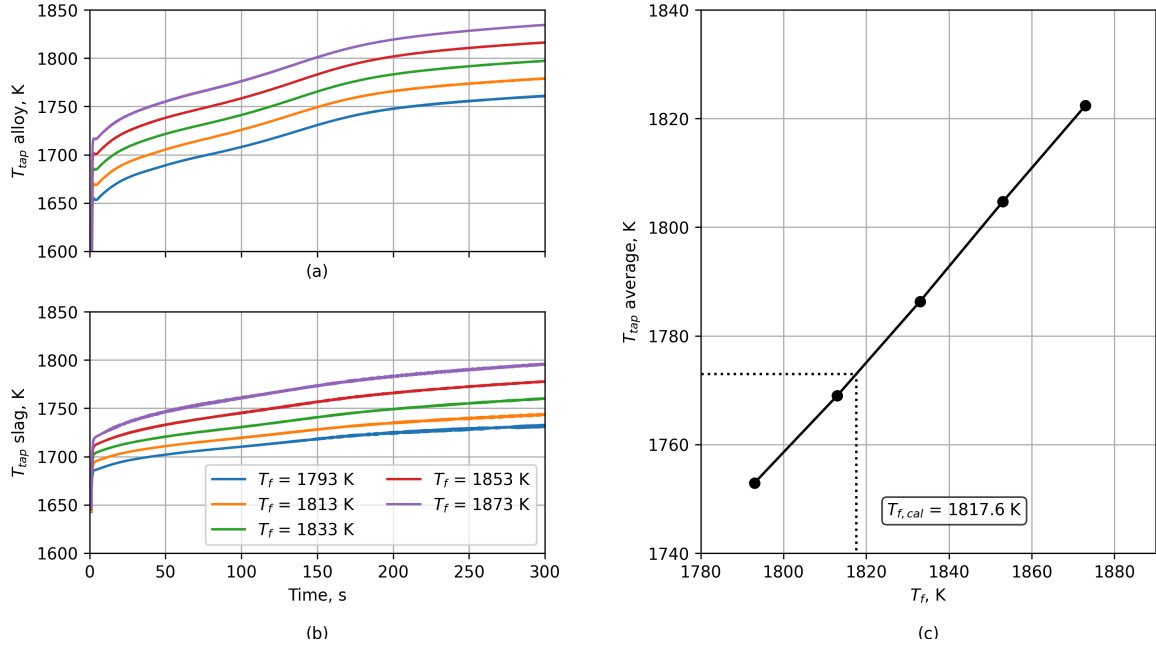


Figure 8: (a), (b) Evolution of tapping temperature in CFD models for different values of  $T_f$ , (c) calibration of  $T_f$  to reach an average tapping temperature of 1773 K at 300 s.

Finally, to prepare the model for use in the sensitivity study, some calibration of the boundary conditions was required. Since the region being modelled represents only a portion of the furnace interior, it is necessary to supply “far-field” conditions which represent the bulk state of the furnace outside the model domain during the startup period of a tap. This amounts to fixing the conditions on the inflow/outflow boundary at the back of the plenum. The phase fields were specified as fixed levels per the starting conditions given in Table I, and the inflow boundary fluid temperature  $T_f$  was chosen such that the average temperature of the tapping stream at the outlet 300 s after opening the tap-hole was equal to the representative 1773 K tap temperature mentioned above. The simulation results used for calibration of  $T_f$  are shown in Figure 8, and a calibrated furnace temperature of 1817.6 K was obtained.

### Sensitivity to temperature-dependent material properties

With the CFD model tested and calibrated, a sensitivity study was conducted to evaluate the impact of temperature dependence in the material properties. As a reference point full tapping simulations were conducted for a case with all properties treated as functions of temperature (per Figure 2), and a case with all properties treated as constant values (per Table II).

Comparative visualisations of the temperature and flow fields for the two cases are shown in Figure 9 and Figure 10. Qualitatively they appear very similar, with only slight variations in the strong converging flows in the vicinity of the tap-hole.

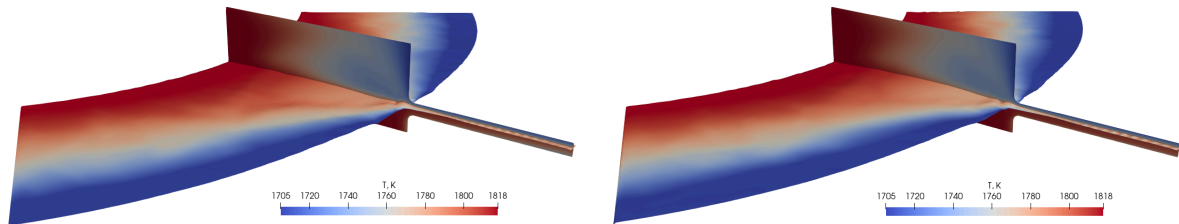


Figure 9: Temperature fields at 300 s on slag-alloy interface and vertical slice through tap-hole centreline, (l) all material properties  $f(T)$ , (r) all material properties constant.

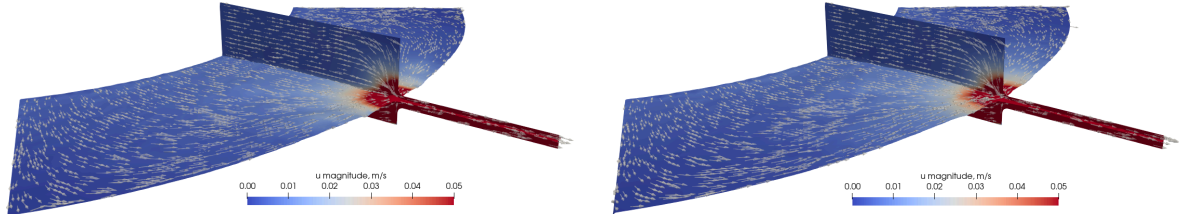


Figure 10: Velocity fields at 300 s on slag-alloy interface and vertical slice through tap-hole centreline, (l) all material properties  $f(T)$ , (r) all material properties constant.

A comparison of the quantitative results from the two cases is given in Figure 11. It is interesting to see that temperatures in the tapping stream are almost identical, but there are some significant differences in the phase flowrates. Alloy and slag tapping are both overpredicted when constant properties are used, with differences in slag rates more significant during early phases of the tap immediately after flow is initiated, and differences in alloy rates increasing further into the tap.

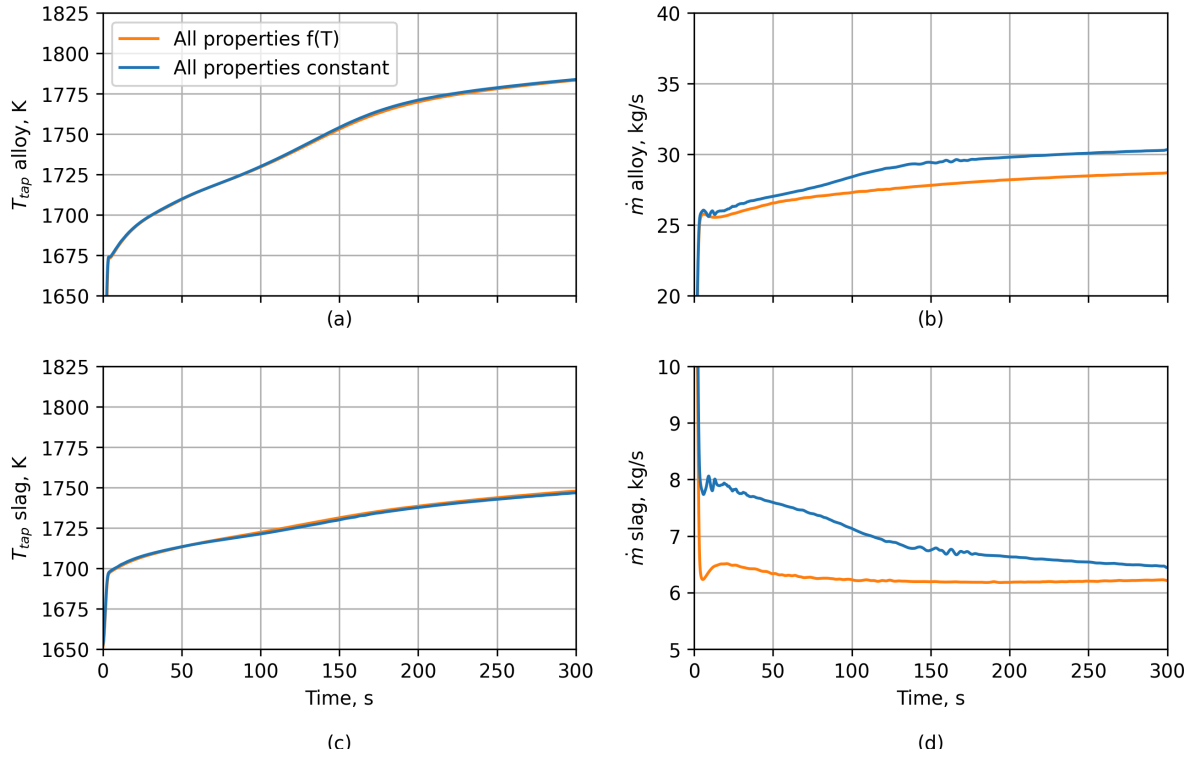


Figure 11: Evolution of (a) alloy tapping temperature, (b) alloy flowrate, (c) slag tapping temperature, and (d) slag flowrate during first 300 s of tapping comparing constant vs  $f(T)$  material properties.

From this point, the effect of the temperature dependence of individual properties was investigated by holding all properties at constant values except for the one being examined. Simulations were conducted in this way for eight additional cases – one for each combination of material (slag, alloy) and material property ( $\rho$ ,  $\mu$ ,  $\kappa$ ,  $C_p$ ). The mean average percentage error (MAPE) for each output variable over the 0 to 300 s duration of the simulation was calculated against the reference case with all properties as  $f(T)$ , assumed to be the ground truth. The results are given in Table III.

Table III. MAPE in % of temperature and flowrate traces vs all properties  $f(T)$  case

| Case                                                | $T_{tap\ alloy}$ | $T_{tap\ slag}$ | $\dot{m}\ alloy$ | $\dot{m}\ slag$ |
|-----------------------------------------------------|------------------|-----------------|------------------|-----------------|
| All constant                                        | 0.0441           | 0.0520          | 4.66             | 11.7            |
| Only $\rho$ alloy $f(T)$                            | 0.0289           | 0.0366          | 4.62             | 12.0            |
| Only $\mu$ alloy $f(T)$                             | 0.0517           | 0.0481          | 4.69             | 11.7            |
| Only $\kappa$ alloy $f(T)$                          | 0.0440           | 0.0520          | 4.66             | 11.7            |
| Only $C_p$ alloy $f(T)$                             | 0.0431           | 0.0520          | 4.66             | 11.7            |
| Only $\rho$ slag $f(T)$                             | 0.0328           | 0.0545          | 4.41             | 12.4            |
| <b>Only <math>\mu</math> slag <math>f(T)</math></b> | <b>0.0229</b>    | <b>0.0305</b>   | <b>0.718</b>     | <b>1.53</b>     |
| Only $\kappa$ slag $f(T)$                           | 0.0441           | 0.0510          | 4.66             | 11.7            |
| Only $C_p$ slag $f(T)$ <sup>1</sup>                 | 0.0441           | 0.0520          | 4.66             | 11.7            |

<sup>1</sup>Note that the slag heat capacity available in the dataset shown in Figure is already constant, and these results are therefore identical to the 'All constant' case

It can be seen that the errors in the tapping temperatures are very small for all variants, as anticipated by the results in Figure 11; assuming constant values for any or all of the physical properties make almost no difference to phase temperatures in the tapping stream. The errors in the predicted phase flowrates are not significantly affected by the assumption of constant or variable properties and are approximately 5% and 12% for alloy and slag respectively in most cases. The major exception to this is the slag viscosity as highlighted – this property alone has large impact on the results and brings the tapping model predictions much closer to the reference case. This can be verified with the output variable trends in Figure 12, which shows that the behaviour of the CFD model with all material properties constant except for the slag viscosity closely matches that of the model with all properties varying.

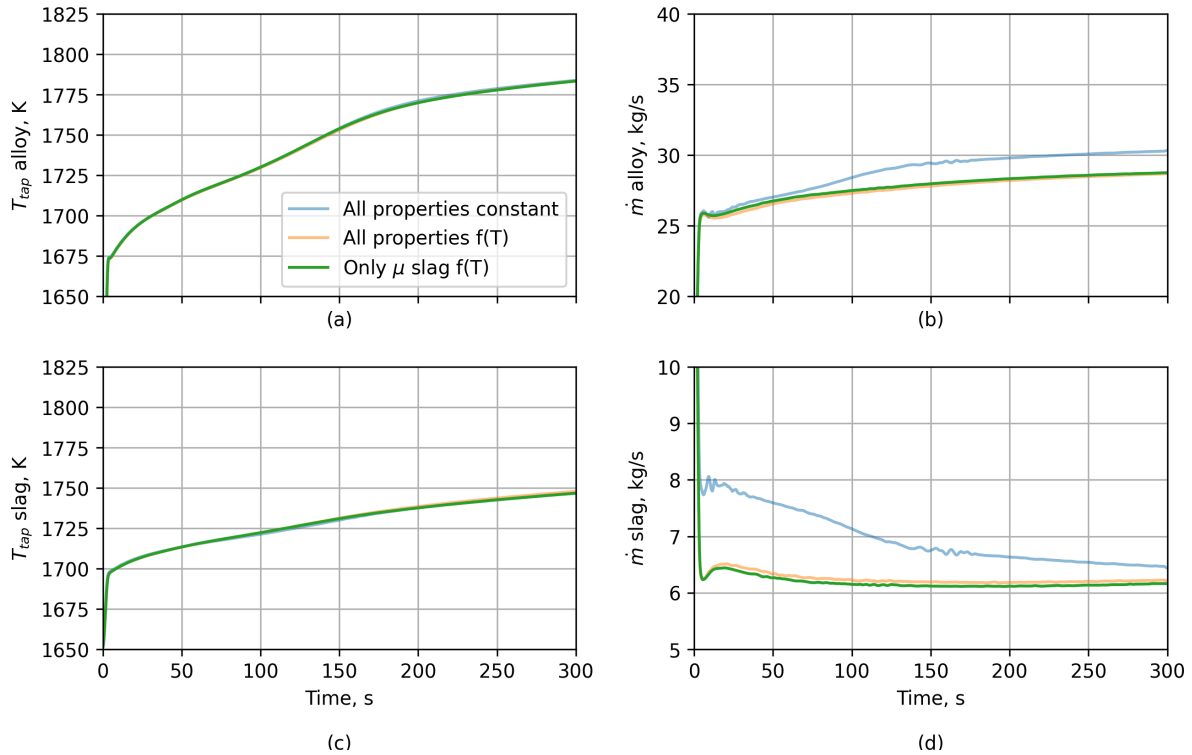


Figure 12: Evolution of (a) alloy tapping temperature, (b) alloy flowrate, (c) slag tapping temperature, and (d) slag flowrate during first 300 s of tapping, showing impact of treating slag viscosity as  $f(T)$ .

Under the particular set of conditions studied in this work it would appear that slag viscosity is the only property whose temperature dependence affects the tapping behaviour to any appreciable degree. This

is likely due to the very strong viscous drag forces that high-viscosity fluids experience while passing through porous media and resulting distortions in the patterns and magnitude of the velocities in the flow field. This effect is proportional to the fluid viscosity via the laminar drag term in Equation [3], and when viscosity is high, it is possible for it to dominate the other drag forces.

## CONCLUSIONS

A computational multiphysics model of furnace tapping was successfully developed and implemented using the OpenFOAM® modelling framework and included the effects of multiphase slag and alloy fluid flow, coupled heat transfer, and flow through porous media. The model was applied to study the case of tapping from a FeMn furnace in the duplex manganese production process. After limited testing and validation, the model was calibrated to reproduce typical FeMn tapping temperatures and then used in a sensitivity study to evaluate the impact of assuming temperature dependence in the material properties of the molten slag and alloy phases. It was found that the slag viscosity had by far the strongest effect on the results, single-handedly accounting for the majority of the differences between constant and temperature-varying properties. This suggests that while it could be acceptable to treat other material properties as constant using broadly representative values, temperature dependence of slag viscosity should be included in multiphysics models of furnace tapping for best accuracy.

Although this is an interesting result, it should be noted that the present study has a number of limitations. Firstly, only a single metallurgical process (FeMn production in a SAF) was studied; other processes and furnace designs may display sensitivities to different properties and should be investigated on a case-by-case basis. Secondly, many of the modelling parameters other than material properties were treated as fixed constants – this includes the thermophysical properties of the solid material in the burden, the geometry of the tap-hole and furnace sidewall, and the level of the slag and alloy in the furnace at the start of a tap. Thirdly, a number of approximations and simplifications were made in deriving the model used here. These included limiting the simulated region to a small subsection of the furnace interior, ignoring transient heat conduction effects in the furnace sidewall and tapblock refractories during tapping, and treating the solid and fluid media in the region as isothermal with one another. Finally, numerical testing and validation of the model was only conducted to a limited degree, and a more extensive suite of tests and comparisons against real-world data is recommended. It will be advisable to evaluate and address all of these shortcomings with care in future versions of furnace tapping models.

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# Novel PAH-Free carbon-based tap hole material for ferroalloy furnaces: Performance, repairability, and rheological characterisation

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Tap-hole clays and repair pastes play a critical role in sealing and maintaining the integrity of the tap-holes of ferroalloy furnaces; achieving a strong, stable baked structure is therefore critical for reliable furnace operation. While conventional oxide-based clays are effective for basic tap-hole closure, the overall performance in tap-hole repair and refractoriness may be limited, especially during extended tapping campaigns. This can lead to more frequent maintenance and a shorter operational service life of the tap-hole. To address these challenges, Elkem has developed ELTAP® G-THL tap-hole paste – a carbon-based tap-hole material that is free from polycyclic aromatic hydrocarbons. The material is engineered to provide stable, predictable flow behaviour and enhanced repair capability compared with traditional oxide-based clays. There are currently no standardised methods to quantitatively measure in-furnace performance of tap-hole pastes and clays. This paper seeks to evaluate lab-scale methods for measurement of tap-hole materials, such as the Marshall extrusion test and a parallel-plate viscometer test, and correlate these with the operational experience of tap-hole materials and how they perform in an industrial setting. In particular, the combination of shear stress and viscosity measurements enables a more consistent comparison between products and supports a clearer interpretation of in-furnace behaviour. A secondary goal of this work is to compare the rheological properties of ELTAP® G-THL tap-hole paste with a standard industrial clay material and highlight the stable viscosity and reliable flow of the paste, which makes it a durable, efficient, and safer alternative to traditional oxide-based clays for demanding ferroalloy furnace conditions. These rheological differences align with operational observations from industrial furnaces, where carbon-based materials have shown improved sealing performance and targeted repair capability.

## INTRODUCTION

Efficient tap-hole management is fundamental to sustaining productivity and extending campaign life in ferroalloy furnaces (Nelson *et al.*, 2016). Beyond basic closure, the binder system governs extrusion behaviour and time-dependent ageing under thermal and mechanical loads (de Oliveira *et al.*, 2022a; de Pretto and Lindstad, 2022b). The increasing regulatory scrutiny of polycyclic aromatic hydrocarbons (PAHs) has motivated the development of alternative binders to conventional pitch with PAH-free formulations (Cameron and Garbers-Craig, 2025; Cameron *et al.*, 2023). Within this context, ELTAP® G-THL tap-hole paste, a PAH-free paste, based on carbon aggregate, is benchmarked against a conventional oxide clay to relate laboratory rheology and extrusion metrics to the operational requirements of sealing performance, repairability, and flow stability in industrial tap-hole practice. The technical objective is twofold: 1) To establish a physically grounded link between squeeze-flow rheology (parallel-plate viscometry) and Marshall extrusion, and 2) Build predictive models that enable accelerated quality assessment while preserving relevance to field performance.

The viscometer theory follows axisymmetric squeeze-flow as developed by Torklep (1988) (Engmann *et al.*, 2005) and refined for the Elkem geometry by Nordbø (2013) (Torklep *et al.*, 1988). The deformation field under parallel plates depends on the instantaneous gap,  $h$ , plate velocity,  $v$ , and specimen volume,  $V$ . The shear rate at the specimen periphery is:

$$\gamma = (3v/h^2) * \sqrt{V/(\pi h)} \quad [1]$$

where  $\gamma$  is shear rate ( $s^{-1}$ ),  $v$  is the upper-plate velocity ( $m \cdot s^{-1}$ ),  $h$  is the plate separation (m),  $V$  is the specimen volume ( $m^3$ ), and  $\pi$  is the mathematical constant. Once the apparent viscosity  $\eta$  ( $Pa \cdot s$ ) is obtained from the measured force response through the squeeze-flow model (Nordbø *et al.*, 2013), the corresponding shear stress follows directly as:

$$\tau = \gamma\eta \quad [2]$$

with  $\tau$  the shear stress (Pa). These relations allow construction of viscosity–shear-rate curves suitable for comparing paste types, diagnosing shear-thinning behaviour, and assessing flow stability (Engmann *et al.*, 2005; Macosko, 1994; Torklep, 1988).

Marshall extrusion theory follows the standardised cylinder-die method widely used in paste and clay evaluation (Benbow and Bridgwater, 1993; Kitamura, 2014; Nightingale *et al.*, 2006). It captures the pressure demand of bulk compression and constricted flow through a die, two coupled regimes that together determine processability in mud-gun operation. Because no single local pressure fully represents this coupled flow, the Marshall Extrusion Pressure (MEP) is defined as the average of the peak piston pressure and the peak die pressure:

$$P_{piston} = F_{max} / A_{piston} \quad [3]$$

$$P_{die} = F_{max} / A_{die} \quad [4]$$

$$MEP = (P_{piston} + P_{die})/2 \quad [5]$$

where  $A_{piston}$  is the curved surface area of the piston ( $m^2$ ),  $A_{die}$  is the curved surface area of the die ( $m^2$ ), and  $F_{max}$  is the maximum load (N). This averaging integrates bulk stiffness/compaction (piston) with die-constriction and wall-friction effects (die), giving a robust single descriptor of overall extrudability.

In this paper, the rheological signature used to predict Marshall pressure comprises apparent viscosity  $\eta$ , shear stress  $\tau$ , shear rate  $\gamma$ , temperature, and storage time. When these descriptors are linked to extrusion measurements, the combined framework enables a direct translation from bench-scale rheological fingerprints to extrusion performance, providing a more representative proxy for mud-gun behaviour. A multivariate treatment of these variables resolves the inadequacy of single-variable correlations and supports predictive models that map laboratory descriptors to MEP, thereby connecting PAH-free carbon paste design (e.g., ELTAP® G-THL tap-hole paste) to practical sealing performance, targeted repair capability, and consistent handling in the furnace environment.

## MATERIALS AND METHODS

Four paste formulations (ELTAP® G-THL tap-hole paste E, A, G, and standard industrial clay) were evaluated in triplicate. Materials A and E appear as RC-A and RC-E, reflecting their formulation as resin-bonded carbon pastes in which a thermosetting binder carbonises during baking to form a continuous carbon matrix. Material G, referred to as BC-G, is likewise carbon-based but employs a bio-derived binder that produces a comparable carbon-forming mechanism with lower initial rigidity. Standard industrial clay, denoted RO-Ox-F, differs fundamentally as an oxide-rich system containing only a minor carbon fraction and a hybrid oil-resin binder composed of a thermosetting resin and a vegetal oil component. For baked-property characterisation, specimens were embedded in coke and

fired at 1000°C to prevent oxidation. Samples were conditioned at 25–60°C for 0–3 weeks in low-density polyethylene bags sealed to reproduce industrial packaging and minimise moisture exchange with the environment.

The rheology of the paste was measured using a temperature controlled parallel-plate viscometer on pre-compressed specimens. Sample masses of approximately 800 g (RC-E, RC-A, and BC-G) and 1000 g (RO-Ox-F) were used. A constant plate velocity of 10 mm · min<sup>-1</sup> was applied. Immediately after the viscometry test, Marshall extrusion pressure testing was performed on the same samples of approximately 550 g (RC-E, RC-A, and BC-G) and 800 g (RO-Ox-F) masses with controlled temperature. Residence-time ageing was also performed on the MEP samples at 60°C by testing after 0, 2, 4, 6 and 8 hours without removing it from the Marshall extrusion apparatus, simulating progressive hardening of the paste while resting inside a mud-gun between tapping operations.

In-situ repair trials using materials RC-A and RO-Ox-F were conducted under industrial conditions to assess operational behaviour, sealing performance and repair capability. Testing was carried out in a 26 MW ferro-silicon furnace, where the paste was loaded into the mud gun immediately after tapping, utilising residual heat to soften the material and support extrusion. Operators recorded observations such as closure quality, slag resistance, drilling effort, and tap-hole stability over multiple tapping cycles. The paste was applied both for routine plugging and as a targeted repair material in situations with reduced drill length, slag ingress or gassing. Continuous multi-day trials further evaluated softening during loading, hardening behaviour inside the mud gun, and drilling response after repeated closures, providing insight into the paste's field-repair functionality and operational handling of materials RC-A and RO-Ox-F.

A modelling framework was constructed using a total of 151 rheology–extrusion paired measurements. Input variables comprised of viscosity, shear stress, temperature and ageing week, with MEP as the response. Three model families that were implemented included linear regression, polynomial regression (degree 2), and gradient boosting (shallow-tree ensembles) (Friedman, 2001; Hastie *et al.*, 2009). Model evaluation used five-fold cross-validation, where approximately 80% of the samples were used for training and 20% as unseen validation data in each fold. All folds were iterated so each sample acted once as the validation subset. The workflow included dataset loading, extraction of temperature and ageing fields, feature assembly and fold-wise model fitting. Aggregated cross-validation outputs provided a robust estimate of predictive behaviour independent of in-sample performance.

## RESULTS AND DISCUSSION

### Baked Property

The baked properties (Table I) clearly highlight the differences between the carbon-based materials (RC-E, RC-A, BC-G) and the oxide-based industrial clay (RO-Ox-F). Materials RC-E and RC-A formed carbon-rich, resin-derived networks during baking, while material BC-G developed a similar structure through its bio-binder. These carbonised frameworks effectively reinforced the baked body, producing lower porosity (26–33%) and substantially higher compressive strength. Material RC-A, which achieved the lowest porosity and highest strength (15.5 MPa) showed the most robust structural development. Material RO-Ox-F differed fundamentally from the carbon-based ELTAP®G-THL tap-hole paste material. Its structure is dominated by oxide aggregates, and because the specific oil-resin blended binder that was used a higher volatilisation occurred during baking rather than carbonises. The material formed almost no fixed-carbon binder structure, leaving a dense but mechanically weak oxide framework. This volatilisation, together with the lack of a reinforcing carbon network could explain the high pore volume (37.8 %) and extremely low compressive strength (2.2 MPa) observed, despite the comparatively high density of the baked material.

Thermal conductivity reinforces this distinction between these materials. Carbon-rich systems (RC-E, RC-A, BC-G) conduct heat efficiently (3.3–5.5 W/m · K), reducing thermal gradients and stabilising the

tapping channel. Material RO-Ox-F shows much lower conductivity ( $1.5 \text{ W/m} \cdot \text{K}$ ), which increases susceptibility to thermal shock and local overheating.

Table I. Baked properties of all tap-hole materials

|                                                            | Material BC-G | Material RC-E | Material RC-A | Material RO-Ox-F |
|------------------------------------------------------------|---------------|---------------|---------------|------------------|
| <b>Baked Apparent Density (<math>\text{kg/m}^3</math>)</b> | <b>1350</b>   | <b>1502</b>   | <b>1598</b>   | <b>1994</b>      |
| <b>Pore Volume (%)</b>                                     | <b>32.6</b>   | <b>30.6</b>   | <b>26.4</b>   | <b>37.8</b>      |
| <b>Compressive Strength (MPa)</b>                          | <b>12.2</b>   | <b>8.6</b>    | <b>15.5</b>   | <b>2.2</b>       |
| <b>Thermal Conductivity (<math>\text{W/mK}</math>)</b>     | <b>5.5</b>    | <b>3.3</b>    | <b>4.4</b>    | <b>1.5</b>       |
| <b>ARR (%)</b>                                             | <b>30</b>     | <b>53.4</b>   | <b>82.4</b>   | <b>32.2</b>      |
| <b>ARD (%)</b>                                             | <b>54.8</b>   | <b>36.2</b>   | <b>10.2</b>   | <b>64.8</b>      |
| <b>ARL (%)</b>                                             | <b>15.2</b>   | <b>10.4</b>   | <b>7.4</b>    | <b>3</b>         |

Oxidation behaviour follows the same trend. Material RC-A exhibits excellent oxidation resistance, with an air reactivity residue (ARR) of 82.4 %, an air reactivity dust (ARD) of 10.2 % and an air reactivity loss (ARL) of 7.4%, indicating that its carbon matrix remains coherent under controlled oxidation. In the air reactivity method, samples are exposed to a defined air flow at elevated temperature; the remaining coarse fraction is reported as ARR, the portion converted to fine particulate dust is reported as ARD, and the unrecovered fraction, corresponds to the mass that is completely lost through oxidation and not recoverable as either residue or dust is reported as ARL. High ARR combined with low ARD and ARL therefore reflects a dense, well-bonded carbon structure that resists oxidative degradation and maintains mechanical integrity. Material RO-Ox-F, by contrast, undergoes extensive oxidation-induced fragmentation during the same test – not because oxides are less thermodynamically stable, but because its oxide-rich, porous microstructure lacks a continuous carbon matrix. This open pore network accelerates oxygen ingress and promotes structural collapse, resulting in low ARR and elevated ARD/ARL despite the thermodynamic stability of its oxide phases.

In summary, the carbon-based materials, particularly material RC-A, display strong structural integrity, favourable thermal behaviour and stable oxidation resistance. Material RO-Ox-F's oxide-dominant formulation and low retained carbon content lead to a weak baked structure, limiting its suitability for demanding ferroalloy tap hole applications.

### Rheological and Extrusion Properties

The temperature- and ageing-dependent rheological results of the four tap-hole materials are summarised in Figures 1, which present viscosity, shear stress and Marshall pressure as a function of temperature for weeks 0–3.

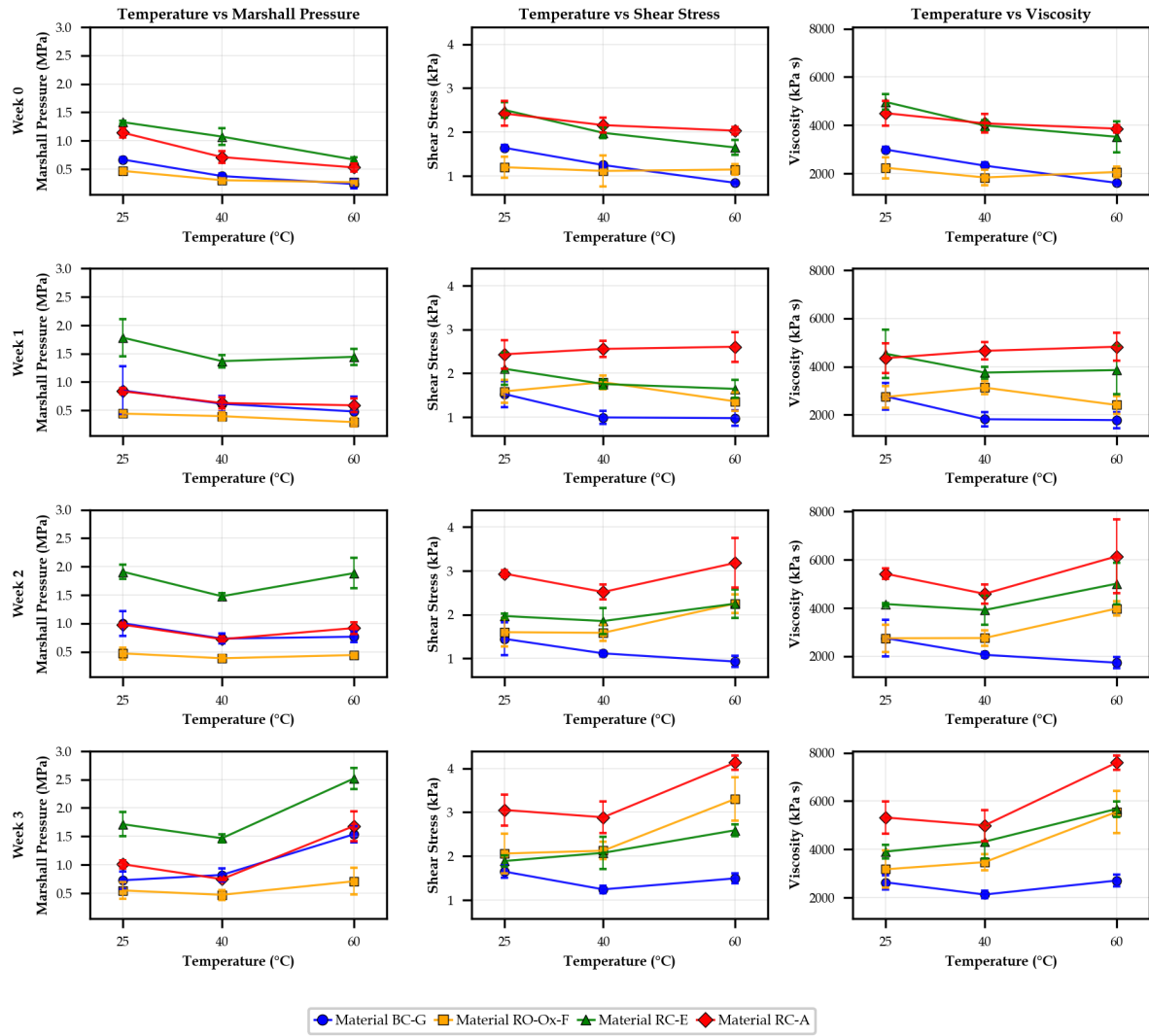


Figure 1: Viscosity, shear stress and Marshall extrusion pressure as a function of temperature (25–60°C) for weeks 0–3 for materials RC-E, RC-A, BC-G and RO-Ox-F.

The results highlight the contrasting behaviours of the carbon-based materials (RC-E, RC-A, BC-G) and the oxide-based material RO-Ox-F. At week 0, all materials soften with increasing temperature, resulting in reduced viscosity, shear stress and Marshall pressure from 25°C to 60°C (Figure 1, week 0). The carbon-based pastes show greater temperature sensitivity, with material RC-A displaying the highest initial rheology, meaning it begins with higher viscosity and shear stress at low temperature, while material BC-G shows the lowest initial rheology, providing good workability at low temperature and therefore lower viscosity and shear stress at the start. As temperature increases, the binder in material BC-G gradually undergoes structural development, which contributes to its progressive stiffening. Material RO-Ox-F, with its oxide-rich matrix and limited carbon content, is consistently easy to extrude because its lubricating binder system does not form significant structures as temperature reaches 60°C.

As ageing progresses, the carbon-based materials develop structure at different rates (see Figure 2). Material RC-E shows the strongest increase in extrusion pressure, especially at 60°C. Material RC-A undergoes substantial increases in viscosity and shear stress but only moderate pressure increase, reflecting effective shear-thinning and favourable constriction-flow behaviour. Material BC-G remains comparatively soft at 25–40°C and shows limited pressure increases even at 60°C.

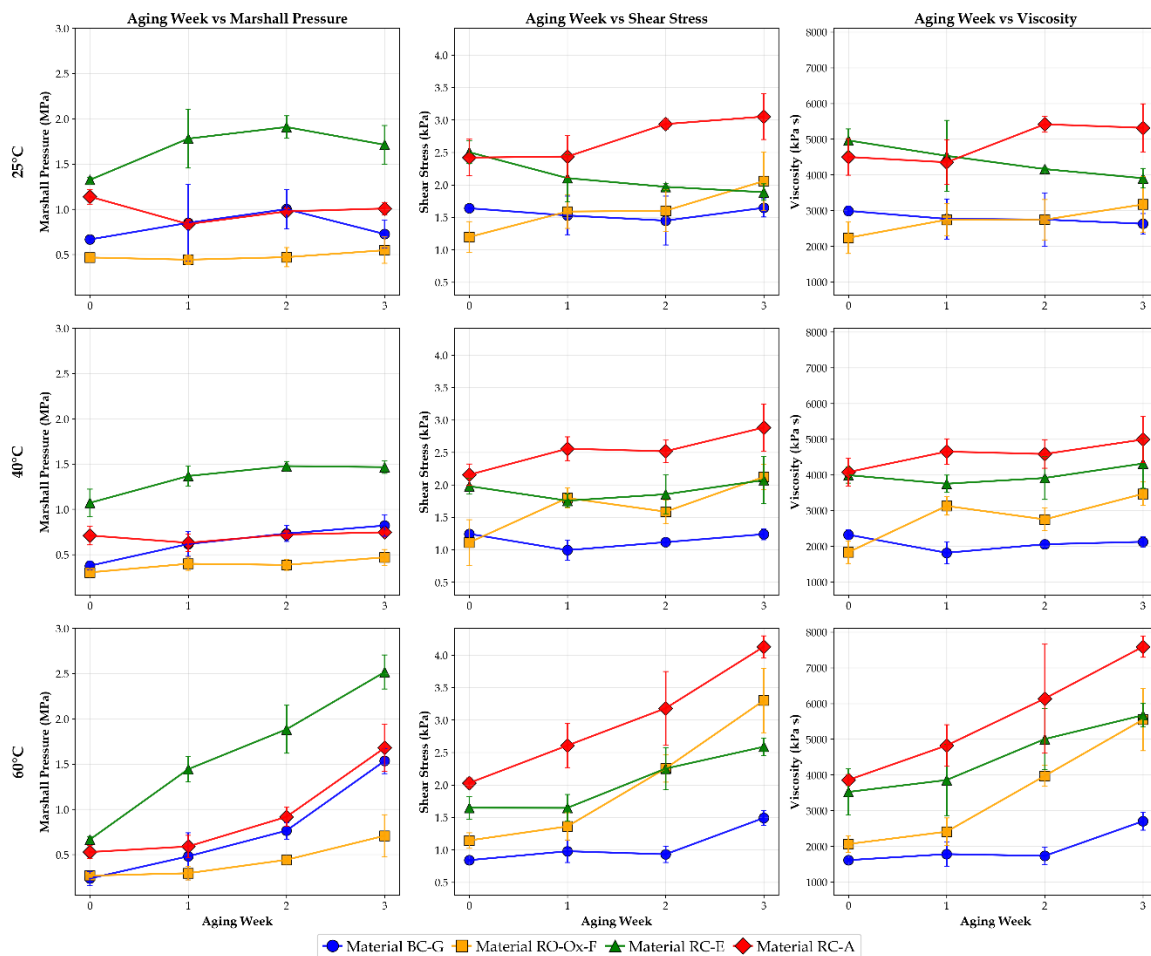


Figure 2: Ageing trends of shear stress, viscosity and Marshall extrusion pressure at fixed temperatures (25°C, 40°C, 60°C) over weeks 0–3 for materials BC-G, RC-E, RC-A, and RO-Ox-F summarising the coupled rheological–extrusion behaviour across temperatures and ageing conditions.

Material RO-Ox-F stands out from the carbon-bonded materials. Its oxide-based granular structure is incorporated into a low-resin-content oil–resin binder system, in which the resin fraction provides only limited crosslinking during heating, while the oil phase does not volatilise at the temperatures used for rheology and extrusion testing. Instead, the oil acts as an internal lubricant, reducing frictional resistance in squeeze-flow and constricted-flow regimes. Consequently, Material RO-Ox-F maintains a consistently low Marshall pressures and exhibits minimal ageing-induced rheological build-up across all conditions (Figures 2). This lubricated, weakly crosslinked binder structure also aligns with the poor baked-properties discussed earlier.

Temperature amplifies the ageing effects of the carbon-bonded materials (RC-E, RC-A, BC-G). At 25°C, rheology evolves slowly; at 40°C, moderate hardening occurs, particularly for materials RC-E and RC-A. At 60°C, ageing strongly reinforces the resin-bonded carbon materials, with material RC-E showing the steepest increase in extrusion pressure by week 3. Material BC-G increases moderately. In contrast, Material RO-Ox-F remains largely unchanged, because its low-resin, oil-dominated binder provides only minimal crosslinking and the lubricating oil phase suppresses structural carbon build-up, leaving the material without the internal reinforcement needed for mechanical load transfer.

These behaviours correlate well with binder chemistry. Resin-bonded carbon materials (RC-E and RC-A) develop a carbon skeleton upon heating, which increases strength and sealing capability but requires temperature and residence-time control to avoid excessive extrusion loads. The bio-binder material BC-G remains user-friendly and stable under routine closure conditions. Material RO-Ox-F, low-carbon,

oxide-dominant composition and oil-resin binder reduce extrusion pressure, an advantage for handling, but the same features suppress structural development and oxidation resistance, limiting its suitability for demanding ferroalloy tap-hole applications.

### Residence Time

The 60°C residence-time tests simulate the thermal exposure of paste held inside a mud-gun barrel between tapping events. All four materials exhibit an increase in Marshall extrusion pressure with time, but the shape and magnitude of the hardening response vary as a function of the type of binder chemistry and carbon-forming potential (Figure 3). Carbon-based material RC-A develops moderate dwell-induced strengthening, the bio-binder material BC-G shows moderate progressive stiffening, and the carbon-based material RC-E together with the oxide-based material RO-Ox-F remain largely unchanged, with RO-Ox-F doing so due to its lubricating, low-resin binder system.

Material BC-G displays a continuous rise from 0.18-0.68 MPa between 0-8 h, with rapid early hardening up to about 4 h followed by a gradual approach toward a kinetic plateau. This behaviour reflects progressive binder structuring and consolidation at elevated temperature.

Material RO-Ox-F maintains the lowest extrusion pressures throughout the dwell period, with only a modest increase over the first 2 h and small oscillations thereafter. This muted ageing response is consistent with its oxide-rich composition and oil-resin binder. The oil fraction provides lubrication during extrusion, reducing internal friction, while the low resin content offers limited crosslinking. As a result, material RO-Ox-F remains consistently easy to extrude, with minimal structural strengthening over time.

Material RC-E shows only a minor early increase in extrusion pressure from 0-2 h, comparable to the small change observed in material RO-Ox-F over the same interval. Small fluctuations are observed later in the dwell period, including a slight rise and decrease, remain within a narrow range and do not indicate significant structural evolution during residence-time exposure. In this regard, material RC-E exhibits behaviour similar to material RO-Ox-F, which is consistent with its binder content and raw material selection.

Material RC-A also hardens progressively, increasing from 0.28 MPa to 0.48 MPa. Its 4–6 h shoulder indicates a transient balance between binder restructuring and shear-thinning behaviour, limiting the rate of pressure increase despite ongoing rheological stiffening.

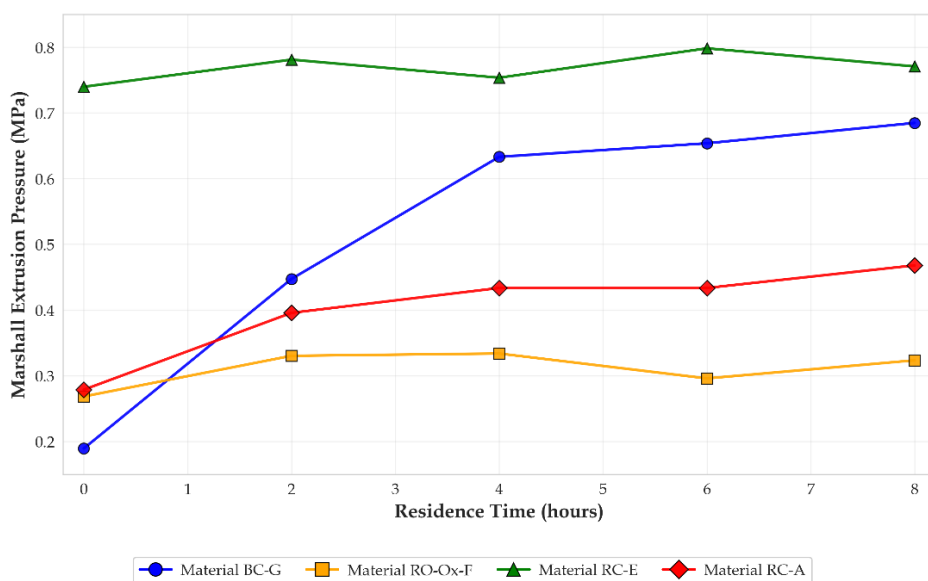


Figure 3: Marshall extrusion pressure as a function of residence time at 6°C for materials RC-E, RC-A, BC-G and RO-Ox-F, showing distinct hardening kinetics governed by binder chemistry.

These results indicate that the resin-bonded carbon material RC-A develops the clearest dwell-induced strengthening during residence-time exposure, while the bio-binder material BC-G also stiffens appreciably at extended exposure despite remaining easier to extrude. Material RC-E, however, shows only minor changes within the tested dwell period, exhibiting a residence time response closely aligned with material RO-Ox-F rather than with material RC-A or BC-G. Material RO-Ox-F, while easiest to extrude because of its lubricating, low-resin binder system, shows minimal structural evolution with low compressive strength, limiting its usefulness for repair applications, although it remains adequate for simple closures and drilling operation.

### Correlative and Predictive Modelling

Marshall extrusion pressure was modelled using four physically motivated predictors: viscosity ( $\eta$ ), representing squeeze-flow resistance; shear stress ( $\tau$ ), capturing shear-resistance behaviour; temperature ( $T$ ), governing thermal softening; and ageing week ( $w$ ), representing structure development during storage. The modelling dataset comprised 151 measurements. Model performance was evaluated using five-fold cross-validation, with 121 samples used for training and 30 samples reserved for validation in each fold. Validation subsets were excluded from model fitting in each iteration (Table II).

The degree-2 polynomial model achieved the highest generalisation performance ( $R^2 = 0.761 \pm 0.108$ ), together with the lowest mean absolute error (MAE) and root mean square error (RMSE), indicating the benefit of including quadratic and interaction terms. The coefficient of determination ( $R^2$ ) represents the proportion of variance explained by the model, while MAE quantifies the average magnitude of prediction errors and RMSE gives greater weight to larger deviations. Although these results suggest an improvement in predictive capability, the combination of a modest sample size and a non-linear model structure introduces a potential risk of over-fitting. For this reason, model performance is evaluated using cross-validated mean  $\pm$  standard deviation rather than in-sample fit, and the reported metrics are interpreted as indicative of generalisation tendencies within the present dataset.

Among the three evaluated strategies, linear regression, degree-2 polynomial regression and gradient boosting, the degree-2 polynomial model achieved the highest mean cross-validated accuracy, together with the lowest MAE and RMSE. The model includes quadratic terms ( $\eta^2$ ,  $\tau^2$ ,  $T^2$ ,  $w^2$ ) and pairwise interaction terms ( $\eta \cdot \tau$ ,  $\eta \cdot T$ ,  $\eta \cdot w$ ,  $\tau \cdot T$ ,  $\tau \cdot w$ ,  $T \cdot w$ ), which are consistent with the experimentally observed non-linear dependence of extrusion pressure on rheological state, temperature, and ageing. These terms reflect known trends such as ageing-induced increase in viscosity and shear stress, thermal softening between 25 and 60°C, and material-dependent behaviour between carbon-bonded and oxide-based systems. However, the observed fold to fold variability indicates that the performance advantage of this model should be regarded as promising rather than definitive.

Table II: Cross-validated performance of three modelling strategies predicting Marshall extrusion pressure from  $\eta$ ,  $\tau$ ,  $T$  and  $w$ .

| Model                            | $R^2$ (Mean $\pm$ Std) | MAE (Mean $\pm$ Std) | RMSE (Mean $\pm$ Std) |
|----------------------------------|------------------------|----------------------|-----------------------|
| Linear Regression                | $0.670 \pm 0.138$      | $0.213 \pm 0.024$    | $0.296 \pm 0.065$     |
| Polynomial Regression (Degree 2) | $0.761 \pm 0.108$      | $0.184 \pm 0.041$    | $0.251 \pm 0.063$     |
| Gradient Boosting                | $0.707 \pm 0.067$      | $0.211 \pm 0.021$    | $0.282 \pm 0.029$     |

Gradient boosting also captured non-linear trends but did not surpass the polynomial model in mean cross-validated performance for this dataset. Linear regression remains the most interpretable baseline, but underperformed because it cannot represent curvature or ageing-temperature interactions.

Overall, the cross-validation supports the need for non-linear, interaction-aware modelling of  $\eta$ ,  $\tau$ ,  $T$  and  $w$ . At the same time, the current sample size limits how strongly model superiority can be claimed. Thus, the degree-2 polynomial model is presented as the best-performing option within the present



dataset, while external validation on larger, independent dataset is required before robust deployment claims can be made.

### Industrial Trial Performance - Repair

Industrial trials were conducted in ferro-silicon furnaces to evaluate the operational behaviour of the carbon-based repair material RC-A relative to the routine closure material RO-Ox-F. When loaded into the mud gun immediately after tapping, material RC-A softened rapidly at the barrel's operating temperature ( $\sim 100^{\circ}\text{C}$ ), becoming extrudable without preheating. This contrasted with material RO-Ox-F, which was softer at room temperature but lacks repair properties of tap-hole blocks. Material RC-A consistently delivered strong sealing and slag resistance, preventing ineffective closure especially after oxygen lancing, indicating effective in-situ structural development (Figure 4).

Operational logs over multiple days showed that material RC-A maintained stable drilling lengths initially ( $> 100\text{ cm}$ ) and resisted slag ingress during early cycles. After extended residence (2–4 days), drilling effort increased, reflecting the ageing-driven hardening observed in laboratory rheology and residence-time tests. Despite this increase in drilling hardness, sealing reliability remained high. Material RO-Ox-F, by comparison, preserved low drilling resistance but offered limited reinforcement of the tap-hole.

Adjusting the quantity of material RC-A applied revealed sensitivity to volumetric dosing: under-filling reduced drilling length stability, whereas correct dosing restored expected performance. During the trial, use of material RO-Ox-F was halted whenever the drilling length approached the operational minimum of approximately  $70\text{ cm}$ , at which point material RC-A was intentionally applied to repair the tap-hole and recover drilling length. Tap-holes left closed with material RC-A exhibited more stable copper-block temperatures, indicating improved thermal contact, heat transfer and adhesion relative to material RO-Ox-F. Operators reported that material RC-A was preferred for targeted repair event, including falling drilling lengths, slag-rich tapping or post-oxygen cycle, while material RO-Ox-F remained adequate for routine daily closures. Overall, material RC-A provides superior sealing and restorative capability, with the trade-off of increasing drilling hardness during prolonged retention, making it best suited for short- to medium-term repair cycles rather than long, uninterrupted closure periods.

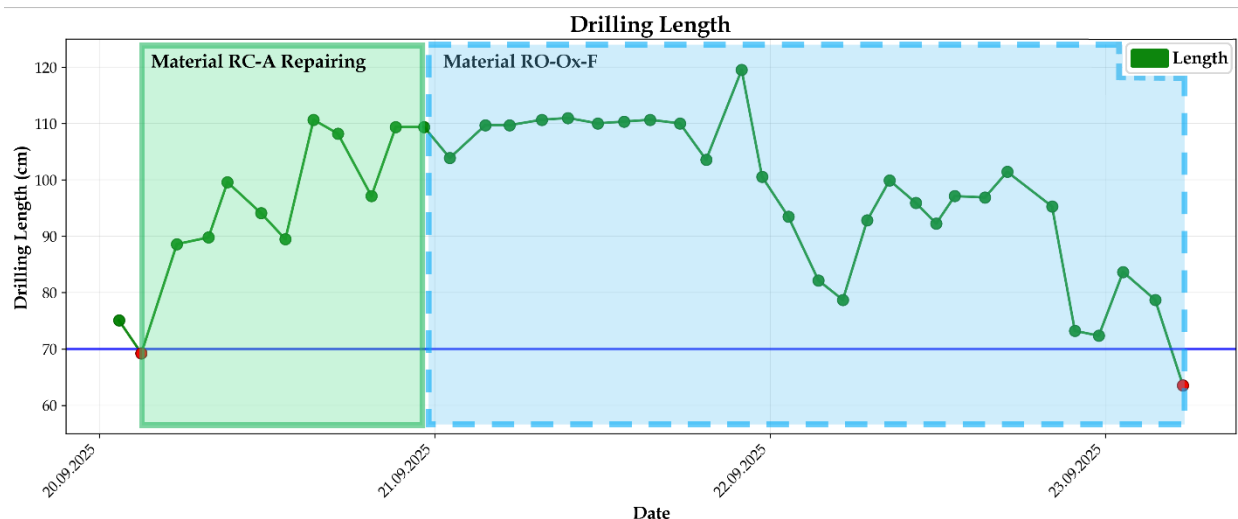


Figure 4: Drilling length evolution during industrial furnace operation, demonstrating reduced drilling length followed by recovery after the targeted use of repair material RC-A (green rectangle). Drilling length stabilises once RC-A is applied, after which normal operation resumes using material RO-Ox-F (blue dashed rectangle).

Data extracted from the ferro-silicon industrial trial log.

### Performance Interpretation and Material Selection

The integrated rheological, extrusion, ageing, baked-property, modelling and industrial-trial results show that binder chemistry and carbon-forming potential govern the behaviour of the four materials. Marshall pressure reflects interacting contributions of viscosity ( $\eta$ ), shear stress ( $\tau$ ), temperature ( $T$ ) and ageing week ( $w$ ). Material RC-A displays the strongest coupled development of viscosity, shear stress and pressure, material BC-G shows moderate rheological and structural evolution, while materials RC-E and RO-Ox-F exhibit comparatively small changes across ageing and temperature, with RO-Ox-F remaining largely unchanged due to its low resin content, lubricating binder system and oxide base composition.

Residence-time ageing reinforces these distinctions. Material RC-A shows a clear hardening response, while material RC-E does not harden rapidly under same conditions, material BC-G increases gradually, and material RO-Ox-F shows negligible change, consistent with limited structural development. Baked-property data further differentiate the systems: RC-A forms the strongest carbon skeleton, material RC-E develops a similar structure, BC-G offers moderate strength with good workability and RO-Ox-F forms a porous, low-strength oxide matrix.

Industrial furnace trials conducted in this study involved materials RC-A and RO-Ox-F only. Material RC-A demonstrated reliable sealing, strong slag resistance and stabilisation of drilling length. Material RO-Ox-F remained easy to extrude but provided limited reinforcement. Materials RC-E and BC-G were not included in the industrial trial campaign of this study, but both have a track record of successful operation in industry, and their behaviour aligns with their measured rheological and baked-property characteristics.

Modelling results, based on 151 samples and evaluated by five-fold cross-validation (approximately 121 training and 30 held-out samples per fold), indicate that extrusion behaviour is influenced by non-linear interactions among  $\eta$ ,  $\tau$ ,  $T$ , and  $w$ . Within the current dataset, the degree-2 polynomial model showed the strongest cross-validated performance and provided the best overall representation. However, confirmation with larger independent datasets is still required.

Based on the integrated interpretation of laboratory results, modelling trends, and available operational evidence, material RC-A is provisionally recommended for targeted repairs, material RC-E for high-integrity sealing, material BC-G for routine closures requiring stable workability, and material RO-Ox-F for simple, non-reinforcing closures.

## CONCLUSIONS

This study establishes a physically grounded relationship between rheology, ageing, temperature, extrusion behaviour, and industrial performance of carbon- and oxide-based tap-hole materials. Carbon-bonded formulations (RC-E, RC-A, BC-G) derive their performance from carbon-forming binder systems that promote structural reinforcement during heating, while oxide-dominant material RO-Ox-F lacks this mechanism and therefore remains easy to extrude but structurally weak after baking.

Ageing and residence time tests demonstrate strong structure development in material RC-A, moderate evolution in material BC-G, and only limited change in materials RC-E and RO-Ox-F, consistent with their measured rheological trends. Baked-property results reinforce these distinctions: material RC-A achieves the highest compressive strength and baked density, material BC-G shows moderate strength with good thermal conductivity, material RC-E develops a comparable but weaker carbon structure, and material RO-Ox-F exhibits the lowest strength and the highest pore volume.

Industrial trials conducted in this work involved materials RC-A and RO-Ox-F only. Material RC-A demonstrated reliable sealing and effective recovery of drilling length during operations, while material RO-Ox-F remained suitable for routine, low-demand closures. Although materials RC-E and BC-G were

not included in the present industrial trial campaign, have been used successfully in furnace operation with similar repair capability as material RC-A, and their known performance is consistent with their characteristics measured in the laboratory.

Correlative and predictive modelling confirms that Marshall pressure arises from non-linear interactions among viscosity, shear stress, temperature and ageing week. Within the present dataset, a degree-2 polynomial regression model provided the most consistent cross-validated performance. However, validation using larger, independent datasets is required before extending its application beyond comparative assessment and quality-trend analysis.

Overall, the results demonstrate that Elkem's ELTAP® Repair pastes and tap-hole paste carbon-based materials provide a complementary suite of functions for tap-hole maintenance and performance recovery.

Material RC-A is identified as the preferred paste for targeted repair interventions and recovery of drilling length, thereby contributing to extended tap-hole service life. Material RC-E serves as a high-integrity sealing material for demanding closure events, while material BC-G offers stable and reliable performance for routine daily closures. Material RO-Ox-F remains suitable for simple, non-critical sealing operations. In this paper, performance comparison was limited to Elkem formulations and the most relevant qualified competitor material, reflecting current industrial practice. Broader cross-manufacturer benchmarking is planned to use the same experimental and modelling framework to enable a consistent and comprehensive comparative assessment. Together, these findings support a data-driven optimisation of tap-hole paste design, application strategy, and predictive quality assessment for industrial furnace operation.

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# Transition from tar and resin-bonded to synthetic binder-based taphole clays: Health, environmental, and operational performance in ferroalloy smelters

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Historically, taphole clays have consisted of anhydrous tar that acts as a binder. Although effective, tar-based clays have challenges such as the release of volatile organic compounds into the atmosphere which play a significant role in the creation of tropospheric (ground level) ozone and fine particulates. In addition, these clays may contain benzo[a]pyrene, which is a polycyclic aromatic hydrocarbon that can attach to human DNA and can result in cell mutations and cancerous effects. Some taphole clay manufacturers have explored the use of resins as alternative binders. Most of the resins used contain formaldehyde as an ingredient. When formaldehyde is present at levels exceeding 0.1 ppm some individuals experience effects such as eye irritation, coughing, nausea and skin irritations. This paper explores a synthetic binder alternative, which can be used to create a taphole clay without any tar or resin present. The experimental procedure included benchmarking the physical and operational parameters to the performance requirements of the taphole clay, and the determination of volatile organic compounds emissions of the novel material. Field trials were conducted at selected smelter operations, and the health and safety metrics were measured as part of the trials. The findings indicated that the synthetic binder could be used in the production of green taphole clays without sacrificing the operational performance, and meeting the sustainable commitment to the environment, and the health and safety of the workers.

## INTRODUCTION

Taphole clay is a critical refractory consumable in the smelting industry, used to seal furnace tapholes during metal production processes such as ironmaking, ferroalloy smelting, and non-ferrous metallurgy (Nelson & Hundermark, 2016). Traditional taphole clays typically employ coal-tar pitch or resin-based binders to provide plasticity, adhesion, and high-temperature performance. While technically effective, these binder systems have long been associated with significant occupational health and environmental concerns due to the release of hazardous emissions during high-temperature operation (Cameron, 2024).

Coal-tar pitch and tar derived binders are known to contain high concentrations of polycyclic aromatic hydrocarbons. PAHs are a class of organic compounds composed of multiple fused aromatic rings. They occur naturally in fossil materials (such as coal tar and crude oil) and can also form during high-temperature processes such as pyrolysis or incomplete combustion of organic matter. Among these, benzo[a]pyrene (BaP) has been identified as one of the most toxic and carcinogenic constituents (Stellman & Guidotti, 2007). The International Agency for Research on Cancer (IARC) classifies BaP as Group 1 human carcinogen, based on sufficient evidence linking exposure to increased cancer risk in humans (Bukowska, et al., 2022) (IARC Working Group, 1988). The use of Tar or pitch creates severe environmental consequences, as accidental spillage creates severe contamination that is difficult to remedy, and safe disposal remains a highly costly exercise (Bernstein, et al., 1984).

Resin binders containing formaldehyde as ingredient are still used in some taphole clays. When human are exposed to airborne formaldehyde at levels exceeding 0,1ppm over a certain period, they can experience sensory irritation, including watery eyes, burning sensation in the eyes, nose, and throat, coughing and skin irritation. (Lucia, et al., 2020). While individuals exhibit varying sensitivity to formaldehyde exposure, alternative binders may present other operational hazards.

### **Background**

Epidemiological evidence from smelting and metallurgical environments further highlights the cumulative nature of PAH exposure. Studies have shown that long term exposure to coal-tar pitch volatiles, even at relatively low concentration, can significantly increase cancer risk over time (Friesen, et al., 2009).

In response to these well documented health risks, regulatory agencies worldwide have introduced increasingly stringent occupational exposure limits for PAHs such as BaP (Bukowska, et al., 2022) (Capleton & Levy, 2005). However, engineering controls and personal protective equipment alone are often insufficient to fully mitigate exposure during tapping operations, where workers are near emission sources. As a result, material-level interventions such as the development of low-emission or PAH free taphole clays have gained growing attention in addressing health, safety and environmental impact (Wu, et al., 2023)

Overall, the literature clearly establishes that traditional resin and tar-based taphole clays pose substantial health risks due to toxic fume emissions, particularly PAHs. The transition toward green, synthetic bonded taphole clay systems represents not only a technological advancement but also critical step in safeguarding worker health (Smith & Wentworth, 2022) (Salam, 2019).

Beyond the immediate occupational health benefits, replacing traditional tar and resin binders with synthetic binder systems significantly reduces the broader environmental footprint of smelting operations. The elimination of coal tar mitigates the risks of persistent environmental pollution such as the accumulation of polycyclic aromatic hydrocarbons (PAHs) in local soil and water systems, thereby advancing the industrial commitment to eco-friendly refractory alternatives (Pal & Sen, 2025).

### **Current Operational Process**

The smelting process is carried out according to a schedule where the molten metal is tapped, particularly from the ferroalloy smelting furnace, every 2 to 3 hours. The alloy temperature is between 1540°C - 1580°C while the slag melt temperature varies between 1640°C - 1680°C. The furnace taphole length is between 1000mm - 1250mm long, including the Mickey block.

The tapping of the melted alloy involves the use of a taphole drilling machine, combined with oxygen lancing. The opening of the taphole should be effortless, requiring the taphole clay to have balanced sealing properties. Both the alloy and /or slag flow out of the furnace through the taphole. During the tapping process, the molten metal will then flow through the launder and into ladles and further transported by overhead cranes to the casting bay. In some cases, the molten metal flows on the floor which is called floor pouring.

The primary function of the taphole clay is to seal the taphole with a “high degree of certainty” that there will be no unplanned taphole re-opening and releasing the hot melt after the required amount of taphole clay has been dispersed and the mudgun has retracted from the taphole. To achieve this physical characteristic, the selection of the taphole clay should be concluded based on the suitability of the following critical properties:

- The clay should be reasonably plastic to be injected into the taphole when pushed by the mud gun. It should be plugged into the taphole to the desired depth in the tapping channel.
- When exposed to elevated temperatures, the clay should sinter without shrinking thus ensuring a tight seal and this phenomenon should take place during the mud gun dwelling time in the taphole.
- During the furnace operation this tight seal must withstand the increasing molten pressure, slag and alloy resistance and temperature.

During the taphole closing, tar and resin bonded taphole clays release black smoke, which may be inhaled by the operators working in this area. This situation is evaluated using personal and biological monitoring, which are used to measure the level of exposure to toxic fumes.

### **Smelting Plant Furnace Section Requirements**

Considering the challenges faced by smelters, stakeholders in the smelting industries are seeking taphole clays that meet stringent requirements, including:

- Prioritizing worker's health and minimizing environmental impact.
- Safe and successful plant operations with minimal downtimes.
- Cost-effective solutions that address current economic pressures on smelters.
- Compatibility with existing equipment and processes.

### **Development of synthetic binder-based taphole clay**

Historically, taphole clays have utilised coal tar pitch binders and hydrocarbon-based binders such as phenolic resin to provide plasticity, adhesion, and green strength during application. These binders soften at moderate temperatures, enabling effective extrusion and sealing, followed by carbonisation at elevated temperatures, which forms a carbonaceous matrix that enhances strength, erosion resistance, and resistance to slag and metal penetration. However, their use is associated with the release of volatile organic compounds (VOCs), including polycyclic aromatic hydrocarbons (PAHs), during heating, posing environmental and occupational health concerns.

These limitations led to the development of taphole clays using a synthetic binder in place of conventional tar and resin systems. The binder was optimised to eliminate VOC emissions and reduce associated health and environmental impacts. The synthetic system is compatible with a range of aggregate grades and matrix fillers, allowing flexibility in meeting temperature and chemical requirements. It also enables tailoring of key properties, including density, workability, strength at elevated temperatures, and permanent linear change for effective sealing, while improving lancing and drilling performance.

Two grades of synthetic-bonded taphole clays were developed: Reftap W S MW for routine operations and Reftap W B SC60 for reconstructive applications. These materials have been successfully used in ferroalloy smelting since 2015 and are supplied as extruded 8 kg blocks, wrapped in clear plastic bags and packaged in various packaging formats.

Reftap W S MW is a high-performance, environmentally, tar-free taphole clay formulated as a medium weight, silica refractory material. It offers a sustainable alternative to traditional taphole clays by eliminating coal tar pitch volatiles (CTPVs) and lowering volatile organic compound (VOC) emissions. It is designed for extreme industrial conditions; it operates at maximum temperature of up to 1600°C and features excellent thermal expansion and high crushing strength at elevated temperatures.

Reftap W B SC60 is a premium, environmentally friendly, tar-free taphole clay specifically formulated with a significant Silicon Carbide (SiC) addition and high alumina content which makes it an ideal refractory paste for reconstructing and building worn-out furnace tapholes. The inclusion of Silicon Carbide provides exceptional resistance to erosion, chemical erosion, and intense molten metal or slag flow, which ensures long term durability and stabilizes high wear areas of the furnace.

The material specifications for the Reftap W S MW and Reftap W B SC60 in comparison with resin and tar bonded taphole clay (Reftap RT S) are given in Table I.

Table I: Material specification – synthetic binder and the resin/tar binder-based taphole clays

|                             | Reftap W S MW                          | Reftap W B SC60                        | Reftap RT S                            |
|-----------------------------|----------------------------------------|----------------------------------------|----------------------------------------|
| Bonding                     | Synthetic binder                       | Synthetic binder                       | Resin/Tar Binder                       |
| Main Raw Material           | Silica                                 | Calcined Bauxite                       | Silica                                 |
| Particle Size               | 0 – 6 mm                               | 0 – 6 mm                               | 0 – 6 mm                               |
| Maximum Service Temperature | 1600°C                                 | 1750°C                                 | 1500°C                                 |
| Workability Index           | 60 – 65% (typical)                     | 60 – 65% (typical)                     | 50 – 55% (typical)                     |
| Installation                | Clay Gun                               | Clay Gun                               | Clay Gun                               |
| Shelf life                  | 4 Months (stored in a cool, dry place) | 4 Months (stored in a cool, dry place) | 4 Months (stored in a cool, dry place) |
| Packaging                   | Pallet, 1000kg, extruded               | Pallet, 1000kg, extruded               | Pallet, 1000kg, extruded               |

## Test Methods

### Laboratory testing

Samples of the Reftap W S MW and the Reftap W B SC60 taphole clays were tested by a third party, SANAS accredited laboratory and compared with Reftap RT S. The critical physical properties measured were the cold crushing strength, the product bulk density, and the linear thermal expansion at selected temperatures according to the standards ISO 10059:2, ISO 5016 and BS 1902: 5.14: 1992 respectively.

The average values obtained for the physical properties are given in Table II.

Table II: Physical properties of the taphole clays

| Property                                      | Units                | Reftap W S MW | Reftap W B SC60 | Reftap RT S |
|-----------------------------------------------|----------------------|---------------|-----------------|-------------|
| Cold crushing strength at 1200°C              | MPa                  | 32            | 35              | 40          |
| Bulk density at 110°C                         | (g/cm <sup>3</sup> ) | 1,8           | 2,0             | 2,25        |
| Linear thermal expansion on heating at 1000°C | %                    | 0.7           | 0.5             | 0.45        |

The chemical analysis was done using XRF, and the C content analysed using LECO. The typical results are presented in Table III.

Table III: Chemical analysis of the taphole clays

| Element (%)                    | Reftap WS MW | Reftap WB SC60 | Reftap RT S |
|--------------------------------|--------------|----------------|-------------|
| Al <sub>2</sub> O <sub>3</sub> | 6.3          | 42.4           | 7.6         |
| SiO <sub>2</sub>               | 75.5         | 39.8           | 64.2        |
| C                              | 8            | 8              | 15.5        |
| SiC                            | 0            | 6.1            | 0           |
| Other constituents             | 10.2         | 3.7            | 12.7        |



The Tables II and III summarise the physical and chemical properties of the synthetic binder-based taphole clays. The measures of cold crushing strength (32-35MPa) for the synthetic binder-based taphole clays are slightly less than that of resin-tar based (40MPa) taphole clay, however, these cold crushing strength measures still guaranteed an adequate resistance to furnace pressure during the trial. This indicates that the cold crushing strength measures attained the adequate resistance to axial forces to prevent the self-opening of a taphole that would result in loss of containment.

#### ***Volatile organic compounds testing***

Table IV shows the comparison of the volatile organic compounds (CPTV) results for the synthetic binder-based taphole clays (Reftap WS MW and Reftap WB SC60), and the results of a tar-based (Reftap RTS) taphole clay.

*Table IV: Total organic volatiles*

| SPECIES                | UNITS   | REFTAP<br>WS MW | % of<br>RTS* | REFTAP<br>WB SC60 | % of<br>RTS* | REFTAP<br>RTS<br>(100%) |
|------------------------|---------|-----------------|--------------|-------------------|--------------|-------------------------|
| Benzene                | (µg/kg) | <20             | 0.17%        | <20               | 0.17%        | 12,000                  |
| Toluene                | (µg/kg) | <200            | 0.18%        | <200              | 0.18%        | 110,000                 |
| Ethylbenzene           | (µg/kg) | <40             | 0.19%        | <40               | 0.19%        | 21,000                  |
| m+p Xylene             | (µg/kg) | <80             | 0.07%        | <80               | 0.07%        | 110,000                 |
| o-Xylene               | (µg/kg) | <40             | 0.13%        | <40               | 0.13%        | 31,000                  |
| 1,3,5-Trimethylbenzene | (µg/kg) | <40             | 0.09%        | <40               | 0.09%        | 43,000                  |
| 1,2,4-Trimethylbenzene | (µg/kg) | <40             | 0.07%        | <40               | 0.07%        | 58,000                  |
| n-Propyl benzene       | (µg/kg) | <40             | 1.00%        | <40               | 1.00%        | <4,000                  |
| tert-Propyl benzene    | (µg/kg) | <40             | 1.00%        | <40               | 1.00%        | <4,000                  |
| sec-Butylbenzene       | (µg/kg) | <40             | 1.00%        | <40               | 1.00%        | <4,000                  |
| n-Butylbenzene         | (µg/kg) | <40             | 1.00%        | <40               | 1.00%        | <4,000                  |
| Isopropyl benzene      | (µg/kg) | <40             | 1.00%        | <40               | 1.00%        | <4,000                  |
| Styrene                | (µg/kg) | <100            | 0.15%        | <100              | 0.15%        | 66,000                  |
| 4-Isopropylbenzene     | (µg/kg) | <100            | 1.00%        | <100              | 1.00%        | <10,000                 |
| Bromobenzene           | (µg/kg) | <40             | 1.00%        | <40               | 1.00%        | <4,000                  |
| Chlorobenzene          | (µg/kg) | <40             | 1.00%        | <40               | 1.00%        | <4,000                  |

Table IV: Total organic volatiles (continued)

|                           |         |       |       |       |       |          |
|---------------------------|---------|-------|-------|-------|-------|----------|
| 1,2-Dichlorobenzene       | (µg/kg) | <40   | 1.00% | <40   | 1.00% | <4,000   |
| 1,3-Dichlorobenzene       | (µg/kg) | <40   | 1.00% | <40   | 1.00% | <4,000   |
| 1,4-Dichlorobenzene       | (µg/kg) | <40   | 1.00% | <40   | 1.00% | <4,000   |
| 1,2,3-Trichlorobenzene    | (µg/kg) | <40   | 1.00% | <40   | 1.00% | <4,000   |
| 1,2,4-Trichlorobenzene    | (µg/kg) | <40   | 1.00% | <40   | 1.00% | <4,000   |
| 1,3,5-Trichlorobenzene    | (µg/kg) | <40   | 1.00% | <40   | 1.00% | <4,000   |
| Naphthalene               | (µg/kg) | <40   | 0.02% | <40   | 0.02% | <240,000 |
| Bromoform                 | (µg/kg) | <100  | 1.00% | <100  | 1.00% | <10,000  |
| Chloroform                | (µg/kg) | <100  | 1.00% | <100  | 1.00% | <10,000  |
| Carbon Tetrachloride      | (µg/kg) | <100  | 1.00% | <100  | 1.00% | <10,000  |
| Trichloroethene (TCE)     | (µg/kg) | <100  | 1.00% | <100  | 1.00% | <10,000  |
| Dibromomethane            | (µg/kg) | <200  | 1.00% | <200  | 1.00% | <20,000  |
| Dichloromethane           | (µg/kg) | <1000 | 1.00% | <1000 | 1.00% | <100,000 |
| Bromochloromethane        | (µg/kg) | <1000 | 1.00% | <1000 | 1.00% | <100,000 |
| Bromodichloromethane      | (µg/kg) | <200  | 1.00% | <200  | 1.00% | <20,000  |
| Dibromochloromethane      | (µg/kg) | <40   | 1.00% | <40   | 1.00% | <4,000   |
| 1,2-Dibromoethane         | (µg/kg) | <40   | 1.00% | <40   | 1.00% | <4,000   |
| 1,2-Dichloroethane        | (µg/kg) | <200  | 1.00% | <200  | 1.00% | <20,000  |
| Tetrachloroethane         | (µg/kg) | <40   | 0.10% | <40   | 0.10% | <40,000  |
| 1,1,1-Trichloroethane     | (µg/kg) | <100  | 1.00% | <100  | 1.00% | <10,000  |
| 1,1,2-Trichloroethane     | (µg/kg) | <100  | 1.00% | <100  | 1.00% | <10,000  |
| Hexachlorobutadiene       | (µg/kg) | <40   | 1.00% | <40   | 1.00% | <4,000   |
| 1,1,1,2-Tetrachloroethane | (µg/kg) | <200  | 1.00% | <200  | 1.00% | <20,000  |
| 1,1,2,2-Tetrachloroethane | (µg/kg) | <200  | 1.00% | <200  | 1.00% | <20,000  |
| 2-Chlorotoluene           | (µg/kg) | <40   | 1.00% | <40   | 1.00% | <4,000   |
| 4-Chlorotoluene           | (µg/kg) | <40   | 1.00% | <40   | 1.00% | <4,000   |
| 1,1-Dichloroethene        | (µg/kg) | <200  | 1.00% | <200  | 1.00% | <20,000  |
| 1,1-Dichloroethane        | (µg/kg) | <200  | 1.00% | <200  | 1.00% | <20,000  |

|                           |         |      |       |      |       |         |
|---------------------------|---------|------|-------|------|-------|---------|
| cis-1,2-Dichloroethene    | (µg/kg) | <200 | 1.00% | <200 | 1.00% | <20,000 |
| trans-1,2-Dichloroethene  | (µg/kg) | <200 | 1.00% | <200 | 1.00% | <20,000 |
| 1,2-Dichloropropane       | (µg/kg) | <200 | 1.00% | <200 | 1.00% | <20,000 |
| 1,3-Dichloropropane       | (µg/kg) | <200 | 1.00% | <200 | 1.00% | <20,000 |
| 2,2-Dichloropropane       | (µg/kg) | <200 | 1.00% | <200 | 1.00% | <20,000 |
| 1,2,3-Trichloropropane    | (µg/kg) | <200 | 1.00% | <200 | 1.00% | <20,000 |
| 1,1-Dichloropropene       | (µg/kg) | <100 | 1.00% | <100 | 1.00% | <10,000 |
| cis-1,3-Dichloropropene   | (µg/kg) | <100 | 1.00% | <100 | 1.00% | <10,000 |
| trans-1,3-Dichloropropene | (µg/kg) | <100 | 1.00% | <100 | 1.00% | <10,000 |

Table IV shows the measures of total organic volatile emissions from the synthetic binder-based taphole clays, compared against results for a resin and tar-bonded taphole clay.

The laboratory results show a 99% reduction in volatile organic compounds when the synthetic binder is used as a substitute for the resin and tar binders.

The laboratory test results confirm the low trace amounts of VOCs that occur in the mix of the two synthetic binder-based taphole clays, Reftap W S MW and Reftap W B SC60. This is a substantial advantage in that the product reduces personal exposure, to virtually elimination levels as well as contributes to companies reducing their pollution footprint.

#### Field trial set-up

The field trial for the synthetic binder based Reftap W S MW and Reftap W B SC60 taphole clays at three different smelters T1, S1 and S2 was carried out over a 30-day duration.

These synthetic binder-based taphole clays were delivered and stored in a cool and dry place prior to trials. The mudgun was checked and reported to be in good operating condition. The mudgun barrel was emptied and a new mudgun nozzle installed prior to the commencement of the trial.

The tapping of the furnace was done every 2 - 3 hours. The time intervals for the drilling, lancing, and sealing of the tapholes were continuously noted during the trial, including the number of lancing rods used during the tapping process. The summary of results are given in Tables V and VI.

*Table V: Drilling & Lancing data of Reftap W S MW taphole clay*

| THC Type     | Drilling time | Lancing time | No. of Rods | Closing Time |
|--------------|---------------|--------------|-------------|--------------|
| Reftap WS MW | 01min:21sec   | 09min:46 sec | 2 rods used | 02min:36sec  |
| Reftap WS MW | 01min:43sec   | 09min:29 sec | 2 rods used | 02min:41sec  |
| Reftap WS MW | 01min:38sec   | 07min:34 sec | 2 rods used | 02min:44 sec |
| Reftap WS MW | 01min:26 sec  | 08min:56 sec | 1 rod used  | 02min:45 sec |

Table VI: Drilling & Lancing data of Reftap W B SC60 taphole clay

| THC Type       | Drilling time | Lancing time | No. of Rods | Closing Time |
|----------------|---------------|--------------|-------------|--------------|
| Reftap WB SC60 | 01min:56sec   | 10min:22sec  | 3 rods used | 03min:56sec  |
| Reftap WB SC60 | 02min:13sec   | 10min:18sec  | 3 rods used | 03min:51sec  |
| Reftap WB SC60 | 02min:09sec   | 10min:29sec  | 3 rods used | 03min:42 sec |
| Reftap WB SC60 | 02min:34 sec  | 10min:45sec  | 4 rods used | 03min:15 sec |

During the trials, the synthetic bonded taphole clay was exposed to the same furnace conditions as the tar-bonded taphole clay. Personal air monitoring was conducted on employees working in the taphole area according to the Regulations for Hazardous Chemicals - 2021. Coal pitch tar volatiles (CTPV) values were monitored and compared to Annexure 2 of the Regulations which stipulates an Occupational Exposure Level (OEL) of 0.4mg/m<sup>3</sup>. The results obtained indicated levels of between 0,19 to 0,25mg/m<sup>3</sup> on personal monitoring of furnace workers compared to previous levels of between 1,79 to 3,69 mg/m<sup>3</sup> for tar bonded taphole clay.

The results of the monitoring are shown in Table VII.

Table VII: Monitoring values for furnace operators

|         |                                       | Tar Bonded Taphole clay          |               | Synthetic Bonded taphole clay    |               |
|---------|---------------------------------------|----------------------------------|---------------|----------------------------------|---------------|
| Smelter | Permissible Exposure Limit OSHA - PEL | CTPV TWA (12 months) Ave Reading | Year / Period | CTPV TWA (12 months) Ave Reading | Year / Period |
| T1      | 0,4mg/m <sup>3</sup>                  | 3,69mg/m <sup>3</sup>            | FY 2021       | 0,19mg/m <sup>3</sup>            | FY 2025       |
| S1      | 0,4mg/m <sup>3</sup>                  | 1.78mg/m <sup>3</sup>            | FY 2018       | 0,25mg/m <sup>3</sup>            | FY 2025       |
| S2      | 0,4mg/m <sup>3</sup>                  | 2,39mg/m <sup>3</sup>            | FY 2021       | 0,25mg/m <sup>3</sup>            | FY 2024       |

#### Observations on the use of the synthetic binder-based taphole clay

The synthetic binder-based taphole clays cured adequately to be drilled and /or lanced to enable the tapping of the molten flow stream as per Figure 1.



Figure 1: Furnace taphole closed with the synthetic-bonded taphole clay.



Figure 2: Smelting furnace's worn-out taphole.

For the trial of the reconstructive taphole clay, the Reftap W B SC60 served the function of repairing the worn taphole to extend its operational time before a relining is commissioned, as is shown on Figure 2. The reconstruction of the taphole was successful as the combination of calcined bauxite and SiC aggregates are engineered to withstand the extreme temperatures, thermal cycling, and erosive forces present in the smelting furnace environment. The reconstructed taphole also enabled the controlled flow of hot metal with reduced splashing.

The key furnace closure findings after 30 days trial:

#### Comparing the wear dimensions against the restored taphole block

Table VIII: Comparing wear dimensions against the restored taphole block

| THC Type                        | Measurement Parameter | Before Rebuilding<br>(Actual Wear) | After Rebuild<br>(Restored Target) | Net change<br>/ Variance |
|---------------------------------|-----------------------|------------------------------------|------------------------------------|--------------------------|
| Reftap W B SC60<br>taphole clay | Horizontal Diameter Ø | ± 210 mm                           | ± 120 mm                           | ± 90 mm                  |
|                                 | Vertical Diameter Ø   | ± 200 mm                           | ± 110 mm                           | ± 90 mm                  |
|                                 | Left freeboard        | ± 20 mm                            | ± 145 mm                           | ± 95 mm                  |
|                                 | Right freeboard       | ± 80 mm                            | ± 125 mm                           | ± 45 mm                  |
|                                 | Top freeboard         | ± 90 mm                            | ± 145 mm                           | ± 55 mm                  |
|                                 | Bottom freeboard      | ± 60 mm                            | ± 145 mm                           | ± 85 mm                  |

- Reduced excessive wear: Taphole diameter decreased by approximately 150 mm.
- Eliminated hole enlargement: Closed the irregular 210 mm horizontal opening.
- Although it cannot restore original sizing, but it is closer to the specification.
- Maximized safety margins: Rebuilding freeboard prevents breakout risks.
- Enhanced wear resistance: Silicon Carbide content prevents premature erosion.
- Optimised tapping flow: nearly centred alignment ensures stable, predictable metal casting.
- Under no circumstances did the furnaces taphole accidentally open by itself throughout the duration of the trials.

- No unexpected problems were observed with clay gun operation throughout the course of the trial, and each closure left a solid plug in the taphole.

## CONCLUSION

This study demonstrates that the transition from conventional tar-and resin-bonded taphole clays to a synthetic binder technology achieves a dual objective: maintaining the critical functional and mechanical performance required for effective taphole sealing, while substantially reducing occupational health risks and environmental emissions.

Laboratory analyses verified that the synthetic binder-based materials meet the essential physical and chemical performance criteria, including adequate cold crushing strength, controlled thermal expansion, and compatibility with high-temperatures smelting conditions. More importantly, the results indicate a reduction of approximately 99% in volatile organic compound (VOC) emissions when compared to traditional tar-based systems, effectively eliminating the primary source of polycyclic aromatic hydrocarbon (PAH) exposure.

Field trial data further substantiates these findings, with personal exposure monitoring demonstrating a marked decrease in coal tar pitch volatile (CTPV) levels to within regulatory occupational exposure limits. This contrasts sharply with the elevated exposure levels historically associated with tar-bonded clays, confirming that material substitution is a more effective control measure than reliance on engineering controls or personal protective equipment alone.

Operationally, the synthetic binder-based taphole clays exhibited consistent and reliable performance across all evaluated parameters, including taphole sealing integrity, drillability, and resistance to premature opening. The reconstructive grade further demonstrated its capability to restore and extend taphole service life under severe furnace conditions, without introducing operational inefficiencies or additional costs.

Overall, the adoption of synthetic binder-based taphole clays provides a viable and scalable solution for smelters seeking to align operational efficiency with increasingly stringent health, safety, and environmental requirements. The results support a broader industry transition toward low-emission consumables, positioning synthetic binder technology as a key enabler of sustainable metallurgical operations.

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# Pilot- and bench-scale test work for comparative carburisation of Hot Metal produced in a DRI smelting process

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Metix and the SMS Group have developed a novel solution for a decarbonised steel production route, incorporating an open-bath electric furnace (OBF) for the reductive smelting of highly metallised, blast furnace grade direct reduced iron (DRI). The OBF pilot test campaigns were conducted for technology demonstration on a custom-built test furnaces, successfully tapping hot metal with 2.6–3.0 wt% C depending on whether H<sub>2</sub>-DRI or natural-gas-based DRI was processed. This was achieved by charging reductant as part of the raw material feed mixture to the furnace. Deep bath pneumatic injection of low-sulphur petroleum coke (PetCoke) increased the hot-metal carbon content above 4 wt%, confirming the OBF's ability to produce basic oxygen furnace (BOF) ready metal and motivating a focused evaluation of alternative carburisers.

To support industrial deployment of the DRP–OBF–BOF route, a bench-scale carburisation study evaluated whether carbon sources commonly used as smelter reductants can also function as carburisers. PetCoke was selected as the industry benchmark due to its long-established high-efficiency performance in commercial post-tap-hole hot metal carburisation, while gas calcined anthracite, hard coal, lignite coke, nut coke, and charcoal were selected as alternative carburisers based on established supply availability, existing handling infrastructure, and, for charcoal, the need to assess renewable-carbon performance.

The bench-scale experiments were conducted in a 60 kW induction furnace, using an argon shield to prevent oxidation, with each test performed at 1500°C where 100 grams of alloy was contacted with ~2 grams of carburiser in a high-purity alumina crucible targeting 4.5–4.9 wt-%C saturation level in alloy. The tests durations between 10 min (kinetic regime) and 120 min (thermodynamic regime) enabled differentiation between initial dissolution rates and long-term saturation behaviour. PetCoke achieved 4.45 wt-%C within 40 minutes and reached a peak of 4.83 wt-%C after 120 minutes. Its superior performance is attributed to its high fixed carbon (>98%), low ash (<0.5%), and a highly accessible turbostratic micro texture, which facilitates rapid dissolution kinetics and a peak carburisation rate of 0.27%C/min. In contrast, other materials showed limited effectiveness, which is attributed to the probable formation of interfacial barriers associated with ash and mineral inclusions. While charcoal reached 4.55 wt-%C after 120 minutes due to its high porosity, its initial rate was low and characterised by high variability. Nut coke (3.98 wt-%C peak), lignite coke (3.79 wt-%C peak), anthracite (3.76 wt-%C peak), hard coal (3.27 wt-%C peak) failed to reliably meet the target of 4.5 wt-%C.

Overall, the comparative results demonstrate that only calcined low-sulfur PetCoke achieves the carburisation kinetics and saturation performance required for BOF-ready hot metal, while the remaining five materials are constrained by ash-related interfacial barriers.

**Keywords:** Carburisation, Carburiser, Hot Metal, Open Bath Furnace (OBF), Carbon dissolution kinetics, calcined low sulfur PetCoke

## INTRODUCTION

### *Steel decarbonisation drivers*

The global steel industry remains one of the most carbon-intensive sectors, accounting for 7–9% of global CO<sub>2</sub> emissions (Manojlovic, et al., 2024). Decarbonising steelmaking is therefore a central pillar of the global net zero emissions (NZE) 2050 strategy, as outlined by the International Energy Agency (IEA), which stresses that the primary ironmaking stage (blast furnaces) contributes over 80% of total steel-sector emissions (SolarTech, 2025). Hydrogen-based direct reduction (H<sub>2</sub>-DRI) coupled with EAF melting is increasingly identified as a central pathway for low-carbon steel production, driven by rapid technological progress and near-zero-emission project deployment (IEEFA, 2024). However, large-scale adoption is constrained by a shortage of DR-grade iron ore, as high-grade ores suitable for hydrogen reduction account for only a small share of global reserves (3–4%) (Shahabuddin, et al., 2024). Furthermore, near-carbon-free H<sub>2</sub>-DRI creates metallurgical challenges, low carbon in the hot metal, necessitating controlled carburisation downstream to restore operability (Ji, et al., 2024).

### *Alternative low-carbon ironmaking route: DRP–OBF–BOF configuration*

SMS Metix in partnership with other SMS subsidiaries have developed a hybrid ironmaking route that integrates a direct reduction plant (DRP) with an open-bath electric furnace (OBF), offering a next-generation low-carbon alternative to the blast furnace (BF). The OBF reduces reliance on coke as the primary energy source and reductant, and uses electrical energy for smelting, significantly lowering greenhouse-gas emissions. When coupled with natural-gas-based DRI, the route achieves emission intensities below 800 kg CO<sub>2</sub>/t steel, and under 400 kg CO<sub>2</sub>/t when hydrogen replaces natural gas, representing more than a 70% reduction compared with modern BF–BOF operations emitting 1.9–2.3 t CO<sub>2</sub>/t. Unlike DR–EAF systems, which require >65% Fe DR-grade pellets, the OBF accommodates a much broader ore-quality envelope, including 58% Fe total, BF-grade pellets, fluxes, and recycled metallics, addressing the critical global shortage of DR-grade ore (~3–4% of traded ore) (INCITE-Innovation Centre for Industrial Transformation and Emissions, European Commission, 2026).

### *Pilot-scale OBF validation and bench-scale testing programme*

To de-risk the OBF technology, Metix and SMS executed a structured pilot-scale test programme using a 200 kW-DC followed by a 0.9–1.4 MW-DC OBF, which were operated in a controlled short open-arc mode (SOAC™) (Geldenhuys, et al., 2025). These units processed DRI blended with reductants and slag modifiers to produce hot metal and BF-type slag, demonstrating reliable reductive smelting of DRI, stable hot-metal carburization, and effective slag chemistry control for cementitious applications. Initial campaigns produced hot metal containing 2.6–3.0 wt% C, depending on whether H<sub>2</sub>-DRI or natural gas DRI was utilised. Subsequent in-situ carburisation via pneumatic transfer, using calcined low-sulfur petroleum coke (PetCoke), successfully increased carbon levels above 4 wt%C in the 0.9–1.4 MW test, confirming the OBF's capability to produce BOF-ready hot metal.

The 0.9–1.4 MW carburisation results motivated a dedicated bench-scale induction furnace study to compare the carburisation performance of six industrial carbon sources. PetCoke remains the primary technical benchmark due to its proven industrial performance and long-term reliability in post-tap-hole carburisation via pneumatic injection. The remaining five carbon sources; gas-calcined anthracite, hard coal, lignite coke, nut coke and charcoal – were selected for specific strategic reasons: commonly used materials as reductants in smelting operations, with established supply chains and handling infrastructure, making them practical candidates for assessing whether readily available reductants can also function effectively as hot metal carburisers; charcoal was included to generate comparative data for renewable-carbon performance, supporting long-term decarbonisation objectives. Although the bench-scale environment does not replicate full-scale furnace dynamics, it provides a critical comparative framework to determine whether these logistically accessible and sustainability-aligned materials can match the dissolution kinetics and carburisation efficiency of the established commercial carburiser, PetCoke.

### *Prior research on carbon dissolution and hot-metal carburisation*

Carburisation, the process of carbon transfer into molten or solid metallic systems, is influenced by multiple factors including alloy properties, reaction kinetics, carbon source properties, and operational

conditions (Khanna, et al., 2011) and (Khanna, et al., 2014). Solubility of carbon and the diffusivity within the alloy matrix depends on temperature, alloy composition, and impurity content (McCarthy, et al., 2003) and (Jiao, et al., 2017). Higher structural order (e.g., in graphite) enhances dissolution predictability, whereas mineral matter and ash can hinder carbon transfer by forming surface layers (Khanna, et al., 2005) and (Jiao, et al., 2017).

The study by (Bandyopadhyay, et al., 2003) investigates the dissolution kinetics of five distinct carbonaceous materials, graphite, PetCoke, nut coke, charcoal, and coal within liquid iron baths. The carbon dissolution kinetics were investigated using a Tamman furnace where various carbon sources were immersed in liquid iron for 12 minutes under argon purging. The study employed electrolytic iron (starting carbon ~0.015wt%) and sponge iron (starting carbon ~0.20wt%) as the liquid media. Maximum carbon values reached approximately 4wt% for petroleum coke at 1600°C, which outperformed graphite (~3.2wt%), charcoal (~1.4wt%), coal (~1.1wt%), and nut coke (~0.8wt%). Petroleum coke's superior performance was attributed to its low ash content, which minimises the formation of a restrictive viscous barrier at the interface and its high porosity, which increases the available reaction surface area. Performance was measured using the 'alloy carbon concentration vs. time' metric.

Carburisation practices in industrial steelmaking and iron casting rely on a range of carbonaceous additives whose selection depends on purity, assimilation efficiency, and operational constraints (Janerka *et al.*, 2019; Janerka *et al.*, 2020). Commercial carbon sources for alloy carburisation are usually fossil-based materials, which include graphite, anthracite, metallurgical coke, pitch coke, calcined petroleum coke, and silicon carbide (Lu, et al., 2022). Synthetic graphite is widely regarded as the most efficient carburiser due to its high carbon purity and minimal ash, achieving transfer rates approaching 94% (Janerka, et al., 2019). Bio-carbon is increasingly adopted to reduce fossil-derived emissions in electric arc furnace operations (Lu, et al., 2022). Carburiser introduction methods strongly affect dissolution kinetics and the final carbon content of the alloy. Common industrial approaches include solid-charge addition, surface addition onto the liquid bath, bottom addition into the ladle before tapping, injection into the tapping stream, and pneumatic injection of finely dispersed carburiser (Janerka, et al., 2007). Metix-SMS group has piloted and patented an in-situ carburisation method for DRI-based hot metal. This process utilises deep-bath pneumatic injection to deliver carbon directly into the molten bath prior to tapping, effectively raising carbon content to BOF-ready specifications (Lötter, et al., 2024).

Literature consistently shows that carbon dissolution is governed by thermodynamics, interfacial phenomena, and reaction kinetics. Carburisation is inherently multifactorial, requiring optimisation of carbon-source properties, alloy chemistry, thermal conditions, and delivery method.

## EXPERIMENT METHODOLOGY

### *Material characteristics*

#### *Carbon Sources*

The following summary table characterises the carbonaceous materials based on their chemical and structural properties.

Table I: Physical and chemical characteristics of test materials crushed and milled: <1 000  $\mu\text{m}$

| Parameter       | Units | PetCoke | Lignite<br>Coke | Nut<br>Coke | Anthracite | Hard<br>Coal | Charcoal |
|-----------------|-------|---------|-----------------|-------------|------------|--------------|----------|
| Fixed Carbon    | wt-%  | >98.0   | 87.3            | 86.7        | 94.4       | 73.9         | 80.3     |
| Ash             | wt-%  | <0.5    | 8.3             | 10.0        | 3.9        | 5.0          | 0.9      |
| Volatile matter | wt-%  | <0.5    | 3.3             | 1.6         | 2.1        | 21.1         | 15.97    |

|                                    |                    |       |       |       |       |       |        |
|------------------------------------|--------------------|-------|-------|-------|-------|-------|--------|
| Sulfur                             | wt-%               | <0.05 | 0.42  | 1.2   | 0.2   | 0.9   | 0.20   |
| Moisture                           | wt-%               | trace | <1    | <1    | trace | trace | 2.9    |
| Granulometry<br>(D <sub>50</sub> ) | μm                 | 380   | 295   | 360   | 380   | 400   | 330    |
| Internal Porosity                  | %                  | 22.7  | 7.4   | 10.3  | 6.2   | 11.2  | 32.6   |
| Mean Pore Area                     | μm <sup>2</sup>    | 4 347 | 1 022 | 1 568 | 3 362 | 345   | 15 388 |
| Pore Volume                        | cm <sup>3</sup> /g | 0.14  | 0.04  | 0.06  | 0.03  | 0.08  | 0.33   |

The six materials span wide chemical and physical variability, ranging from ultra-pure PetCoke (>98% C, <0.5% ash) to high-ash lignite and nut coke (8–10%). Their porosity differs significantly, from dense anthracite (6.2%) to highly porous charcoal (32.6%), reflecting substantial contrasts in mineral matter, pore structure, and thermal history.

#### *Alloy*

The chemical compositions of the initial alloy phases are detailed in Table II. These baseline matrices provided a consistent starting point for subsequent testing, exhibiting average carbon contents of 2.60 wt-% and 2.65 wt-% for Phase 1 and Phase 2, respectively.

Table II: Alloy average phase composition

|                      | Fe    | C*   | Si   | S    | P    | Mn   | V    | Others |
|----------------------|-------|------|------|------|------|------|------|--------|
|                      | wt-%  | wt-% | wt-% | wt-% | wt-% | wt-% | wt-% | wt-%   |
| <b>Phase 1 Alloy</b> | 95.90 | 2.60 | 0.68 | 0.12 | 0.20 | 0.13 | 0.17 | 0.20   |
| <b>Phase 2 Alloy</b> | 96.00 | 2.65 | 0.54 | 0.11 | 0.20 | 0.09 | 0.19 | 0.22   |

\* Three replicate alloy samples were analysed per experimental phase, and the corresponding minimum and maximum carbon contents are presented in Figures 3 and 5.

#### *Test Furnace*

All carburisation experiments were conducted in a 60 kW induction furnace (Mintek Facility) equipped with a graphite susceptor and high-purity alumina crucibles, operated under an argon atmosphere to prevent oxidation, see Figure 1. The furnace was heated at a controlled rate of ~10°C/min to 1500°C, where eddy-current induction provided natural bath stirring. A B-type thermocouple continuously monitored temperature during isothermal holding and controlled cooling. The required mass of each carbon source was calculated to achieve 4.5 wt% carbon saturation in 100 g of alloy, after which approximately 2 g of carburiser was placed at the bottom of a high-purity alumina crucible and covered with the 100 g alloy charge; the induction furnace accommodated up to four such crucibles per heat.

The test program comprised two systematic phases: Phase 1 established baseline carburisation behaviour using industrial materials; calcined low-sulfur PetCoke, gas-calcined anthracite, and raw hard coal, tested across 10-40 minute intervals. Phase 2 expanded the study to lignite coke, nut coke and charcoal, incorporating short 5-10 minute kinetic intervals and extended 120 minute saturation tests. To map the kinetics, individual and independent tests were conducted for each distinct time increment (10, 20, 30, 40 and 120 minutes) rather than sequential sampling from a single melt. The test start time was defined by thermocouple readings reaching the target temperature of 1500 °C. PetCoke was tested in both Phase 1 and Phase 2 of carburisation experiments, as an industry benchmark and as a basis of performance comparison for other carburisers.

This test program design enabled isolation of initial dissolution kinetics and evaluation of long-term thermodynamic limits influenced by ash chemistry, impurity content, and interfacial resistance.

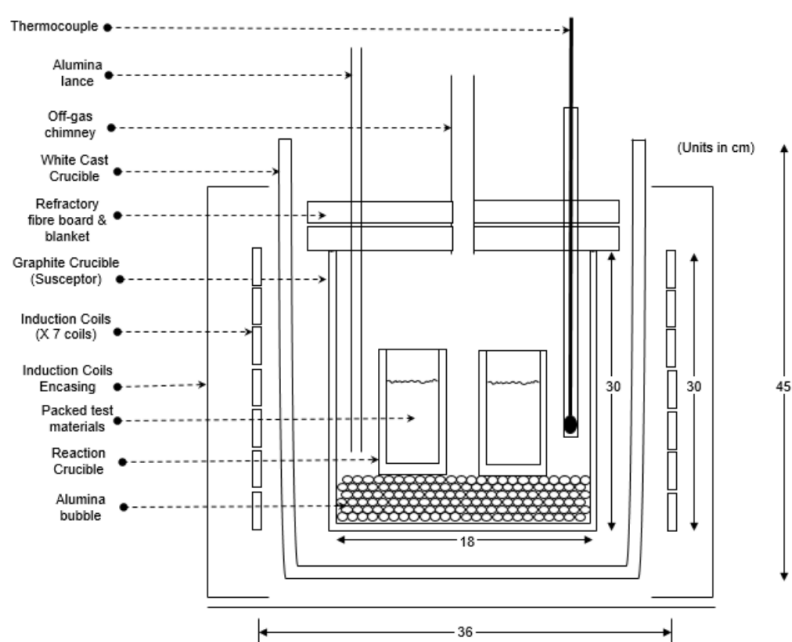


Figure 1. Schematic diagram of Induction Furnace used for alloy carburisation tests (Mintek).

## RESULTS AND DISCUSSION

### Phase 1 Results

#### SEM Analysis of Carburisers

The initial phase of testing comprised three distinct carburiser variants: PetCoke, anthracite, and hard coal. These materials underwent scanning electron microscopy (SEM) to assess surface morphology [secondary electron images (SEM-SE)] and cross-sectional ash distribution [backscattered electron (SEM-BSE) images]. These micrographs are presented in Figure 2 to illustrate the structural and compositional characteristics inherent to each carburiser type.

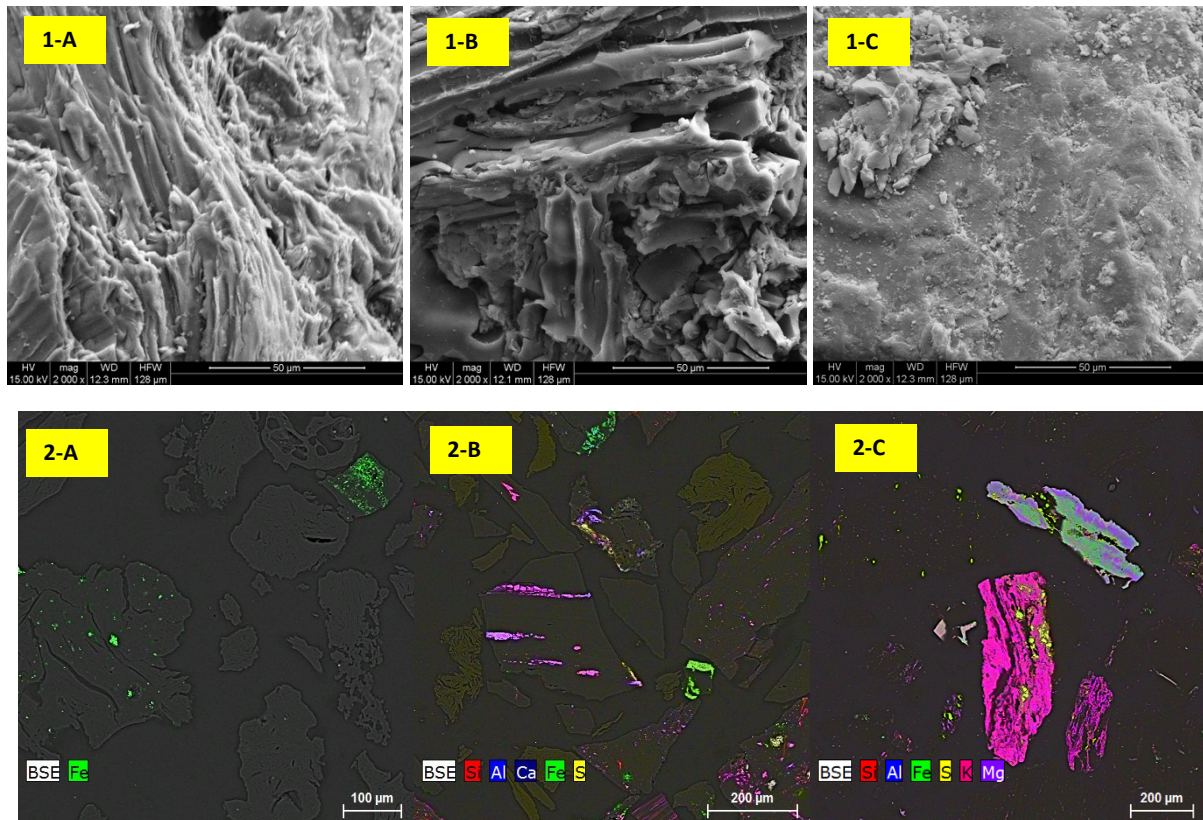


Figure 2. Image (1-A to 1-C) shows x2000 magnification, SEM-SE images of PetCoke (1-A), Anthracite (1-B), and Hard Coal (1-C) (particles in their original form). Image (2-A to 2-C) SEM-BSE images of PetCoke (2-A), Anthracite (2-B) and Hard Coal (2-C) (polished cross section of particles).

The SEM analysis reveals distinct morphological and compositional differences across the three carburiser types. PetCoke (Figures 2: image 1-A and 2-A) exhibits a highly porous, layered surface morphology, with elemental mapping identifying isolated, concentrated mineral inclusions of iron within a relatively clean carbon matrix. In contrast, the gas calcined anthracite (Figures 2: image 1-B and 2-B) displays a dense, angular structure where mineral matter, specifically aluminum, calcium, iron, silicon, and sulfur appears as integrated, elongated veins and discrete clusters. Finally, the raw hard coal (Figures 2: image 1-C and 2-C) shows a heterogeneous surface with significant mineral adherence, while polished cross-sections reveal extensive, finely dispersed ash content containing silicon, sulfur, aluminum, iron, potassium, and magnesium, indicating the highest level of inherent impurities among the tested materials.

Calcined PetCoke's high porosity and isolated impurities should yield the fastest dissolution and highest final carbon of alloy. Conversely, the high ash content and finely dispersed impurities in raw hard coal will likely form interfacial barriers, significantly reducing carburisation efficiency. Gas calcined anthracite is expected to perform moderately, limited by its higher density and integrated mineral veins. Figure 3 shows the performance of the three carburisers.

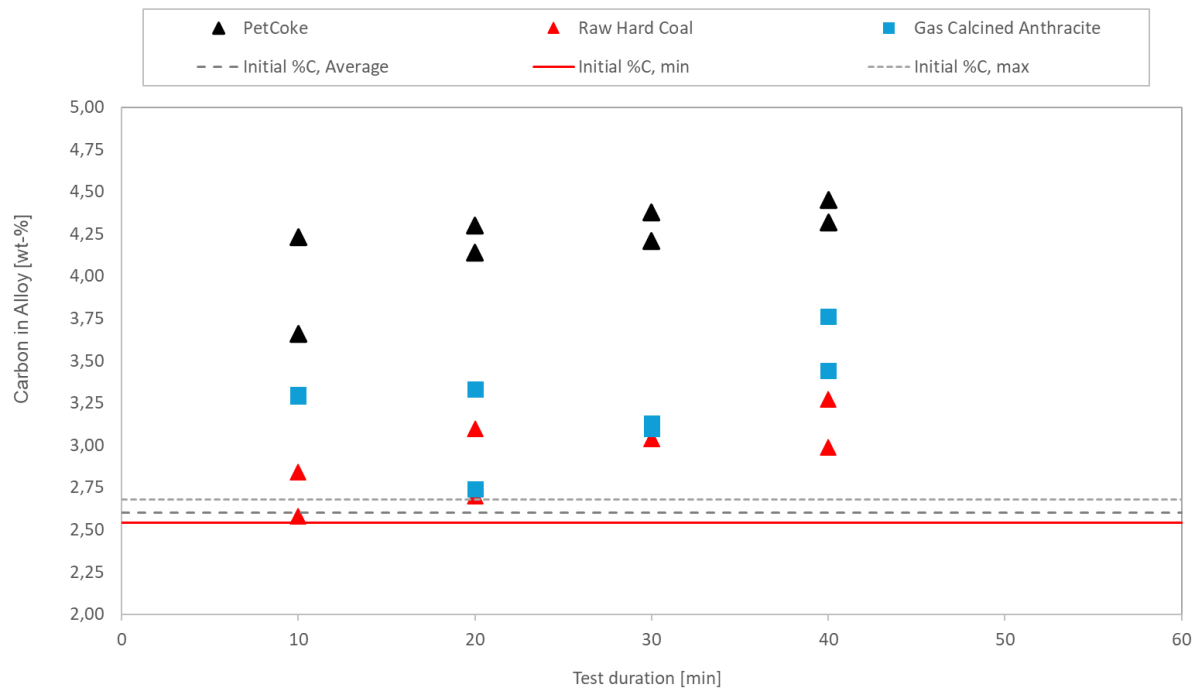


Figure 3. Phase 1 Alloy carburisation results: Carbon content measured in alloy button samples over time for PetCoke, hard coal, and anthracite compared to initial carbon levels.

The experimental results demonstrate a clear kinetic divergence at the critical 10-minute mark, where the PetCoke achieved a maximum carbon peak of 4.14 wt-%, significantly outperforming anthracite and hard coal, which reached a maximum of only 3.30wt-% and 2.84 wt-%, respectively. By the 40-minute mark, PetCoke demonstrated a superior ability to reach saturation, peaking at 4.45 wt-%, whereas anthracite and hard coal exhibited limited potential, reaching maximums of 3.76 wt-% and 3.27 wt-%, respectively. Anthracite displayed the highest inter-test variability in the dataset; SEM-BSE micrographs (Figures 2: image 1-B and 2-B) and Table I explain this through the heterogeneous distribution of discrete mineral veins containing Fe, Ca, S, Si, and Al, which leads to inconsistent interfacial slag formation and unpredictable kinetic lag.

In contrast, raw hard coal exhibits significantly constrained dissolution kinetics, demonstrating a flat and restricted carbon pickup trend over time. Although the induction test furnace was operated with a gradual temperature increase from 25°C to 1 500°C which allows volatile matter to evolve before the alloy melts, the high residual ash content and dense mineral phases in both hard coal and anthracite create persistent interfacial barriers at 1500°C. Unlike PetCoke's clean carbon matrix and low ash content, which facilitate unobstructed mass transfer and near-complete saturation, the ash chemistry, microtexture and mineralogical structure of these fossil-based reductants are key factors governing their limited suitability as rapid carburisation agents.

## Phase 2 Results

### SEM Analysis of Carburisers

Figure 4 shows SEM micrographs of additional carburisers used in phase 2 of hot metal carburisation test.



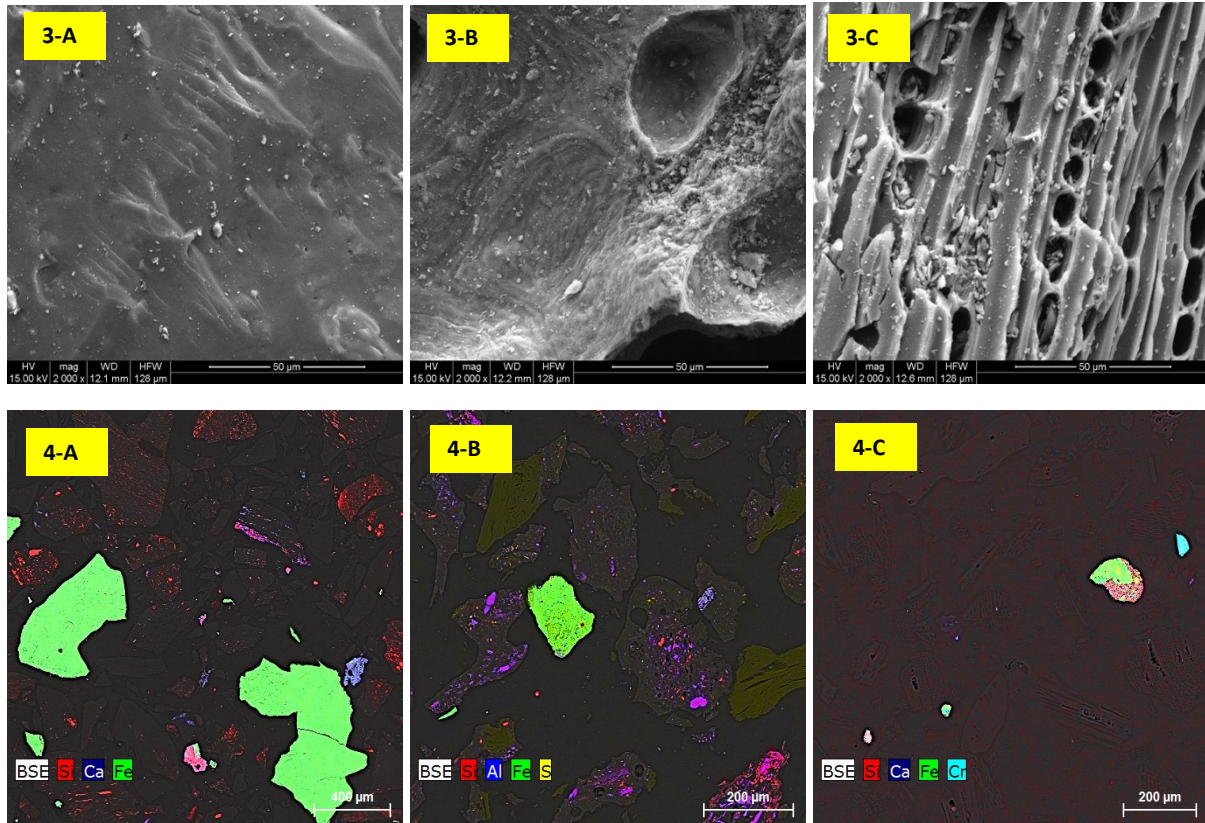


Figure 4. Image (3-A to 3-C) shows x2000 magnification, SEM-SE for lignite coke (3-A), nut coke (3-B) and charcoal (3-C)(particles in their original form). Image (4-A to 4-C) SEM-BSE images for lignite coke (4-A), nut coke (4-B) and charcoal (4-C) (polished cross section of particles)

Building on the Phase 1 characterisation, Phase 2 micrographs in Figure 4 reveal critical microstructural constraints across the additional carburisers. Lignite coke and nut coke (Figure 4: image 3-A, 3-B, 4-A, 4-B) exhibit dense matrices characterised by concentrated mineral phases of iron, silicon, aluminum, sulfur, and calcium. These mineral inclusions are likely to promote severe interfacial impedance during alloy carburisation. These structural features are hypothesised to yield a significantly lower mass transfer coefficient, effectively limiting final carbon levels in the alloy. Conversely, charcoal (Figure 4: image 3-C, 4-C) possesses a highly evolved cellular network with minimal inherent ash, suggesting a high reaction surface area that could facilitate a mass transfer coefficient rivalling the industrial benchmark, calcined PetCoke. The average mineral-matter composition of lignite coke, nut coke, and charcoal, together with the calculated solidus and liquidus temperatures of their ash phases, is presented in Table III.

Table III. Chemical composition of mineral matter found in carbon sources

| Carburiser Material | Al <sub>2</sub> O <sub>3</sub> | CaO  | Fe <sub>2</sub> O <sub>3</sub> | MgO   | MnO  | SiO <sub>2</sub> | *T <sub>Solidus</sub> | **T <sub>Liquidus</sub> | Viscosity at 1500°C |
|---------------------|--------------------------------|------|--------------------------------|-------|------|------------------|-----------------------|-------------------------|---------------------|
|                     | wt-%                           | wt-% | wt-%                           | wt-%  | wt-% | wt-%             | °C                    | °C                      | Pa.s                |
| Charcoal            | 8,04                           | 20   | 6,25                           | 21,43 | 5,36 | 38,92            | 948                   | 1 400                   | 0.19                |
| Lignite Coke        | 16,82                          | 6,07 | 37,07                          | 2,8   | 3,58 | 33,66            | 1 010                 | 1 430                   | 0.22                |
| Nut Coke            | 31,22                          | 0,41 | 7,04                           | 1,41  | 0,35 | 59,57            | 1 150                 | 1 480                   | 11.6                |

\*Liquidus temperature, and \*\*Solidus temperature of carburiser ash, calculated using FactSage, version 8.4.



Table III indicates that mineral inclusions in lignite coke, nut coke, and charcoal are expected to melt at the induction furnace test temperature of 1500 °C, based on FactSage calculations. These molten mineral phases exhibit viscosities in the range 0.2–11.6 Pa.s and are therefore likely to form slag films that may partially encapsulate carbon particles. This behaviour is consistent with the formation of interfacial barriers that could contribute to reduced mass transfer rates and variability in carburisation performance.

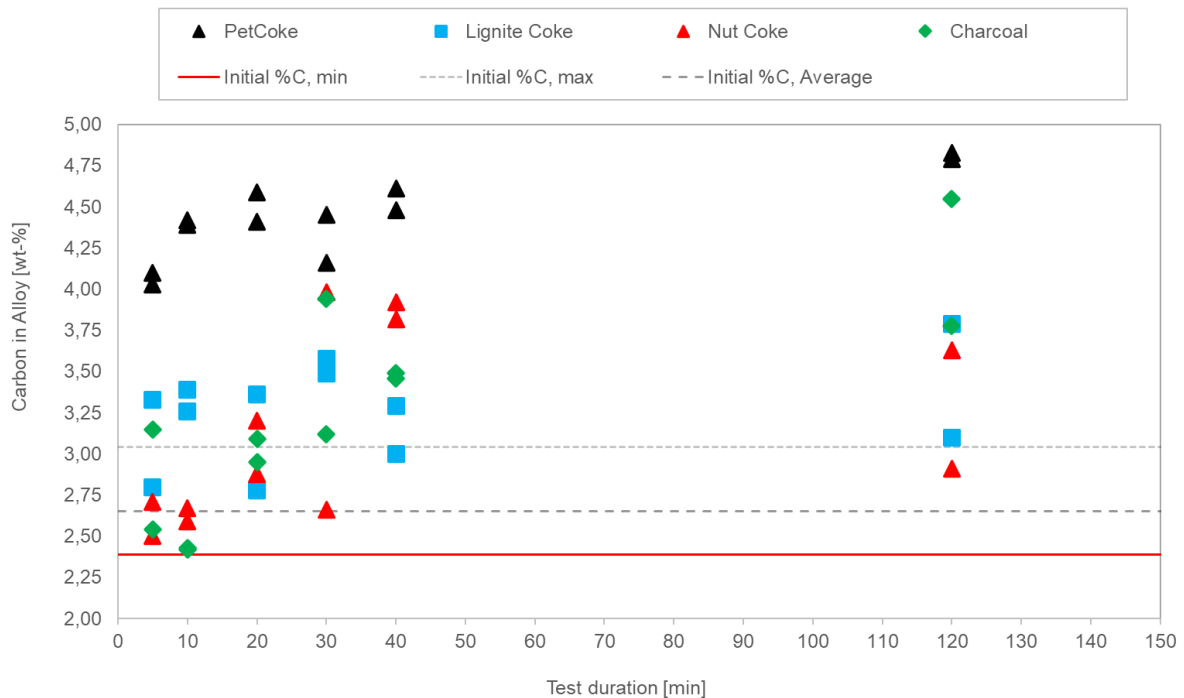


Figure 5. Phase 2 Alloy carburisation results: Carbon content measured in alloy button samples over time for PetCoke, Lignite Coke, NutCoke and Charcoal compared to initial carbon levels.

Carburisation results in figure 5 above show distinct differences in dissolution behaviour that correlate strongly with the microstructural and chemical properties of each carbonaceous material. PetCoke delivered the highest and most consistent carburisation performance, increasing carbon in metal from 4.03wt% at 5 minutes test to a peak of 4.83wt% at 120 minutes; the performance was consistent with Phase 1 results. Its superior performance is attributed to its highly disordered, turbostratic carbon structure visible in SEM-BSE images, combined with very high fixed carbon (>98%) and minimal ash. These features promote clean interfacial contact and reduce kinetic barriers to carbon dissolution. Charcoal reaches 4.55 wt% carbon after 120 minutes, driven by its amorphous atomic structure providing high active site density along accessible outer grain boundaries and structural fragility. However, its initial carburisation rate is extremely low due to lower fixed-carbon content and high volatile matter leading to large early-stage variability.

Nut coke showed moderate performance, with large inter-test variability and early-stage carbon pickup between 2.5% and 3.3%, later increasing to 3.98% at 30 minutes and reaching 3.63% after 120 minutes. Consistent with SEM evidence of irregular pores and abundant mineral phases that inhibit dissolution by forming interfacial slag films. Its higher ash content and microtexture reduce carbon availability and slow dissolution. Lignite coke produced the weakest carburisation response, generally stabilising between 3.1% and 3.6%, with a maximum of 3.79% at 120 minutes. SEM-BSE imaging revealed a dense, low-porosity matrix with widespread silicate and calcium-rich inclusions, which correspond to the high ash content measured. These non-wettable mineral phases impede melt infiltration and significantly suppress carburisation rates.

### Initial Rate of Alloy Carburisation

The initial carburisation rates of combined phase 1 and phase 2 performance are indicated in Figure 6.

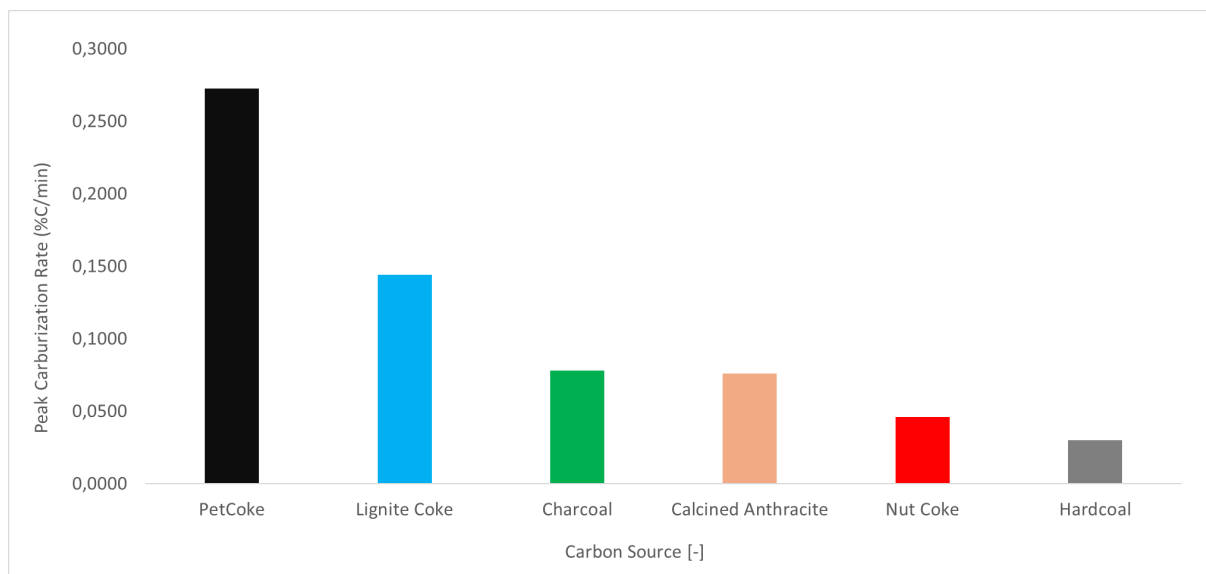


Figure 6. Initial peak carburization rates (%C/min) for various carbonaceous materials during the first 10 minutes of alloy dissolution.

The initial peak carburisation rate is the definitive kinetic parameter for evaluating carbon mass transfer coefficients, as it quantifies the dissolution velocity before the system is hampered by the diminishing concentration gradient. Petcoke's dominant initial carburisation rate of 0.27%C/min is directly linked to its highly accessible turbostratic microtexture and extremely low ash, as shown in the SEM-BSE images. These features create clean reaction interfaces and allow rapid dissolution, explaining why PetCoke is 2x faster than lignite coke and 6x faster than nut coke. Nut coke and hard coal, with extensive mineral phases and irregular pore networks, show much lower rates. PetCoke is 9x faster than hard coal while charcoal's extremely open pore structure offers little benefit initially due to fragile carbon walls, making Petcoke 3.5x faster.

While PetCoke's peak rate of 0.27%C/min indicates a high initial carbon activity gradient, the performance data from the 120 minute heats confirm a sharp decay in flux as the metal carbon content ( $C_m$ ) approaches the saturation limit ( $C_s$ ). This asymptotic behaviour, governed by the ( $C_s - C_m$ ) driving force, explains why even high-performing materials show a plateauing effect at extended durations. SEM-BSE analysis suggests that this thermodynamic slowing is exacerbated by physical impediments; specifically, the mineralogical phases (Fe, S, Si, Ca and Al) identified in nut coke, lignite coke, anthracite and hard coal increase interfacial resistance. These inclusions effectively thicken the boundary layer, further reducing the mass transfer coefficient as the concentration gradient weakens, leading to the inconsistent final carbon values reported in Phases 1 and 2.

## CONCLUSION

1. To support the transition to the DRP-OBF route, five carbon sources commonly used as reductants in smelters were evaluated for their hot metal carburisation capability and compared to PetCoke, the established industry benchmark.
2. The experimental results demonstrate that calcined low-sulfur petroleum coke is the only material among those tested that reliably meets both kinetic (initial carburisation rate) and carburisation requirements (saturation), making it the preferred industrial carburiser for achieving BOF-ready hot metal (4.0-4.5 wt-%) in the DRP-OBF-BOF ironmaking route.

3. Results show that material mineralogy, including ash distribution, microtexture and pore structure, plays a dominant role in governing carburisation behaviour, although the relative contribution of each factor cannot be fully isolated. Smelter operators cannot assume all available reductants will be able to carburise effectively as PetCoke.
4. Calcined low sulfur PetCoke demonstrated superior carburisation capability by reaching 4.39 wt-% C within 40 minutes (Phase 1) and a maximum of 4.83 wt-% C at 120 minutes (Phase 2), followed in descending order of performance by charcoal (4.55 wt-% at 120 min), nut coke (3.98 wt-% at 30 min), lignite coke (3.79 wt-% at 120 min), gas calcined anthracite (3.76 wt-% at 40 min), and raw hard coal (3.27 wt-% at 40 min). All materials exhibit flux decay as metal carbon approaches the saturation limit.
5. The investigation identifies calcined low sulfur PetCoke as the definitive benchmark for high-efficiency ironmaking, achieving a peak initial carburisation rate of 0.27%C/min. This performance establishes a significant kinetic gap when compared to the other five materials, with PetCoke being 2 times faster than Lignite Coke, 3.5 times faster than Charcoal, 3.6 times faster than gas calcined anthracite, 6 times faster than nut coke, and 9 times faster than raw hard coal.
6. PetCoke benefits from a highly disordered, turbostratic carbon structure and negligible ash content that ensures clean reaction interfaces; the alternative materials lag significantly due to their inherent mineralogy. At 1500 °C, mineral inclusions in lower-performing materials are expected to melt and form viscous slag films, which likely act as interfacial barriers to carbon mass transfer.
7. The comparative results align with previous experimental work and are corroborated by SEM-SE and SEM-BSE micrographs; these images demonstrate that carburisers with clean matrices, low ash, and high porosity (such as PetCoke) facilitate unobstructed interfacial contact. Conversely, materials with dense mineral phases or slag-forming inclusions (such as lignite coke, nut coke, raw hard coal, and anthracite) are likely to develop interfacial barriers that may impede dissolution.

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# Hydrogen pre-reduction of different manganese ores in a vertical retort toward demonstration of the HALMan process

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The HALMan process is a zero-waste flowsheet for the production of manganese alloys using hydrogen gas and aluminium metal as non-fossil reductants. The process focuses on treating primary and secondary raw materials in a sustainable manner while pursuing decarbonisation of industrial metal production and recycling of by-products for secondary application. The hydrogen pre-reduction step of the process can be undertaken in vessels of different designs including vertical retorts, rotary furnaces, shaft furnaces, and fluidised bed reactors. The current study investigated hydrogen pre-reduction of two different manganese ores in a pilot-scale vertical retort. The retort was externally electrically heated, and the hydrogen gas was introduced at the bottom of the retort through a distribution plenum to percolate through a packed bed of coarse particulates. The study evaluated the pre-reduction of Nchwaning and UMK manganese ores, screened to a size class of +6 mm –20 mm, with 100% hydrogen gas under different reaction temperatures and residence times. The paper will present a detailed account of the characterisation of the manganese ores, experimental equipment and methods adopted, and discuss the experimental results obtained with respect to the extent of reduction achieved for each type of manganese ore.

**Keywords:** HALMan process, hydrogen pre-reduction, vertical retort, manganese ore

## INTRODUCTION

The production of manganese (Mn) and its alloys is characterised by high-energy consumption and intense carbon dependency, resulting in significant greenhouse gas (GHG) emissions (Holappa, 2010; Nielsen *et al.*, 2025). This industrial sector has a substantial carbon dioxide (CO<sub>2</sub>) footprint, responsible for 2.6 gigatons (Gt) in global CO<sub>2</sub> equivalent (CO<sub>2</sub>e) emissions annually (Davies *et al.*, 2023). Conversely, stringent environmental sustainability and conservation policies require industrial processes to abate emissions (Hsu and Dunand, 2025). Therefore, in order to meet the demands of sustainable metallurgical operations, the extraction and recovery of valuable metals must be replaced with alternative, carbon-neutral process routes.

The integrated hydrogen (H<sub>2</sub>) pre-reduction and aluminothermic smelting of manganese (HALMan) process demonstrates an applicable technology with significant potential to drastically diminish the specific CO<sub>2</sub> emissions and decarbonise the production of Mn and its alloys. HALMan is an integrated process involving H<sub>2</sub> pre-reduction of Mn ores, followed by aluminothermic reduction smelting. This process can potentially eliminate CO<sub>2</sub> emissions from ferromanganese (FeMn) production (Safarian, 2021; Sarkar *et al.*, 2024).

The use of H<sub>2</sub> gas as an alternative reductant presents a promising and attractive alternative technology contributing towards the decarbonisation of metallurgical operations (Eijk *et al.*, 2023). Oxide reactions with H<sub>2</sub> produce water (H<sub>2</sub>O), instead of carbon monoxide (CO) and/or CO<sub>2</sub> as by-products. Additionally, H<sub>2</sub> has demonstrated enhanced reaction kinetics than CO in the reduction of higher manganese oxides, optimised at low oxygen partial pressures (PO<sub>2</sub>), which results in increased pre-reduction conversion rates (Schanche and Tangstad, 2021). This leads to lower energy consumption in subsequent smelting operations (Nielsen *et al.*, 2025). The utilisation of H<sub>2</sub> gas as a direct reductant of metal oxides to metallic their phase has been shown to be ineffectual for the production of other metals such as chromium (Cr), silicon (Si) and Mn (Holappa, 2010, Wu *et al.*, 2024). The reduction of the higher Mn oxides (MnO<sub>2</sub>, Mn<sub>2</sub>O<sub>3</sub> and Mn<sub>3</sub>O<sub>4</sub>) to MnO is possible by H<sub>2</sub>, as represented by Equations 1 – 3. The subsequent metallisation of the MnO to Mn is still required, of which the reductant is low-carbon aluminium (Al) (Equation 4) in the HAlMan process, rendering this a low-carbon FeMn production route (Safarian, 2021).



Studies were conducted to investigate the impact of various parameters on the reaction kinetics and extent of conversion when H<sub>2</sub> gas is employed in the reduction of Mn ore. These parameters include reduction gas mixture ratios, reaction time, temperature, particle size and mineral properties and origin (Sarkar *et al.*, 2024). Improved conversion rates and reaction extents were observed when H<sub>2</sub> gas was fully or partially employed in gas mixtures with CO, CO<sub>2</sub> or H<sub>2</sub>O in the reduction of Mn ore (Davies *et al.*, 2023, Mchabe *et al.*, 2025). Davies *et al.* (2023) pre-reduced a carbonate-rich United Manganese of Kalahari (UMK) ore in a thermogravimetric (TG) furnace set-up, and the reduction performance of various reducing atmospheres was compared. The ore was reduced in gas mixture variants of CO/CO<sub>2</sub>, H<sub>2</sub>/H<sub>2</sub>O and pure H<sub>2</sub>, at reaction temperatures ranging between 700°C – 900°C. The findings revealed that the CO/CO<sub>2</sub> and H<sub>2</sub>/H<sub>2</sub>O pre-reducing gas mixtures lowered the oxidation states of only the higher Mn- and Fe-oxides, while the 100% H<sub>2</sub> atmosphere metallised FeO. At atmospheres within which H<sub>2</sub> was present, the decomposition of most of the carbonates was observed at temperatures  $\geq 700^\circ C$ , while a higher temperature of 900°C was required for the CO/CO<sub>2</sub> environment to achieve a similar outcome. Increasing the reaction temperature and the presence of H<sub>2</sub> enhanced the degree and accelerated the rate of reduction (Davies *et al.*, 2023). In another study, Schanche and Tangstad (2021) observed that increasing the reaction temperature, while decreasing the oxygen partial pressure, improved reduction rates of Nchwaning Mn ore a CO/CO<sub>2</sub> atmosphere. At constant temperature, the partial replacement of the CO in the mixture with H<sub>2</sub> up to 37% increased the reduction rate by 1.3 – 2.6 fold, at low and high oxygen partial pressure, respectively (Schanche and Tangstad, 2021).

Safarian (2021) achieved reduction of Mn<sub>2</sub>O<sub>3</sub> pellets to the MnO phase in a H<sub>2</sub> atmosphere at a temperature of 1000°C, with a holding time of two hours. A colour change from a dark grey to green was observed in the physical characteristics of the pellets, indicative of sufficient reduction to MnO (Safarian, 2021).

In this paper, Mintek in South Africa demonstrated the H<sub>2</sub> pre-reduction stage of the HAlMan process at pilot scale and technology readiness level (TRL) 4 – 5. Manganese lumpy ore was pre-reduced in a vertical retort. The pre-reduced ore was used in subsequent aluminothermic reduction smelting test work at Mintek.

## METHODOLOGY

## Materials

Nchwaning (N-ore) and UMK manganese ores from the South African Kalahari region were the subjects of this study. The ore samples were crushed and sized for the selection of a fraction with a particle size range of  $-20 + 6$  mm for both ores. This size range ensures compatibility with the pilot plant feed system operational specifications. The UMK and N-ore samples were supplied by Transalloys (Pty) Ltd and Assmang Manganese (Pty) Ltd, respectively. Additionally, high-purity Ar and H<sub>2</sub> gases were sourced from local gas suppliers. The chemical composition of the ores was analysed for base metal oxides by inductively coupled plasma – optical emission spectroscopy (ICP-OES). Total C and sulfur (S) were analysed by a combustion method (LECO). The bulk chemical compositions are reported in Table I. Both ores predominantly contained a combination of various phases of manganese oxide (represented as MnO in Table I.) at 46.3% and 50.5% for UMK and N-ore, respectively. It should be noted that chemical analysis reports MnO to represent all the manganese oxide phases present in the ore samples. Hematite (Fe<sub>2</sub>O<sub>3</sub>) was also present with concentrations of 8.0% and 18.2%, while CaO reported in concentrations of 14.0% and 8.1% in the respective ores. Other metal oxides such as MgO and SiO<sub>2</sub>, as well as C, are present in lower concentrations between 2.6 – 5.1%. A Bruker D8 Advance x-ray powder diffractometer was used for the bulk phase composition analysis, presented in Table II.

Table I. Bulk chemical composition of raw ore samples, mass %

| Ore   | Al <sub>2</sub> O <sub>3</sub> | CaO  | Cr <sub>2</sub> O <sub>3</sub> | Fe <sub>2</sub> O <sub>3</sub> | MgO | MnO  | SiO <sub>2</sub> | C   | S   |
|-------|--------------------------------|------|--------------------------------|--------------------------------|-----|------|------------------|-----|-----|
| UMK   | 0.2                            | 14.0 | 0.1                            | 8.0                            | 2.6 | 46.3 | 4.7              | 4.3 | 0.2 |
| N-ore | 0.1                            | 8.1  | 0.1                            | 18.2                           | 3.1 | 50.5 | 5.1              | 1.9 | 0.1 |

Note: The chemical analysis reporting uses MnO to represent all manganese oxide phases present in the samples.

Table II. Bulk phase analysis of raw ore samples, mass %

| Mineral       | Ideal chemical formula                                                         | UMK        | N-ore      |
|---------------|--------------------------------------------------------------------------------|------------|------------|
| Braunite      | Mn <sup>2+</sup> Mn <sup>3+</sup> <sub>6</sub> SiO <sub>12</sub>               | 39.5       | 41.4       |
| Hausmannite   | Mn <sup>2+</sup> Mn <sup>3+</sup> <sub>2</sub> O <sub>4</sub>                  | 16.8       | 3.6        |
| Manganosite   | MnO                                                                            | 0.2        | -          |
| Hematite      | Fe <sub>2</sub> O <sub>3</sub>                                                 | 7.3        | 13.7       |
| Dolomite      | CaMg(CO <sub>3</sub> ) <sub>2</sub>                                            | 13.7       | 7.9        |
| Calcite       | CaCO <sub>3</sub>                                                              | 21.1       | 5.6        |
| Quartz        | SiO <sub>2</sub>                                                               | 1.4        | -          |
| Bixbyite      | Mn <sup>3+</sup> <sub>1.5</sub> Fe <sup>3+</sup> <sub>0.5</sub> O <sub>3</sub> | -          | 14.2       |
| Manganite     | MnO(OH)                                                                        | -          | 7.0        |
| Marokite      | CaMn <sup>3+</sup> <sub>2</sub> O <sub>4</sub>                                 | -          | 2.7        |
| Kutnohorite   | Ca(Mn,Mg,Fe <sup>2+</sup> )(CO <sub>3</sub> ) <sub>2</sub>                     | -          | 3.5        |
| Rhodochrosite | Mn <sup>2+</sup> (CO <sub>3</sub> )                                            | -          | 0.4        |
| <b>Total</b>  |                                                                                | <b>100</b> | <b>100</b> |

## Equipment description

The experimental tests were conducted in a vertical retort furnace, which features a stainless steel shell tube with a height and diameter of 2200 mm and 520 mm, respectively. The bottom-end is closed with a gas distribution plenum that contains the furnace ore charge while permitting upwards gas percolation through a packed bed of coarse particulates. The shell is surrounded by heating elements, enclosed in a rectangular steel cage that is insulated with a compacted refractory fiberfrax blanket. The structure is mounted and supported on a steel rise stand. The bottom-end also serves as the entry channel for the process gases, as well as the material discharge point. At the top-end is the

off-gas chimney, through which the thermocouples and off-gas analysis probe were fitted into the furnace shaft. Hovering above the chimney is the off-gas extraction duct leading to the environmental baghouse of the off-gas handling system. Connected to the furnace is a gas supply network consisting of gas cylinders and a pipeline. A proportional-integral-derivative controller is in place for temperature input control. Thermocouples are placed at different depths within the furnace for temperature measurements and connected to a data logger for the capturing and storage of temperature data. A Micro GC Agilent 990 analyser equipped with a personal computer is fitted for off-gas analysis. At the bottom of the furnace is an empty drum for furnace discharge containment. The schematic diagram of the retort furnace with its auxiliary components is presented in Figure 1.

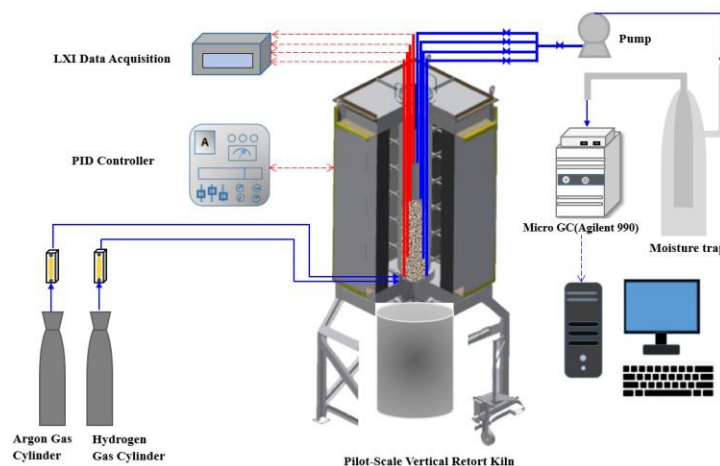


Figure 1. The schematic diagram of the vertical retort furnace.

### Experimental approach and operating conditions

A series of tests was carried out to study the implementation of the H<sub>2</sub> pre-reduction of Mn ores at pilot plant scale. Material batch sizes of, on average, 120 kg of raw lumpy Mn ore were charged into the furnace. The furnace was pre-heated under Ar flowing at 21.5 Sl/min until the charge reached a temperature of 500°C. At this temperature, the Ar was gradually displaced by H<sub>2</sub> gas flowing at a constant flowrate of 112.5 Sl/min. The furnace continued to heat up until the charge reached the target temperature of 750°C and 800°C for N-ore and UMK, respectively. When the target temperature was reached, the material was held at this temperature for residence times of 6, 8, 10, 12 or 16 hours. Following the elapse of the specified residence time, the furnace was switched off and allowed to cool down. The H<sub>2</sub> gas flow was stopped, and Ar was re-introduced until the material temperature returned to 500°C. Following the elapse of the specified residence time, the furnace was again switched off and allowed to cool down. The H<sub>2</sub> gas flow was stopped, and Ar was re-introduced until the material temperature returned to 500°C. The material was then discharged into an empty drum and allowed to continue cooling to ambient temperature. The storage drum was fitted with a nozzle through which Ar gas was pumped to displace the air. When cooled, material was sampled and submitted for analysis. The remaining material was stored in a vertical polyvinyl chloride cylindrical container with gas inlet fittings and a pressure relief gauge. The container was filled with Ar gas to prevent the re-oxidation of the ore.

## RESULTS AND DISCUSSION

### Test work overview

The H<sub>2</sub> pre-reduction of Mn ore test work involved the reduction of UMK and N-ore at target temperatures of 750°C and 800°C, respectively. These operating temperatures were determined as optimum for the ores from thermochemical simulations and prior lab-scale experiments. The ores were



reduced in batches of 120 kg at the specified temperatures for residence times of 6, 10, 12 and 16 hours. A total of 16 tests were conducted on the UMK, and 15 on N-ore. There was a combined raw ore input mass of 3727 kg, consuming 670.2 kg Ar and 135.6 kg H<sub>2</sub> gases. Final pre-reduced masses of 1622 kg and 1618 kg for UMK and N-ore, respectively, were produced. Mass loss due to a combination of thermal decomposition and material loss during physical handling was experienced.

### Hydrogen pre-reduction of manganese ores

The outcomes from the study by Safarian (2021) were used as a benchmark in determining whether or not the pre-reduction of the Mn ores treated during the current study had occurred. The benchmark study indicated that the change in concentration of target metals, phase transformation and total removed oxygen from the samples were used as metrics for assessing the pre-reduction performance of the ores. The total removed oxygen allowed for the determination of the extent of reduction (EOR) using Equation [5]. It was also observed that transformation in the physical appearance of the particles in the form of colour change from grey to a dark green served as supportive evidence that the ore was properly reduced from its higher oxide phases to MnO (Safarian, 2021).

$$EOR = \frac{\text{Mass of oxygen removed by reduction}}{\text{Mass of removable oxygen in sample}} \times 100 \quad [5]$$

A preliminary test run was conducted to assess the performance of the vertical retort furnace in comparison to the prior lab-scale tests. This test run was conducted on a 230 kg sample of N-ore for 10 hours following the experimental approach described prior. The physical appearance assessment of the product sample from this run revealed that a non-uniform reduction of the ore occurred. Four distinct fractions characterised by a difference in colour and resultant particle size were observed. The fractions were identified as (a) green-brown, (b) grey-white, (c) grey and (d) fines, presented in Figure 2.

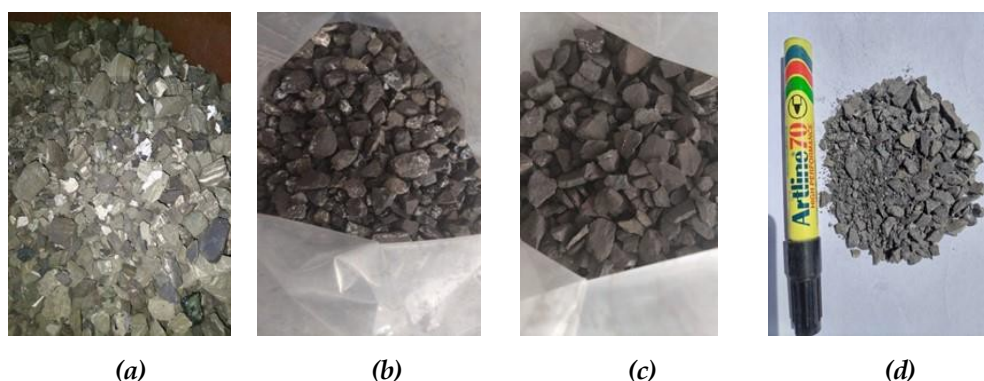


Figure 2. Pre-reduced product sample fractions (a) green-brown, (b) grey-white, (c) grey, and (d) mixed fines.

This non-uniform reduction phenomenon is attributed to the different positioning of the ore particles along the material bed height, suggesting inconsistent temperature and gas distribution within the furnace. The fractions were also sub-sampled and submitted for bulk chemical and oxygen content analysis, the results of which are presented in Table III. All the sample fractions show an oxygen content lower than the raw ore to various extents, which indicates that reduction has indeed occurred. The green/brown fraction constituted 5.0% of the product sample and reported an EOR of 83.3%. This fraction was properly and sufficiently reduced, suggesting that it was located at the bottom of the furnace and experienced sufficient contact time with H<sub>2</sub> gas. There was also a grey/white coloured fraction, constituting only 3.0% of the product sample. The presence of white patches on the surface of the particles indicated the presence of carbonates which did not thoroughly decompose. The reported EOR for this fraction was 76.3%. Furthermore, the grey fraction contributed to 43.0% of the product sample mass, and was reduced to an EOR of 53.0%. Another fraction that was observed was the mixed fines fraction, which was generated from the abrasion and attrition of the ore particles during charging and discharging of the furnace. The mixed fines accounted for 52.0% of the total mass of the product

sample, with the least EOR reported as 42.2%. Though it was expected that the smaller particle sizes would experience a better EOR due to a higher surface area, this outcome revealed that the bigger particles were reduced. Moreover, since the fractions that were less reduced dominated in this product sample, the overall EOR remained low, calculated to be 52.9%.

Table III. Chemical analysis and extent of reduction of fractions in preliminary sample, mass%

| Sample      | Mn   | Fe   | Al  | Ca  | Mg  | Si  | S   | O    | EOR, % |
|-------------|------|------|-----|-----|-----|-----|-----|------|--------|
| Raw ore     | 44.0 | 12.2 | 0.2 | 5.1 | 1.2 | 1.8 | -   | 21.2 | -      |
| Green-brown | 51.7 | 10.6 | 0.4 | 5.3 | 1.6 | 2.4 | 0.2 | 3.8  | 83.3   |
| Grey-white  | 52.8 | 10.2 | 0.1 | 6.3 | 1.1 | 2.1 | 0.1 | 5.4  | 76.3   |
| Grey        | 47.1 | 12.4 | 0.2 | 5.0 | 1.1 | 1.9 | 0.1 | 10.8 | 53.0   |
| Mixed fines | 46.9 | 12.9 | 0.1 | 4.8 | 0.8 | 1.6 | 0.3 | 13.3 | 42.2   |

The occurrence and extent of the H<sub>2</sub> pre-reduction was confirmed through XRD analysis. The mineralogical characterisation of the sample fractions indeed confirmed that the ore was pre-reduced. The relative abundances of phases that the ore transformed into during the H<sub>2</sub> pre-reduction reactions is given in Table IV.

Table IV. Qualitative relative phase proportions of fractions (a) green-brown, (b) grey-white, (c) grey and (d) mixed fines.

| Compound name                | Ideal formula                                                                                                | Relative abundance in sample fractions |            |              |              |
|------------------------------|--------------------------------------------------------------------------------------------------------------|----------------------------------------|------------|--------------|--------------|
|                              |                                                                                                              | Green-brown                            | Grey-white | Grey         | Mixed fines  |
| Manganosite                  | MnO                                                                                                          | Major                                  | Major      | -            | -            |
| Iron manganese oxide         | (FeO) <sub>0.099</sub> (MnO) <sub>0.901</sub>                                                                | Major                                  | Major      | Intermediate | Trace        |
| Iron manganese Bixbyite      | (Fe <sub>0.97</sub> Mn <sub>0.03</sub> )(Mn <sub>0.37</sub> Fe <sub>0.63</sub> ) <sub>2</sub> O <sub>3</sub> | Minor                                  | Minor      | -            | -            |
| Hausmannite                  | Mn <sup>2+</sup> Mn <sup>3+</sup> <sub>2</sub> O <sub>4</sub>                                                | -                                      | -          | Minor        | Intermediate |
| Calcium manganese iron oxide | Mn <sup>2+</sup> Mn <sup>3+</sup> <sub>2</sub> O <sub>4</sub>                                                | -                                      | -          | Minor        | Trace        |
| Jacobsite                    | Ca <sub>0.70</sub> (Mn <sub>1.53</sub> Fe <sub>0.47</sub> )O <sub>4</sub>                                    | -                                      | -          | Intermediate | -            |
| Hollandite                   | (Mn,Fe,Mg)(Fe,Mn) <sub>2</sub> O <sub>4</sub>                                                                | -                                      | -          | Minor        | Minor        |
| Braunite                     | Ba(Mn <sup>4+</sup> ,Mn <sup>2+</sup> ) <sub>8</sub> O <sub>16</sub>                                         | -                                      | -          | Minor        | Trace        |
| Hematite                     | Mn <sub>7</sub> O <sub>8</sub> (SiO <sub>4</sub> )                                                           | -                                      | -          | -            | Major        |
| Ilmenite                     | Fe <sub>2</sub> O <sub>3</sub>                                                                               | -                                      | -          | -            | Trace        |
|                              | Fe <sup>2+</sup> TiO <sub>3</sub>                                                                            | -                                      | -          | -            | Trace        |

Note: Predominant (> 50 mass %), major (30 - 50 mass %), intermediate (15 - 30 mass %), minor (5 - 15 mass %), trace (< 5 mass %) and no detection (-)

The green-brown and grey-white fractions are dominated by MnO and iron manganese oxide ((FeO)<sub>0.099</sub>(MnO)<sub>0.901</sub>) phases, which indicated a high extent of reduction in accordance to Equations 1 - 3. The minor presence of FeMn in these fractions is also notable. This means that these fractions have been properly reduced, in agreement with the high extent of reduction (from oxygen removal) reported in Table III. The presence of the higher phase Mn- and iron (Fe)-oxides in the grey and fines fractions align with the low extent of reductions as previously indicated.

#### United Manganese of Kalahari ore

A total of 16 tests were conducted for the H<sub>2</sub> pre-reduction of UMK ore, at a target temperature of 800°C. Among the 16 tests, the first six set had a residence time of six hours, followed by two sets at 10

hours and a set of eight pre-reduced for 12 hours. Figure 3 shows the colour comparison between the raw ore and how it changes colour in the product samples from Test 6 and Test 13, with reaction residence times of six hours and 12 hours, respectively. Test 6 shows the presence of grey and brown ore particles. This indicates that pre-reduction has occurred at the six hours residence time. However, when the residence time was increased to 12 hours, the particles colour transformed to a mix of green-brown in Test 13. This indicated that by increasing the residence time, the EOR was improved. In both of these samples, the resulting colour of the particles is not uniform, which revealed that pre-reduction of the sample in its entirety was incomplete. Representative samples per residence time were selected from the test runs. The XRD results from the oxide-phase analysis for samples from Tests 6, 8 and 13, representing residence times of 6, 10 and 12 hours, are shown in Table V. It can be noted that the reduction of the higher oxide phases, though minimal, has occurred in all the samples. The increased presence of MnO can be identified, which was only present at a relative abundance of 0.2 mass% in the raw UMK ore reported in Table II.

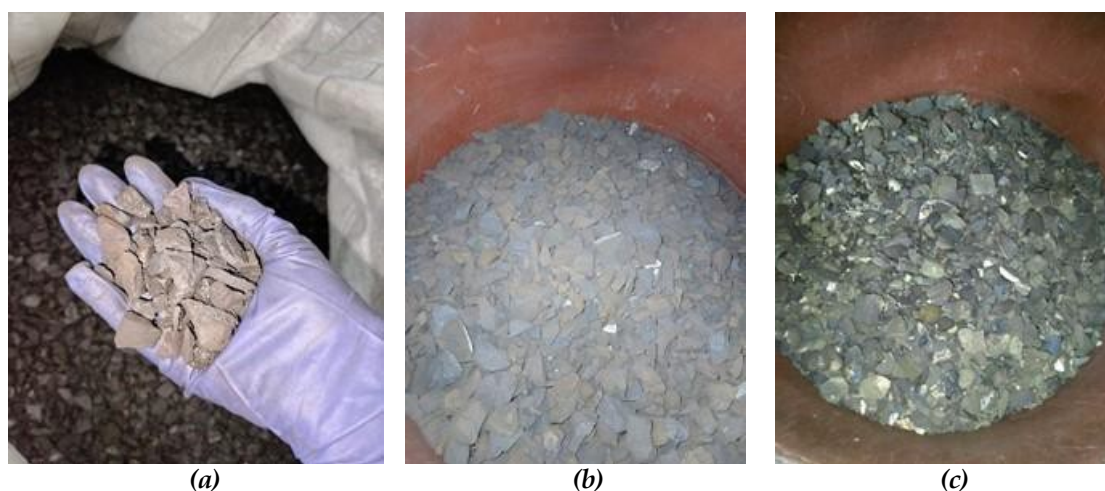


Figure 3. Colour change from (a) raw ore to UMK ore samples pre-reduced for residence times of (b) Test 6 at 10 hours and (c) Test 13 at 12 hours.

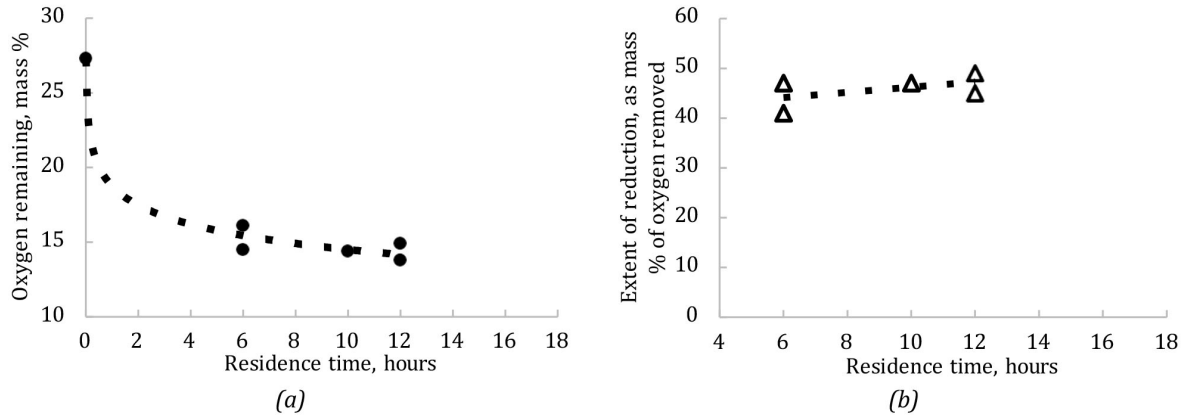
Table V. Qualitative relative phase proportions at differing residence times for UMK

| Compound name | Ideal formula                                                        | Relative abundance with residence time |              |          |
|---------------|----------------------------------------------------------------------|----------------------------------------|--------------|----------|
|               |                                                                      | 6 hours                                | 10 hours     | 12 hours |
| Manganosite   | MnO                                                                  | Minor                                  | Minor        | Minor    |
| Bixbyite      | (Mn <sub>0.37</sub> Fe <sub>0.63</sub> ) <sub>2</sub> O <sub>3</sub> | Minor                                  | Minor        | Major    |
| Hausmannite   | Mn <sup>2+</sup> Mn <sup>3+</sup> +2O <sub>4</sub>                   | Minor                                  | Intermediate | Minor    |
| Jacobsite     | (Mn,Fe,Mg)(Fe,Mn) <sub>2</sub>                                       | Minor                                  | Minor        | -        |
| Hematite      | Fe <sub>2</sub> O <sub>3</sub>                                       | Minor                                  | Minor        | Trace    |
| Calcite       | CaCO <sub>3</sub>                                                    | Intermediate                           | Intermediate | Major    |

The oxygen content of the samples was analysed by a third party laboratory, and the results are reported in Table VI and Figure 4. From Table VI, it can be seen that all the pre-reduced samples report a lower remaining oxygen concentration in comparison to the raw ore. Figure 4 (a) shows the extent of reduction that has occurred in the different pre-reduced samples, as mass% of oxygen removed, and Figure 4 (b) shows the mass % of oxygen remaining in the samples. The trends shown in Table VI and Figure 4 suggest that pre-reduction improved with an increase in residence time. However, it can also be noted that some samples pre-reduced over a similar residence time reported different EORs. This may be attributed to a possible re-oxidation during material discharging from the furnace.

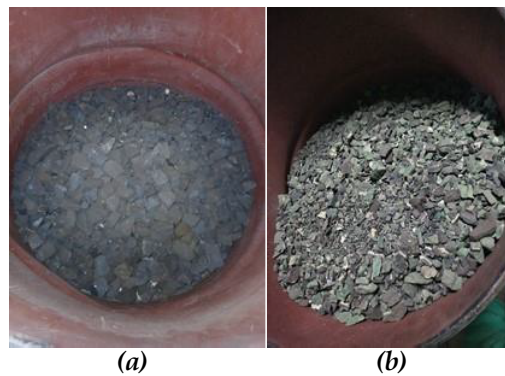
Table VI. Remaining oxygen and EOR of UMK ore as a function of residence time

| Test no. | Residence time, hours | Oxygen remaining, mass % | EOR, % |
|----------|-----------------------|--------------------------|--------|
| Raw ore  | -                     | 27.3                     | -      |
| 3        | 6                     | 14.5                     | 47     |
| 6        | 6                     | 16.1                     | 41     |
| 8        | 10                    | 14.4                     | 47     |
| 12       | 12                    | 13.8                     | 49     |
| 13       | 12                    | 14.9                     | 45     |



#### Nchwaning ore

Hydrogen pre-reduction tests were conducted on N-ore, with a total of 15 tests ran at a target temperature of 750°C. The residence times for the N-ore tests were 10, 12 and 16 hours. Figure 5 shows the product samples from Tests 18 and 31, which had residence times of 10 and 16 hours, respectively. Consistent with the UMK ore, a change in the colour of the ore particles was observed. The colour changed from the plain grey colour observed from the raw sample, to a mixture of green-brown particles for samples pre-reduced for 10 hours. Increasing the residence time to 12 hours further transformed the colour of the particles to a mix of green-brown.



The qualitative phase proportions of the N-ore samples pre-reduced over different residence times are given in Table VII. The N-ore samples were reduced over durations of 10, 12 and 16 hours. It can be seen that good pre-reduction occurred in all the samples. The 10 hours pre-reduced sample shows a content of MnO with a relative abundance in the major zone i.e., 30 – 50%, whereas it was undetectable in the raw ore (presented in Table II). It can also be observed that the braunite phase, which predominated the raw ore, broke down into lower-phase oxides. The concentration of hematite in the

10 hours sample has also decreased from 13.7% in the ore to the trace zone (< 5%). An increase in the concentration of hausmannite from 3.6% in the raw ore to an intermediate range (15 – 30%) was observed. This may be attributed to a possibility of re-oxidation likely from the post-reduction material handling stages.

Table VII. Qualitative relative phase proportions at differing residence times for N-ore

| Compound name  | Ideal formula                                                        | Relative abundance with residence time |              |              |
|----------------|----------------------------------------------------------------------|----------------------------------------|--------------|--------------|
|                |                                                                      | 10 hours                               | 12 hours     | 16 hours     |
| Manganosite    | MnO                                                                  | Major                                  | Intermediate | Intermediate |
| Bixbyite       | (Mn <sub>0.37</sub> Fe <sub>0.63</sub> ) <sub>2</sub> O <sub>3</sub> | Intermediate                           | Major        | Major        |
| Hausmannite    | Mn <sup>2+</sup> Mn <sup>3+</sup> <sub>2</sub> O <sub>4</sub>        | Intermediate                           | Minor        | Minor        |
| Jacobsite      | (Mn,Fe,Mg)(Fe,Mn) <sub>2</sub> O <sub>4</sub>                        | -                                      | -            | Minor        |
| Hematite       | Fe <sub>2</sub> O <sub>3</sub>                                       | Trace                                  | Minor        | Intermediate |
| Calcite        | CaCO <sub>3</sub>                                                    | Minor                                  | Intermediate | Minor        |
| Iron manganese | FeMn                                                                 | -                                      | Trace        | -            |

Increasing the residence time to 12 hours has seen the breakdown of the higher-phase oxide hausmannite, as well as the evolution of the FeMn phase. This indicated an improvement in pre-reduction. However, a declined relative abundance of MnO to the intermediate range (15 – 30%) and the increase in the concentration of Fe<sub>2</sub>O<sub>3</sub> to the minor range (5 – 15%) also indicates a reduced EOR. Further increasing the residence time to 16 hours had no impact on the relative abundance of MnO reducing the concentration of hausmannite. Furthermore, an increase in relative abundance of Fe<sub>2</sub>O<sub>3</sub> was observed throughout the tests. This suggests that increasing the residence time at constant temperature degrades the reduction of Mn oxides, while slightly improving the reduction of Fe oxides; and that the residence time of 10 hours leads to optimum reduction of N-ore. A persistent presence of CaCO<sub>3</sub> was observed in both the UMK and N-ore. A fluctuation in the concentration of CaCO<sub>3</sub> in the pre-reduced samples with increased residence time was observed. Therefore, the impact of the residence time length on the decomposition of CaCO<sub>3</sub> in the ores cannot be concluded. The oxygen analysis results for the pre-reduced N-ore, showing the total remaining oxygen, expressed in mass %, and the achieved EOR in each sample, are presented in Table VIII.

Table VIII. Extent of reduction as a function of residence time for N-ore

| Test sample | Residence time, hours | Oxygen remaining, mass % | EOR % |
|-------------|-----------------------|--------------------------|-------|
| Raw ore     | -                     | 21.2                     | -     |
| 17          | 10                    | 16.0                     | 25    |
| 18          | 10                    | 17.8                     | 16    |
| 19          | 10                    | 17.8                     | 16    |
| 28          | 12                    | 15.8                     | 25    |
| 29          | 16                    | 13.6                     | 36    |
| 31          | 16                    | 14.6                     | 31    |

The results indicate that the remaining oxygen is lower in all the pre-reduced samples in comparison to the raw ore. This is in alignment with the qualitative phase proportions results reported and discussed in the section prior and also serve to confirm that the overall pre-reduction of the N-ore has occurred. This table also reports the EOR for each test sample. The raw sample had an oxygen content of 21.2%. This reduced to 17.8 – 16.0% after a 10 hours pre-reduction residence time, reporting a maximum EOR of 25%. Increasing the residence time resulted in the further reduction of the remaining oxygen to 15.8%, with the EOR at 25%. A residence time of 16 hours reduced the remaining oxygen to 13.6 – 14.6%, reporting an EOR of 31 – 36%. These results confirmed that an increased residence time

results in the improvement of the EOR to MnO. The graphical presentation of these results is shown in Figure 6.

Overall, the pre-reduction of the UMK has outperformed that of the N-ore. This may be attributed to the raw N-ore having a higher Fe<sub>2</sub>O<sub>3</sub> concentration (13.7%) in comparison to UMK (7.3%). The Fe<sub>2</sub>O<sub>3</sub> receives preferential reduction prior to the oxides of manganese in the presence of a reducing atmosphere.

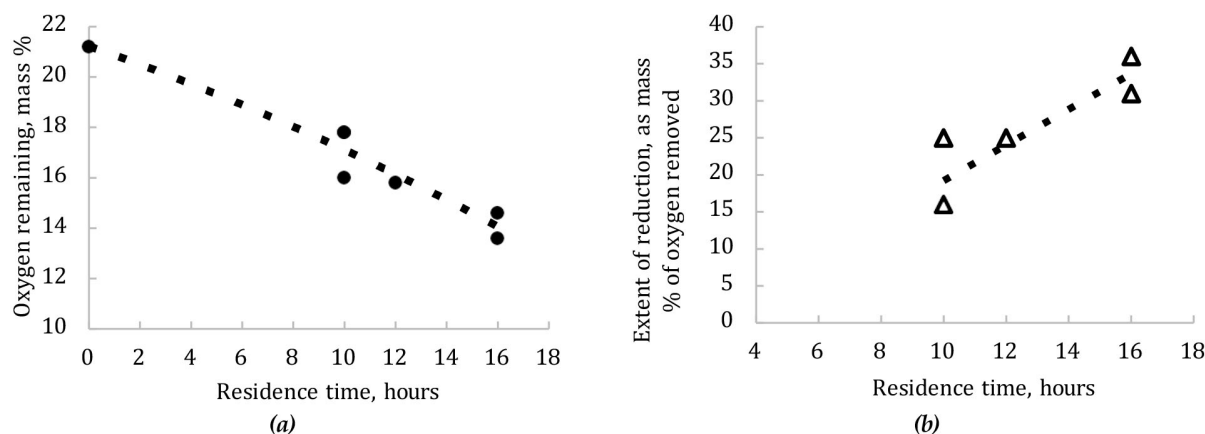


Figure 6. (a) Mass of oxygen remaining in sample and (b) extent of reduction for N-ore.

The results from conducted H<sub>2</sub> pre-reduction tests on UMK and N-ore in a vertical retort furnace at pilot-scale showed that reduction had occurred. This was revealed by the colour change observed on the ore particles, the phase transformation from high to low oxide and the reduction in oxygen content. However, the performance of pilot-scale tests was evidently inferior to the lab-scale tests, which reported proper and complete reduction to MnO on similar ore types (Safarian, 2021). This outcome was not unexpected because operational challenges and deviation during process upscaling are usually inevitable. The non-uniformity in the colour of the pre-reduced particles and subsequent oxygen analyses indicated that the vertical retort furnace is limited against achieving complete reduction. Overall, the test work obtained products that were not uniformly pre-reduced and lower extent of reduction mainly due to the inherent design of the pre-reduction unit (vertical retort), and the inherent fixed bed of the batch process. Established pre-reduction technologies utilising vertical retorts exist at commercial scale that can be used in a continuous process to achieve uniformly pre-reduced products and achieve a much higher extent of reduction of the Mn ores.

## CONCLUSION

Hydrogen pre-reduction tests of two different manganese ores, i.e., UMK and N-ore, were conducted in a vertical retort furnace. A total of 16 tests on UMK were done at a target pre-reduction temperature of 800°C. The reactions were held at residence times 6, 10 and 12 hours. On the other hand, 15 batch tests were conducted using N-ore at a target temperature of 750°C. The tests were held for residence times of 10, 12 and 16 hours. The sample products displayed a colour change variation from grey to green-brown, which indicated that reduction had occurred. The samples were also analysed for oxygen content and compared to the raw ore. The oxygen analysis revealed that oxygen was removed. Finally, transformation from the high to low oxide phases in the product samples provided further evidence of reduction. It was observed that the increase in residence time improved the overall extent of reduction at any constant temperature. The UMK ore achieved a maximum EOR of 47% after the sample was held for a residence time of 12 hours, while N-ore achieved a 36% EOR over 16 hours. Overall, the test work obtained products that were not uniformly pre-reduced and a lower extent of reduction mainly due to the inherent design of the pre-reduction unit (vertical retort), and the inherent fixed bed of the batch process.

## ACKNOWLEDGEMENTS

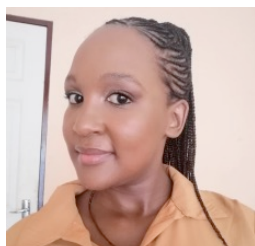
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### **Madinoge Mampuru**

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Dr. Madinoge Mampuru joined the Pyrometallurgy Division at Mintek in October 2023 as a postdoctoral research fellow. She has since joined the division permanently as a Research Engineer in June 2024, seconded to the Technology Demonstration Group. She has a PhD in Mechanical Engineering from North-West University and MSc & BSc in Chemical Engineering from the University of the Witwatersrand. Prior to her appointment, Madinoge held various roles in research, including as Project Engineer in Energy Management responsible for conducting energy audits and initiating energy savings initiatives at various companies of iron and steel manufacturing, as well as gold and platinum mining. Madinoge's strong background in applied research and industry has augmented the discipline's efforts in growing competencies in offering value-added commercial and research services aimed towards developing energy and resource efficiency in pyrometallurgy. Her current passions involve applied research to explore and demonstrate the practical use of hydrogen towards decarbonising pyrometallurgy and thus promoting of sustainable metallurgical operations.



# Hydrogen reduction of iron and manganese oxides in ferroalloy ores

M.S. Ernst, M. Tangstad

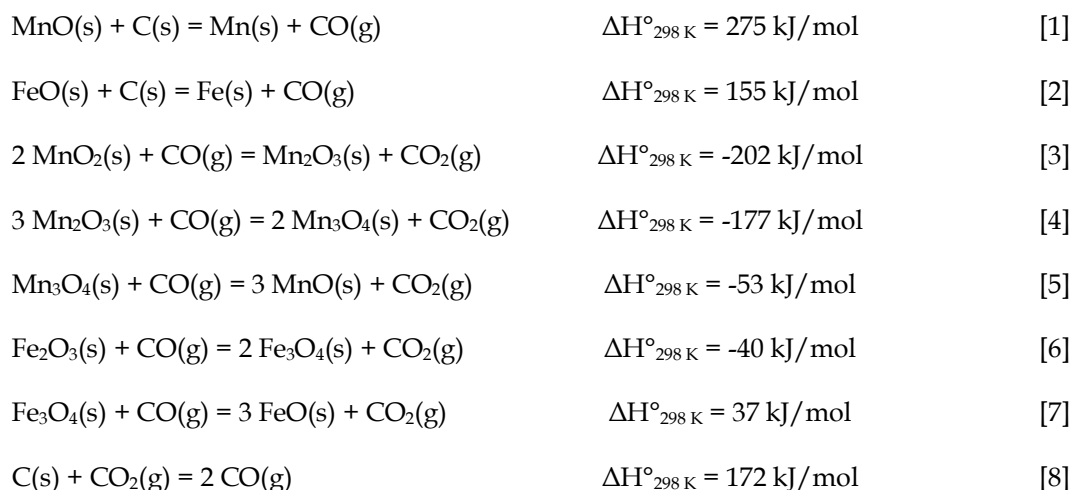
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The decarbonisation of manganese ferroalloy production requires alternative 'prereduction' strategies to replace carbon-based reductants. Hydrogen offers a promising option; however, the chemical and physical transformations of manganese ores under industrially relevant, non-isothermal hydrogen and water vapour conditions are not yet fully understood. In this paper, the non-isothermal prereduction behaviour of Assmang Mn ore was investigated in hydrogen and water vapour atmospheres up to 1100°C using thermogravimetry, supported by X-ray fluorescence, X-ray diffractometry, and physical characterisation. The maximum experimental mass loss reached 15.89 mass% in pure hydrogen, in good agreement with the theoretical value, based on stoichiometry, of ~14 mass%. Normalised results show that the extent of mass loss is governed primarily by reducing atmosphere composition, with pure hydrogen achieving >98% of the maximum mass loss across all particle sizes, whereas an 80%-20% hydrogen-water vapour atmosphere was limited to ~80%. Particle size had a minor influence on the final extent of reduction but strongly affected the rate of mass loss. Chemical analysis confirmed substantial oxygen removal, with the oxygen-manganese ratio decreasing from 1.53 in the raw ore to 1.00 after prereduction. Preferential iron oxide reduction occurred at lower temperatures, followed by progressive manganese oxide reduction and carbonate decomposition between 600°C and 800°C. Prereduction was accompanied by particle decrepitation, reaching up to ~60% for large particles in pure hydrogen. These results support hydrogen-based prereduction as a viable pathway towards lower-carbon manganese ferroalloy production.

**Keywords:** manganese, ferroalloy, non-isothermal, hydrogen, prereduction

## INTRODUCTION

Manganese (Mn) is a critical alloying element in steel production, primarily consumed in the form of Mn ferroalloys such as high-carbon ferromanganese (FeMn), silicomanganese, and refined FeMn grades (Cheraghi *et al.*, 2020; Davies *et al.*, 2023). These alloys serve essential roles in deoxidation, desulphurisation, and strengthening of steel (Olsen and Tangstad, 2004). With more than 90% of global Mn production directed toward ferroalloys, Mn demand closely follows fluctuations in steel production (Cheraghi *et al.*, 2020; Davies *et al.*, 2023). The submerged arc furnace (SAF) process remains the dominant industrial route for Mn ferroalloy production, particularly when processing ores with Mn to Fe fractions above 6.2 (Eissa *et al.*, 2012). In the SAF, two reaction zones are distinguished: the high-temperature smelting zone (>1300°C), where Fe and Mn oxides are reduced by solid carbon (reactions 1–2), and the prereduction zone (<1300°C), where carbon monoxide gas (CO) reduces higher valence oxides to lower valence oxides (reactions 3–7; Larssen and Tangstad, 2022; Ngoy *et al.*, 2020). While effective, this carbon-based approach generates significant emissions of carbon dioxide gas (CO<sub>2</sub>) and energy losses due to the endothermic Boudouard reaction (Reaction 8; Cheng *et al.*, 1999; Olsen *et al.*, 2007). A mass balance conducted by Tangstad (2022) showed that about 900 kg CO<sub>2</sub> equivalent per ton FeMn is emitted, with a net generation of CO<sub>2</sub> equivalent in the prereduction zone.



To align with global decarbonisation goals, alternative reductants are being explored. Hydrogen ( $\text{H}_2$ ) offers a promising option, producing water vapour ( $\text{H}_2\text{O(g)}$ ) as its only by-product. Thermodynamic assessments show that  $\text{H}_2$  can reduce both Fe and Mn oxides, with stability determined by oxygen partial pressure set by the  $\text{H}_2$ - $\text{H}_2\text{O(g)}$  ratio (Larssen and Tangstad, 2022; Schanche and Tangstad, 2021). Compared with  $\text{CO(g)}$ ,  $\text{H}_2$  achieves lower equilibrium oxygen potentials at temperatures above  $800^\circ\text{C}$ , resulting in a greater reduction driving force for higher oxides at prereduction temperatures of  $800$ – $1100^\circ\text{C}$ . However,  $\text{MnO}$  is considerably more stable than  $\text{FeO}$ , and complete metallisation of Mn oxides by  $\text{H}_2$  alone requires strong reducing conditions. Thus, while  $\text{Fe}_2\text{O}_3$  readily reduces to metallic Fe in a pure  $\text{H}_2$  atmosphere, Mn reduction typically terminates at  $\text{MnO}$  (Davies *et al.*, 2023; Ernst *et al.*, 2024; Larssen and Tangstad, 2022; Ngoy *et al.*, 2020; Sarkar *et al.*, 2023; Schanche and Tangstad, 2021).

Beyond thermodynamic feasibility, extensive studies have examined the kinetics of  $\text{H}_2$  reduction. Thermogravimetry (TG) studies consistently show faster mass loss and higher reducibility under  $\text{H}_2$  than with  $\text{CO(g)}$  (D'Angelo *et al.*, 2025; Davies *et al.*, 2023; Hammam *et al.*, 2023; Ma *et al.*, 2022; Sarkar *et al.*, 2024; Souza Filho *et al.*, 2021). Reported apparent activation energies vary widely from  $20$  to  $60\text{ kJ/mol}$ , depending on ore type, temperature, and conversion range (Sarkar *et al.*, 2025; Schanche and Tangstad, 2021; Wang *et al.*, 2024). This variability reflects changes in the rate-controlling mechanism: (i) chemical reaction or nucleation dominates at early stages, (ii) pore development and gas diffusion influence intermediate stages, and (iii) product layer formation (*i.e.*,  $\text{MnO}$ ) restricts solid-state diffusion at high conversions. Non-isothermal studies reveal additional insights, including reduction onset temperatures, transient phase transformations, and overlapping thermal events such as moisture release and carbonate decomposition, all of which complicate kinetic interpretation (Kononov *et al.*, 2009; Ngoy *et al.*, 2020). Microstructural analyses further highlight that decrepitation and porosity strongly affect gas access and reaction rates (Ernst *et al.*, 2024). Together, these findings confirm that  $\text{H}_2$ -based reduction kinetics are highly condition-dependent and cannot be described by a single universal mechanism.

Laboratory studies often employ pure  $\text{H}_2$  to probe fundamental reduction behaviour, but industrially relevant atmospheres will always contain  $\text{H}_2\text{O(g)}$ , owing to its formation during reduction reactions. The  $\text{H}_2$ - $\text{H}_2\text{O(g)}$  ratio not only governs the driving force for reduction but also influences diffusion and phase stability (Davies *et al.*, 2023; Ernst *et al.*, 2024; Larssen and Tangstad, 2022). Investigating Mn ore reduction in  $\text{H}_2$ - $\text{H}_2\text{O(g)}$  atmospheres is therefore crucial for simulating real-world conditions. Recent reviews (Spreitzer and Schenk, 2019; Zakeri *et al.*, 2023) emphasise this complexity, but also highlight that most prior work has focused on isothermal systems or dry  $\text{H}_2$  environments (D'Angelo *et al.*, 2025; Davies *et al.*, 2023; Hammam *et al.*, 2021, 2023; Larssen and Tangstad, 2022; Lyu *et al.*, 2022; Ma *et al.*, 2022; Ngoy *et al.*, 2020; Sarkar *et al.*, 2024; Schanche and Tangstad, 2021; Souza Filho *et al.*, 2021; Zakeri *et al.*, 2023). Notably, no studies have yet explored the parallel reduction of Fe and Mn oxides in  $\text{H}_2$ - $\text{H}_2\text{O(g)}$  atmospheres under non-isothermal conditions, despite the fact that SAF operations inherently expose descending particles to temperature gradients.

In this paper, we address this knowledge gap by investigating the reduction behaviour of Assmang Mn ore during non-isothermal reduction in  $\text{H}_2\text{-H}_2\text{O(g)}$  atmospheres up to  $1100^\circ\text{C}$ . The extent and rate of mass loss are quantified using TG. These results advance understanding of Mn ore reduction mechanisms under dynamic, industrially relevant conditions and support the development of  $\text{H}_2$  integrated low-carbon-smelting strategies for Mn ferroalloy production.

## MATERIALS AND EXPERIMENTAL PROCEDURE

### Materials

A 10 kg representative sample of Assmang Mn ore (also known as Nchwaning Mn ore from Assmang Black Rock mine, South Africa) was procured for testing. This sample was partitioned according to particle size with a sieve stack consisting of sieve aperture sizes of 14, 10, 8, 6.3, 4, 2, and 1 mm. The particle sizes selected to be subjected to  $\text{H}_2$  prereduction were 2–4, 6.3–8, and 10–14 mm and are hereafter referred to as small, medium, and large, respectively.

The chemical phases of the raw and prereduced samples were identified through X-ray diffractometry (XRD). The samples were ground in a Retsch vibratory disc mill RS 200 at 900 rpm for 90 s. The grinding set consisted of tungsten carbide rings and a plate. XRD was conducted on pulverised samples with a Bruker D8 A25 DaVinci X-ray diffractometer with  $\text{CuK}\alpha$  radiation and a LynxEye™ SuperSpeed detector. Data were collected over a total of 7200 s at a  $2\theta$  scanning range of  $10\text{--}90^\circ$  with a step size of  $0.013^\circ$ . Phases were identified using DIFFRAC.EVA V6.1 software in conjunction with the PDF-4+ database. It is noted that  $\text{CuK}\alpha$  radiation can induce fluorescence in Mn-containing samples, resulting in an elevated background signal. This effect was mitigated through the use of a LynxEye™ SuperSpeed detector with energy discrimination and extended counting times to improve the signal-to-noise ratio. Despite an elevated background due to Mn fluorescence, the diffraction patterns exhibited well-defined peaks, allowing consistent qualitative phase identification. The principal Mn bearing phases of the raw ore were identified as braunite ( $3(\text{Mn,Fe})_2\text{O}_3\cdot\text{MnSiO}_3$ ) and bixbyite ( $(\text{Mn,Fe})_2\text{O}_3$ ). Secondary phases included hematite ( $\alpha\text{-Fe}_2\text{O}_3$ ), calcite ( $\text{CaCO}_3$ ), and manganite ( $\text{MnO(OH)}$ ).

The bulk chemical compositions of the raw and prereduced samples were determined by X-ray fluorescence (XRF) spectrometry with a Bruker S4 Pioneer XRF spectrometer, employing the fused-bead technique. Elements quantified by this technique are reported either in elemental form (Fe, Mn, P, and S) or as oxides ( $\text{Al}_2\text{O}_3$ , BaO, CaO,  $\text{K}_2\text{O}$ , MgO,  $\text{SiO}_2$ , and  $\text{TiO}_2$ ), following convention. The oxygen bonded to Mn (expressed as  $\text{MnO}_2$ ) was quantified by permanganometric titration (ASTM 465–11:2017). The carbon content was measured with a LECO Combustion-IR instrument; the  $\text{CO}_2$  content was estimated under the assumption that all carbon in the ore exists as  $\text{CO}_2$ . The loss on ignition was measured thermogravimetrically by combusting the sample in air to a stable mass at  $950^\circ\text{C}$ .

To identify the chemical composition of the raw ore, three independent samples (one sample each from the small, medium, and large particle size range) were subjected to analysis. The results are presented in Table I. Uncertainties were estimated from the three independent samples, assuming normally distributed data. The resulting variability was used to calculate the uncertainty of the mean, which is reported at the 95% confidence level. While certain minor constituents show high relative uncertainties ( $\text{Al}_2\text{O}_3$ , BaO, MgO, and S; 39–147%), this is attributed to their low absolute concentrations. The major components display low relative uncertainties (CaO, Fe, Mn, and  $\text{MnO}_2$ ; 1–12%), confirming consistent composition across size fractions. Consequently, the raw ore may be regarded as chemically homogeneous in terms of its principal components. The raw ore contains  $47.23 \pm 0.41$  mass% Mn and is therefore considered medium-grade Mn ore (Acharya *et al.*, 2004). High-purity gases were employed during the tests, specifically argon (Ar, 99.999%) and  $\text{H}_2$  (99.9%).  $\text{H}_2\text{O(g)}$  was introduced by passing nitrogen through a water reservoir at ambient temperature, ensuring saturation of the carrier gas with  $\text{H}_2\text{O(g)}$ .

Table I: The chemical characterisation [mass%] of small, medium, and large raw ore particles

| Parameter                      | Raw Assmang Mn ore |       |       | Mean $\pm$ 95% CI | Relative uncertainty |
|--------------------------------|--------------------|-------|-------|-------------------|----------------------|
|                                | S                  | M     | L     |                   |                      |
| Al <sub>2</sub> O <sub>3</sub> | 0.32               | 0.55  | 0.36  | 0.41 $\pm$ 0.31   | 74%                  |
| BaO                            | 0.48               | 0.38  | 0.36  | 0.41 $\pm$ 0.16   | 39%                  |
| C                              | 0.79               | 0.74  | 0.76  | 0.76 $\pm$ 0.06   | 8%                   |
| CaO                            | 6.63               | 6.07  | 6.61  | 6.44 $\pm$ 0.79   | 12%                  |
| CO <sub>2</sub>                | 2.90               | 2.70  | 2.80  | 2.80 $\pm$ 0.25   | 9%                   |
| Fe                             | 9.76               | 9.45  | 9.94  | 9.72 $\pm$ 0.62   | 6%                   |
| K <sub>2</sub> O*              | <0.03              | <0.03 | <0.03 | —                 | —                    |
| MgO                            | 0.77               | 1.30  | 0.59  | 0.89 $\pm$ 0.92   | 103%                 |
| Mn                             | 47.28              | 47.37 | 47.05 | 47.23 $\pm$ 0.41  | 1%                   |
| MnO <sub>2</sub>               | 38.93              | 38.94 | 40.58 | 39.48 $\pm$ 2.36  | 6%                   |
| P                              | <0.03              | <0.03 | <0.03 | —                 | —                    |
| S                              | 0.11               | 0.04  | 0.04  | 0.06 $\pm$ 0.10   | 147%                 |
| SiO <sub>2</sub>               | 3.70               | 4.06  | 3.39  | 3.72 $\pm$ 0.83   | 22%                  |
| TiO <sub>2</sub> *             | <0.03              | <0.03 | <0.03 | —                 | —                    |

\*Below detection limit

### Experimental Procedure

During pre-testing, several observations were made. First, during isothermal reduction, a temperature difference of approximately 65°C was observed between the controlled furnace temperature and measured retort temperature. In contrast, during non-isothermal reduction, this difference was reduced to approximately 30°C. This deviation was accounted for in the furnace control programming. Second, the measurement of the axial temperature gradient along the height of fixed bed indicated that, for a charged retort, the temperature varied only by  $\pm 4^\circ\text{C}$  relative to the thermocouple measurement. This variation is sufficiently small to have no significant effect on the results. Finally, to prevent pre-heated H<sub>2</sub>O(g) from condensing on the cold charge, the sample was pre-heated to 200°C under an Ar atmosphere prior to the introduction of the reducing gas.

The mass loss during the reduction of raw Assmang Mn ore was measured using an Entech VTF 80/15 TG vertical resistance tube furnace, also known as the Disvadri furnace. To verify the net mass loss from the TG data, the mass of each charge was recorded before and after each test using an Ohaus Pioneer PA4202 scale. Minor discrepancies between the continuous mass loss measured by TG and the mass loss calculated from the difference between the initial and final sample masses were observed. On average, the TG-derived mass loss was 1.14 times more than the true mass loss, which is attributed to instrumental drift over extended experimental durations. To account for this effect, the TG data were corrected to the recorded mass difference. All mass-loss values reported in this work therefore represent corrected values.

A charge of ~150 g of raw ore was placed in a gas-tight, temperature-resistant stainless steel (253 MA) double-wall retort, measuring 45 cm in length with an inner diameter of 4.8 cm. A K-type thermocouple, housed in an alumina sleeve, was positioned at the centre of the charge to measure the temperature of the sample. This measured temperature is the temperature reported. The retort was then sealed and suspended from a Mettler Toledo PR2003DR balance to record masses during operation. Flexible gas inlet and outlet pipes were connected to the screw top of the retort, allowing unrestricted movement. Throughout all experimental stages, the gas flow rate was maintained at a constant 4 L/min, controlled with Bronkhorst F-201C mass flow controllers.

The furnace hot zone measured 68 cm in height and 36 cm by 36 cm in cross-section. The furnace, which can be raised and lowered to allow uninterrupted mass recording, was initially in the raised position. The charged retort was heated in Ar at a constant rate of 5°C/min to 200°C. At this temperature, the atmosphere was switched from Ar to the reducing gas, and heating continued at the same rate to 1100°C. Upon reaching the final temperature, the atmosphere was switched back to Ar, and the furnace was

lowered for cooling. Once the retort and charge had cooled to ambient temperature, they were removed from the suspension system. The reacted charge is recovered for further characterisation. The plot of gas flows and temperatures are shown in Figure 1.

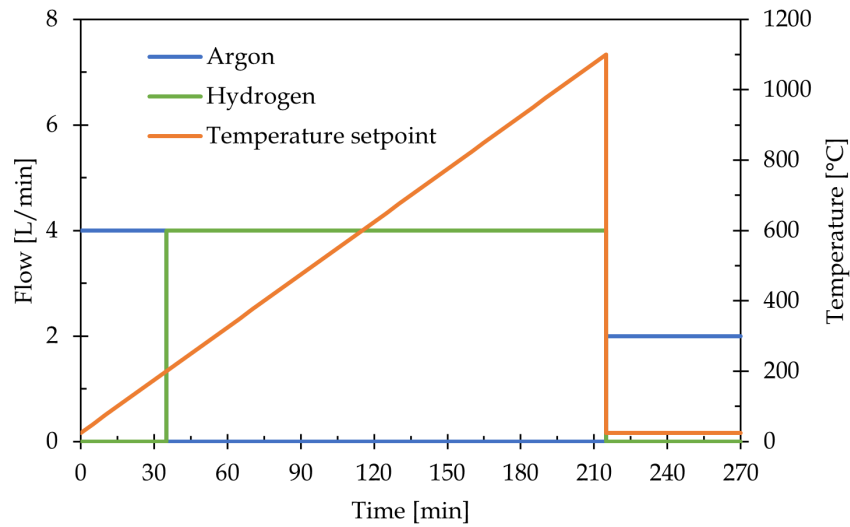


Figure 1: The furnace temperature programme and atmospheres for non-isothermal prereduction.

The variable parameters are the ore particle sizes (*i.e.*, small, medium, and large) and the reducing gas composition (*i.e.*, 100% H<sub>2</sub>, 95% H<sub>2</sub>-5% H<sub>2</sub>O(g), 90% H<sub>2</sub>-10% H<sub>2</sub>O(g), and 80% H<sub>2</sub>-20% H<sub>2</sub>O(g)). The variable matrix is shown in Table II. For the small particles in pure H<sub>2</sub>, the TG tests were repeated several times but intentionally ended at lower temperatures to assess the chemical and physical characteristics of Assmang Mn ore during prereduction.

Table II: The variable matrix for mass TG measurements

| Particle size range [mm] | Reducing gas composition: H <sub>2</sub> -H <sub>2</sub> O(g) [v%] |           |           |           |
|--------------------------|--------------------------------------------------------------------|-----------|-----------|-----------|
|                          | 100-0                                                              | 95-5      | 90-10     | 80-20     |
| L: 10-14                 | 100-L-1100                                                         | 95-L-1100 | 90-L-1100 | 80-L-1100 |
| M: 6.3-8                 | 100-M-1100                                                         | 95-M-1100 | 90-M-1100 | 80-M-1100 |
| S: 2-4                   | 100-S-1100                                                         | 95-S-1100 | 90-S-1100 | 80-S-1100 |
|                          | 100-S-800                                                          |           |           |           |
|                          | 100-S-600                                                          |           |           |           |
|                          | 100-S-400                                                          |           |           |           |

## RESULTS AND DISCUSSION

### Theoretical mass loss

The chemical analysis of the raw ore was used to determine the theoretical mass loss one can expect in reduction; the approach is described in previous work (Ernst *et al.*, 2024). Ore properties such as the basicity (B<sub>CMSA</sub>), manganese-iron ratio (Mn/Fe), and oxygen-manganese ratio (O/Mn) were also calculated. Uncertainties were estimated from three independent samples and expressed at the 95% confidence level in Table III.

Table III: Properties of raw ore

| Ore properties                        | Mean $\pm$ 95% CI | Relative uncertainty |
|---------------------------------------|-------------------|----------------------|
| B <sub>CMSA</sub>                     | 1.79 $\pm$ 0.42   | 23%                  |
| LOI 950                               | 4.48 $\pm$ 0.44   | 10%                  |
| Mn/Fe                                 | 4.86 $\pm$ 0.35   | 7%                   |
| O/Mn                                  | 1.53 $\pm$ 0.04   | 2%                   |
| Fe <sub>2</sub> O <sub>3</sub> to Fe  | 4.17 $\pm$ 0.26   | 6%                   |
| Mn <sub>2</sub> O <sub>3</sub> to MnO | 7.27 $\pm$ 0.43   | 6%                   |
| Carbonate dissociation                | 2.80 $\pm$ 0.25   | 8%                   |
| Theoretical mass loss                 | 14.25 $\pm$ 0.76  | 5%                   |

The O/Mn was calculated to be  $1.53 \pm 0.04$ , which indicates that the Mn oxides are predominantly in the Mn<sub>2</sub>O<sub>3</sub> form and that the case study ore is a semi-oxidised ore. The Mn/Fe of the raw ore was calculated to be  $4.86 \pm 0.35$ , indicative of low Fe content. The B<sub>CMSA</sub> of the raw ore was calculated to be  $1.79 \pm 0.42$ , suggesting that the case study ore has a relatively high basicity, which is suitable for the ‘discard slag method,’ also called the ‘flux method’ (Tangstad, 2013). The theoretical mass loss of the raw ore was calculated to be  $14.25 \pm 0.76$  mass%. It is worth noting that Mn<sub>2</sub>O<sub>3</sub> reduction to MnO equates to ~50% of theoretical mass loss during prereduction.

#### Experimental results from TG

The extent of mass loss [%] and rate of mass loss [%/min] curves obtained during non-isothermal reduction of Assmang Mn ore under H<sub>2</sub>-H<sub>2</sub>O(g) mixed atmospheres at a heating rate of 5°C/min are presented in Figure 2. The maximum mass loss recorded during prereduction was 15.89 mass%, obtained for the smallest particle size fraction in pure H<sub>2</sub>. This value is consistent with the theoretical maximum mass loss of  $14.25 \pm 0.76$  mass%. The maximum experimental mass loss was therefore used to normalise all TG curves, shown in Figures 2(a), (c), and (e), for comparison.

The TG curves display sigmoidal behaviour characteristic of kinetically controlled reduction, which comprises an initial induction period up to ~350°C, an accelerated stage, and a deceleration phase above ~800°C as completion is approached. Gas composition significantly affects the reduction behaviour: the 100% H<sub>2</sub> atmosphere consistently resulted in lower temperature onset and highest overall extent of mass loss, exceeding 98% of the maximum, across all particle size ranges. Increasing the H<sub>2</sub>O(g) content (95%H<sub>2</sub>-5%H<sub>2</sub>O(g) and 90%H<sub>2</sub>-10%H<sub>2</sub>O(g)), lowers the final extent of mass loss achieved slightly to the range of 80%–98%. Moreover, reduction in an 80%H<sub>2</sub>-20%H<sub>2</sub>O(g) atmosphere was notably less effective, yielding only ~80% of the maximum mass loss regardless of particle size. Particle size also affects the reduction behaviour, with smaller particles showing a more gradual and earlier increase in conversion, indicative of more uniform reaction throughout the particle. In contrast, medium and especially large particles display a more delayed but steeper transition in the intermediate temperature range, which is likely due to the increase of both heat transfer and diffusion limitations within the medium and large particles, compared with small particles.

The rate curves are presented as the rate of mass loss as a function of temperature in Figures 2(b), (d), and (f). Across all particle sizes, a dominant peak occurs in the range of ~700°C, accompanied by two smaller low-temperature peaks at ~450°C and ~550°C, after which the reaction rate declines rapidly beyond ~800°C. Particle size has a pronounced effect on the peak shape and intensity: small particles exhibit broader and more distributed peaks, suggesting reaction-controlled behaviour, while medium and especially large particles display sharper and higher peaks, suggesting an increase of both heat transfer and diffusion limitations, compared with small particles, as mentioned previously.

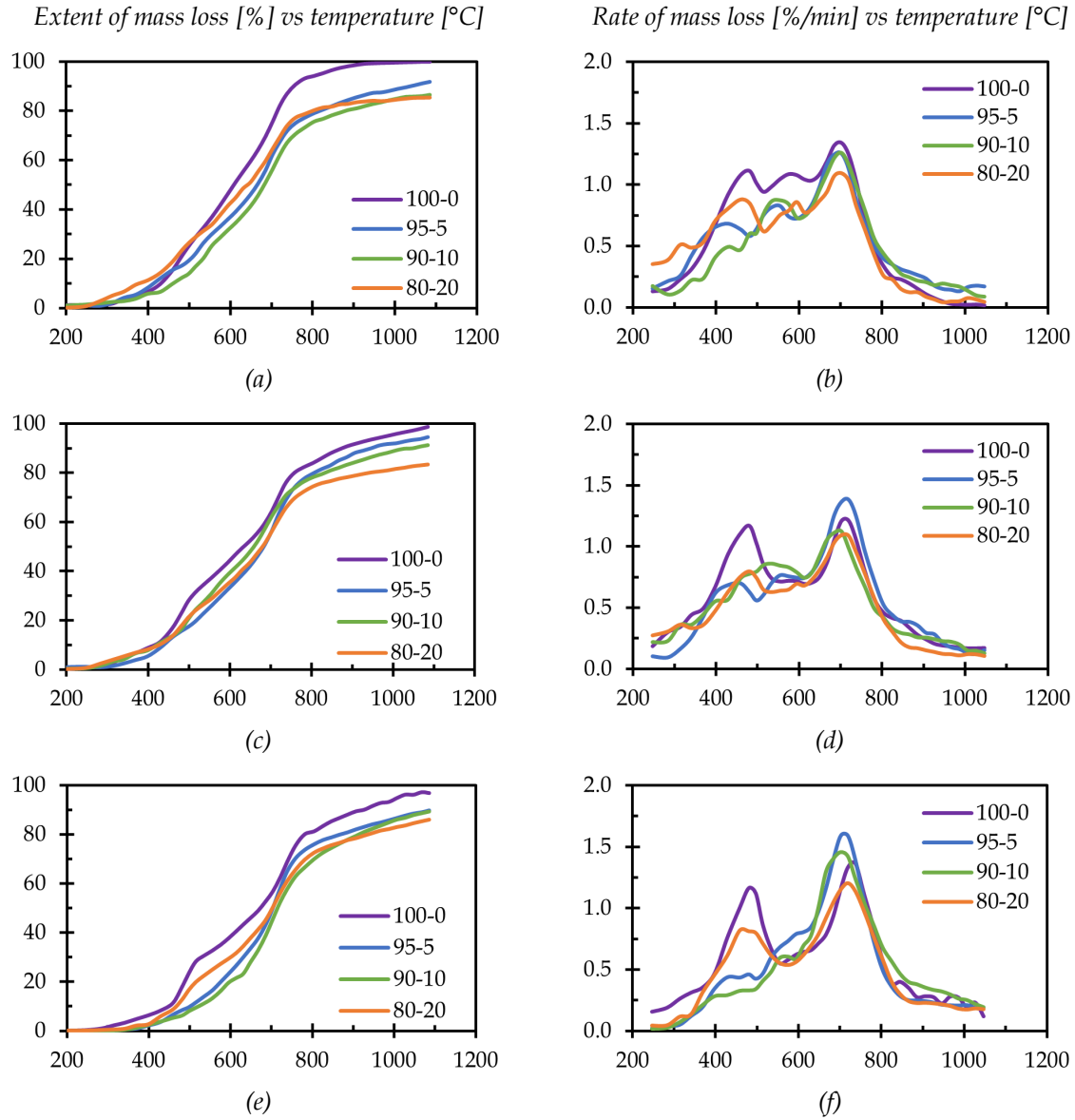


Figure 2: The experimental results for ore prereduction in  $\text{H}_2\text{-H}_2\text{O(g)}$  atmospheres. The (a) extent of mass loss and (b) rate of mass loss curves of small particles. The (c) extent of mass loss and (d) rate of mass loss curves of medium particles. The (e) extent of mass loss and (f) rate of mass loss curves of large particles.

The observed increase in both the extent of mass loss and rate of mass loss under  $\text{H}_2$ -rich atmospheres is consistent with previous TG studies on Mn ores, which report faster reduction and higher reducibility in  $\text{H}_2$  than in  $\text{CO}$ - or  $\text{CO}_2$ -containing gases (Davies *et al.*, 2023; Ernst *et al.*, 2024; Kononov *et al.*, 2009; Larssen and Tangstad, 2022; Ngoy *et al.*, 2020; Schanche and Tangstad, 2021). Similar to the findings of Ngoy *et al.*, (2020), the present results show that gas composition governs primarily the final extent of reduction, whereas particle size mainly affects the rate of mass loss. While these results describe the macroscopic reduction behaviour, further insight can be obtained by examining the chemical and physical changes occurring during pre-reduction.

#### Chemical characteristics affected by prereduction

The change in chemical composition of the Mn ore during prereduction was investigated with XRF and XRD. XRF measures changes in bulk composition resulting from oxygen and carbonate removal, whereas XRD reveals the phase transformations associated with the reduction of Fe- and Mn-bearing minerals. Together, these techniques allow the chemical changes occurring during prereduction to be

correlated with the mass-loss behaviour observed in TG. The results of bulk chemical analysis are given in Figure 3. Uncertainties were estimated from the three independent samples (small, medium, and large particles), assuming normally distributed data. The resulting variability was used to calculate the uncertainty of the mean, which is reported at the 95% confidence level. Note that the Mn/Fe value has the units of [mass%/mass%], and that components such as C, K<sub>2</sub>O, MnO<sub>2</sub>, P, and TiO<sub>2</sub> were below detection limits in all samples and are therefore not included in the figure.

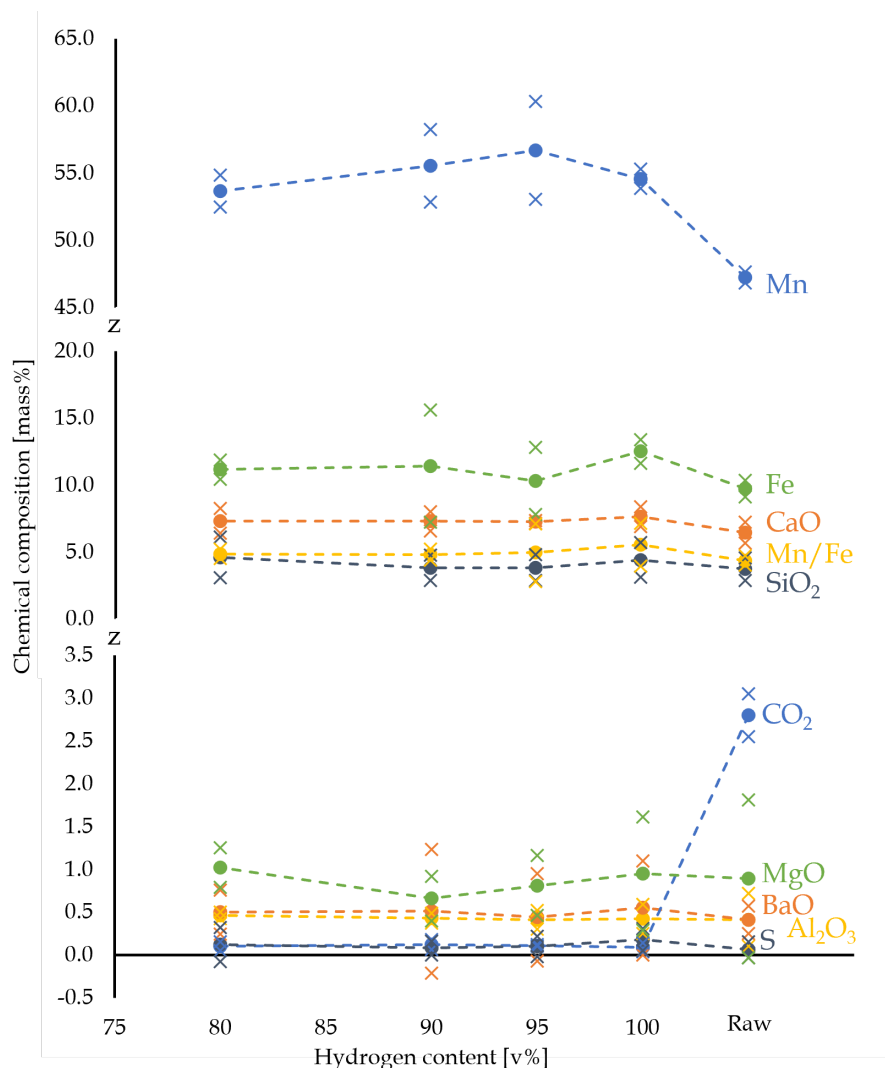


Figure 3: The chemical characterisation [mass%] of raw and prereduced ore.

Shown in Figure 3, the bulk composition of the prereduced ore remains relatively stable across the investigated H<sub>2</sub> contents, with more pronounced differences observed when compared to the raw ore. The Mn content increases significantly from the raw ore to the prereduced samples, reflecting the removal of excess oxygen bound to Mn during reduction; the O/Mn ratio changed from  $1.53 \pm 0.04$  in the raw ore to 1.00 in all the prereduced samples. The Mn content remains relatively constant ( $55.12 \pm 2.06$  mass%) after prereduction with only minor variation across gas compositions. In contrast, Fe content shows moderate variability of  $11.35 \pm 2.08$  mass%. This is also reflected in the Mn/Fe ratio, which remains fairly stable with small fluctuations at  $4.92 \pm 1.14$  mass%.

The major gangue components, CaO and SiO<sub>2</sub>, show limited variation ( $7.38 \pm 0.79$  mass%, and  $4.16 \pm 1.18$  mass%, respectively) across the different H<sub>2</sub> contents but differ from the raw ore ( $6.44 \pm 0.62$  mass%, and  $3.72 \pm 0.83$  mass%, respectively), consistent with compositional changes expected from oxygen removal and carbonate decomposition. The CO<sub>2</sub> content shows a clear decrease from  $2.80 \pm 0.25$  mass%



in the raw ore to  $0.11 \pm 0.04$  mass% in the prereduced samples, supporting the removal of carbonate phases during treatment. Among the minor components, MgO content exhibits some scatter ( $0.86 \pm 0.38$  mass%) but no clear trend with H<sub>2</sub> content, while Al<sub>2</sub>O<sub>3</sub> remains relatively constant ( $0.43 \pm 0.10$  mass%). BaO shows notably large error margins ( $0.50 \pm 0.51$  mass%) compared to the other oxides. This variability arises from differences between particle size fractions, where the large particles consistently contain significantly less BaO than the medium and small particles after prereduction. Consequently, the spread in BaO values is amplified in the prereduced samples compared to the raw ore. While these results show that H<sub>2</sub> content has a limited effect on the final bulk chemistry after prereduction (across three particle size ranges and four reducing atmospheres), the interrupted prereduction tests of small particles in pure H<sub>2</sub> reveal temperature-dependent progression of chemical composition as shown in Table IV.

Table IV: Chemical characterisation [mass%] of small raw and prereduced ore particles in pure H<sub>2</sub> atmosphere

| Parameter                      | Raw   | 100% H <sub>2</sub> |       |       |        |
|--------------------------------|-------|---------------------|-------|-------|--------|
|                                |       | 400°C               | 600°C | 800°C | 1100°C |
| Al <sub>2</sub> O <sub>3</sub> | 0.32  | 0.29                | 0.32  | 0.41  | 0.50   |
| BaO                            | 0.48  | 0.61                | 0.55  | 0.53  | 0.67   |
| C                              | 0.79  | 0.83                | 0.85  | 0.08  | 0.03   |
| CaO                            | 6.63  | 6.72                | 7.07  | 8.23  | 7.75   |
| CO <sub>2</sub>                | 2.90  | 3.00                | 3.10  | 0.29  | 0.07   |
| Fe                             | 9.76  | 10.13               | 10.77 | 11.72 | 12.19  |
| K <sub>2</sub> O*              | <0.03 | <0.03               | <0.03 | <0.03 | <0.03  |
| MgO                            | 0.77  | 0.85                | 0.87  | 1.10  | 0.94   |
| Mn                             | 47.28 | 47.88               | 50.46 | 57.72 | 54.88  |
| MnO <sub>2</sub> *             | 38.93 | 31.08               | 10.15 | <0.05 | <0.05  |
| P*                             | <0.03 | <0.03               | <0.03 | <0.03 | <0.03  |
| S                              | 0.11  | 0.26                | 0.22  | 0.21  | 0.22   |
| SiO <sub>2</sub>               | 3.70  | 3.33                | 3.74  | 4.28  | 3.89   |
| TiO <sub>2</sub> *             | <0.03 | <0.03               | <0.03 | <0.03 | <0.03  |
| Mn/Fe                          | 4.84  | 4.73                | 4.69  | 4.92  | 4.50   |
| O/Mn                           | 1.52  | 1.41                | 1.13  | 1.00  | 1.00   |

\*Below detection limit

The MnO<sub>2</sub> content decreased from 38.93 mass% in small raw ore particles to 10.15 mass% at 600°C and reached <0.05 mass% by 800°C, indicating complete removal of excess oxygen from higher Mn oxides. This is also reflected in the O/Mn that decreased from 1.52 for small raw particles to 1.13 at 600°C and reaches 1.00 by 800°C, which corresponds to MnO as the thermodynamically stable phase under these conditions. From a thermodynamic perspective, the reduction of higher Mn oxides to MnO becomes increasingly favourable with temperature, while further reduction to metallic Mn is not expected within this temperature range in pure H<sub>2</sub>. Furthermore, Fe content increased gradually from 9.76 mass% in the raw ore to 12.19 mass%. This increase is expected as mass loss increases over the temperature range. There is a sharp decrease in CO<sub>2</sub> content between 600°C and 800°C from ~3 mass% to ~0.3 mass%, which is consistent with the decomposition of carbonate phases *i.e.*, calcite, in this temperature range. The convergence of the O/Mn towards 1.00 after prereduction agrees well with both thermodynamic and experimental studies reporting that Mn oxide reduction in H<sub>2</sub> terminates at MnO under industrially relevant conditions (Cheraghi *et al.*, 2020; Larssen and Tangstad, 2022; Schanche and Tangstad, 2021). Although the bulk chemical analysis captures the overall compositional changes, it does not reveal the underlying phase transformations during the reduction process. The results of XRD of small particles reduced in pure H<sub>2</sub> show the phase transformation and is presented in Figure 4. Phase identification was conducted using reference patterns from the PDF-4+ database, considering multiple characteristic peaks and their relative intensities. Due to peak overlap between several Mn and Fe oxide phases, the phase analysis is considered qualitative.

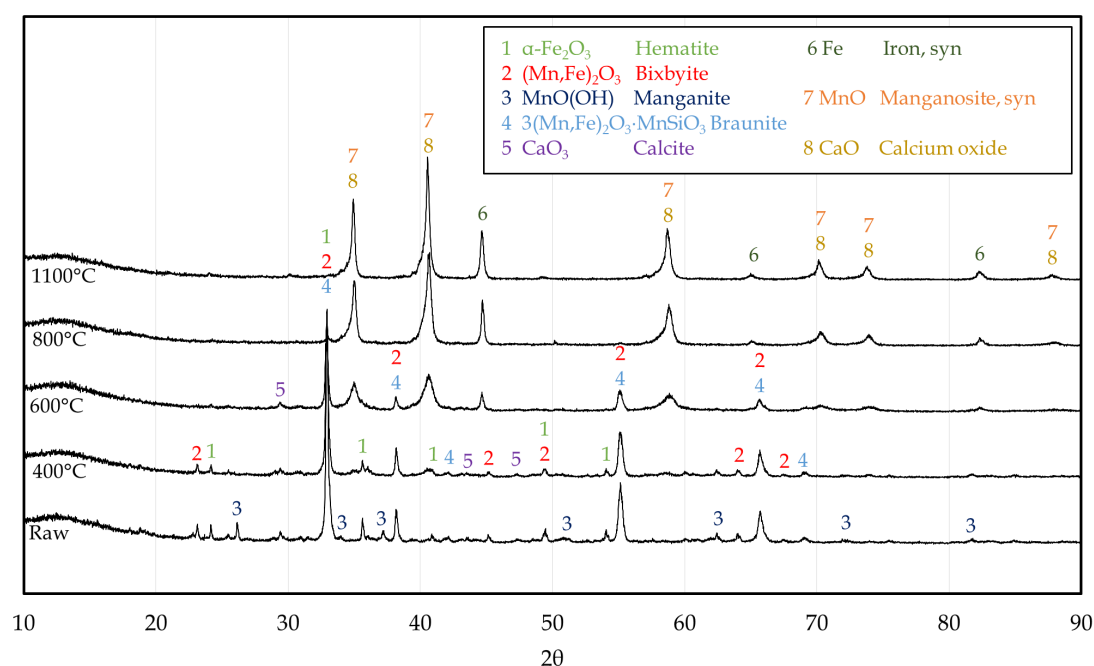


Figure 4: The X-ray diffractograms for small particles reduced in pure  $H_2$ , showing the progression of phase transitions at different temperatures.

The diffractograms in Figure 4 show a gradual, temperature-dependent transformation of the mineral phases during prereduction in  $H_2$ , rather than the appearance of entirely new phases at intermediate temperatures. As mentioned in the materials section, the raw ore consists of  $MnO(OH)$ ,  $\alpha-Fe_2O_3$ ,  $(Mn,Fe)_2O_3$ ,  $3(Mn,Fe)_2O_3 \cdot MnSiO_3$ , and  $CaCO_3$ . At  $400^\circ C$ ,  $MnO(OH)$  is no longer detected, indicating that the decomposition of  $MnO(OH)$  occurs at relatively low temperatures. At  $600^\circ C$ ,  $\alpha-Fe_2O_3$  is no longer detected, indicating Fe oxide reduction occurs at lower temperatures compared with Mn oxide reduction. Additionally,  $(Mn,Fe)_2O_3$  and  $3(Mn,Fe)_2O_3 \cdot MnSiO_3$  remain present; however, their decreasing peak intensities, compared to  $400^\circ C$ , suggest that the reduction of thermodynamically stable Mn oxide phases proceeds gradually over the temperature range. Additionally,  $CaCO_3$  peaks also decrease in this temperature range. Moreover, at  $600^\circ C$ , peaks associated with MnO, metallic Fe, and CaO begin to appear. A more significant transformation occurs between  $600^\circ C$  and  $800^\circ C$ ; at  $800^\circ C$  the peaks corresponding to MnO, metallic Fe, and CaO dominate; while  $(Mn,Fe)_2O_3$  and  $3(Mn,Fe)_2O_3 \cdot MnSiO_3$  are no longer detected. This significant change between  $600^\circ C$  and  $800^\circ C$  in mineral phases agrees with the bulk chemical composition. Finally, at  $1100^\circ C$ , the phase assemblage is dominated by MnO and metallic Fe, with sharp and well-defined peaks indicating increased crystallinity. CaO is also clearly present, consistent with carbonate decomposition at elevated temperatures. No higher Mn oxides remain detectable, confirming that reduction has proceeded to completion with MnO as the stable manganese phase.

The sequence of phase transformations identified in Figure 4 closely matches those reported for Mn ores reduced in  $H_2$ -containing atmospheres, where  $MnO(OH)$  decomposition and Fe oxide reduction precede the reduction of Mn oxides to MnO (Kononov *et al.*, 2009; Ngoy *et al.*, 2020; Sarkar *et al.*, 2023). The parallel occurrence of multiple mass-loss events observed under non-isothermal conditions further supports earlier observations that oxide reduction and carbonate dissociation occur over partially overlapping temperature ranges (Ernst *et al.*, 2024; Kononov *et al.*, 2009; Larssen and Tangstad, 2022; Schanche and Tangstad, 2021). The preferential reduction of Fe oxides observed in this study is likewise consistent with earlier work showing that  $Fe_2O_3$  readily reduces to metallic Fe in  $H_2$  rich atmospheres, whereas Mn oxides require higher temperatures and stronger reducing conditions (Larssen and Tangstad, 2022; Ngoy *et al.*, 2020; Sarkar *et al.*, 2023).

XRF and XRD analyses show that prereduction in the investigated  $\text{H}_2\text{-H}_2\text{O(g)}$  atmospheres removes excess oxygen and carbonates. Mn oxides reduce to MnO,  $\text{Fe}_2\text{O}_3$  reduces to metallic Fe in a pure  $\text{H}_2$  reducing atmosphere, and  $\text{CaCO}_3$  decomposes to CaO. Changes are temperature-dependent, with major transformations occurring between 600°C and 800°C, aligning with thermodynamic expectations.

#### *Physical characteristics affected by prereduction*

In addition to a chemical transformation, prereduction induces significant physical transformations within the ore, most notably internal pore formation, microcracking, and particle decrepitation. The removal of oxygen from Fe and Mn oxides, together with the decomposition of carbonate phases, is inherently accompanied by the release of gaseous species namely  $\text{H}_2\text{O(g)}$  and  $\text{CO}_2$ . The evolution of these gases promotes the formation of internal voids and microcracks within the ore particles, resulting in progressive structural weakening as reduction proceeds. The degree of structural change increases with the degree of reduction and is greatest in  $\text{H}_2$  rich atmospheres, where mass loss is highest.

In parallel with internal pore and crack formation, prereduction is accompanied by particle decrepitation, revealed as fragmentation of larger particles into smaller size fractions. The degree of decrepitation is defined for each initial particle size range as the fraction of material, relative to the mass after prereduction, that is fragmented below its lower size limit. This was determined by screening using sieve sizes of 2 mm, 6.3 mm, and 10 mm for the small, medium, and large particles, respectively. As different undersize thresholds are applied for each particle size range, and a relatively fine screen size is required for the small particles, the absolute values are not directly comparable to standard lump ore decrepitation data. However, the method enables consistent comparison of trends within this study. The results are presented in Figure 5, showing the degree of decrepitation as a function of  $\text{H}_2$  content and particle size, as well as temperature during the prereduction of small particles in pure  $\text{H}_2$ .

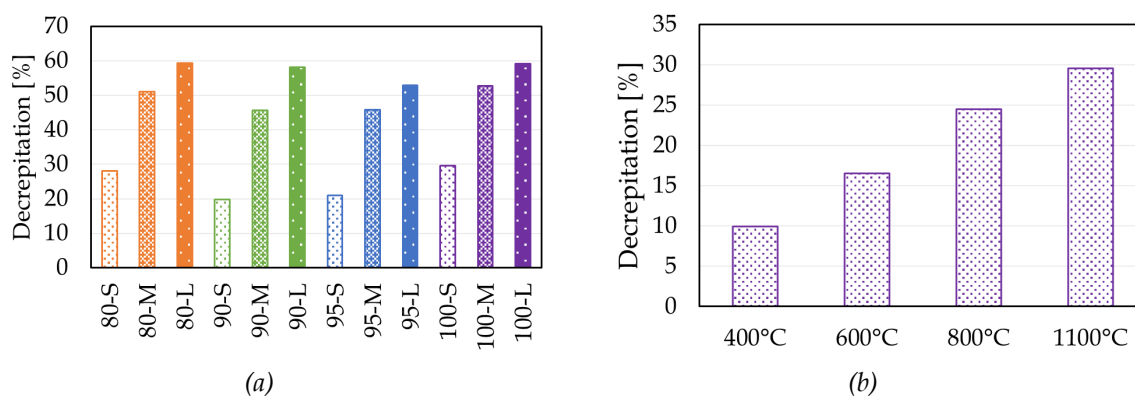


Figure 5: The degree of decrepitation as a function of (a)  $\text{H}_2$  partial pressure and particle size and (b) temperature during the prereduction progression of small particles in pure  $\text{H}_2$ .

From Figure 5(a), it is observed that the degree of decrepitation increases consistently with particle size ( $\text{S} < \text{M} < \text{L}$ ) across all atmospheres. Small particles had ~20-30% of decrepitation, while medium and large particles had ~45-50% and ~50-60% decrepitation, respectively. For each particle size fraction, only minor variation is observed between atmospheres, indicating that particle size has a stronger influence on decrepitation than gas composition under the conditions investigated. The higher degree of decrepitation in larger particles is attributed to stronger internal temperature gradients, resulting in non-uniform volume changes and stress development during prereduction. The degree of decrepitation increases progressively with increasing temperature during prereduction, as shown in Figure 5(b). The degree of decrepitation rises from approximately 10% at 400°C to about 30% at 1100°C, with a more distinct increase observed between 600°C and 800°C. This trend indicates that fragmentation becomes increasingly significant at higher temperatures, consistent with the progression of reduction reactions and associated structural changes in the ore.

The strong dependence of decrepitation on temperature and reducing strength is consistent with earlier studies on Mn ore prereduction, which attribute particle fragmentation to internal stresses generated during gas-evolving reactions (Davies *et al.*, 2023; Ernst *et al.*, 2024; Larssen and Tangstad, 2022). Prereduction induces physical changes in the ore, including pore formation, microcracking, and particle fragmentation, driven by gas evolution during oxygen removal and carbonate decomposition. Decrepitation increases with particle size and temperature, while showing limited dependence on H<sub>2</sub> content. The greater susceptibility of larger particles is commonly attributed to stronger internal stress gradients and longer gas-diffusion paths, which promote stress development during prereduction (Davies *et al.*, 2023).

## SUMMARY OF FINDINGS

This study investigates the non-isothermal H<sub>2</sub> prereduction of Fe and Mn oxides in Assmang Mn ore under H<sub>2</sub>-H<sub>2</sub>O(g) atmospheres up to 1100°C. The results demonstrate that H<sub>2</sub> is a viable and effective alternative to carbon-based reductants for Mn ferroalloy production, with important implications for decarbonisation.

TG showed that the maximum mass loss reached 15.89 mass% in pure H<sub>2</sub>, closely matching the theoretical value (~14 mass%), confirming expected prereduction under investigated conditions. The extent of reduction was found to depend primarily on gas composition rather than particle size. Pure H<sub>2</sub> achieved >98% of the maximum mass loss for all particle sizes, whereas increasing H<sub>2</sub>O(g) content reduced the reduction potential, with an 80%H<sub>2</sub>-20%H<sub>2</sub>O(g) atmosphere limited to ~80% conversion. The rate of reduction, however, was strongly influenced by particle size. Smaller particles exhibited more gradual and uniform reduction, while larger particles showed delayed but sharper reaction rates, indicating increased heat transfer and diffusion limitations. All tests displayed typical sigmoidal reduction behaviour, with distinct stages: an induction phase (<350°C), an accelerated reaction stage, and a deceleration phase above ~800°C.

Chemical analysis confirmed significant oxygen removal during prereduction. The O/Mn decreased from 1.53 to 1.00, indicating reduction of higher Mn oxides to MnO, the thermodynamically stable phase under the studied conditions. Fe oxides were reduced preferentially at lower temperatures, forming metallic Fe, while Mn oxide reduction occurred progressively at higher temperatures. Major transformations, including carbonate decomposition and Mn oxide reduction, occurred primarily between 600°C and 800°C. XRD results of prereduction in pure H<sub>2</sub> supported these findings, showing a clear sequence of phase transformations: early decomposition of MnO(OH), rapid reduction of  $\alpha$ -Fe<sub>2</sub>O<sub>3</sub>, and gradual conversion of Mn-bearing phases to MnO. At 1100°C, the final phase assemblage consisted mainly of MnO, metallic Fe, and CaO, confirming completion of prereduction without further reduction of MnO to metallic Mn under these conditions.

In addition to chemical changes, significant physical transformations were observed. Prereduction caused pore formation, microcracking, and particle fragmentation due to gas evolution. Decrepitation increased with particle size and temperature, reaching up to ~60% for large particles, while gas composition had a relatively minor effect. These structural changes are important, as they influence gas diffusion and reaction kinetics during processing.

Overall, the study shows that H<sub>2</sub> enables efficient prereduction of Mn ores, with gas composition controlling the extent of reduction and particle size governing reaction rates and physical stability. The results indicate that prereduction is kinetically controlled in terms of reaction rate, while the final extent of reduction is governed by thermodynamic constraints imposed by the H<sub>2</sub>-H<sub>2</sub>O(g) gas composition. The findings provide new insight into non-isothermal reduction mechanisms under realistic conditions, supporting the integration of H<sub>2</sub> into Mn ferroalloy production as a pathway toward lower CO<sub>2</sub> emissions and more sustainable metallurgical processes.

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# Forged resilience: A retrospective on the Furnace Tapping conference series (2014–2022)

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Furnace tapping sits at the intersection of art, science, and engineering, yet for decades it lacked a dedicated scholarly platform despite its central role in the safety, performance, and longevity of pyrometallurgical furnaces. The Furnace Tapping Conference series – initiated in 2014 and held again in 2018 and 2022 – was established to address this gap by fostering focused technical dialogue and strengthening collaboration between industry, academia, and technology providers. This paper presents a retrospective analysis of the series, examining its evolution, research contributions, and influence on the development of furnace-tapping knowledge over nearly a decade. Drawing on 65 published papers, authorship and collaboration trends, and citation impact, the review highlights the emergence of furnace tapping as a cohesive research domain supported by systematic documentation, modelling advances, industrial surveys, and technological innovation. Key themes include tap-hole design and maintenance, monitoring and sensing, modelling and risk management, operational safety, and equipment development – alongside insights from coordinated international research initiatives. The paper concludes with reflections on how the conference series catalysed problem-driven engagement within the pyrometallurgical community and considers opportunities for future innovation, particularly in the context of decarbonisation and evolving industrial challenges.

## INTRODUCTION

The Furnace Tapping Conference series was established to address a recognised gap in technical literature and professional discourse related to furnace tapping – an operational activity that plays a critical role in the safety, availability, and performance of pyrometallurgical furnaces. Despite its importance, furnace tapping had historically received limited systematic treatment in peer-reviewed publications, with much of the knowledge residing in operational practice rather than in the open literature.

The origin of the conference series can be traced to the research conducted by Steenkamp, J. (Steenkamp, 2014) on tap-hole performance, specifically accelerated wear observed at slag tap-holes in silicomanganese tap-holes as part of PhD studies (Steenkamp, 2014) at the University of Pretoria. A review of the available literature at the time highlighted the fragmented nature of published work on furnace tapping and the absence of a dedicated platform where researchers, engineers, operators, and technology suppliers could engage with the topic in a focused and technically rigorous manner. This identified need provided the motivation for initiating a conference series dedicated exclusively to furnace tapping.

This paper reviews the Furnace Tapping Conference series of 2014, 2018, and 2022, with a particular focus on the collaborations, research outputs, themes, and technical advancements documented in the published proceedings. The central question addressed is:

*What has been the impact of the Furnace Tapping Conference series on research, development, and innovation in furnace tapping since 2014?*

## BALANCING ART, SCIENCE, AND ENGINEERING

From its inception, the Furnace Tapping Conference was designed with two primary objectives. The first was to create a forum that deliberately brought together perspectives from operations, engineering, and research to address furnace tapping as an integrated technical and operational challenge rather than as an isolated sub-topic. The second objective was to ensure that the knowledge shared at the conference was preserved through the publication of peer-reviewed proceedings, thereby contributing to a growing and accessible body of work for the broader pyrometallurgical community. These objectives were met where 65 papers were included in the proceedings, 19 in 2014, 19 in 2018, and 27 in 2022. Of the 65 papers, 12 had first authors affiliated with industry at the time of writing, 29 with service providers, and 24 with research institutions.

The conference series also aligned with a broader professional philosophy of strengthening the interface between industry and academia. Conferences were used as a mechanism to facilitate problem-driven engagement, where industrial challenges defined the agenda and applied research, innovation, and development was positioned in direct response to operational needs. This approach later became formalised as ‘problem-based conferencing’ and informed subsequent conference and symposium initiatives hosted by the Southern African Institute of Mining and Metallurgy and The Minerals, Metals and Materials Society.

Most of the 153 unique authors authored only one paper (Table I) and papers were mostly collaborative in nature (Figure 1(a)). Most of the papers were collaborations within single institution or between two institutions (Figure 1(b)). Of the 65 papers published, 13 were collaborative papers where authors originated from industry, service providers, and research institutions. The ratio of male to female authors was 80:20.

Table I: Number of authors grouped by the count of Furnace Tapping–related papers they have authored

| # papers authored     | 1   | 2  | 3 | 4 | 7 | 8 |
|-----------------------|-----|----|---|---|---|---|
| # authors in category | 130 | 13 | 5 | 1 | 3 | 1 |

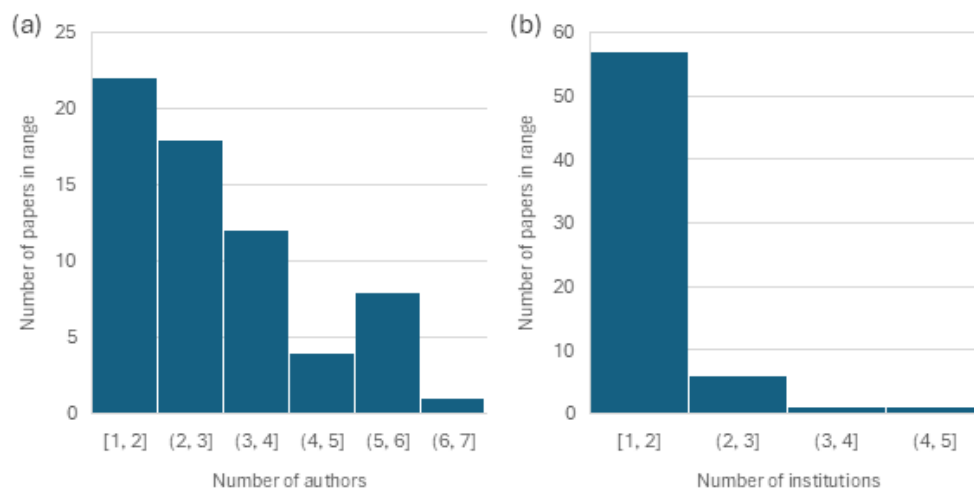


Figure 1: Histograms presenting the number of papers in the range of (a) number of authors and (b) number of institutions.

Since the first Furnace Tapping Conference in 2014, the series has expanded in scope, participation, and influence. Subsequent conferences in 2018 and 2022 reflected increasing international engagement (

Figure ) and were closely linked to coordinated research initiatives, including collaborative projects



between South African and Norwegian institutions focused on controlled tapping and tap-hole behaviour in manganese ferroalloy production. Over time, the conference series has contributed to the emergence of furnace tapping as a recognised research domain, supporting advancements in tap-hole design, refractory performance, tapping practices, monitoring, and risk management.

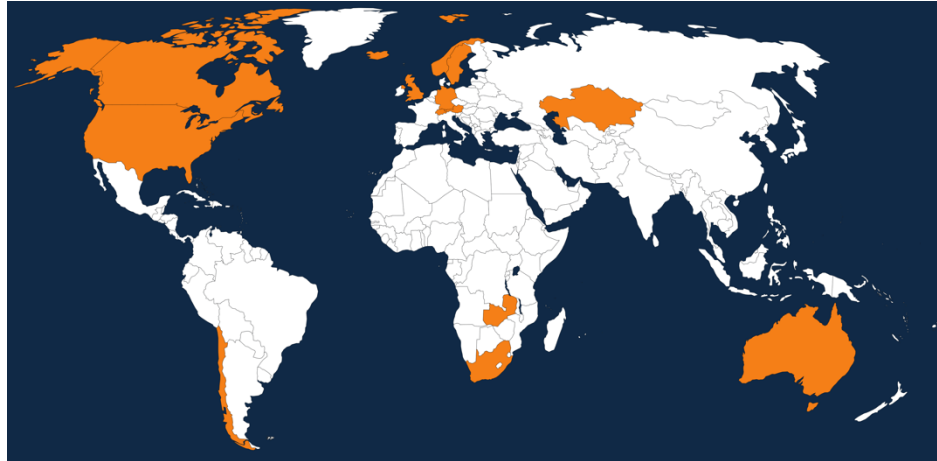


Figure 2: Countries where authors were based.

## MANAGING EXTREMES

In research, one of the metrics measured to determine the impact of a specific paper on a body of knowledge is the number of citations that specific paper received in other papers. The results for the top 10 most referenced papers, based on Google Scholar or ResearchGate as of 5 January 2026 and including references to journal versions of the papers, were summarised in Table II. Time influences the results, with papers from 2022 having had only four years to accumulate citations, whereas those from 2014 have had 12 years. In a sense, the papers listed in Table II formed the foundation of the work to follow in subsequent Furnace Tapping Conferences.

Table II: Top 10 most referenced papers included in the Proceedings of Furnace Tapping 2014, 2018, and 2020

| Rank | Paper                                                                                                        | Citations | FT   |
|------|--------------------------------------------------------------------------------------------------------------|-----------|------|
| 1    | 'The tap-hole' – key to furnace performance (Nelson and Hundermark, 2014)                                    | 62        | 2014 |
| 2    | Slag carry-over and the production of clean steel (Pistorius, 2018)                                          | 27        | 2018 |
| 3    | The Tapping Process in Silicon Production (Tveit <i>et al.</i> , 2002)                                       | 21        | 2014 |
| 4    | A short history of the development of tapping equipment (Dienenthal, 2014)                                   | 15        | 2014 |
| 5    | Tap-hole monitoring technologies (Braun <i>et al.</i> , 2014)                                                | 13        | 2014 |
| 6    | Tapping of PGM-Ni mattes: an industry survey (Nolet, 2014)                                                   | 13        | 2014 |
| 7    | Tap-hole opening: Advances and improvements (Morales, Morales and Nunez, 2018)                               | 13        | 2018 |
| 8    | Wear analysis of a tap-hole from a SiMn production furnace (Steenkamp <i>et al.</i> , 2014)                  | 12        | 2014 |
| 9    | Blast furnace tapping practices at ArcelorMittal South Africa, Vanderbijlpark Works (Geyer and Halifa, 2014) | 12        | 2014 |
| 10   | Non-water-based metal tap-hole cooling - a safer alternative (Kennedy <i>et al.</i> , 2018)                  | 11        | 2018 |

The paper with the highest number of citations was the review paper by Nelson and Hundermark (Nelson and Hundermark, 2014) of which the journal version (Nelson and Hundermark, 2016) referenced more than 130 papers. In his keynote address, Nelson emphasised the critical role of tap-hole design and management in furnace performance and longevity across ironmaking, ferroalloy, and non-ferrous smelting operations. The paper reviewed the evolution of tap-hole configurations, cooling systems, refractory materials, and maintenance practices, highlighting how process conditions (temperature, superheat, heat flux) dictate design choices. The authors advocated for integrated tap-hole systems, advanced monitoring (including fibre-optics), and preventative maintenance to ensure safety, reliability, and extended campaign life. It set the stage for numerous follow-up studies on furnace tapping related matters but also non-related topics; the journal version (Nelson and Hundermark, 2016) a truly seminal paper worthy of the Southern African Institute of Mining and Metallurgy's Silver Medal Award.

The 2<sup>nd</sup> and 3<sup>rd</sup> most referenced papers were both keynote addresses as well.

Pistorius (Pistorius, 2018) brought in the perspective from steelmaking. His paper reviewed the impact of slag carry-over during steel tapping on steel cleanliness, deoxidiser consumption, phosphorus reversion, and ladle refractory wear. It described detection methods such as infrared imaging and electromagnetic sensors; rapid slag-stopping technologies including slag darts, pneumatic stoppers, slide gates, and eccentric bottom tapping. The paper emphasised that minimising slag carry-over would be essential for producing clean steel and achieving operational efficiency in both oxygen converters and electric arc furnaces.

Unable to travel, the paper by Tveit (Tveit *et al.*, 2002) was presented by one of his colleagues at Elkem, Ragnild Jensen. In her keynote address, Ragnild addressed challenges in silicon furnace tapping, including fugitive emissions, operator safety, and process stability; and presented modelling work to improve understanding and control. A 3D multiphase flow model revealed critical phenomena such as the 'doughnut' shape of liquid silicon distribution, informing improved tapping standards and fume hood designs. Implementation of advanced fume extraction systems and corporate projects like DustEx significantly reduced dust exposure, improved environmental compliance, and enhanced occupational health and safety in silicon smelting operations.

A personal favourite was the paper with the 4<sup>th</sup> most citations authored by Dienenthal (Dienenthal, 2014), a 5<sup>th</sup> generation builder of tapping equipment. The paper traced 120 years of tapping technology evolution, from manual opening and closing of tap holes to modern hydraulic clay guns and drills. Key milestones included the invention of the clay gun (1895), oxygen lancing (1901), and progressive developments in hydraulic systems, slewing mechanisms, and high-frequency hammers to improve safety and efficiency. More recent advancements focussed on automation, compliance with EU safety directives, and optimising hydraulic controls, while future innovations aimed at extending tap-hole life and reducing reliance on lancing.

The 5<sup>th</sup> most cited paper by Braun and his co-authors (Braun *et al.*, 2014) reviewed advanced monitoring systems supplied by Hatch Ltd for tap-blocks, including the Tap-block Diagnostic System (TDS), Tap-hole Acoustic Monitoring (TAM), and fibre-optic temperature sensors. TAM used acoustic emissions to detect refractory wear and skewed drilling/lancing in real time, while fibre-optic sensors provided high-resolution temperature data for condition assessment. Combined with thermal modelling and diagnostic algorithms, these technologies would enable predictive maintenance, improve safety, and support future automation of tapping operations. Advanced monitoring systems were also addressed by others (Hopf, 2014; Rödfalk *et al.*, 2014; Ksiazek *et al.*, 2018; Soltys *et al.*, 2022; Vukovic, 2022; Wenzl, Persson and Koncik, 2022).

The follow-up study formed the basis of her keynote address in 2022 (Nolet *et al.*, 2022), where Nolet reported in the 6<sup>th</sup> most cited paper on an ambitious survey of matte tapping practices and tap-hole maintenance strategies across platinum group metals and nickel smelters, highlighting differences in tap-block design, refractory selection, and operational practices (Nolet, 2014). Findings reported

widespread use of water-cooled copper blocks, varied tapping frequencies driven by converter coupling, and evolving clay technologies, with productivity benchmarks ranging from 15 to 210 t/day per tap-hole. The study emphasised record-keeping and benchmarking as critical tools for optimising tap-hole life and improving furnace availability.

The 7<sup>th</sup> most cited paper was presented by Nunez (Morales, Morales and Nunez, 2018). The paper reviewed the evolution of tap-hole opening tools from manual methods to modern thermal lances, emphasising the physics and thermodynamics behind iron-oxygen combustion and energy concentration. Latest-generation lances developed by Trefimet featured multi-insert designs that optimised axial energy release, reduced radial damage, and improved efficiency compared to traditional oxygen pipes, resulting in smoother openings and extended tap-block life. Practical guidelines were provided for lance selection and operation, addressing oxygen flow, material properties, temperature ranges, and common mistakes, with proven benefits including faster opening times, improved safety, and up to three times longer tap-hole life. The paper was followed by another in 2022 (Nunez, Umana and Morales, 2022) which formed the basis for a very successful collaboration on lancing and robotics in tapping which was introduced at the same conference (Madariaga *et al.*, 2022). Steenkamp *et al.*, (2014) was the 8<sup>th</sup> most cited, resulting from her PhD studies. The paper reported on refractory wear mechanisms in a silicomanganese furnace tap-hole observed through excavation analysis and thermodynamic modelling of slag/refractory and metal/refractory interactions. Results showed that wear was driven by chemical reactions between slag and refractory (SiC and carbon), forming metal and gas phases, with wear rates increasing significantly at higher temperatures and during oxygen lancing. The paper concluded that taphole wear is exacerbated by elevated tapping temperatures and recommends further research into operational practices to mitigate refractory degradation.

The 9<sup>th</sup> most cited paper (Geyer and Halifa, 2014) was presented by Geyer and detailed blast furnace tapping practices at ArcelorMittal, focusing on tap-hole design, maintenance, and operational strategies to ensure safety and extend campaign life. It explained the influence of refractory design (small vs. big block hearths), cooling systems, and liquid level control on furnace stability, while emphasising proper drilling, clay plugging, and monitoring to prevent catastrophic failures. The authors highlighted best practices such as full-pool trough design, thermocouple-based temperature monitoring, and decision trees for drill bit selection to optimise tapping performance and minimise wear. The paper is an example of several industrial reviews: (Van Beek *et al.*, 2014; Mgenge and Steenkamp, 2014; Rioux *et al.*, 2014; Thomson, 2014; Davidsen and Honstad, 2018; Hoozen, Sichone and Rambiyana, 2018; Sutherland and Gous, 2018; Cele and Nzima, 2022; Grandfield, Wagstaff and Wagstaff, 2022; Hogg *et al.*, 2022; Mwanza *et al.*, 2022) – one of the strengths of the conference series which resulted in Furnace Tapping 2022 being co-hosted at the TMS Annual General Meeting and Exhibition.

Another keynote presenter who was absent during the 2018 event and the 10<sup>th</sup> most cited paper (Kennedy *et al.*, 2018) was presented by one of the organising committee members, Harmen Oterdoom. This paper explored the use of monoethylene glycol (MEG)-water mixtures and other fluids as safer alternatives to water for cooling tap-holes, reducing explosion risk while maintaining thermal performance. Finite element modelling and analytical calculations showed that MEG-water mixtures could achieve adequate heat transfer at higher flow velocities, with manageable temperature increases and pressure drops. The study emphasised the need for rigorous flammability and explosion testing and highlights successful industrial applications of oil-cooled tap-holes, paving the way for broader adoption of non-water coolants in high-risk furnace areas. Water cooling was used extensively in especially slag tap-blocks (Mc Dougall, 2014; Belford, Conradie and Mwanza, 2018) and slag valves (Essack, 2014) and alternative cooling was also proposed by others (Filzwieser, Konetschnik and Dreyer, 2014).

The Controlled Tapping project, managed by Merete Tangstad (Tangstad *et al.*, 2022), deserves a special mention as outputs from this project contributed significantly to the success of the events in 2018 and 2022. The paper outlined the Norwegian Controlled Tapping research program (NTNU, SINTEF, Mintek) aimed at reducing uneven tapping through coupled CFD/ROM modelling, laboratory studies,

and industrial campaigns (excavations, temperature measurements, and interfacial tension experiments). Key findings included the strong sensitivity of tapping rate to particle-bed configuration near the tap-hole, evidence of TiC/graphite banks affecting SiMn furnace drainage, and metal temperatures at the tap hole in Si/FeSi often >1800–1900°C despite lower ladle measurements, driven by radiation and conduction losses in runners. The project also developed cascade-tapping models showing that thicker slag layers and optimised ladle positions reduced metal carryover, and provided a conceptual framework linking furnace interior states to tapping behaviour for operator decision-making (Folstad *et al.*, 2018; Olsen, Reynolds and Erwee, 2018; Reynolds *et al.*, 2018; Ringdalen and Ksiazek, 2018; Johansen and Ringdalen, 2018; Hoover, Saevarsdottir and Tangstad, 2022; Olsen, Ksiazek and Tangstad, 2022; Surup, Koseniuk and Tangstad, 2022; Loomba *et al.*, 2022; Loomba, Olsen and Einarsrud, 2022; Mukono, Wallin and Tangstad, 2022; Steenkamp, Olsen and Reynolds, 2022).

The major themes addressed by the conference series, and papers not yet referenced, included:

1. Tap-hole design and maintenance (Lindstad, 2014; O'Shaughnessy, Van der Merwe and Botes, 2014; Faux *et al.*, 2018; Steenkamp, 2018; Gregurek *et al.*, 2022; Sitefane and Steenkamp, 2022; Unterreiter *et al.*, 2022)
2. Opening and closing equipment and materials (Lindstad, 2018; De Pretto and Lindstad, 2022; Moreira de Oliveira, T., Moulin-Silva, W., Chiaretti Bassalo, Hm., Ribeiro, A.S., Alves de Moura Brito, 2022)
3. Monitoring and sensing
4. Modelling and optimisation (Els *et al.*, 2014; Erwee *et al.*, 2018; Ghorbani, Al-Dojayli and Chomyn, 2018; Kreuzer, Marschall and Wagner, 2018; Cherkaev *et al.*, 2022; Pistorius, 2022; Reynolds, Steenkamp and Sutherland, 2022; Scheiblehner *et al.*, 2022)
5. Safety and sustainability

## ADAPTING TO CHANGE

In 2022, I emigrated to Canada with my two children and my research interests pivoted towards decarbonisation of nickel production. It has been a steep learning curve, which required resilience on many levels. Fortunately, many of the learnings from furnace tapping and experience in ferroalloys production, ilmenite roasting and smelting, and secondary steelmaking were transferable. Going forward, I will be most interested in participating in problem-based conferences focused on:

- Decarbonisation of metals production following a value-based approach
- Energy recovery from slag
- Efficiency improvements in combustion-based pyrometallurgical processes

Two additional topics of interest to industry would be hydration of magnesia refractories – how to prevent it from happening and once it occurs remedial actions to take – and management of electrodes with special emphasis on Soderberg electrode management.

## CLOSING REMARKS

The Furnace Tapping Conference series has not only expanded the technical body of knowledge around one of the most critical aspects of pyrometallurgical operations; it has also demonstrated the value of problem-based collaboration in accelerating practical innovation. By bringing together operators, engineers, researchers, and solution providers, the series created a shared language and an enduring research community where previously only fragmented knowledge existed. As the industry enters a period defined by decarbonisation, digitalisation, and increasing expectations for safe and sustainable furnace operation, the foundations laid between 2014 and 2022 will continue to guide both technological progress and collaborative practice. My own transition into research on decarbonisation has underscored how deeply interconnected these domains are: the same principles of resilience, systems thinking, and interdisciplinary engagement that shaped the furnace tapping community now inform

the next generation of challenges. The spirit of the conference series – practical, rigorous, and grounded in real operational needs – remains essential as we collectively navigate the future of high-temperature metallurgical processing.

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# Analysis of carbon emission reduction opportunities in an integrated steel plant for environmental and business sustainability

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The steel industry is a major contributor to global greenhouse gas emissions, with a large portion coming from the integrated blast furnace – basic oxygen furnace steel production route. Designated a hard-to-abate sector, the steel industry is examining a range of pathways to reduce carbon emissions to align with global sustainability initiatives. As the industry pursues sustainable steel production and reduced greenhouse gas emissions, several step changes will be required, covering all key areas of the integrated steel plant operations. These include raw material selection, utility operations, and technology upgrades in areas such as the coke plant, sinter plant, and blast furnaces.

This paper investigates carbon emission reduction opportunities that can be implemented in the integrated steel plant and compares them to a representative base case to highlight the degree of possible improvement. Specific emission reduction opportunities investigated include equipment and operational improvements such as increased pulverised coal injection, blast furnace burden distribution improvements, raw material changes, process monitoring and control improvements, and green energy production and use. The challenges associated with emission reduction and the corresponding reduction in on site power generation using available process gases is discussed with various methods to overcome these challenges. Financial sustainability is a critical aspect to maintaining the steel sector's competitiveness, in addition to carbon emission reduction and environmental sustainability. This paper analyses the operating cost impacts of the suggested emission reduction pathways and outlines some important cost constraints.

**Keywords:** Emission Reduction, Blast Furnace, Integrated Steel Plant, Greenhouse Gas Emissions

## INTRODUCTION

The steel industry contributes 7-9% of the global greenhouse gas (GHG) emissions, making it a key area of focus for emission reduction (World Steel in Figures 2024, 2024). Within the steel industry, there are initiatives to significantly reduce emissions by 2030 and approach carbon neutrality by 2050. While different countries have individual targets and timelines, the emission reduction imperative is applicable to steelmakers worldwide. From worldsteel data, about 70% of global steel production is through the blast furnace (BF) and basic oxygen furnace (BOF) production route with a typical emission rate of 2.3 t-CO<sub>2</sub>/t-cast steel (World Steel in Figures 2025, 2025). The remaining 30% of global steel is produced using the electric arc furnace (EAF) route, using scrap, direct reduced iron (DRI) and combinations thereof. The scrap-based EAF route has a typical emission rate of 0.7 t-CO<sub>2</sub>/t-cast steel while DRI-based EAF steelmaking typically emits 1.4 t-CO<sub>2</sub>/t-cast steel (World Steel in Figures 2025, 2025). These emission intensities come from world steel's CO<sub>2</sub> data collection methodology which includes all scopes; 1, 2, and some scope 3, excluding mining.

Since most steel is produced using the highly emitting BF-BOF route, emission reduction pathways for this value-chain are required to meet steel industry emission targets. Pathways requiring large capital expenditures, complete process changes, or novel technology are often difficult to implement and can create a barrier for change. The authors have investigated stepwise emission reduction opportunities that can be implemented in the BF-BOF value-chain to reduce emissions without making drastic process and equipment changes.

### Industry background

Steel production and related GHG intensity have both been consistent since 2020 (World Steel in Figures 2024, 2024). This highlights the need for changes in production methods to adhere to emission targets sought regionally and globally. The top BF-BOF steel producers located in China, India, Japan, Russia, Europe, and South Korea will be the key drivers for global steel emission reduction initiatives.

Using worldsteel data, the emission intensities of selected global BF-BOF operators were analysed to understand and rank their emission profiles (Janjua and Reimink, 2019). Hatch then compared key emission profiles for selected North American, Russian, Japanese, South African, and Indian steel producers (Aubry *et al.*, 2023). Figure compares these regional profiles to global emission intensity trends, considering scope 1 and 2 emissions, per tonne of cast steel and the associated percentage of BF-BOF operators operating at that point.

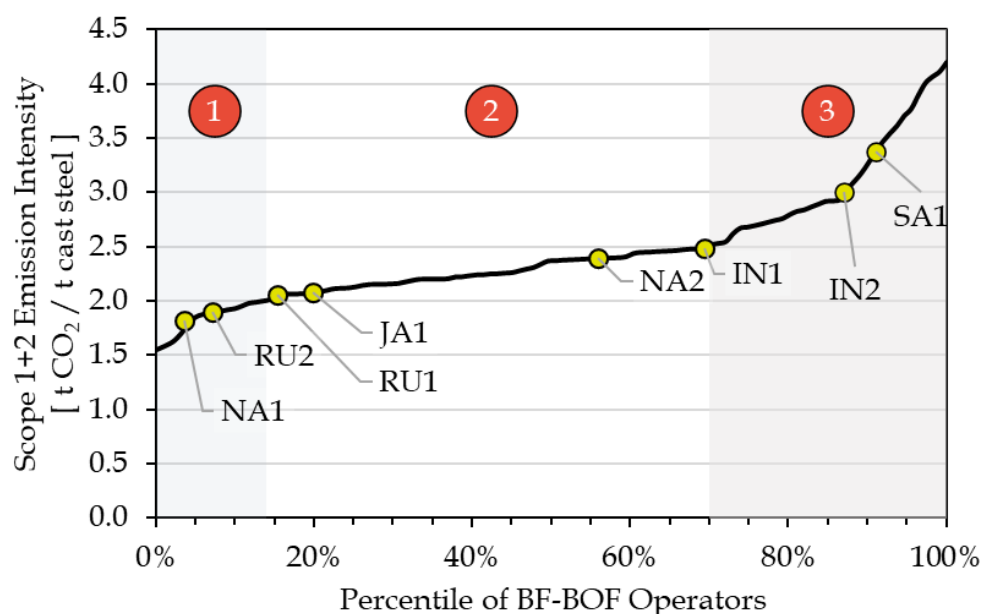


Figure 1: Emission intensity of representative BF-BOF producers (NA = North America, RU = Russia, JA = Japan, IN = India, SA = South Africa (ArcelorMittal South Africa ESG Report 2023, 2023), specific points added based on public disclosure) (Janjua and Reimink, 2019; Aubry *et al.*, 2023)

In our analysis, 'direct' emissions align with scope 1 emissions presented by worldsteel and 'utility' emissions align with scope 2 emissions provided by worldsteel. The authors have considered the captive power plant to be within the BF-BOF steel plant boundary; therefore the emissions associated with steam and electricity consumption are allocated as direct emissions. To enable comparison, the direct emissions have been sub-divided into the 'utility' and 'process' emissions for the respective process units in the integrated steel plant's value-chain.

In Figure 1, three categories of BF-BOF steel plants were identified to further group the global BF-BOF operations (Aubry *et al.*, 2023). Each category was based on the emission intensity achieved which depends on the process equipment available, raw materials used, and the electrical grid emission intensity. These categories are outlined below:

- Category 1 – includes about 15% of BF-BOF producers that achieve emissions below 2.0 t-CO<sub>2</sub>/t-cast steel. These are classified as the best-in-class facilities with excellent raw material, equipment, and operations.
- Category 2 – makes up the largest portion of the BF-BOF industry group, about 55%, and includes BF-BOF producers with emission intensities between 2.0–2.5 t-CO<sub>2</sub>/t-cast steel. This is closely aligned with the typical emission rate for the BF-BOF route of 2.3 t-CO<sub>2</sub>/t-cast steel mentioned in the Introduction.
- Category 3 – consists of about 30% of the BF-BOF steel producers. These are the heavy emitters with emission between 2.5–4.0+ t-CO<sub>2</sub>/t-cast steel. These producers are often constrained by raw materials, available equipment, and operational practices.

This paper outlines strategies to move steel producers in category 2 and 3 closer to category 1 emission rates. While it is important to consider geographical constraints and challenges, our analysis has a global perspective and highlights opportunities that can improve most BF-BOF operations. Opportunities for countries with low or declining steel production, such as South Africa, are identified. These regions also contribute to global steel industry emissions via raw material extraction and export, specifically iron ore exports in the case of South Africa.

### Base case steel plant

To quantify potential emission reduction opportunities, a representative base case integrated BF-BOF steel plant was created. The base case was selected to represent category 3 producers to show how these ‘heavy emitters’ can implement changes that would shift their emissions closer to category 1 producer rates.

The base case considers the unit operations that are shown in Figure 2. All process units within the boundary line are considered in this analysis.

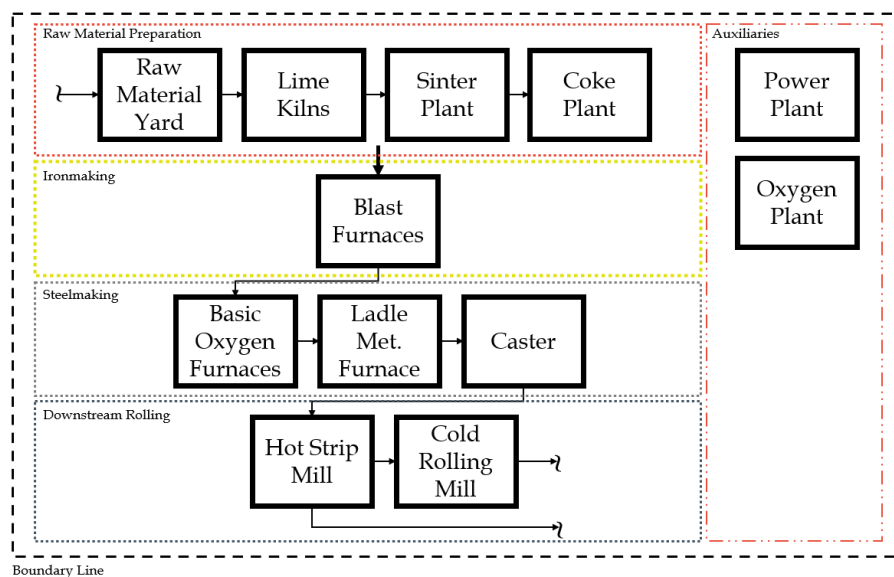


Figure 2: Block flow diagram of the base case integrated steel plant.

The key design and operating parameters for the base case steel plant are outlined in Table I:

Table I: Operating parameters for base case steel plant

| Parameter                            | Unit                 | Value    |
|--------------------------------------|----------------------|----------|
| <i>General</i>                       |                      |          |
| Hot Metal Production                 | Mtpa <sup>1</sup>    | 2.5      |
| <i>Raw Material</i>                  |                      |          |
| Sinter Fe Content                    | %                    | 56       |
| Pellet Fe Content                    | %                    | 64       |
| Lump Fe Content                      | %                    | 62       |
| Coke Ash Content                     | %                    | 14       |
| <i>Blast Furnace</i>                 |                      |          |
| Pellet: Sinter: Lump Charge          | %                    | 15:75:10 |
| Pulverized Coal Injection (PCI) Rate | kg/t HM <sup>2</sup> | 100      |
| Coke Rate (Dry)                      | kg/t HM              | 441      |
| Oxygen in Blast                      | %                    | 25       |
| Gas (CO) Utilisation                 | -                    | 47       |
| <i>Hot Stoves</i>                    |                      |          |
| Blast Temperature                    | °C                   | 1000     |
| <i>Steel Making</i>                  |                      |          |
| Scrap Rate                           | %                    | 10       |
| <i>Products</i>                      |                      |          |
| HRC (Hot Rolled Coil)                | %                    | 70       |
| CR (Cold Rolled) Products            | %                    | 30       |

Working from the battery limits and key design and operating parameters presented above, a complete mass, energy, and emission balance was prepared for the base case steel plant. For simplicity, a steel plant with a single BF producing 2.5 Mtpa of hot metal (HM) was selected. The BF operation was modelled using a modified two-stage heat and mass balance (Cameron *et al.*, 2020; Aubry *et al.*, 2023). Important calculation assumptions and methodologies for the base case steel plant include:

- All calcined fluxes (lime and dolime) were produced from an on-site lime kiln where the flux production target was to feed the respective steel plant consumers. No excess fluxes were produced nor purchased.
- All coke was made on site, where all the coke and coke breeze generated were consumed by the BF and sinter plants. By-product cokemaking technology was assigned and all by-product chemicals were sold.
- All sinter produced on site was consumed by the BF.
- All excess process gases [coke oven gas (COG), blast furnace gas (BFG), and basic oxygen furnace gas (BOFG)] were first used as fuel throughout the plant. Any excess was sent to the on-site power plant for steam and electricity generation.
- The power plant was assumed to burn all excess process gases to produce steam and electricity for the steel plant operations. Any electrical power or steam deficits were filled by additional coal co-firing in the power plant.
- The emission accounting considered all direct emissions on site:
  - Direct emissions refer to the emissions generated and released by burning gases, coke, coal, and any other fossil fuel/carbon source.

<sup>1</sup> Million tonnes (metric) per annum

<sup>2</sup> Tonnes of hot metal

- Direct emissions from burning process gases and thermal coal in the power plant were assigned to the on site power plant. 'Utility' emissions were listed in each process unit to show each unit's impact on electricity and steam consumption. These utility emissions simply show electricity and steam splits throughout the plant; they are not added to the total for each sub-unit.
- Transportation, mining, and other upstream activities were not considered in this analysis. This applied universally to all raw materials. No downstream credits were given for sold products.
- Emissions associated with iron ore pellets were considered based on ISO 14404-1 standard emission factors (ISO, 2024). Pellet emissions considered were limited to high temperature induration only. This was done to ensure emissions were not being shifted off site when sinter was replaced with pellets. If this upstream factor was not applied, it would skew the true emissions savings at the plant, especially for the steel plants with a captive pellet plant. While this is a slight scope asymmetry, it was an important consideration for a fair analysis. No transport or mining emissions were included in the pellet emission factor, only pellet induration related emissions were included to contrast to sinter processing on site.
- All process gas emissions were considered as direct emissions, allocated to the process unit where they were burned.
- All solid fuels, such as coke breeze, that were combusted were accounted for as direct emissions at the process unit where they were burned.
- All released carbonate materials were accounted for where they were calcined.
- Direct emissions were considered and accounted for the entire steel plant including the hot strip mill, cold rolling, and auxiliary areas per the boundary provided in Figure . The emission intensity value was determined by taking the total steel plant emissions and dividing by the produced cast steel so that yield losses would not impact results.

The emission intensity of each base case process area was determined and is provided in Table II. Total site emissions were calculated to be 2.62 t-CO<sub>2</sub>/t-steel.

Table II: Emission breakdown, direct and utility emissions for the base case steel plant

| Plant Location                                   | Direct Emissions<br>(t-CO <sub>2</sub> /t-steel) | Utility Emissions<br>(t-CO <sub>2</sub> /t-steel) |
|--------------------------------------------------|--------------------------------------------------|---------------------------------------------------|
| Lime Kiln                                        | 0.09                                             | 0.02                                              |
| Sinter Plant                                     | 0.40                                             | 0.12                                              |
| Coke Plant                                       | 0.26                                             | 0.06                                              |
| Blast Furnace                                    | 0.61                                             | 0.34                                              |
| BOF / LMF / Caster                               | 0.01                                             | 0.17                                              |
| Reheat Furnace                                   | 0.12                                             | 0.01                                              |
| Hot & Cold Rolling /<br>Auxiliaries <sup>1</sup> | 0.06                                             | 0.31                                              |
| Pellets (Upstream) <sup>2</sup>                  | 0.04                                             | -                                                 |
| Power Plant                                      | 1.03 <sup>3</sup>                                | -                                                 |
| <b>Total Plant / Utility Emissions</b>           | <b>2.62</b>                                      | <b>1.03</b>                                       |

<sup>1</sup>Hot & Cold Rolling / Auxiliaries include the hot rolling mill, cold rolling mill, offices, oxygen plant, and supporting areas

<sup>2</sup>Pellets are upstream emissions, listed under the direct column as outlined in the methodology section

<sup>3</sup>Direct power plant emissions are divided into the utility emissions for each area based on specific electricity and steam consumption

### Opportunity identification to reduce emissions

Several improvement opportunities are available for BF-BOF operators to reduce emission intensity. These opportunities are organised into four cases, outlined in Table III. For each opportunity case, only the identified parameters were changed, with all others remaining aligned with the base case steel plant.

Table III: Opportunity case identification for analysis

| Case | Description               | Changes Implemented                                                                                                                                                                                    |
|------|---------------------------|--------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------|
| 1    | Operational Improvements  | <ul style="list-style-type: none"> <li>Burden distribution improves CO gas utilisation to 49%</li> </ul>                                                                                               |
| 2    | Equipment Improvements    | <ul style="list-style-type: none"> <li>PCI rate increased to 200 kg/t HM, with oxygen increase</li> <li>Hot blast temperature increased to 1200°C</li> </ul>                                           |
| 3    | Raw Material Improvements | <ul style="list-style-type: none"> <li>85% pellets and 15% sinter in the blast furnace ferrous feed</li> <li>Fired sinter iron content increased to 60% Fe</li> <li>Coke ash reduced to 10%</li> </ul> |
| 4    | 'Best in Class' Operation | <ul style="list-style-type: none"> <li>Combination of cases 1-3</li> </ul>                                                                                                                             |

Operational changes, advanced equipment, and use of high-quality raw materials are key opportunities to improve the steel plant performance. The benefit of leveraging these improvement opportunities were outlined in previous publications focused on how best to achieve emission reductions (Aubry *et al.*, 2023; Sadri *et al.*, 2025). The 'Best-in-Class' operation considers changes from previously published cases, but also considers recommendations detailed in the Joint Research Centre (JRC) Best Available Techniques report (Remus *et al.*, 2013). There are several operational practices and equipment technologies that can achieve these improvements. For example, Hatch proposed a novel approach using plasma torches to increase hot blast temperature and reduce coke consumption and related emissions (Sukhram *et al.*, 2017). These specific practices and technologies were not investigated, only their overall impact of reducing emissions was considered.

### Calculation methodology

Mass, energy, and emission balances were completed to understand the relative impact of the cases identified in Table III. Notable changes and assumptions are shown in Table IV and discussed below:

- Total coke production was scaled to produce enough for plant consumption only.
- Sinter and lime production were scaled to produce enough to match consumption.
- All operating parameters of the BF and other process units were the same as the base case, except for those improvement opportunities described in Table III.
- Compared to the base case, all predicted BF productivity improvements were included, these were driven by the selected BF technologies and practices. All downstream equipment was assumed to be able to process any increase in hot metal production. All other consumptions were scaled to match estimated hot metal and steel production requirements.
- All auxiliary area energy consumptions were scaled with cast steel production.
- The increase in energy requirements was filled by an increase in purchased thermal coal used at the power plant.

Table IV: Major parameter changes for opportunity analysis

| Parameter                  | Unit       | Base Case | 1    | 2    | 3    | 4    |
|----------------------------|------------|-----------|------|------|------|------|
| BF Hot Metal Production    | Mtpa       | 2.50      | 2.56 | 2.55 | 2.65 | 2.84 |
| BF Coke Rate (Dry)         | kg/t HM    | 441       | 427  | 324  | 408  | 291  |
| Power Plant Coal Use (Dry) | Mtpa       | 0.28      | 0.32 | 0.31 | 0.24 | 0.31 |
|                            | kg/t-steel | 114       | 128  | 125  | 93   | 112  |

To account for the differences in emission rates, the same calculation procedure was followed as outlined in the Base Case Steel Plant section.



### Emission reduction opportunities quantified

Working from the opportunity improvements identified in Table III, the emission reductions were calculated and compared to the base case. Figure 3 shows the emission changes for each case for direct emissions, including emissions associated with imported pellets and the on-site power plant.

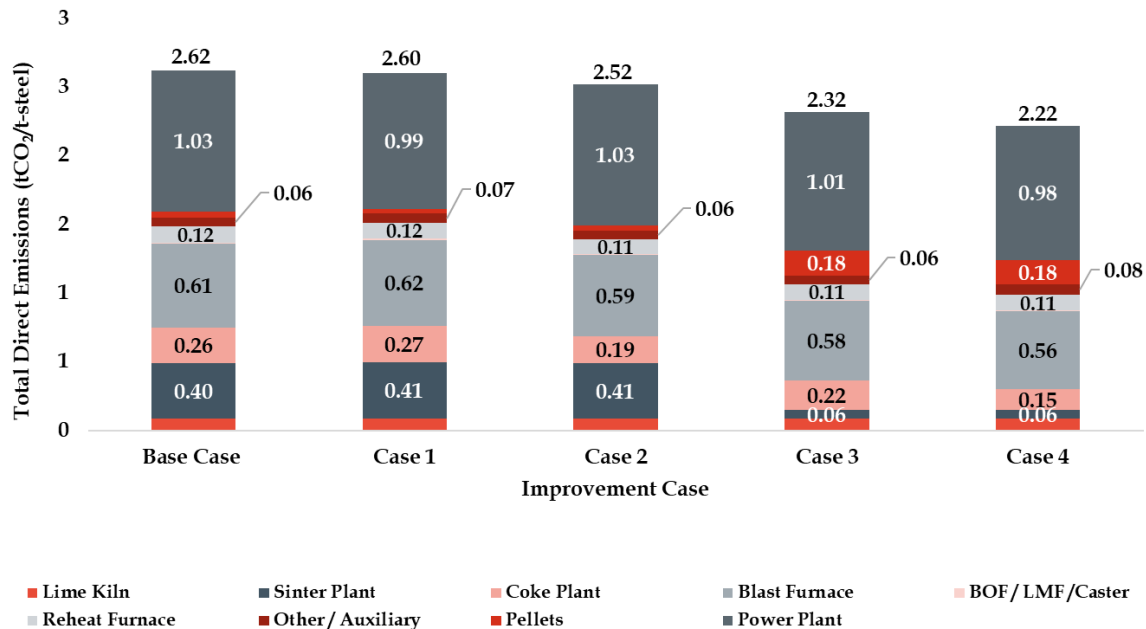


Figure 3: Emission improvement cases. Lime Kiln consistent in all cases at 0.09 tCO<sub>2</sub>/t-steel; Base Case to Case 2 have associated pellet emission at 0.04 tCO<sub>2</sub>/t-steel; and BOF/LMF/Caster consistent in all cases at 0.01 tCO<sub>2</sub>/t-steel. Each bar shows the key process areas and main drivers for emissions changes.

All cases show a stepwise reduction in BF-BOF emissions. Case 1 had a site-wide emission improvement of 0.50% based on implementing burden distribution improvements, increasing CO gas utilisation and reducing coke usage in the BF. This emission reduction is achievable with little or no capital investment. Better instrumentation, improved data monitoring and modelling, and awareness of industry best practices are sufficient to improve burden distribution significantly.

Case 2 had an emission reduction of 3.7%. This case considered stove improvements to increase the hot blast temperature to 1200°C, enabling a PCI increase to 200 kg/t HM. Increasing the PCI rate decreases the coke rate, plus injected coal is a less costly fuel. The fossil fuel savings observed are not large as this fuel swap involves substituting one carbon form for another. That said, an emission reduction would be observed as less coke is produced reducing the related coke plant emissions. Achieving higher hot blast temperature introduces more sensible energy into the BF, which lowers coke consumption. Stove efficiency improvements, such as improved burner design, allow less BFG to be consumed, allowing more BFG to be sent for power and steam generation, reducing the power plant's coal purchasing requirements to supply the power needed by the steel plant.

Case 3 quantified the merits of improved raw material quality by increasing the sinter iron content from 56 to 60% Fe and charging the BF with 85% pellets and 15% sinter. An overall burden iron content of 63.4% was achieved compared to the base case, where the burden iron content was 58%. Additionally, the coke ash was reduced from 14 to 10%. These improvements alone would reduce site-wide emissions by 11.4%. Better sinter quality decreases the amount of gangue minerals charged to the BF, reducing slag production and coke rate. An increased burden iron content by using more pellets allows for less ferrous materials to be charged per tonne of hot metal, further reducing coke demand. Coke with a lower ash content also decreases the amount of slag produced. Low ash coke has increased carbon weight percent and energy content, improving the BF operation and reducing fuel consumption.

Case 4 estimated the total emission reduction when all improvements are implemented in parallel. When combined, an overall emission reduction of 15.2% was achieved. This is a significant improvement using well-established, low-risk technologies, suggesting these changes may be implemented in the near term to achieve meaningful emission reductions.

These improvement changes can be reasonably implemented within five years and have the potential to reduce emissions by 15.2% from the base case. This is a significant reduction that can better position steel plants as they work to meet future emission reduction targets. While many recommended improvements are well-known technologies or process changes, time and training will be critical to ensuring these improvements are successfully implemented. Based on the improvements identified in Cases 1-3, raw material improvements are a key driver for emission reduction in the BF-BOF process route. Implementing these changes must align with the objectives and business realities of the iron ore and coal suppliers to each steel plant.

When comparing the modelled cases against the worldsteel BF-BOF emission intensity data, implementing the nominated changes would move the steel plant from a category 3 producer to category 2, and within 35% of top BF-BOF producers globally, as seen in Figure 4.

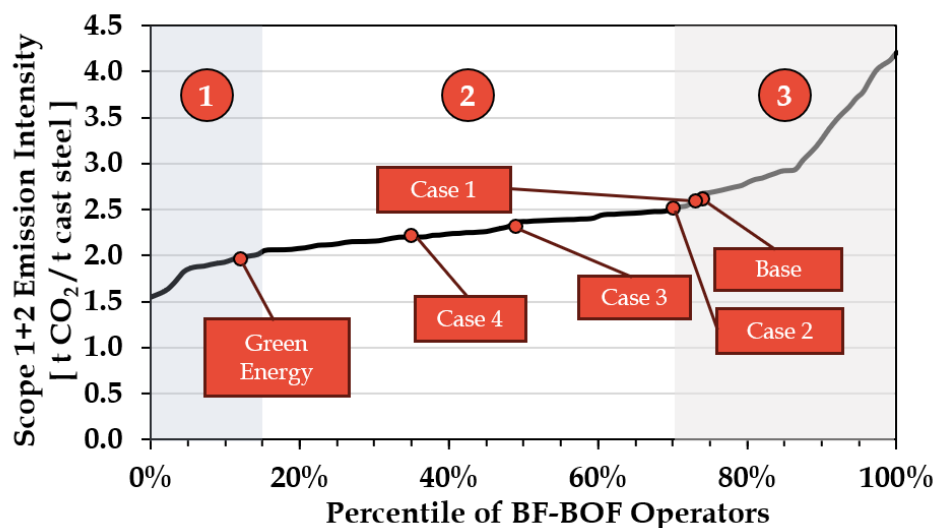


Figure 4: Comparison of each improvement case to the emission intensity of global BF-BOF producers (Janjua and Reimink, 2019; Aubry et al., 2023).

This highlights the extent of possible improvement with the proposed stepwise changes to BF operations. An additional point was also added that considers green energy use to fully replace thermal coal purchases to meet the steel plant electricity demand. In this scenario, all available process gases were still combusted in the power plant; however, instead of purchasing coal to fill any power deficit, green energy was used.

As seen in Figure 4, Case 1 provided a small improvement based on the nominated BF practice changes. When equipment improvements are considered in Case 2, the base case steel plant moves out of the category 3 producer group into category 2, all be it at the high end of reported emission rates. With raw material improvements illustrated in Case 3, the reference plant can be pushed well into category 2 and into the top 50% of BF-BOF steel producer for emission intensity. With the combination of all improvements, the reference plant can reach the top 35% of BF-BOF steel producers. To move fully into category 1, green energy must replace all power plant thermal coal purchases in combination with Case 4 improvements. Using purchased green energy reduced overall site emissions to 1.97 tCO<sub>2</sub>/t-cast steel, moving the steel plant into category 1. While green energy viability in the future remains unknown, this paper describes its impact on steel producers to highlight the importance of green energy even when an on site power plant is in operation. The authors understand that green energy cost and availability

will be highly dependent on plant location, power purchasing strategies, such as virtual power purchase agreements, and relevant government policies.

### Financial considerations

The operating cost impacts (OPEX) were considered for all improvement cases. Based on assumed unit costs, an OPEX was estimated for each case, considering all consumption changes. Fixed costs, such as overhead, maintenance, or labour costs were not considered so that the net income margin was the same across all cases. The unit costs assumed for this analysis are outlined in Table V.

Table I: OPEX unit costs and/or selling prices

| Material - Costs             | Value | Unit  |
|------------------------------|-------|-------|
| Pellets                      | 120   | USD/t |
| Lump Ore                     | 100   | USD/t |
| Iron Ore Fines (Base)        | 90    | USD/t |
| Iron Ore Fines (Upgraded)    | 110   | USD/t |
| Pellet Concentrate           | 110   | USD/t |
| Metallurgical Coal (14% Ash) | 170   | USD/t |
| Metallurgical Coal (10% Ash) | 195   | USD/t |
| Thermal Coal                 | 90    | USD/t |
| PC                           | 150   | USD/t |
| Material - Selling Prices    |       |       |
| Steel                        | 750   | USD/t |

While unit costs depend on plant location and mine ownership, a representative cost was selected at seaborne prices to look at relative changes across all cases. To understand the impact on net income, an 8% margin was assumed on the sale of steel for the base case. As a result, the total steel production cost, including depreciation and sustaining capital, was 690 USD/t-steel. The change in OPEX from the base case was used to determine the relative income change for each new case opportunity. Production increases were considered for applicable cases when calculating the total net income change. The change in net income for each case is shown in Figure 5.



Figure 5: Net income changes for the four improvement cases.

Note that no capital expenditures were considered in this analysis to achieve the benefits outlined for cases 1-4. Only impacts to operating costs were identified and compared. Overall, the biggest impact to net income is the cost increase associated with raw material improvements. Case 3 only focuses on raw material improvements and reduces net income by 57%, which is primarily driven by the increase in high-cost pellet consumption. Cases 1 and 2 both have positive impacts to net income of 7 and 30%, respectively. These operational and equipment improvements did not use high quality material, rather, they focus on process improvements that allow for consumption reductions, increasing the net income. Case 4 combines cases 1, 2, and 3 cost impacts, greatly reducing the negative impact of using higher cost raw materials. That said, an income reduction of 15% was estimated. Considering all the improvement opportunities, the biggest challenge for achieving significant emission reductions will be sourcing high quality raw material without increasing costs excessively.

A sensitivity analysis was performed on Case 3 to determine at what pellet and metallurgical coal prices would allow the net income change to be 0%. This provides helpful benchmarking to show what discounts would be needed to make this case cost-competitive. Assuming no change to pellet prices, the metallurgical coal price would need to drop 26% from 195 to 143 USD/t for the Case 3 net income change to reach 0%. Alternatively, assuming metallurgical coal prices remain constant, the pellet price would need to drop 23% from 120 to 92 USD/t to achieve a 0% net income change in Case 3. Such a discount would only be realised in a soft market with weak pellet demand and a low iron ore price or for captive pellets produced by a steel company that is integrated backward into upstream mining operations. With these metallurgical coal and pellet discounts applied, Case 4's net income change becomes +29% and +46%, respectively. This analysis shows that if steel producers need to buy better raw materials, a low fuel rate is important to minimise the discount required to get to zero increase in net income.

The financial benefits of reducing carbon emissions may be further improved when considering carbon pricing/the cost of carbon emissions. Carbon pricing is still an emerging initiative that varies by location. If implemented on a global scale, steel producers have potential to see significant income savings by lowering steel carbon intensity, accelerating technological change. Specifically, South African steel producers that have a strong interest in exporting to the European Union will need to focus on minimising emissions, reaching Category 1 emission rates. This would reduce any costs imposed through the emerging EU carbon border adjustment mechanisms for imported steel.

### **Opportunity discussion and industry outlook - a South African context**

The two biggest drivers to reduce emissions to category 1 best-in-class rates are high quality raw materials and the ability to buy green electrical energy. Acquiring both is heavily geographically dependent; and therefore, are more readily implemented for steel plants with natural advantages. Prioritising beneficiation processes for raw material producers improves quality at existing mines and positions their steel clients with a more sustainable production model. While green initiatives such as solar power can be difficult to implement, focusing on proven technology such as coke dry quenching (CDQ) and top gas recovery turbines (TRT) will provide reliable emission-free electricity via energy recovery. A TRT utilises the top gas pressure energy to generate electricity that can amount to 25-35 kWh/tHM (Li and Kuangdi, 2022). CDQ recovers the heat as coke is cooled and can produce up to 120 kWh/t-coke (Itakura, 2016; Fabbri *et al.*, 2024). While these recommendations can be implemented in most locations and steel industry landscapes, it is important to consider the unique industry and business drivers at each site.

South African steel production has been declining over the last several years, with a drop in crude steel production of 12% in October 2025 alone, reflecting the closure of ArcelorMittal's Newcastle BF ('SteelMatters–November 2025,' 2025). While domestic production is slowing, South Africa's access to high quality domestic iron ore is a key advantage that could support future steelmaking. Logistical challenges for extracting and transporting ore across South Africa must be overcome to bring iron ore to the steel consuming regions. In the near term, South Africa still has an impact on emission reductions in the steel industry via exporting high-quality iron ore. South Africa produces over 65 million tonnes of iron ore and exports over 85% ('World Steel in Figures 2025,' 2025). By investing in ore beneficiation

initiatives to increase iron ore quality material at competitive prices, South Africa could become a key player to help significantly reduce emissions in steel plants wherever this iron ore is consumed, and when paired with other on site technology improvements to reduce fossil fuel usage.

## CONCLUSIONS

For BF-BOF steel producers to reduce emissions, key operational, equipment, and raw material changes are needed. Operational changes, such as improved gas utilisation and burden distribution, along with equipment changes, were shown to positively impact emission intensity from the base case presented by the authors, with net income improvements. Raw material improvements had the most impactful individual emission reduction but come with a net income loss. Implementing all changes together provided a significant reduction in emission with a minor income loss. Ultimately this paper highlights feasible stepwise improvements that BF-BOF steel producers can implement in the near-term to achieve emission reductions of 15%. These emissions can be further reduced when replacing purchased thermal coal with green electrical power, increasing total emission reductions from the base case to 25%.

These improvement opportunities highlight possible areas of new interest for BF-BOF steel producers for future action, including:

- Mines – steel producers could consider vertically integrating and buying into mines to have more control over raw material quality and help reduce cost impacts.
- National Grids and Green Power – steel producers could consider virtual power purchase agreements with emerging renewable power generators to help green national grids. This way, steel producers enable green power initiatives that benefit the producer and meet national interests. In some cases, steel producers may implement behind the meter renewable green power if space and conditions allow.
- Beneficiation - current raw material producers can invest in new beneficiation initiatives to help improve raw material quality while being cost-competitive. Steel plants and raw material producers must consider the entire value chain so that material upgrading initiatives, such as adding pellet concentrate to sinter, make the most of raw material currently available.

Overall, there are many interesting areas, beyond the improvements discussed in the paper for steel producers and raw material providers, to investigate that can help contribute to emission reductions.

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### **Megan Estabrooks**

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Megan Estabrooks is a process engineering trainee with three years of experience working at Hatch Ltd. in Mississauga, Canada. Megan graduated from Western University's chemical engineering program and Ivey Business School's HBA program. Through working on Hatch's Pyrometallurgy team, Megan has worked on projects in both the ferrous and non-ferrous industry, ranging from concept studies to basic engineering work. Megan has work experience with graphite purification and ferro-alloy electrolysis. Megan also has several project experiences working with integrated steel plants. Most recently, Megan has been working on studies investigating carbon emission alternatives in steel plants, considering the site wide emission impact as well as operating cost and capital expenditures. Megan has also co-authored a paper published in AIST's Iron and Steel Technology magazine investigating methanation as an emission reduction opportunity in the blast furnace.

# Advances in graphite electrode manufacturing: Implications for electric arc furnace efficiency and sustainability

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Graphite electrodes are essential components in electric arc furnace (EAF) operations, significantly influencing process economics, energy efficiency, and sustainability. While EAFs are widely used in steelmaking and aluminium production, their applications extend to other metallurgical processes as well. This paper reviews the chronological improvements and recent innovations in graphite electrode manufacturing, including raw material selection, process technology, and mechanical design, and evaluates their impact on electrode performance and lifecycle. The review also explores the role of high-performance electrodes in optimising EAF operations, reducing electrode consumption, and minimising carbon emissions. Lifecycle assessments and recycling strategies are discussed to highlight environmental considerations. The paper concludes with recommendations for future research and the industrial adoption of advanced electrode technologies to improve competitiveness and sustainability in EAF-based processes, with particular emphasis on steelmaking and related metallurgical applications.

## INTRODUCTION

Electric arc furnace (EAF) steelmaking has become a cornerstone of modern metallurgical production, offering a flexible and energy-efficient alternative to the blast furnace–basic oxygen furnace (BF–BOF) route. As the global steel industry intensifies efforts to reduce greenhouse-gas emissions and transitions toward lower-carbon production pathways, the EAF process is increasingly favoured due to its ability to utilise recycled scrap, incorporate direct-reduced iron (DRI), and operate with electricity from low-carbon and renewable sources. According to recent estimates from the World Steel Association and the International Energy Agency, EAFs currently account for approximately 30% of global crude steel production, with significantly higher shares in regions prioritising electrification over coke-based ironmaking (International Energy Agency, 2025; World Steel Association, 2024). This share is expected to grow further as hydrogen-based DRI technologies mature and electrification strategies expand in traditionally coal-dominated steelmaking regions.

The progression toward modern Ultra High Power (UHP) operation is rooted in early fundamental studies on arc characteristics. Notably, Bowman (1969) established the early technical frameworks for high-power steelmaking, defining the relationship between current density and electrode performance that remains a cornerstone of furnace design.

Central to the operation of electric arc furnaces are graphite electrodes, which enable the conversion of electrical energy into thermal energy through high-intensity arcs that melt metallic charge materials. More than 90% of global graphite electrode consumption is associated with EAF and ladle furnace (LF) steelmaking, with the remaining demand arising from metallurgical applications such as aluminium, silicon, and phosphorus production, as well as electrochemical processes including chlor-alkali production. Reflecting the continued expansion of EAF-based routes, the global graphite electrode market is projected to grow at a compound annual growth rate of approximately 6.3% between 2024 and 2032 (Fortune Business Insights, 2024).

Graphite electrode performance exerts a direct and measurable influence on furnace efficiency, electrical stability, and operating cost (Bershtskii *et al.*, 2008). In industrial practice, electrode consumption typically represents 10–20% of EAF conversion costs, making the optimisation of electrode lifetime and reliability a priority for steel producers (AIST Steel News, 2023). Electrode degradation arises from a combination of thermal, chemical, and mechanical mechanisms, including oxidation, sublimation, erosion, and mechanical damage; all of which are sensitive to both electrode material characteristics and furnace operating conditions (Pierson, 1993; Daneshmand and Heydari Vini, 2024). Consequently, improvements in electrode quality, design, and consistency translate directly into lower specific consumption, improved furnace stability, and enhanced energy efficiency.

In parallel with advances in EAF technology, graphite electrode manufacturing has undergone sustained evolution driven by increasing power densities, larger furnace sizes, and the growing dominance of ultra-high-power (UHP) furnace operation. Developments in raw material selection, particularly the use of high-quality petroleum needle coke and optimised coal-tar pitch systems, have enabled substantial improvements in electrical conductivity, thermal-shock resistance, and mechanical strength (Ragan and Marsh, 1983; Pierson, 1993). At the process level, refinements in forming, baking, impregnation, rebaking, and graphitisation have improved product consistency, reduced defect rates, and lowered energy intensity, allowing manufacturers to meet increasingly stringent performance requirements (Jäger and Frohs, 2021; Baker, 2020).

Despite these advances, the graphite electrode industry faces a number of structural challenges. The availability of premium-grade needle coke remains constrained, exacerbated by competition with lithium-ion battery supply chains and by the geographic concentration of key processing capabilities. Tightening environmental regulations, the energy-intensive nature of thermal processing, and market volatility further influence electrode cost structures and supply security. At the same time, sustainability considerations are gaining prominence, with growing attention directed toward electrode recycling, life-cycle assessment, and the reduction of indirect emissions associated with electrode production and use.

Against this backdrop, a clear understanding of the relationship between manufacturing technology, material selection, and end-use performance is essential to guide both industrial practice and future innovation. While numerous studies have addressed individual aspects of graphite electrode production or EAF operation, a consolidated review linking advances in electrode manufacturing to furnace efficiency and sustainability outcomes remains limited.

This paper addresses that gap by reviewing the development and recent innovations in graphite electrode manufacturing, with particular emphasis on raw materials, process technologies, and design improvements relevant to modern EAF operation. The implications of these advances for electrode performance, consumption, and environmental impact are examined, and emerging trends in energy efficiency, recycling, and digitalisation are discussed. The analysis is intended to support informed decision-making in electrode selection, plant design, and technology development as EAF-based metallurgy continues to expand in a decarbonising industrial landscape.

### **Classification and Performance Requirements of Graphite Electrodes**

Graphite electrodes used in EAF steelmaking are classified according to their ability to sustain increasing current densities, thermal loads, and mechanical stresses. The commonly adopted categories, Regular Power (RP), High Power (HP), Super High Power (SHP), and Ultra-High Power (UHP), reflect progressive improvements in raw material selection, processing intensity, and structural integrity, and are directly linked to furnace operating practice and productivity requirements.

RP electrodes are primarily applied in low-power EAFs and ladle furnaces, where electrical loading and thermal gradients are moderate and cost efficiency is prioritised. HP electrodes support higher current densities and improved thermal-shock resistance, enabling their use in medium-power furnaces with increased power-on times. SHP and UHP electrodes represent the highest performance classes and are specifically engineered for modern high-capacity EAFs operating under ultra-high-power conditions.



UHP electrodes routinely sustain current densities exceeding approximately  $30 \text{ A cm}^{-2}$  and are essential for achieving high melting rates, short tap-to-tap times, and stable furnace operation in large-scale steelmaking facilities (Pierson, 1993; Ekinaga, 1992).

The increasing global shift towards UHP furnace operation has placed stringent demands on electrode performance, driving continuous development in material quality, joint design, and manufacturing practice. As a result, the classification of electrodes is not merely descriptive but serves as a framework linking manufacturing technology to operational efficiency and sustainability outcomes in EAF steelmaking.

Beyond AC power ratings, the emergence of high-power DC furnaces introduced unique demands on electrode columns. Seminal work by Sven-Einar Stenkvisst and Ben Bowman in the development of DC furnace technology highlighted the necessity for electrodes capable of sustaining extreme vertical thermal gradients and the mechanical stresses inherent in single-electrode configurations. Their contributions facilitated the transition to high-capacity smelting and specialised electrode designs, such as hollow graphite electrodes for direct material injection.

### Physical and Chemical Properties Governing Electrode Performance

The performance of graphite electrodes in EAF service is governed by a combination of physical and chemical properties that evolve systematically from RP to UHP grades. Key properties include apparent density, electrical resistivity, thermal conductivity, coefficient of thermal expansion (CTE), mechanical strength, and oxidation resistance, all of which are closely related to electrode microstructure, porosity distribution, and crystallographic order (Pierson, 1993; Bershetskii *et al.*, 2008).

As indicated in Table I, oxidation resistance improves markedly from RP to UHP grades, underscoring the importance of material quality and grade selection for electrode lifetime and furnace efficiency.

Table I. Key Physical & Chemical Properties by Electrode Grade

| Property                                         | RP       | HP        | SHP       | UHP       |
|--------------------------------------------------|----------|-----------|-----------|-----------|
| Density ( $\text{g/cm}^3$ )                      | ~1.55    | 1.60–1.65 | 1.65–1.70 | >1.70     |
| Electrical Resistivity ( $\mu\Omega \text{ m}$ ) | ~15      | ~10       | ~8        | <8        |
| Thermal Conductivity ( $\text{W/m K}$ )          | 100–120  | 120–140   | 130–145   | 130–150   |
| CTE ( $10^{-6} / \text{K}$ )                     | 2–3      | 2–3       | 1.8–2.5   | 1.5–2.0   |
| Tensile Strength (MPa)                           | ~10–15   | 15–25     | 20–30     | $\geq 25$ |
| Oxidation Rate (mass loss)                       | moderate | lower     | low       | lowest    |

Apparent density increases progressively with electrode grade, typically ranging from approximately  $1.55 \text{ g cm}^{-3}$  for RP electrodes to values exceeding  $1.70 \text{ g cm}^{-3}$  for UHP products. Higher density reflects improved packing efficiency and reduced open porosity, enhancing both current-carrying capacity and mechanical robustness. Concurrently, electrical resistivity decreases with increasing grade, falling from approximately  $15 \mu\Omega \text{ m}$  for RP electrodes to below  $10 \mu\Omega \text{ m}$  for UHP electrodes, a consequence of higher crystallinity, greater structural alignment, and lower impurity levels (Ragan and Marsh, 1983; Pierson, 1993).

Graphite electrodes exhibit high thermal conductivity, generally in the range of  $100\text{--}150 \text{ W m}^{-1} \text{ K}^{-1}$ , facilitating rapid heat dissipation during arc operation. This property, combined with the low and anisotropic CTE characteristic of well-graphitised materials, provides resistance to thermal shock under the severe temperature fluctuations typical of EAF operation. CTE values decrease from approximately  $2\text{--}3 \times 10^{-6} \text{ K}^{-1}$  for RP and HP electrodes to around  $1.5\text{--}2.0 \times 10^{-6} \text{ K}^{-1}$  for UHP electrodes, significantly improving resistance to cracking and structural failure (Ekinaga, 1993).

Chemically, graphite electrodes remain stable in the strongly reducing conditions prevalent within the furnace core; however, oxidation becomes significant above approximately 500°C, particularly along electrode sidewalls exposed to oxygen-bearing atmospheres. Oxidative mass loss is a dominant contributor to overall electrode consumption and is strongly influenced by surface area, pore connectivity, and impurity content (Pierson, 1993; Daneshmand and Heydari Vini, 2024).

### **Electrode Consumption Mechanisms and Operational Influences**

Electrode consumption during EAF operation results from a combination of continuous and discontinuous mechanisms that are strongly influenced by both material properties and furnace operating practice. Continuous consumption processes include tip erosion caused by intense arc exposure, sidewall oxidation resulting from high-temperature reactions with oxygen-containing gases, and sublimation under extreme thermal conditions. Tip sublimation and erosion are not merely functions of temperature but are deeply linked to arc stability. Bowman's (1969) analysis of the high-current arc provided the first rigorous engineering models for these mechanisms, demonstrating that electromagnetic forces and arc plasma behaviour are primary drivers of tip loss in UHP environments. Field studies generally indicate that sidewall oxidation and tip erosion dominate overall consumption, with oxidation accounting for up to approximately 35–40% of electrode mass loss in typical operations (Daneshmand and Heydari Vini, 2024).

Discontinuous losses arise from mechanical damage such as electrode breakage, joint failure, and butt loss during handling and replacement. These events are particularly sensitive to joint geometry, nipple design, column stability, and thermal-shock resistance (Ekinaga, 1992; Ekinaga, 1993). Improvements in joint length, thread geometry, and clamping systems have significantly reduced the frequency of catastrophic failures in modern UHP electrode columns.

Operational parameters exert a strong influence on both continuous and discontinuous consumption mechanisms. Arc stability, slag-foaming behaviour, oxygen lancing practice, charge composition, and cooling-system performance all affect local thermal and chemical conditions at the electrode surface. Variability in these operational factors complicates predictive modelling of electrode wear and underscores the importance of consistent electrode quality and robust furnace control strategies.

### **Raw Materials for Graphite Electrode Manufacturing**

#### ***Petroleum Needle Coke***

High-quality petroleum needle coke serves as the primary carbon filler in graphite-electrode manufacturing and is selected for its low coefficient of thermal expansion (CTE), high crystallinity, and pronounced anisotropic texture. These attributes collectively govern thermal-shock resistance, electrical conductivity, and mechanical stability during EAF operation (Pierson, 1993; Ragan & Marsh, 1983). Needle coke is produced via delayed coking of aromatic-rich petroleum feedstocks, during which mesophase formation and directional growth generate the characteristic needle-like microstructure required for high-power and ultra-high-power electrode applications.

Feedstock chemistry plays a decisive role in determining needle-coke quality. High aromaticity promotes well-ordered mesophase development, whereas elevated levels of heteroatoms (S, N, O) and metallic impurities such as vanadium and nickel disrupt microstructural alignment, increase puffing during graphitisation, and degrade final electrode performance. Optimised delayed-coking conditions, typically near 460°C under carefully controlled pressure, are therefore critical for achieving low-CTE needle coke suitable for UHP electrode production (Ragan & Marsh, 1983; Pierson, 1993).

In this context, Table II summarises established relationships between feedstock aromaticity, carbonisation severity, mesophase texture development, and resulting CTE trends, consistent with classical mesophase theory and modern needle-coke studies (Marsh & Diez, 1996; Mochida *et al.*, 2000); Gabdulkhakov *et al.*, 2021). Recent investigations further demonstrate that polymeric mesogen additives can enhance mesophase alignment and electrical conductivity, enabling incremental performance gains while moderating calcination severity.

Table II. Effect of Feedstock Type and Carbonisation Severity on Mesophase Texture and Resulting CTE in Needle Coke

| Feedstock Type          | Carbonisation Severity*        | Mesophase Texture Quality   | Resulting CTE Trend | Key Observation                                                                              |
|-------------------------|--------------------------------|-----------------------------|---------------------|----------------------------------------------------------------------------------------------|
| FCC Decant Oil (FCC-DO) | Moderate T, low-moderate P     | Well-oriented, flow-type    | Low                 | Benchmark feedstock for premium needle coke due to high aromaticity and low impurity content |
| FCC-DO                  | High temperature, low pressure | Randomised / degraded       | Increased           | Over-carbonisation disrupts mesophase alignment and increases CTE                            |
| FCC-DO                  | Moderate T, elevated pressure  | Refined, more uniform       | Low-moderate        | Elevated pressure suppresses mosaic coke formation                                           |
| LSVR (alone)            | Typical conditions             | Poorly developed, isotropic | High                | High asphaltene and sulfur content limits mesophase development                              |
| FCC-DO + LSVR blend     | Controlled T and/or P          | Improved vs LSVR alone      | Moderate            | Blending balances aromaticity and asphaltenes, improving mes                                 |

#### ***Binder and Impregnating Pitch***

Coal tar pitch functions as both a binder during forming and an impregnant during densification, and its properties exert a strong influence on electrode density, electrical resistivity, and mechanical strength. Key quality parameters include quinoline insoluble (QI) content, softening point, viscosity, molecular-weight distribution, and coking value (Pierson, 1993).

Low-QI impregnating pitches ( $\leq \sim 3$  wt %) with high coking values ( $\geq 40$ –50%) facilitate deep penetration into fine porosity and minimise filter-cake formation, yielding substantial gains in bulk density and conductivity following rebaking and graphitisation (CSIR-NPL, 2019; Cho and Im, 2022). High-softening-point pitches, typically in the range of 180–220°C, combined with vacuum-pressure impregnation at pressures of approximately 40–50 bar, enable stepwise improvements in mechanical integrity and service life, making pitch quality a critical lever in modern electrode manufacturing.

#### **Manufacturing Process of Graphite Electrodes**

Graphite electrode manufacturing comprises a sequence of well-established unit operations that have remained conceptually consistent since the early adoption of EAF steelmaking, while benefiting from continuous optimisation and technological refinement (Ragan and Marsh, 1983; Pierson, 1993). The process begins with milling and classification of calcined needle coke to produce a controlled particle-size distribution, which is essential for achieving uniform packing density and mechanical stability in the formed electrode body (Jäger and Frohs, 2021).

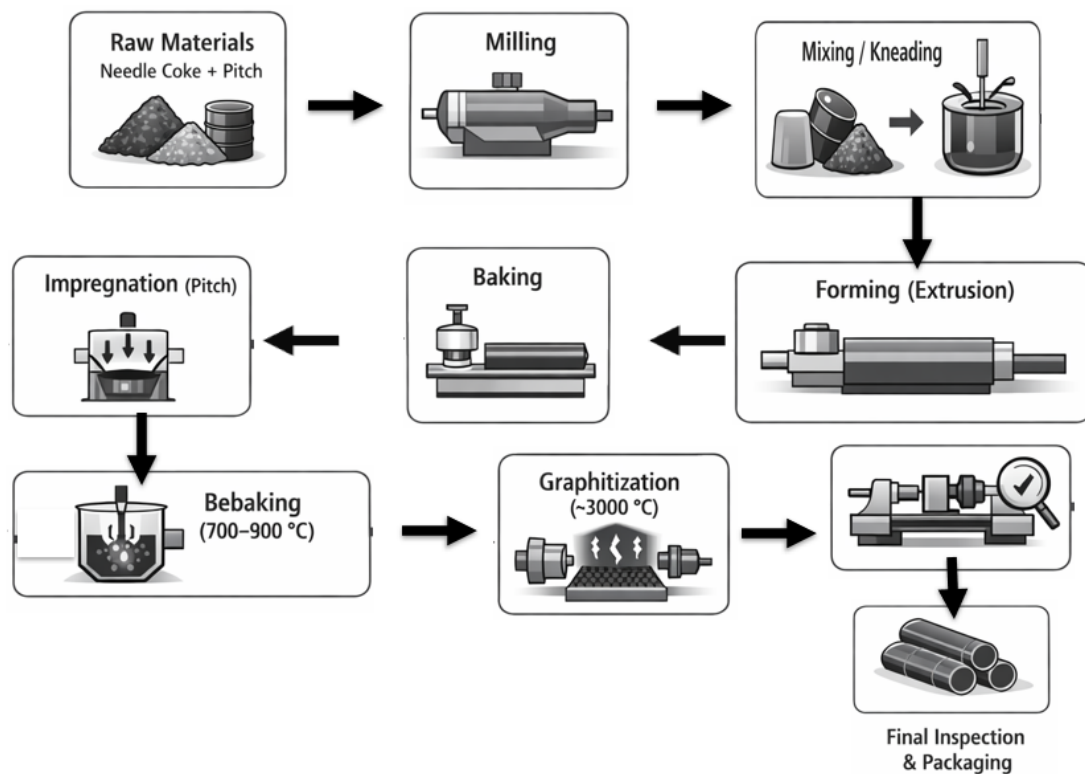


Figure 1. Typical Block Flow Diagram for graphite electrode manufacturing.

The classified coke fractions are blended with coal tar pitch to form a homogeneous paste, which is shaped into green electrodes by extrusion or, in some cases, isostatic pressing. Vacuum or vibration assistance is commonly employed during forming to reduce entrapped porosity and improve green-body uniformity (Baker, 2020). The green electrodes are then subjected to primary baking at temperatures of approximately 900–1000°C in ring or chamber furnaces, during which the binder pitch carbonises to form a rigid carbon matrix, typically protected by coke bedding to prevent oxidation (Pierson, 1993).

To further enhance density and strength, baked electrodes undergo one or more impregnation cycles in which molten pitch is forced into residual open porosity under controlled vacuum and pressure conditions, followed by rebaking at approximately 700–800°C to carbonise the impregnated pitch (Marsh and Rodríguez-Reinoso, 2006; Jäger and Frohs, 2021). The final and most energy-intensive step is graphitisation, in which electrodes are heated to approximately 2800–3000°C in electric furnaces, transforming amorphous carbon into crystalline graphite and imparting the high electrical and thermal conductivity required for EAF operation (Pierson, 1993). After graphitisation, electrodes are machined to final dimensions, threaded for nipple connections, and subjected to quality-control testing prior to shipment (Baker, 2020).

### Electrode Structure and Joint Design Considerations

Graphite electrodes used in electric arc furnaces consist of three primary structural elements: the electrode body, the nipple, and the joint interface. Together, these components must ensure reliable electrical continuity, mechanical stability, and thermal resilience under the extreme conditions of EAF operation. Failures at the joint remain a dominant cause of discontinuous electrode loss, making structural design a critical contributor to both electrode consumption and furnace reliability (Ekinaga, 1992; Ekinaga, 1993).

The electrode body is a cylindrical, graphitised component manufactured from petroleum needle coke and coal tar pitch, designed to conduct high electrical currents while maintaining dimensional stability

under repeated thermal cycling. As electrodes are progressively consumed during melting, continuity is maintained by mechanically joining multiple bodies using threaded nipples. These nipples are typically machined from high-density graphite and incorporate tapered threads conforming to established standards such as those defined by the Japanese Industrial Standards (JIS), which specify thread pitch, taper angle, and permissible tolerances (Pierson, 1993).

The mechanical behaviour of the electrode joint is strongly influenced by thread geometry and nipple design. Finite-element stress analyses have demonstrated that conventional JIS thread profiles exhibit pronounced stress concentrations near the maximum thread diameter, particularly under bending loads associated with scrap collapse and asymmetric arc forces (Ekinaga, 1992). Modifications to joint geometry, specifically reductions in thread height and taper in the high-stress region, have been shown to redistribute stress more uniformly along the nipple length without compromising electrical contact area.

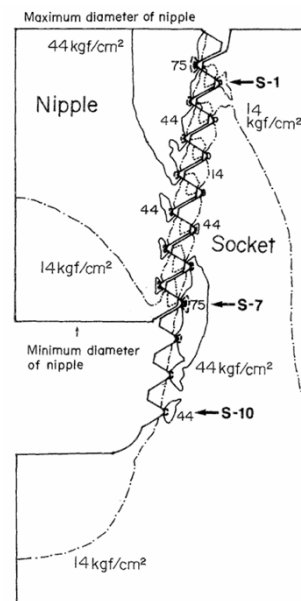


Figure 2. Contour lines of  $\sigma_z$  (axial stress component (normal stress along the electrode axis) in the case of an 8-thread nipple.

Experimental fracture tests corroborate these findings, indicating that nipples with reduced thread height (e.g.,  $L \approx 2$  mm relative to standard designs) exhibit higher bending strength and fracture resistance than unmodified JIS geometries (Ekinaga, 1993).



Figure 3. Graphite electrodes with nipples (left; assembly diagram of graphite electrode nipple (right).

These results highlight that joint optimisation represents a comparatively low-cost and high-leverage pathway to improving electrode reliability, particularly in modern ultra-high-power EAFs where mechanical loading and thermal gradients are severe. Optimised joint designs reduce the incidence of breakage and butt loss events, thereby lowering unproductive electrode consumption and improving operational stability.

#### ***Advances in Electrode Holders and Clamping Systems***

Parallel to improvements in electrode and joint design, significant advances have been made in electrode holder technology. Modern modular electrode holders integrate current-carrying sleeves with segmented, replaceable subassemblies to improve clamping reliability, reduce contact resistance, and enhance thermal management. Water-cooled copper or bimetallic (Cu-steel) sleeves are now widely adopted in large EAFs operating at currents of several tens of kiloamperes, where thermal and mechanical loads at the clamp interface are severe (Bershitskii *et al.*, 2008).

Operational analyses demonstrate that water-cooled holder designs lower phase resistance, shorten unproductive short-circuit events, and stabilise mechanical performance under dynamic loading. From a process standpoint, effective cooling at the clamp interface mitigates localised oxidation and thermal degradation of graphite at the contact zone, contributing to reduced electrode wear and improved arc stability. Collectively, advances in holder and joint design complement material- and process-level improvements in electrode manufacturing, reinforcing gains in furnace efficiency, safety, and energy utilisation.

#### ***Advances in Graphite Electrode Manufacturing Practice***

Recent advances in graphite electrode manufacturing have focused on improving energy efficiency, product consistency, and durability to meet the demands of modern UHP EAF operation. Baking and rebaking processes dominate overall energy consumption, and the adoption of modern multi-chamber furnaces with improved insulation, automated temperature control, and optimised firing strategies has significantly reduced fuel use, thermal gradients, and defect rates during extended baking cycles of approximately 17–30 days (Porsev and Malozemov, 2023). Emerging heat-recovery concepts applied to rebake furnaces offer additional reductions in energy intensity without compromising carbonisation quality.

Advances in impregnation technology, particularly the widespread use of multi-cycle vacuum-pressure impregnation, have yielded stepwise improvements in electrode density and mechanical strength. Pore-scale studies confirm effective filling of fine porosity under pressures of approximately 40–50 bar,

translating directly into longer electrode service life and lower specific consumption during EAF operation (Marsh and Rodríguez-Reinoso, 2006).

Graphitisation technology has also evolved beyond conventional Acheson-type furnaces. Lengthwise graphitisation (LWG) configurations provide shorter heating cycles, reduced specific power consumption, and tighter resistivity distributions, advantages that are particularly significant for large-diameter UHP electrodes used in high-productivity furnaces (COM 2023 Proceedings).

In parallel, increasing integration of automation, advanced monitoring, and data-driven control systems has enabled tighter thermal-profile control during baking and graphitization, reducing variability in final electrode properties and improving plant yield (Bershitskii *et al.*, 2008). Measures to mitigate oxidation, including ceramic anti-oxidation coatings and optimised joint and holder designs, further complement material- and process-level improvements, addressing a dominant electrode consumption mechanism and reinforcing gains in operational efficiency (Daneshmand and Heydari Vini, 2024; Ekinaga, 1992).

Table III. Key Process and Design Advances in Graphite Electrodes and Their Measurable Performance Impacts

| Advance                                         | Key Mechanism                                                         | Measurable Outcomes                                                | Source                              |
|-------------------------------------------------|-----------------------------------------------------------------------|--------------------------------------------------------------------|-------------------------------------|
| Modular, water-cooled holders                   | Lower phase/contact resistance; improved thermal management           | ↓ electrical losses; ↑ clamp reliability; ↓ oxidation at contact   | Bershitskii <i>et al.</i> , (2008)  |
| Optimised joint (nipple/socket) geometry        | Reduced stress concentration via thread taper and height optimisation | ↑ bending strength; ↓ joint failures and breakage                  | Ekinaga (1992); Ekinaga (1993)      |
| High-grade petroleum needle coke                | Low CTE, high anisotropy; reduced impurity content                    | ↓ electrical resistivity; ↑ mechanical integrity in UHP electrodes | Gabdulkhakov <i>et al.</i> , (2021) |
| Engineered impregnating pitch (low QI, high CV) | Enhanced pore filling and densification efficiency                    | ↑ density and strength; ↓ open porosity; ↑ conductivity            | CSIR-NPL (2019); Cho & Im (2022)    |
| Energy-efficient baking / rebaking              | Improved insulation, automation, and heat-recovery integration        | ↓ fuel and power consumption; ↓ defects; uniform carbonisation     | Porsev & Malozemov (2023)           |
| Lengthwise graphitization (LWG)                 | Resistive strand heating with uniform temperature profiles            | ↓ specific energy; ↓ cycle time; tighter resistivity distribution  | COM 2023 Proceedings                |
| Anti-oxidation coatings                         | Ceramic multilayers applied to electrode sidewalls                    | ↓ oxidation wear; ↓ electrode consumption                          | Daneshmand & Heydari Vini (2024)    |

### Sustainability and Environmental Impact

Graphite electrode manufacturing is characterised by a high energy intensity concentrated in three unit operations: calcination, baking/rebaking, and graphitisation. Multiple studies identify these steps as dominant contributors to electricity and natural-gas demand and, consequently, to cradle-to-gate carbon intensity per tonne of finished electrodes. Improvements in furnace insulation, temperature measurement, and heating control measurably improve furnace energy balance and reduce losses (Bershitskii *et al.*, 2008); Porsev & Malozemov, 2023].

Lengthwise graphitisation (LWG) consistently outperforms conventional Acheson beds in specific energy consumption and product uniformity. Reduced heating duration and narrower temperature gradients translate into lower kWh t<sup>-1</sup> and tighter property distributions, which in turn improve machining yield and joint consistency, lowering embodied emissions per usable electrode.

### Comparative Analysis of Commercial Electrodes

Electrode performance across RP, HP, and UHP grades correlates strongly with needle-coke anisotropy and impurity levels, binder quality (QI, CV, viscosity), impregnation schedules, and graphitisation technology. Petroleum needle cokes with high anisotropy and low heteroatom content benchmark higher in property uniformity and service life per heat (Gabdulkhakov *et al.*, 2021).

Vacuum-pressure impregnation at 40–50 bar captures fine porosity down to 24–39 nm. Multi-cycle impregnation at ~200°C delivers stepwise gains in density and porosity reduction, key determinants of consumption rate and arc stability (Kim *et al.*, 2022; Youn *et al.*, 2024). LWG graphitisation further reduces resistivity scatter, improving machining yield and joint consistency. Field case studies confirm these trends. Oxidation-resistant coatings applied to 24-inch electrodes achieve 25–30% reductions in consumption, with reported improvements in arc stability and reduced penciling; automated application enhances repeatability (AIST Steel News, 2023; Daneshmand & Heydari Vini, 2024).

Electrode-holder modernisation, particularly water-cooled, current-conducting sleeves, reduces contact resistance, mitigates thermo-mechanical damage, and lowers short-circuit frequency, improving power delivery consistency at high current levels (Bershitskii *et al.*, 2008).

Premium feedstocks and binders carry higher upfront cost but consistently deliver longer service life, reduced kWh t<sup>-1</sup> steel, and fewer breakage events. Lower-grade materials correlate with higher oxidation losses and joint failures, imposing hidden downtime and quality costs (CSIR-NPL; Gabdulkhakov *et al.*, 2021).

High-density UHP electrodes with low resistivity enable higher current densities and shorter tap-to-tap times without elevated fracture risk. Coatings and optimised joints stabilise mechanical integrity across variable scrap mixes, sustaining furnace productivity ([Hidnert & Sweeney, 1927; Ekinaga, 1992/1993; Daneshmand & Heydari Vini, 2024).

### Challenges and Research Gaps

The global supply of premium needle coke remains sensitive to refinery feedstocks and economics. Mesogen-assisted development routes, including polymer-modified decant oil, improve microstructural indices and reduce CTE; however, scale-up consistency and batch variability remain unresolved risks (Gabdulkhakov *et al.*, 2021). Binder availability presents similar challenges. Coal-tar pitch quality is strongly governed by QI content, coking value, and viscosity, which control impregnation efficiency and final electrode density (Pierson, 1993; Soltani Hosseini & Chartrand, 2022). Oxidation above approximately 500–600°C becomes a significant contributor to electrode consumption, particularly along sidewalls exposed to oxygen-bearing atmospheres. Electrode consumption is governed by the combined effects of sidewall oxidation and tip erosion. Ceramic multilayer coatings and densification via impregnation provide effective mitigation; however, coating durability, uniformity, and cost scalability remain unresolved for widespread.

Performance metrics across suppliers remain insufficiently standardised. Resistivity, density, oxidation rate, and mechanical strength are frequently measured under non-uniform conditions. The field would benefit from harmonised protocols defining test temperature, atmosphere, specimen geometry, and duration, analogous to existing pitch viscosity standards.

Finally, integration of electrode condition monitoring into smart EAF platforms remains incomplete. Linking electrode wear, joint temperature, and holder contact resistance to EAF control logic offers significant potential to reduce unproductive events and improve energy efficiency (Bershitskii *et al.*, 2008; Soltani Hosseini & Chartrand, 2022).

### Future Directions and Conclusions

Future progress in graphite electrode technology will depend on coordinated advances in materials, process control, and digital integration. Nanocarbon-modified binder pitches (0.5–2 wt% PCNF or carbon black) have demonstrated increases in carbonisation yield (5–13%), reduced pore size, and



concurrent improvements in density, tensile strength, and oxidation resistance, indicating that modest nanoparticle additions can refine matrix structure without compromising rheology (Ono *et al.*, 2024). Broader reviews forecast graphene- and CNT-assisted pathways for defect-tolerant graphitisation, contingent on QI suppression and viscosity control (Liu & Zhu, 2023).

Digital-twin frameworks integrating recipe parameters, impregnation cycles, and thermal profiles enable prediction of volatile evolution, energy demand, and defect risk. Adaptive firing schedules guided by such models reduce over- and under-processing. Extension to electrode holders and joints enables predictive maintenance targeting consumption and breakage modes (Soltani Hosseini & Chartrand, 2022; Bershetskii *et al.*, 2008).

Circular-economy strategies combining consumption reduction, refurbishment, reuse of stubs, and secondary markets such as EDM graphite demonstrate technical feasibility. Quality-assurance systems and traceability remain key enablers for large-scale adoption. Figure 3 illustrates that meaningful reductions in electrode consumption and concurrent efficiency gains are achieved through combined advances in electrode quality, surface treatments, and optimised EAF operating practices. These results underscore the importance of integrated materials, process, and digital strategies for future performance and sustainability gains.

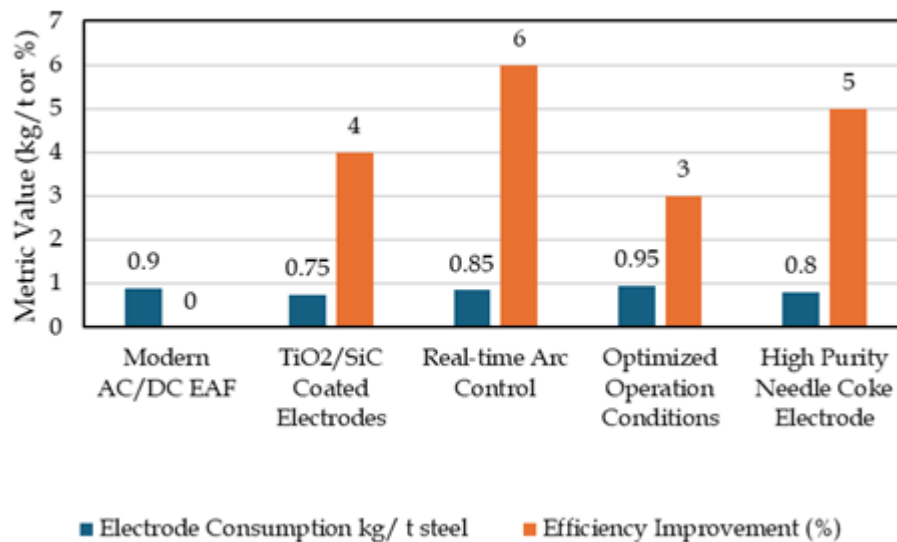


Figure 4. Comparison of Electrode Consumption and Efficiency Improvement for Different EAF Electrode Technologies and Operating Strategies.

In conclusion, sustainability and performance gains in graphite electrodes hinge on reducing process energy via LWG and heat-recovery, selecting high-quality needle coke and binders, institutionalising multi-cycle impregnation, and deploying oxidation-resistant coatings and optimised joint geometries. Modern electrode holders and digital tools further enhance furnace stability, safety, and productivity. Coordinated R&D, standardised metrics, and ESG-aligned procurement will be critical to sustaining progress and competitiveness in EAF steelmaking (Porsev & Malozemov, 2023; Kim *et al.*, 2022; Daneshmand & Heydari Vini, 2024).

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# Silicon production through aluminothermic reduction of solar photovoltaic glass

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Glass from solar panels typically becomes waste after they are decommissioned. In this paper, we explore producing a silicon alloy from this glass using aluminium as the reductant. For the aluminium source, we compared scrap Al with two fractions of dross, a byproduct of aluminium production containing alumina as well as metallic aluminium. As a proof of concept, we heated glass and scrap/dross to 1500°C for 60 minutes together with CaO as a flux in a graphite crucible. We characterised the resulting alloy and slag with electron probe microanalysis. Experiments consistently yielded alloys with a silicon content of >95wt%. Both dross fractions and the scrap produced similar metal phases, indicating that this process can effectively make use of recovered solar glass and lower grade aluminium streams to produce high grade silicon.

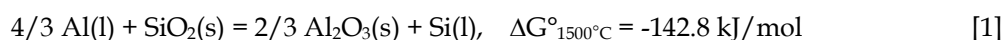
**Keywords:** Aluminothermic reduction, silicon production, solar PV glass

## INTRODUCTION

Si-based solar photovoltaic (PV) panels have a limited lifespan and therefore need to be decommissioned and recycled after typically 20 years of service. As a fast-growing energy source, it is expected that the amount of decommissioned solar panels will increase significantly in the coming years (Shrestha, 2024). A significant part of the mass of the solar panel (>70%) is the glass panel which protects the silicon wafer and mainly consists of SiO<sub>2</sub>, Na<sub>2</sub>O, and CaO. EUs WEEE recycling legislation (EU, 2012) requires >80% of the mass of the panel to be recycled, making recovery of this glass essential.

Solar panel glass is made to protect the wafer without hindering its efficiency. This puts requirements on its optical properties, which in turn dictates the glass chemistry. Impurities that cause absorption of sunlight are strictly controlled, in particular Fe<sub>2</sub>O<sub>3</sub> content is generally required to be below 150 ppm (Karasu *et al.*, 2020). Furthermore, some producers add antimony oxide to act as fining agent by forming larger bubbles at high temperatures that sweep smaller bubbles of CO<sub>2</sub> leading to clearer glass (Cai *et al.*, 2015). Due to its toxicity, this addition may inhibit conventional recycling of the glass, however with the large scale of solar installations, it is also feasible to keep this glass separate from other glass waste when decommissioning and use it for specialised applications where its antimony content is not problematic. The high silica, single source, and low impurity nature of crushed solar glass make it an interesting SiO<sub>2</sub> raw material for Si production. The current preliminary study hence aims to investigate the viability of producing silicon from waste PV glass, using non-carbon reductants.

As an alternative to carbothermic production of silicon, metallothermic reduction of silica in CaO-SiO<sub>2</sub>-based slags, using secondary aluminium sources such as scrap and dross (a byproduct of Al production), has been successfully demonstrated in large scale (Zhu *et al.*, 2024). The overall reaction of the process can be seen below. In such a process a slag phase and a metal alloy would be formed.



The relative ratio of Al added to the SiO<sub>2</sub> in slag will determine the distribution between both Si and Al in slag and alloy. Elements with a higher affinity for oxygen than Si, such as Ca and Mg will largely report to the slag, while elements with a lower affinity for oxygen such as Fe or Sb would end up in the metal phase. The low activity of Na<sub>2</sub>O in aluminosilicate slags (Rego *et al.*, 1988) predicts Na will remain in the slag despite having a lower oxygen affinity than Si. The more volatile elements such as Na and Sb might have non-negligible evaporation rates, and their behaviour cannot easily be predicted by thermodynamics. Experiments for CaO-SiO<sub>2</sub>-Al<sub>2</sub>O<sub>3</sub> slags show that while there is significant Na evaporation, a notable fraction can remain in the slag after being held at 1500°C for 7000s (Li *et al.*, 2015). At 1500°C, Sb has a vapour pressure 4-5 orders of magnitude higher than Si or Fe, and it has an activity coefficient of 4.9 in dilute Si metal solutions at 1414°C (Safarian *et al.*, 2012). While this indicates that selective evaporation of Sb is possible, the amount of evaporated Sb is difficult to predict as this is dependent on the evaporation kinetics of the metal.

## MATERIALS AND METHOD

The glass in these experiments came from end-of-life solar panels recovered by a European recycling plant after removing the Si wafers and polymer (EVA) encapsulation. Its composition was measured with inductively coupled plasma mass spectroscopy (ICP-MS) by Australian Laboratory Services (ALS). ICP-MS cannot measure light elements such as oxygen, so the metals were assumed to be in their most stable oxide forms. This accounted for 112% of the mass of the sample, which was normalised to 100%. The measured amounts, their oxides, and the normalised amounts can be seen in Table I.

Table I. Composition of the solar glass. Measurements can be less precise at high concentrations, so the normalised amounts are rounded

| Element | Measured (wt%) | Assumed oxide     | Calculated amount (wt%) | Normalised amount (wt%) |
|---------|----------------|-------------------|-------------------------|-------------------------|
| Si      | 40             | SiO <sub>2</sub>  | 85.57                   | 76                      |
| Na      | 10             | Na <sub>2</sub> O | 13.48                   | 12                      |
| Ca      | 6              | CaO               | 8.4                     | 8                       |
| Mg      | 2              | MgO               | 3.32                    | 3                       |
| Others* | 1.2            | Unknown           | 1.2**                   | 1**                     |
| Sum     |                |                   | 111.97                  | 100                     |

\*Main impurities include Sb, Mn, and Ag.

\*\*Oxygen content of impurities are not included in the calculations

White dross used as a reductant was supplied from Norsk Hydro Karmøy and was divided into size fractions by sieving. The 1.25-5 mm fraction and the 5-10 mm fraction was used for experiments. The metallic Al content of each fraction was measured by melting the dross together with salt-flux and separating/weighing the resulting phases. The metallic content is shown in Table II, with the rest being primarily Al<sub>2</sub>O<sub>3</sub> and small amounts of impurities such as Fe or V, carbides, nitrides and electrolysis bath compounds. Al scrap from cable was also used as a reference and is expected to contain 99-99.99wt% Al, with the main impurities P, S, Si, and Ca being below 0.1wt% (Kwaha *et al.*, 2011).

Table II. Amount of selected size fraction and the content of metallic Al in each size fraction. Error of metallic content is based off three parallel experiments

| Size fraction (mm) | Fraction Amount (wt%) | Metallic Al in fraction (wt%) |
|--------------------|-----------------------|-------------------------------|
| 0-1.25             | 17.7                  | 11.7 ± 1.7                    |
| 1.25-5             | 17.4                  | 56.3 ± 2.7                    |
| 5-10               | 11.1                  | 70.4 ± 1.5                    |
| 10-30              | 20.2                  | 77.6 ± 3.8                    |
| >30                | 33.6                  | Not measured                  |

Table III. Material charge of the six conducted experiments

| Experiment | Glass (g) | Scrap Al (g) | Dross 1.25-5 mm (g) | Dross 5-10mm (g) | Uncalcinated Lime (g) |
|------------|-----------|--------------|---------------------|------------------|-----------------------|
| 1,2        | 2000      | 455          | 0                   | 0                | 592                   |
| 3,4        | 2000      | 0            | 808                 | 0                | 939                   |
| 5,6        | 2000      | 0            | 0                   | 646              | 780                   |

### Procedure

We conducted the experiments in an open induction furnace. An alumina sheathed C-type thermocouple measured the temperature, while a computer logged the temperature over time. We first calcinated the lime at 950°C, then mixed the materials before filling the crucible. We increased the temperature gradually over 10-15 minutes to 1500°C and held it there for 60 minutes. After the holding time, we cast the slag and metal from the crucible onto a graphite mould and let it cool. We then extracted metal and slag samples, which were mounted into epoxy and polished. We analysed these samples with electron probe microanalysis (EPMA): six points in the slag with dispersive wavelength X-ray spectrometry (WDS), and on the metal samples, we ran energy dispersive X-ray spectroscopy (EDS). WDS is more reliable for light elements but relies on a sampling strategy (points). This is acceptable for a relatively homogenous single-phase material like the slag, but the crystalline multiphase metal requires an area scan (EDS) for reliable composition measurements.

The reason for the difference in technique is that the slag is largely an amorphous single-phase material where individual points give reasonable indication of the composition, while the metal is a multiphase crystalline solid where individual points can vary significantly, thus requiring a measurement over an area, such as EDS.

### Thermodynamic simulations

We ran thermodynamic simulations of the equilibrium composition of the experiments using FactSage 8.3 with FTLite and FToxid database for the metal and slag phases. For this model we assumed the reaction with the carbon crucible was negligible due to the slow kinetics of low porosity isostatic graphite and the possible formation of a passivating carbide layer. We modelled the gas phase as a closed isobaric system. This reduced the model down to a thermodynamic one, as a full kinetic model of the system was outside the scope of this project.

## RESULTS AND DISCUSSION

Because of a similar density and viscosity of the alloy and the slag, casting them separately was unreliable. From approximately 3000 g of slag, we were able to separate out 130-311 g of alloy depending on the casting. Compare this to the theoretical amount of Si metal, which would be 355 g assuming that all Al reacts according to equation 1. This indicates that a high Al utilisation is possible, but ensuring the alloy is separate from the slag remains a challenge.

### Alloy Composition

Figure 1 shows that all six experiments produced a metal phase consisting of 95-100% Si, with trace

amounts of other elements. For all three Al sources, the two parallels produced very similar alloys, where the differences are smaller than the margin of error, demonstrating that the results and the process are accurate and reproducible. We also see that the produced metal is similar for all Al sources, indicating that this process makes equally good use of the Al in scrap as the metallic content in the dross.

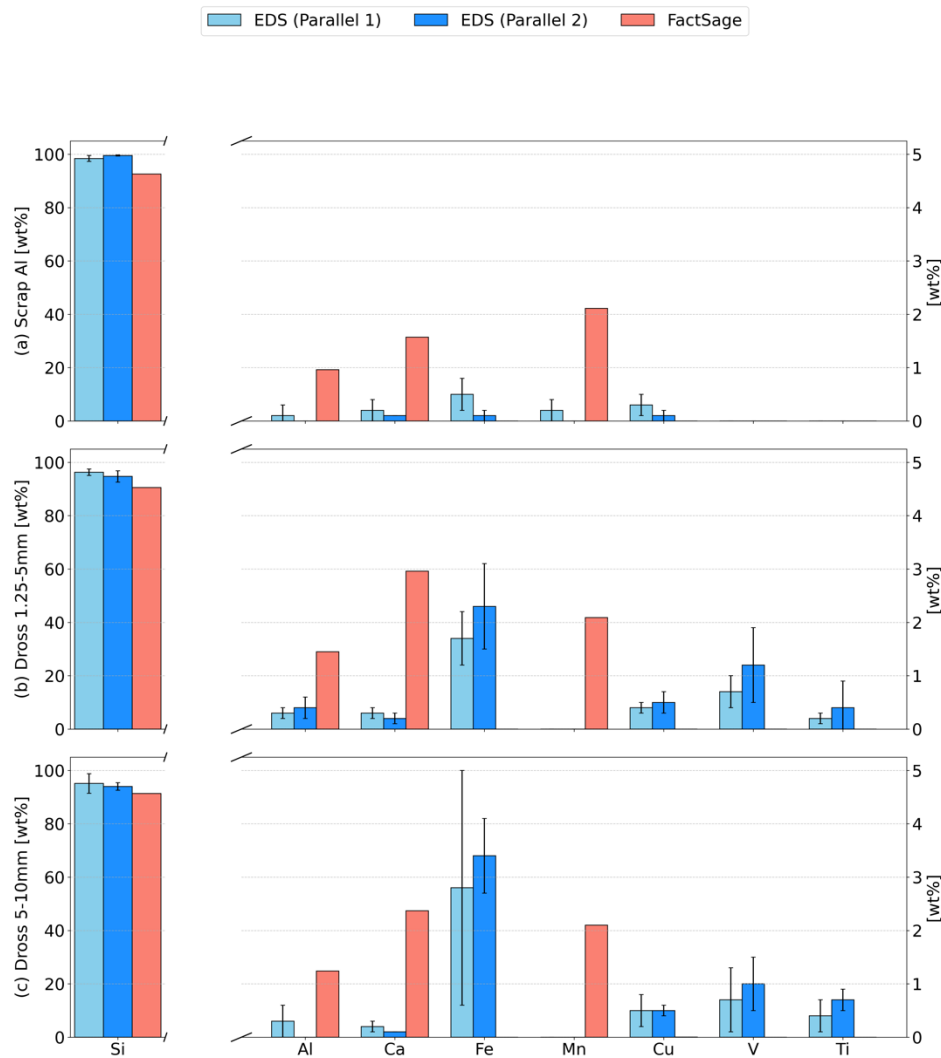


Figure 1. Composition of metal phase as measured by EDS (in blue) and the FactSage prediction (in red). Two parallels are shown as different columns, and the three aluminium sources are shown above each other. The error bars represent one standard deviation of the measured composition. Fe, Cu, V, and Ti are not included in the FactSage simulations, as the concentration of these impurities in the dross is unknown.

The amount of trace elements seems to be different in the experiments with dross compared to those done with scrap. There is a statistically significant amount of Fe in the experiments carried out with dross that accounts for 2.3% of the metal phase. We also see V and Ti in the dross experiments, while the scrap experiments have amounts of these elements below the EPMA detection limit of 0.1%.

The FactSage simulations predict a less pure Si alloy phase and higher amounts of impurities like Al, Ca, and Mn. Particularly for Ca, it has previously been noted that the activity coefficient used for Ca in Si does not align with the experimentally determined coefficient (Ahn *et al.*, 2016)



Figure 2 shows how Si and trace elements are distributed in the alloy. The sample seems to be characterised by Si grains that have pushed most of the impurities to the grain boundaries as the Si crystals grew during solidification.

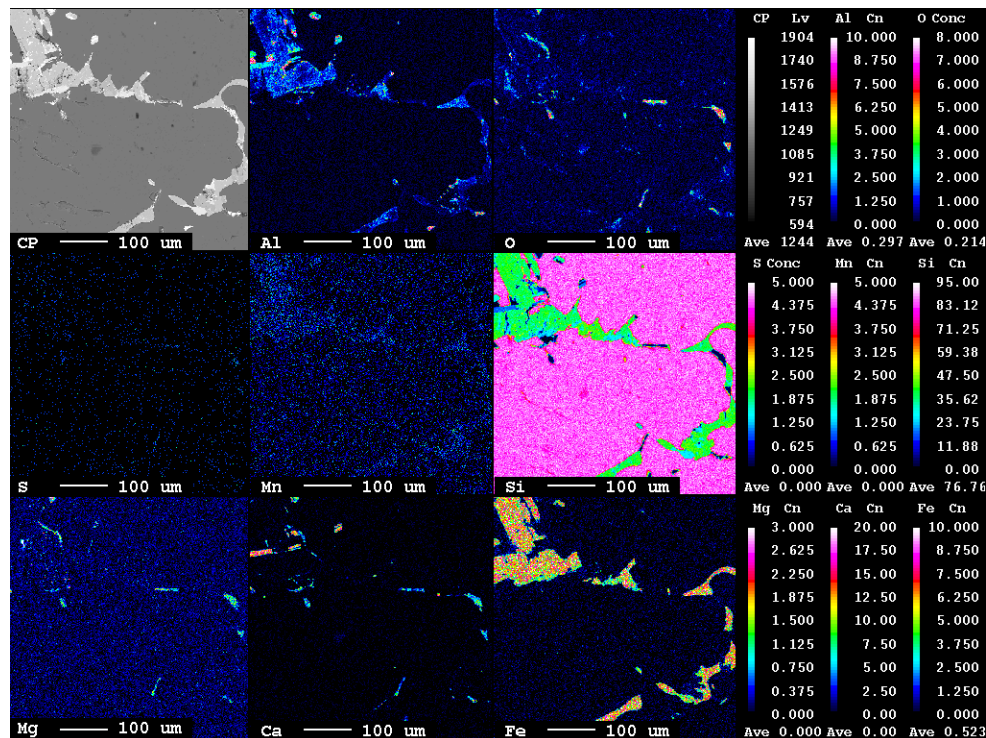


Figure 2. EPMA Elemental mapping of Si alloy for the parallel two of the dross 5-10 mm experiment. The mapping shows large dark regions of predominantly Si, while impurities such as Fe, Ca, Mg, and Al have a higher concentration in smaller regions and grain boundaries.

### Slag Composition

The corresponding slags are presented in Figure 3. We again observe very similar results between the two parallels for all three Al sources. All three are Al-Si-Ca slags, with small amounts of Na<sub>2</sub>O and MgO in addition to other trace components. The dross slags show a higher alumina and calcium oxide content, which aligns with the alumina content in the dross as well as the additional lime added.

The FactSage simulations agree with the experiments with the exceptions of Al<sub>2</sub>O<sub>3</sub>, where the simulations predict 20-25% less than what is observed, and Na<sub>2</sub>O, where the simulation predicts much less than what is observed. In the case of Na<sub>2</sub>O, this discrepancy is most likely caused by the difference between the real evaporation kinetics of an open system, which is how the experiments were ran, and the isobaric closed system that was simulated using FactSage. For Al<sub>2</sub>O<sub>3</sub>, the reason for the discrepancy is less clear. Future work including an open simulation and a mass balance of the system might provide more insight.

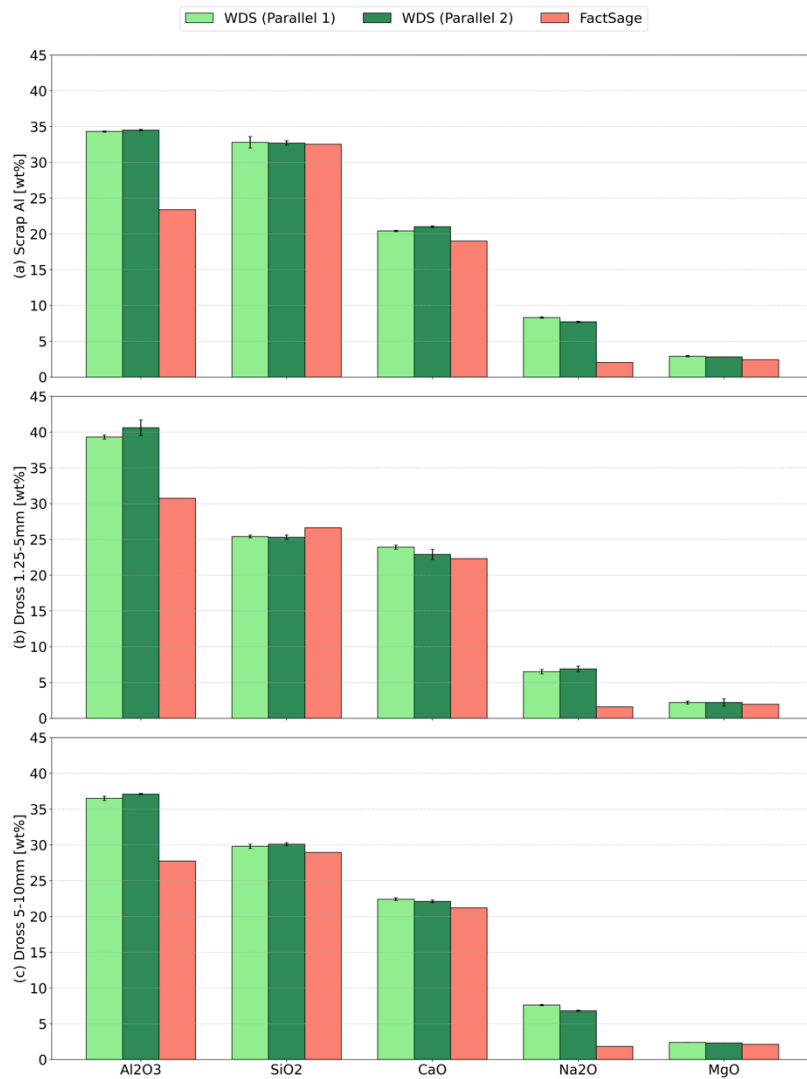


Figure 3. Composition of slag phase as measured by EDS (in blue), WDS (in green), and the FactSage prediction (in red). Two parallels are shown as different columns, and the three aluminium sources are shown above each other. The error bars represent one standard deviation of the measured composition.

Figure 4 shows BSE images of the slag samples. Some metal seems to be entrapped in the slag. The inclusions appears to be smallest in the case of the cable scrap and largest in the 1.25-5 mm dross fraction, where the 5-10 mm dross fraction has more numerous but smaller inclusions. While this goes against the general expectation of more viscous slags causing more entrapment, one possible explanation of the observation is that the higher viscosity, lower basicity slag (higher Si and lower Ca), from the cable scrap resists the turbulence in the metal slag interface caused by the aluminothermic reduction leading to less metal entrapment.

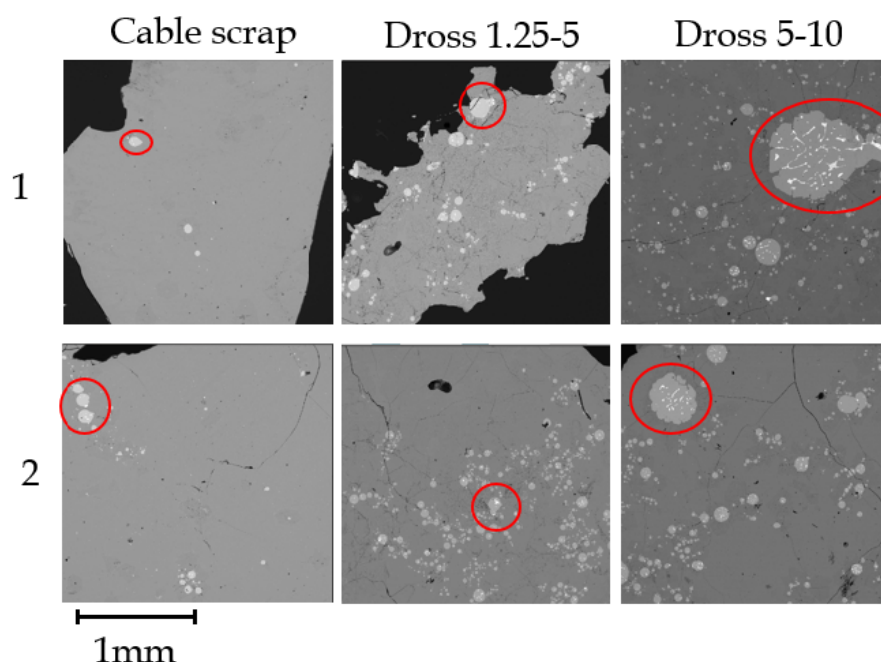


Figure 4. BSE images at 40x of six slag samples. Three Al sources and two parallels (1 and 2). Metallic inclusions are circled in red.

### Resource Efficiency

This process attempts to employ waste streams as alternatives to new raw materials in silicon production. The low Fe glass is leveraged to produce an alloy with >95% Si. By adding half the stoichiometric amount of metallic Al through dross, we ensure full utilisation of the metallic Al while retaining enough silica in the slag that it might be useful as a geopolymer, where the residual  $\text{Na}_2\text{O}$  from the glass would be an alkali activator. Mixing this geopolymer with Portland cement might be problematic due to the high Na content. Instead, this slag is an interesting starting material for a one-part geopolymer concrete, which could be an alternative to cement-based concrete, especially in applications requiring acid resistance.

This work also opens interesting avenues for further research that were outside the scope of this paper. The low concentration of Sb in the glass (approximately 1700 ppm) makes it difficult to establish how much evaporates during the current experiment; however the vapour pressure and activity of dilute Sb in Si alloys indicates that there might be significant evaporation. If this is the case, the process could be expanded to output three products: Sb-free alloy and slag, as well as an  $\text{Sb}_2\text{O}_3$  dust. This would lead to purer alloy and slag, in addition to the new valuable product. From a recycling perspective, this would also be the ideal step to recover valuable Sb.

### CONCLUSION

This work shows the feasibility and reproducibility at laboratory scale of producing >95% Si alloys from solar PV glass by aluminothermic reduction. This proof of concept builds upon previous research by demonstrating that a dross-based aluminothermic process works on solar PV glass as a Si source. It also shows that it is technically feasible to produce Si alloys using minimal to no new raw materials.

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# Ferroalloys furnace lining management – guideline for operators to extend furnace lifetime

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Due to the existing market downturn of commodities and cost pressures on ferroalloys producers, extending refractory lining lifetime, postponing complete relines and preventing furnace run-outs are major economic and safety drivers. Furnaces are run at maximum capacity until they reach the end of lifetime. Furnace relines are not only costly but having multiple furnaces offline results in less production and directly impacts profitability. In this competitive, cost-driven market, the role of lining monitoring and management is becoming increasingly important. Lining management requires a comprehensive understanding of furnace design criteria, engineering principles and thermal modelling (heat transfer and heat flux calculations) of a particular furnace. Lining monitoring is so much more than just thermocouple readings. There is a direct correlation between refractory wear, sidewall and hearth cooling, operational and metallurgical factors, tap-hole management, maintenance and original furnace design criteria. Thermocouples are damaged by lining and shell movement, refractory wear and being subject to proximity of metal and slag. On older furnaces especially the accuracy and validity of the thermocouple reading can be questionable. The aim of this paper is to review all on-site methods operators can use to monitor and improve furnace lining lifetime in combination with establishing a basic understanding of furnace engineering principles.

## INTRODUCTION

In South Africa there have not been greenfield ferroalloys smelter projects for more than a decade. The operators on smelters today come on-site with very little prior knowledge of the engineering and design considerations that went into constructing these metallurgical vessels. Metallurgical engineers receive basic refractories training at university level, but not at a level to have an in-depth knowledge of refractory lining design principles. Even skilled operators have a basic understanding of thermocouple readings, alarms, water cooling requirements and furnace repairs to keep their furnace operational. The main goal for a smelter is to produce the maximum tonnages at the lowest cost of production. When looking at furnace lifetime extension the analogy should be drawn with 'Asset Management'. Asset Management operates the furnace longer between major relines, implementation of on-site methods to monitor lining integrity, predictive maintenance and repairs to ensure furnace longevity. Longer lifetime is the most significant factor in the economics of linings (Mohantry, 2015).

Degel *et al.*, (2018) used the term "furnace integrity" to achieve the best possible furnace performance. "Furnace integrity is defined as the combination of mechanical design, process and operation, refractories and cooling." The paper will outline different types of lining monitoring and the importance of having proper thermal modelling as the basis for all lining analyses combined with cooling requirements. The impact of metallurgy and operational control, combined with on-site assessments will also be elaborated on.

## FURNACE DESIGN CRITERIA AND ENGINEERING PRINCIPLES

Ferroalloy furnaces operate under extreme thermal and chemical conditions, with temperatures sometimes exceeding 2000°C, which necessitates a robust lining system that is designed for operational efficiency, longevity and safe operation. Refractory lining is divided into different zones, the hearth zone faces direct contact with molten metal and slag, the lower side wall zone is in contact with molten metal and slag and experiences thermal cycling and mechanical abrasion during tapping cycles. Upper side wall zone is exposed to feed material and lower temperatures (feed temperature dependant, pre-heated and freeboard gas temperatures) The design is influenced significantly by several key parameters such as electrode current, pitch circle diameter (PCD), shell diameter, hearth depth, furnace power densities, integrated cooling systems and chemical suitability of refractory materials. Traditional designs of refractories primarily favoured insulated lining types to minimise heat loss from the furnace, while the modern approach tends to leverage cooling to incorporate freeze lining concepts to form a protective slag crust on the lining face. The lifespan of the lining can be significantly reduced by suboptimal design, lack of process control, poor installation and poor maintenance.

### Key Design Parameters

#### *Refractory Materials and Design Principles*

Material selections must be specific to alloy types and tailored to specific zones, where shaped refractories (bricks and blocks) and unshaped (castables, ramming mixes) are employed. Typically, a mix of carbon (bricks, blocks, ramming), magnesia, silicon carbide and alumina refractories are selected for the various alloy furnaces.

#### *Shell Diameter and Heart Depth*

This determines the overall capacity of the furnace and the thermal gradient across the lining; larger furnaces diameter and depth require thicker lining to maintain structural integrity and manage heat loss as larger diameters have increased surface area exposed to thermal and chemical attack. Steenkamp *et al.*, (2019), noted that in a 63 MVA furnace with a shell diameter of 10 m, refractory side wall thickness could vary between 600-800 mm.

#### *Electrode Pitch Circle Diameter*

This arrangement can have a significant effect on current distribution and arc stability, an improper design can lead to uneven heat flux, hot spots which can accelerate refractory wear in specific zones. Larger PCDs may have the effect of better heat distribution but complicate lining design due to increased surface exposure. Optimal PCD would balance refractory protection with electrical efficiency.

#### *Furnace Power Density*

Higher power densities accelerate reaction rates but also influence the temperature distribution and lining wear by increasing the heat flux to the lining, which increases the thermal stresses experienced in the lining.

#### *Cooling System*

This system is an integral part of the refractory and furnace performance, with water and air-cooled panels designed as part of the refractory and steel cooling system. Improper design or a malfunctioning system can lead to overheating of the steel components, reducing the cooling on the refractories and accelerating wear on them as well; this could lead to catastrophic failure of the steel containment. In the case of freeze-lining designs, cooling is imperative to ensure the integrity of solidified slag layer.

## FREEZE LINING BACKGROUND

Freeze lining design and wear mechanisms for ferroalloys have been extensively published since the early 2000s. The aim of this paper is not to go into these details, but it is however important to note that a lining thermal model is the basis of lining management. A thermal model as shown in Figure 1

contains vital information during the furnace engineering design stages. The lining thermal model is used to:

- Determine the bottom plate cooling requirements.
- Determine the sidewall cooling requirements.
- Adjust refractory design should the shell and bottom plate temperatures be too high.
- Shows typical salamander penetration into the hearth refractories.
- Gives operators the temperature range for thermocouple set points and alarms.
- Deviation from the isotherms over time will be indicative of lining wear.
- Any IR scans should also be read in conjunction with thermal models.

Over time however, lining wear is inevitable as per schematic shown in Figure 2. Unfortunately, lining wear does not happen in isolation – all the factors listed here should be reviewed together to make any accurate assessments of the lining condition.

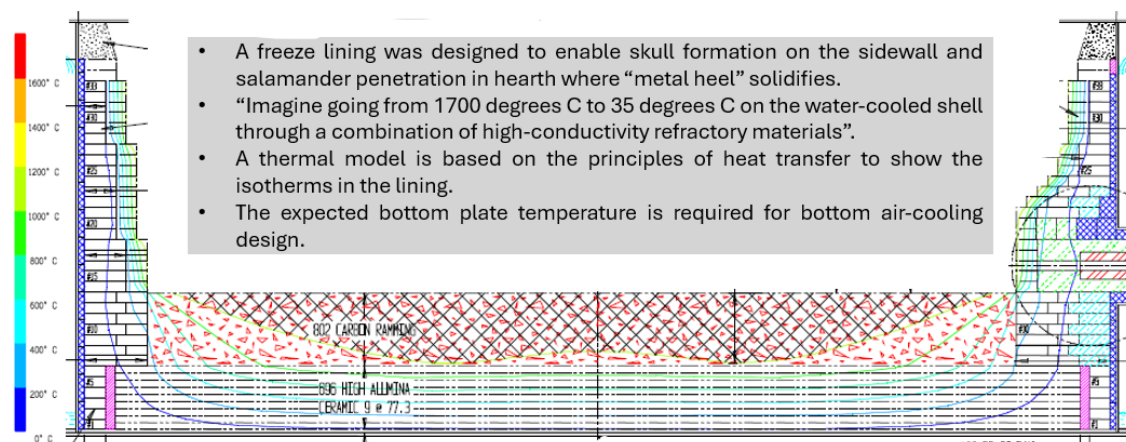


Figure 1: Typical freeze lining thermal model with simple explanation of the freeze lining.

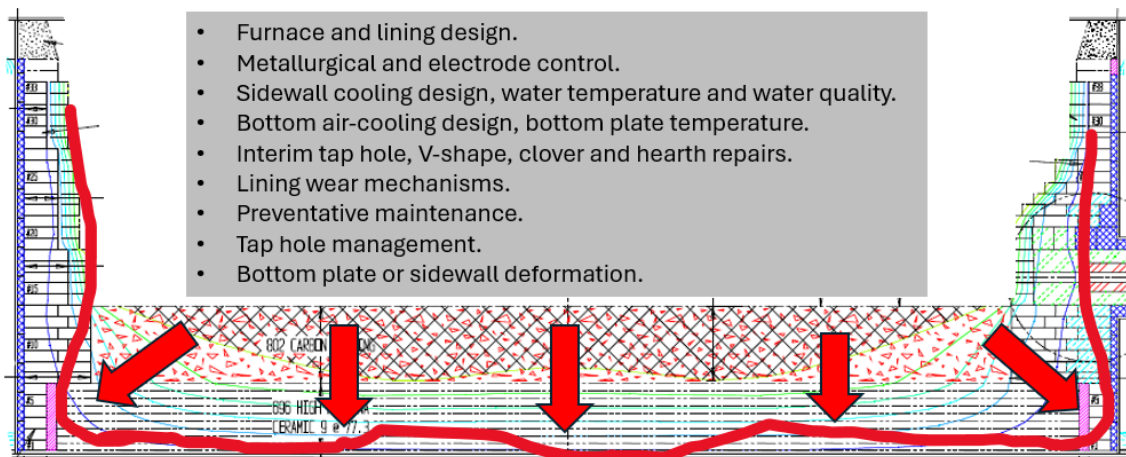


Figure 2: 'Elephant's foot' profile on freeze linings with a list of factors impacting lining wear.

## MODELLING, MEASUREMENT AND LINING MANAGEMENT

### Dual or single type K thermocouples SCADA display and trends over time

When assessing thermocouple data, it is of utmost importance to not only look at data as displayed instantaneously. Interpreting thermocouple data should be done as trends over a period. The trends can



be linked back to operational control, electrode management, power inputs and signal any possible issues with hearth and sidewall cooling.

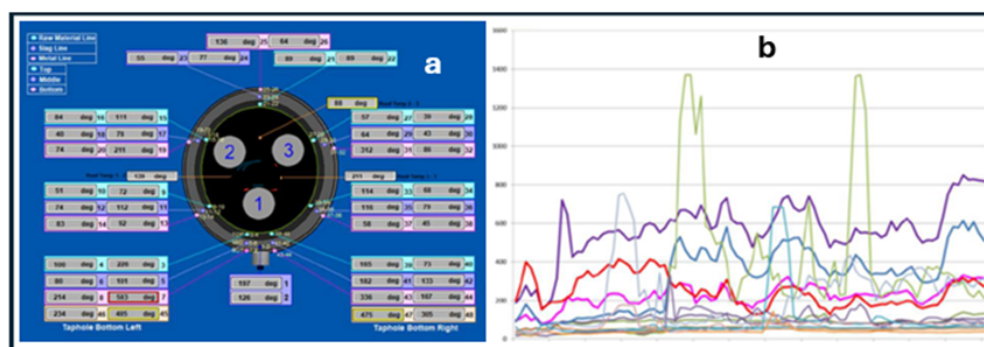


Figure 3: (a) Thermocouple display screen; (b) thermocouples plotted as trends over time.

## Inverse geometry heat transfer modelling

This assessment assumes 1D heat transfer between a group of linearly aligned thermocouples, the thickness of the remaining refractory in the sidewall and hearth is extrapolated based on thermal properties of the system. The thickness of the protective skull can also be estimated using the 1D heat transfer analysis considering thermocouple history (Ghorbani *et al.*, 2018). Temperatures measurements and their respective position are the only ongoing data required for the inverse geometry method. This is a simple and inexpensive method using 1D heat transfer analysis considering thermocouple history and results are shown through an online monitoring system. This system is only as good and reliable as thermocouple data but does not incorporate 2D and 3D heat transfer through the cylindrical shaped vessel.

## One-dimensional heat transfer analysis for predicting freeze lining thickness

The time-varying thickness of protection layer on refractory wall inside the submerged arc furnace was estimated using the finite difference method and the mathematical tool MATLAB. This methodology is possible to evaluate the skull formation efficiently. The analysis is carried out by assuming the furnace wall as multilayer including the solidified layer of slag. This layer protects the furnace wall from thermal shock, erosion and wear (Karyamsetti *et al.*, 2018).

## Lining Management System

The lining management system (LMS) is a tool that allows furnace operators and managers to monitor sidewall and hearth cooling, as well as the delivery of energy to the process. The LMS provides useful information regarding lining temperatures, heat losses, remaining refractory thickness and freeze lining formation. The LMS typically has a 3D representation of the furnace showing the computed shape of the crucible, freeze layer and refractory lining. A 2D view will also show the heat transfer through the shell of the furnace over any given area. Advance trending allows for trending of all measured and calculated data. This LMS integrates easily with most PLC systems via OPC servers whilst storing data in SQL database (Joubert *et al.*, 2007). Figure 4a shows the LMS screen display. Figure 4b is an example of a clover profile at the metal line and Figure 4c a typical clover profile of the slag line (Coetzee *et al.*, 2010). Previous studies of ferrochrome smelters showed a reduction of 25% refractory failures in plants that have implemented embedded thermocouples in the refractory and was monitored continuously (Poirier & Geiger, 2016).



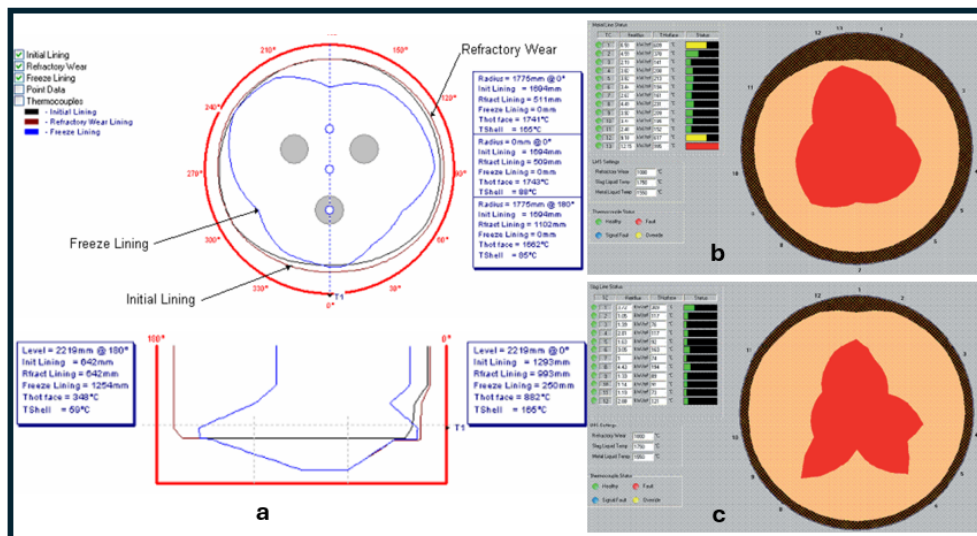


Figure 4: (a) Lining Management System display screen; (b) metal clover profile on LMS; (c) slag clover profile on LMS.

### Infrared scanning

Infrared (IR) scanning is an excellent technique to measure the shell and bottom plate temperatures and identify any localised hot spots. Advances have been made to scan all areas between grillage beams to give actual steel temperatures on all areas of the hearth. IR scans should be interpreted combined with a thermal model of furnace lining.

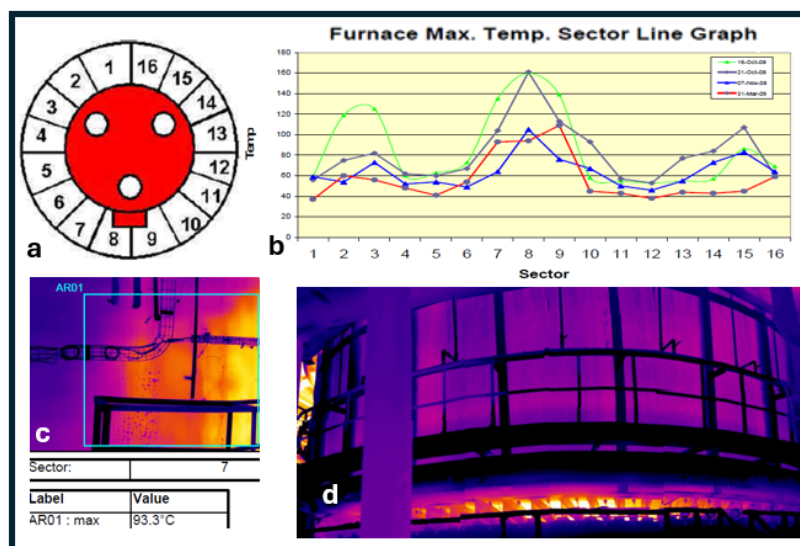


Figure 5: (a) IR scan schematic of different zones on the furnace for scanning; (b) maximum temperatures per sector; (c) section on the shell with resultant temperature; (d) temperature display between grillage beams.

### Acousto Ultrasonic – Echo

Sadri *et al.*, (2010) describes AU-E technology as a non-destructive technique which can be used during furnace operation to predict skull and hearth refractory thickness using stress waves. AU-E is a stress wave propagation technique using frequency data analysis. A mechanical impact on the surface of the structure (via hammer or a mechanical impactor) generates a stress pulse propagating into the furnace layers. Waves are reflected by change in refractory layer properties, and the main energy propagates through solid refractory layers. A receiver in the impactor picks up signals that are analysed and interpreted.

## LINING MANAGEMENT AND MONITORING FOR OPERATORS

This section serves to give operators a guideline of on-site inspections and factors that must be looked at during furnace operation. The combination of all these factors can lead to an increased lifetime with very little capital expenditure.

### Process and metallurgical control impact on lining wear

Metallurgical control and operation have a direct impact on the lining temperatures and heat flux to the sidewall and hearth refractories. Careful consideration must be given to the following:

- Power rate – higher power rates result in higher temperatures and heat flux levels. Arcing is also more likely to occur.
- Slag basicity – acid slags are more aggressive than basic slags and resultant slag chemistry directly impacts slag liquidus.
- Carbon control: Too short electrodes – these will overheat the slag and lead to boiling conditions in the furnace; (an over-carbon situation would result in electrodes running higher in the furnace).
- Carbon control: Too long electrodes – Deeper smelting and hearth damage will be seen along with excessive heat transfer to the hearth; (an under-carbon situation would lead to electrodes running deeper in the furnace).
- Bank formation – bank formation on the sidewalls can lead to a smaller smelting crucible resulting in more heat concentrated in a smaller smelting zone. To achieve the desired tonnages with a smaller crucible longer electrodes will be required, and these have a direct impact on shifting isotherms into the hearth. Banks can also push up roof panels and disturb feeding orientation.
- Alloy superheat – this has a direct impact on the hearth and lower sidewall refractories and increased heat flux to the hearth.
- Carbon dissolution – carbon dissolution in unsaturated liquids can only occur at sufficiently high temperatures and results in degradation of the carbon refractories. In such cases no skull formation can occur as these unsaturated liquids are in direct contact with the lining. Liquid carbon levels can also be unsaturated when the process is run ‘under carbon’.

Figure 6b shows the typical operational temperatures and reactions during ferrochrome smelting (Ollila *et al.*, 2010). Steenkamp (2019) did extensive research into the impact of slag and metal on carbon refractories as shown in Figure 6a and 6c. A direct correlation was drawn between higher temperatures’ resultant impact on slag and metal solubility in carbon refractories.

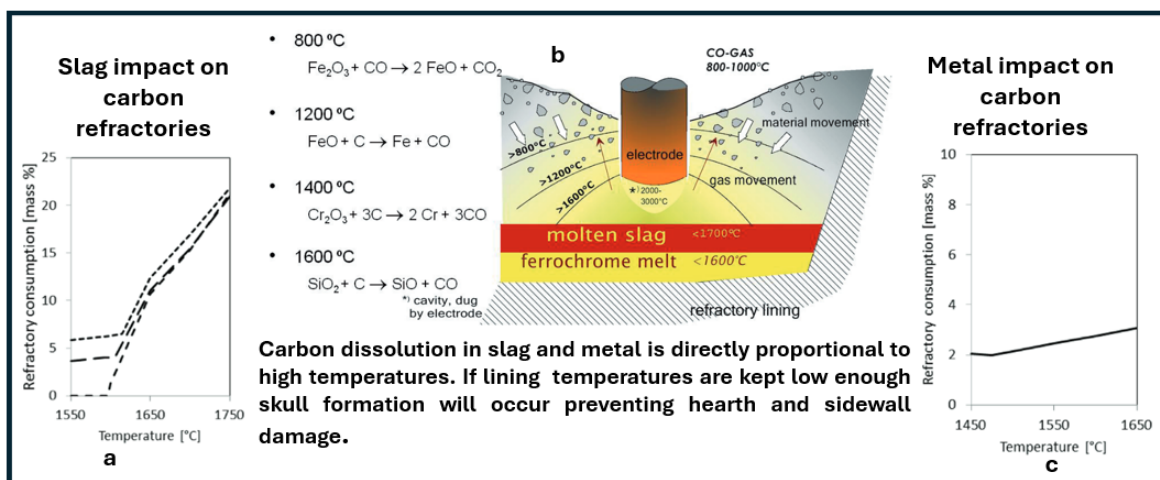


Figure 6: (a) Slag impact on carbon dissolution as a function of temperature; (b) typical ferrochrome reactions; (c) metal impact on carbon dissolution as a function of temperature.

### Tap-hole management

Tap-hole management is a critical aspect of achieving maximum lining lifetime. The tap-hole is the area where the most aggressive wear is seen on the furnace. The basics of tap-hole management are rooted in understanding carbon linings on blast furnaces. Good tap-hole management can be described as follows:

- Mudgun and drill (Most modern furnaces are fitted with a fixed mudgun and drill, although mobile mudgun and drills are still used).
- Opening the tap-hole with the drill and minimal lancing if required.
- Ensuring proper plugging of the tap-hole opening with high quality tap-hole clay.
- Most operators will use a less expensive tap-hole clay for day-to-day closing of the tap-hole.
- In the event of degradation of the tap block, a more expensive 'repair type clay' is required.
- Clay consumption and drill lengths should be monitored daily (Sutherland *et al.*, 2018).
- Regular mickey changes (the mickey is the consumable carbon block on the cold face of the tap-hole in the arch).
- The mickey is also changed to protect the tap block and assess any tap block wear.
- During operational upsets lancing is also done below the tap block as illustrated in Figure 7c.

Protecting the tap block also serves the purpose of protecting the surrounding brickwork. Lancing is the number 1 cause of tap block wear. In the event of metallurgical instabilities lancing is expected, but this will result in damage not to brickwork below the tap block but also damage to lintels above the blocks and brickwork to the sides. The impact of lancing oxygen into the melt significantly increases the temperatures in the vicinity of the tap block. Temperature maxima can be for instance 1900 – 2150°C compared to the average alloy bath temperature of 1550°C (Erwee *et al.*, 2023). Oxidation is a direct result of lancing as is seen from Figures 7a, b.

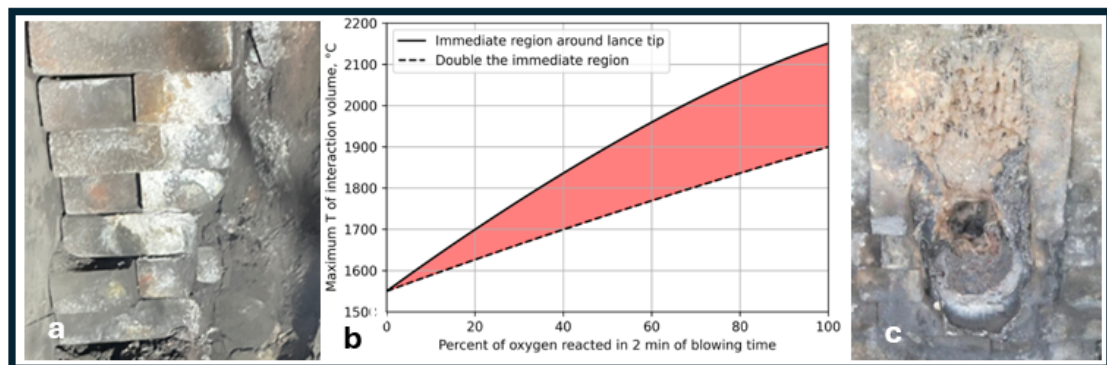


Figure 7: (a) Oxidation on brickwork surrounding the tap hole area; (b) Estimated adiabatic temperature of the alloy-gas interaction volume; (c) lancing below the tap block during operational upsets.

### Shell cooling

On furnaces with conductive type refractories, the water-cooling system is essential to ensure a long campaign life. Cooling systems are designed based on maximum heat flux capability based on the equilibrium heat flux in the specific region of the furnace. On most ferrochrome furnaces either spray/film/shower cooling (Figure 8a) or forced cooling channels (Figure 8b) are used. Furnaces can also be designed with film cooling on the shell combined with forced channel cooling around the tap-hole arch (Figure 8c). On higher intensity DC-furnaces copper coolers will however be used in areas of increased heat flux. It is however important to note that efficient cooling is dependent on all refractory components being in intimate contact with each other. Any possible air gaps will lead to a massive loss in heat transfer from the lining to the water-cooled shell.

Film cooling effectiveness will decrease due to scaling and dust deposition on the shell combined with microbiological impurities (Figures 8c, d). Scaling is a function of water quality and water treatment, but is also directly proportional to higher temperature. It must be noted that increased scaling reduces heat transfer and efficient energy extraction from the shell. Any scaling on the shell must be addressed



via sandblasting during a longer shutdown as well as addressing water treatment. Water treatment is easier on closed loop systems than open loop systems.

Forced channel cooling is better for heat extraction than film cooling, but without sufficient water treatment, scaling will also occur on the inside of the forced cooling channels (Figures 8a, b). A drawback of forced cooling is that hotspots cannot be as easily detected on the shell as with film cooling.

Insufficient cooling on the shell will lead to higher lining temperatures and resultant lining wear. To ensure furnace longevity and asset integrity water cooling temperatures, flow rates, quality, treatment and cleaning any possible scale are of the highest importance.

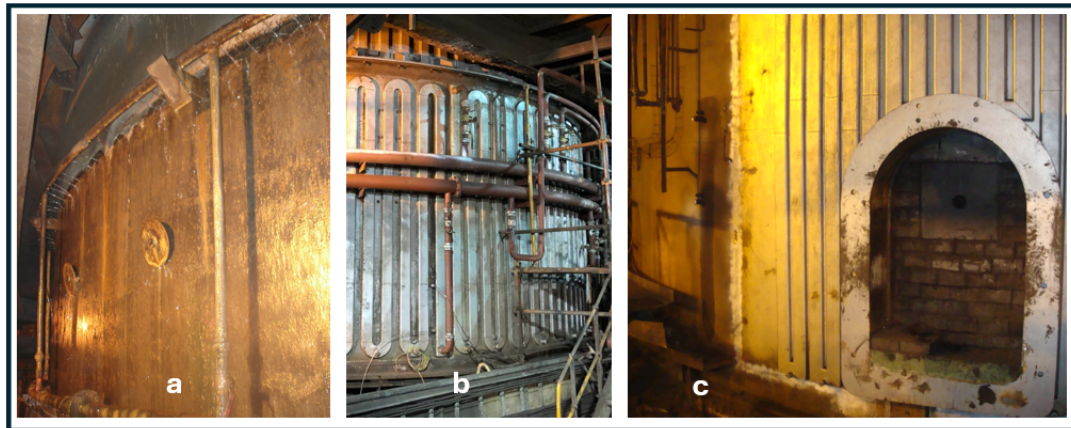


Figure 8: (a) Film cooling on a furnace shell; (b) forced cooling on a furnace shell; (c) film cooling on the shell combined with forced cooling around the tap-hole area; (d) Any water leak inside the furnace will reflect as rust on the inside of the shell.

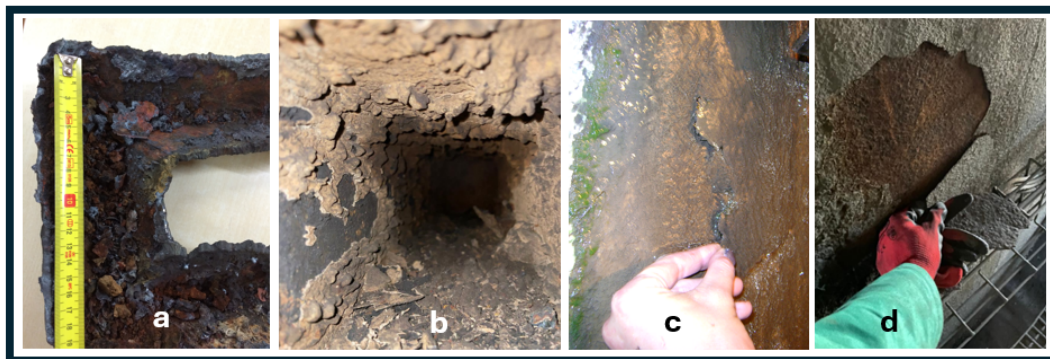


Figure 9: (a) Examples of forced cooling channel cut open to display rust and scaling; (b) blockages in forced cooling channel; (c) example of algae and scaling; (d) piece of scale coming off the shell.

### Hearth cooling and bottom plate lifting

Bottom plate lifting is the first indicator of hearth lining wear. This phenomenon occurs normally towards the later stages of a furnace campaign but can also be seen in the early stages of operation. Bottom plate lifting can be directly linked to furnace engineering (too-shallow hearth and refractory design), deeper electrodes, possible furnace refractory expansion or lack of air cooling. Bottom plate lifting is also a function of blockages/build-up between grillage beams that are barriers to heat transfer and air flow.

Van Laar (2001) explained bottom plate lifting as too-high bottom plate temperatures and a rigid connection of the outer bottom plate segments to the vertical shell via a bolted and/or welded construction. Radial expansion provisions of the outer bottom plate segments are immobilised by the

rigid connection to the vertical shell. Consequentially, radial movements are transposed into vertical movements. The vertical movements are significantly larger than the original radial movements would have been. It can be shown that for a typical furnace radius of 5000 mm, each 1 mm radial movement could be transposed into a 100 mm vertical movement. Vertical movements can also introduce a separation of refractories from the cooling system and introduce barriers to heat transfer.

It is recommended that operators track bottom plate lifting movements over time with a direct link back to operation and bottom air cooling. With bottom plate lifting the furnace hearth is also not getting the air cooling required as per design criteria. Figure 9a shows bottom plate lifting over time. Extreme bottom plate lifting is shown in Figure 9b and Figure 9c shows the actual measurements at different positions on the shell. Shell movement can also be tracked with lasers as well as rope monitors and the data fed into the PLC/SCADA. Bottom plate air flow and temperature monitoring is crucial to pick up any possible anomalies on the hearth or blockages in between the grillage beams.

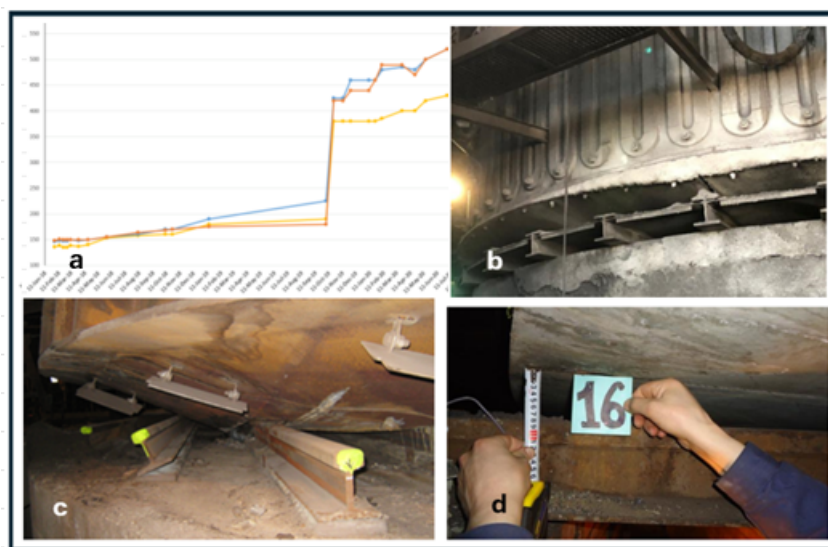


Figure 10 (a) Plotting bottom plate lifting over time; (b) example of bottom plate lifting and grillage beam damage on a shell with forced cooling channels; (c) significant bottom plate lifting and grillage beam damage; (d) measuring bottom plate lifting.

### Interim repairs

Coetzee *et al.*, (2024) provided a detailed overview of the various types of repairs that could be done on ferrochrome furnaces. These repairs would include annual tap-hole repairs, V-shape repairs in the tap-hole area, clover repairs as well as partial hearth repairs. These types of repairs do, however, require highly skilled demolition and installation teams to ensure that the furnace lifetime can be extended. Repairs have also evolved to complete furnace demolition while leaving the sidewall refractories intact. In such cases, major steel plate, grillage beams and hearth installation can be done. This is an excellent alternative to a furnace relining with minimal downtime and capital compared to a full relining. When operating any furnace lining, grouting is essential to extend lining lifetime and reestablish contact between all refractories and the shell for proper heat transfer. On older furnaces grouting is absolutely essential. Figures 11a and b show grouting nozzles on the furnace shell in the tap-hole and clover areas. Figure 11c shows grouting nozzles on a furnace with forced cooling channels. Two types of grouting can be done: grouting to establish contact between the graphite tile and shell and grouting to establish contact between the carbon bricks and graphite tiles (Figure 11d).

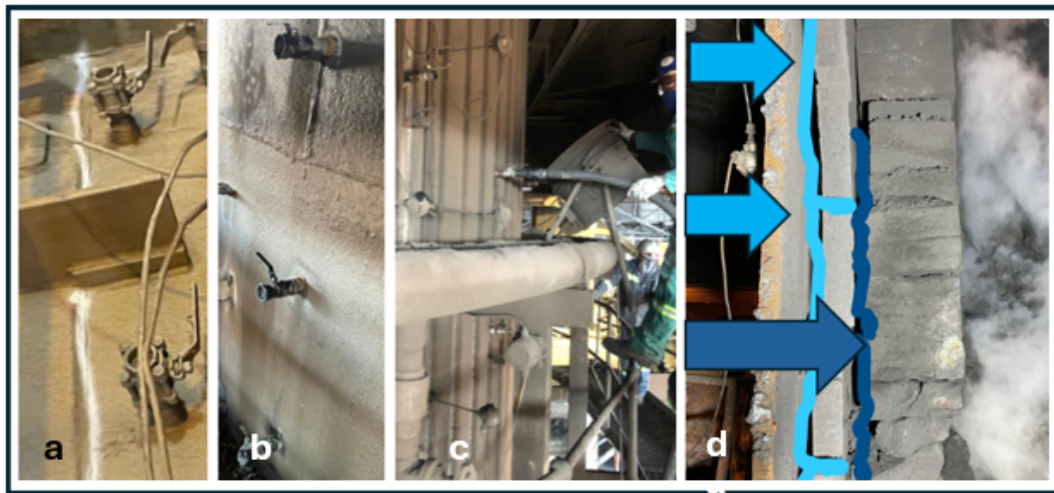


Figure 11: (a), (b) Grouting nozzles on furnace shell; (c) Grouting being done on a furnace with forced cooling channels; (d) grouting can be done between graphite tiles and shell (bright blue) or between the tiles and the carbon bricks (dark blue).

#### Linking Thermal models to IR scans and thermocouple trends to indicate lining thickness

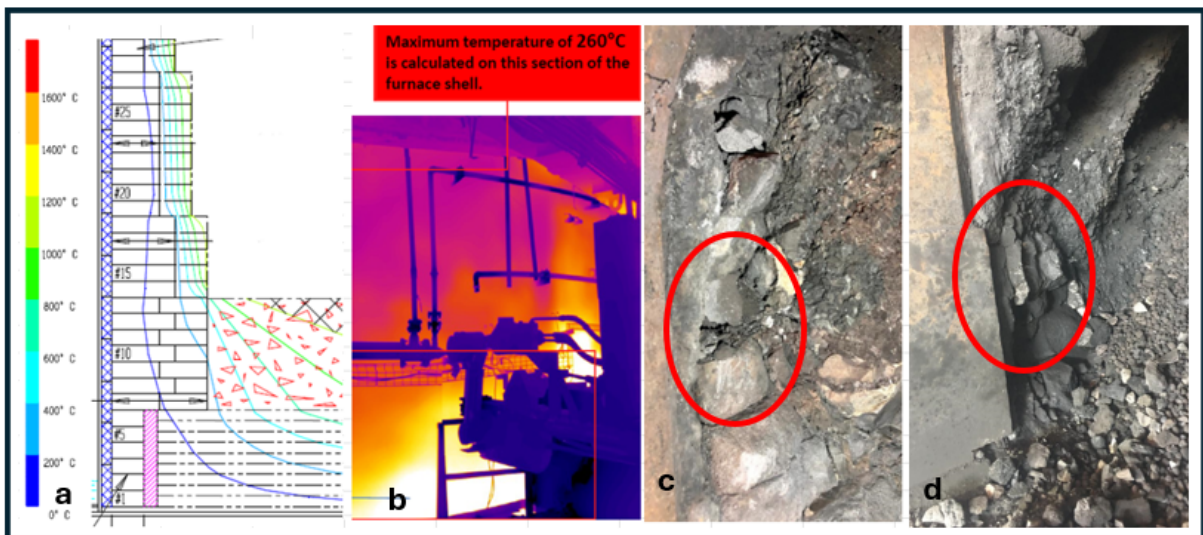


Figure 12: (a) Typical thermal model for a ferrochrome furnace; (b) 260°C on the shell during an IR scan; (c) Expected lining wear seen when 260°C is seen on the IR scan; (d) Expected wear seen when temperatures exceeds 260°C on an IR scan.

The authors suggest the following simplistic heat transfer model to assist with lining management and interpretation of IR scan for operators. A parallel must be drawn between the original thermal model, thermocouples and IR scans compared to actual on-site findings. Although this model cannot give an exact thickness of the protective skull, it is extremely useful to explain the interpretation of the IR scan as well as possible scaling on heat transfer. These results will also show the impact of just 3 mm of scaling on the shell. Scaling on the shell significantly reduces heat transfer from the lining to water cooled shell.

Figure 12 shows how the model can be used in industry. Figure 12a shows the expected isotherms on the original lining thermal model. Thus, for example the dark blue line on the thermal model would show the 200°C isotherm well into the sidewall lining. Figure 12b shows an IR scan with a temperature



of 260°C on the shell. When this temperature was observed on an IR scan the resultant impact on the lining could be seen on Figures 12c and d. To quantify these results the authors added a 3 mm thick scaling on the shell on the impact of lining thickness. Figure 13 shows that should you have 26°C 0 on the shell (which would be expected well into the sidewall, not outside) the resultant lining thickness would be as little as 99 mm. Note how well this corresponds with actual findings from Figures 12c and d. Figure 14 shows the model, assuming 70 mm thick graphite tiles, carbon bricks 457 mm in length and assuming no scaling. Note that without scaling the expected shell temperature would have been 106°C. These results must however be checked against the thermocouple data to give an even better understanding of lining isotherms. The role of IR scans on determining lining thickness is hugely underestimated. This is a low-cost option that provides a wealth of information if interpreted correctly.

This very simplistic heat transfer model linking IR scans to thermal models should however be verified against the furnace thermocouple trends to get a broader picture.

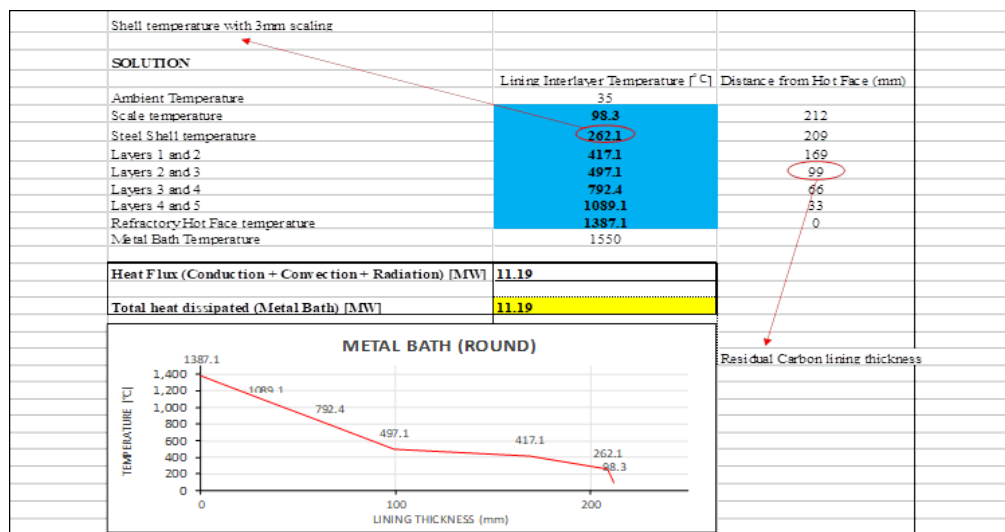


Figure 13: Model for 3 mm scaling on the shell, 260° C on the IR scan showing that the lining thickness would be only 99 mm.

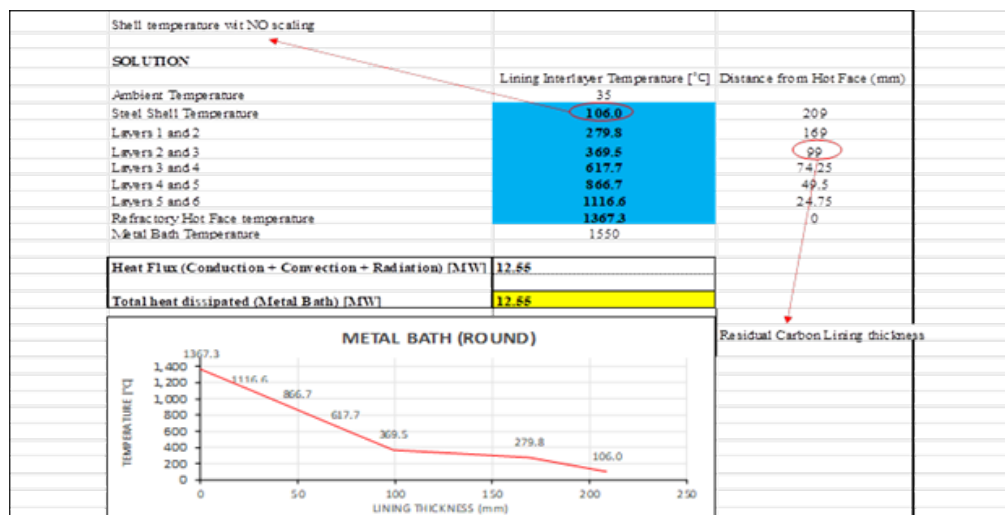


Figure 14: Model assuming 457 mm carbon bricks, 70 mm graphite tile with no scaling on the shell. Note the resultant steel temperature comes down to 106° C.

## CONCLUSION

This paper looked at measures operators can use to extend lining lifetime to obtain a longer furnace campaign, thus yielding higher long-term ROI. A furnace lining is a huge capital expenditure for any operator. Having premature failures not only result in lining and steelwork and auxiliary equipment damage, but massive production losses and excessive downtime. It is of utmost importance that furnace monitoring should be seen as Asset Management. Kindly refer to Figure 15 on how the authors envisage Asset Management which is not a single act, but combining all factors affecting furnace operation, preventative maintenance, lining management and monitoring. The furnace is an asset to any operator; this asset must be maintained and understood. In South Africa's current economic climate, there is no money for multiple relines. By returning to the basics of understanding furnace engineering and design and small low-cost measures on site, we can significantly extend furnace lifetime. We have entered an era where cost of production is the main decider of whether our ferroalloys industry will survive in South Africa. It is recommended that all these measures be employed to save our smelters in South Africa – we may not get a second chance.

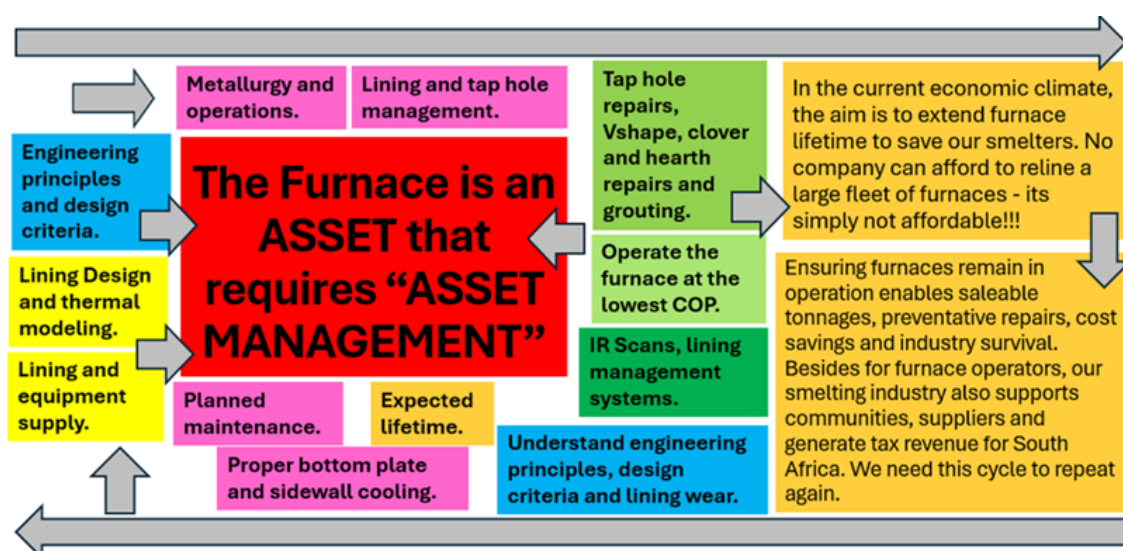


Figure 15: Furnace asset management schematic outlining factors influencing furnace lifetime.

## ACKNOWLEDGEMENTS

The authors would like to acknowledge all furnace operators in South Africa. We are a close community of individuals who are still passionate about our furnaces and our industry. This paper is dedicated to every person out there who is still willing to fight for our industry's survival. Special thanks to my co-authors who share a similar vision and passion for our industry. We can all make a difference. The most important legacy we can leave is transferring these skills and passion to the future generations – it's our duty to ensure this industry's survival.

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Technical Sales Ferroalloys  
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She started her career as a Senior Research Metallurgist at Billiton as well as Production Engineer at Metalloys. As Account Manager Africa and Middle East at GrafTech she spent time at smelters in South Africa, Korea, India, DRC, Europe, USA, Mexico and China, promoting freeze lining technology for platinum, ferrochrome, steel, ferromanganese, ferrosilicon, silicomanganese and titanium operators. At Tenova Pyromet she was involved with process, plant design and commissioning in South Africa, India and Russia.

At Intocast South Africa she is involved with furnace lining design, materials supply, demolition, and installation as well as technical expertise to ferroalloys suppliers globally. She is a keen supporter of skills and knowledge transfer - Transferring skills, knowledge and experience to the next generation is the most important legacy we can leave behind. What we decide to do today will determine where we will be tomorrow!

# Extension of ferrochrome furnace lifetime – Technical dig-outs to repair furnace hearths and clovers leaving sidewall brickwork refractories intact

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South Africa's ferrochrome producers are facing significant cost pressures with these factors leading to reduced production, smelter closures and a shift towards chrome ore exports instead of smelting ferrochrome. Key cost pressures include high electricity cost, global price competition, rising ore and reductant costs, logistics costs and ageing infrastructure. In this cut-throat environment the aim for survival is preserving cash flow and cost-saving initiatives. Ferrochrome producers do not have the luxury to spend millions on spare linings for every furnace in their fleet. Producers are increasingly looking at new innovative types of repairs to extend furnace and refractory lifetimes. A novel approach to extend freeze lining lifetime without relining has been developed by South African producers and Intocast South Africa. This approach includes fast high-level demolition to keep refractory sidewalls intact to get to problem areas at the clovers, tap-holes and furnace hearths.

## INTRODUCTION

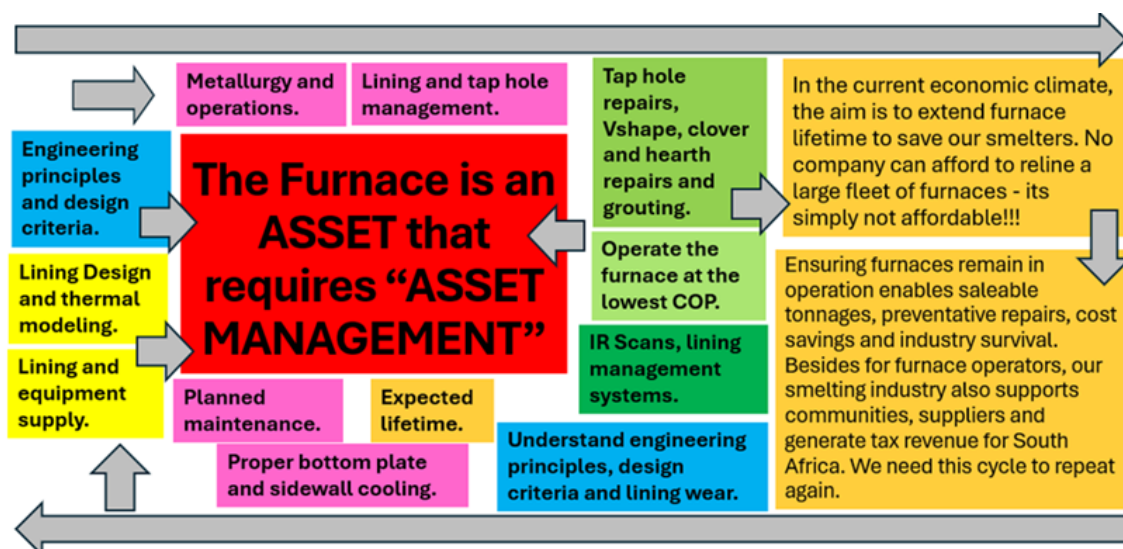


Figure 1: Furnace asset management schematic outlining factors influencing furnace lifetime.

Complete furnace relines including demolition, possibly civils, new steel shells/grillage beams and installation can take up to 3-4 months. With large ageing fleets, producers cannot afford to take multiple furnaces offline for reline at the same time. In this cost-conscious environment producers also cannot take furnaces offline to do 'textbook' dig-outs which would require significant cool-down periods, expensive demolition to identify reaction zones, sampling and detailed technical analysis and interpretation of results. Changes in ores and reductants and running furnaces beyond reline dates also lead to the main wear areas on ferrochrome furnaces now being much deeper into the hearths with resultant damage on bottom plate steelwork. The approach outlined in this paper can be summarised as 'Dig-outs with the aim to repair furnace refractories, gather as much information as possible on reaction zones and metallurgy and cost saving'. This approach is not 'demolition to destroy furnace refractories as was done prior to relines'. This paper will give an overview of dig-outs done globally on furnaces and discuss the main considerations for demolition and installation leaving sidewall brickwork intact. An overview of steelwork considerations will also be provided to elaborate on steelwork observations, such as bottom plate lifting, separation of sidewall shell from the bottom plate and steelwork integrity. Figure 1 summarises the furnace as an asset that requires asset management. A furnace lifetime is affected by numerous factors from engineering, design, lining design, maintenance, metallurgy and operations. Asset management requires an understanding of how these factors interlink and what measures can be taken to extend furnace lifetime.

Pablo Picasso famously said: *"Every act of creation is first an act of destruction"*. Furnace demolitions, excavations and dig-outs are not mere acts of destruction, but in essence profoundly beautiful art works revealing scientific and metallurgical marvels. Figure 2 pays tribute to the majestic UK Redcar blast furnace; an industrial landmark dating back to the late 1800s that used to line the River Tees. These demolitions will become more frequent globally, marking an emotional end to the former glory days of steelmaking (BBC, 2022). The aim of this paper however is not to focus on ending furnace legacies – but extensions thereof!



Figure 2: Redcar's blast furnace having stood for decades (left), being flattened in seconds (right) (BBC, 2022).

## HISTORY OF FURNACE EXCAVATIONS (DIG-OUTS)

Decades ago, most dig-outs have been carried out on small scale furnaces to understand ferroalloys smelting principles and refractory wear. These small-scale dig-outs were however scaled up to industrial size ferromanganese furnaces. Ozeki *et al.*, (1974) outlines the excavation of their 19 MVA no.2 furnace at the Mizushima plant in Japan in 1972. The objectives of the 'dig out' at the time were to investigate erosion of the lining and the condition of the interior of the furnace. Extensive erosion of the carbon blocks in the hearth had occurred as a result of molten metal under-saturated in carbon

Yamagishi *et al.*, (1974) reported on their study of the interior of a small 500 kVA unit which was used to produce ferrochromium. The zones identified included a charge or burden layer, a slag layer, an ore

layer, a coke bed, and a metal layer. Melting and reduction of the charge began next to the tip of the electrodes, where particles of metal were observed. The furnace had been quenched with nitrogen, which assisted in the rapid cooling of the reaction zone and in preserving the integrity of the interior of the furnace. Furnace excavations or dig-outs provided a wealth of knowledge into furnace operations and reactions zoned in ferroalloys furnaces. In 1979 Barcza *et al.*, conducted the first of its kind dig-out on Metalloys M10 75 MVA ferromanganese furnace. This study was crucial to map the reaction zones and provide insights into smelting of manganese ores (Figures 3a and 3b).

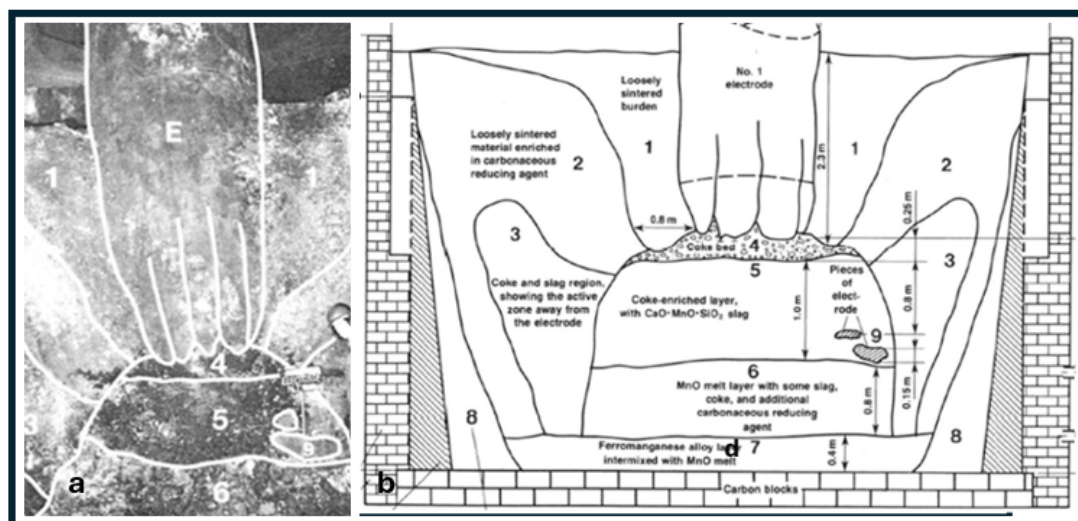


Figure 3: (a) Furnace dig-out showing different reaction zones surrounding an electrode; (b) 75 MVA ferromanganese furnace reaction zones (Barcza *et al.*, 1979).

In the 1980s in South Africa the largest operating ferrochrome furnace was a 48 MVA unit. It was realised that although the high-temperature chemistry of the reduction of chromite ore by carbon was well understood, very little was known about the overall reduction and smelting behaviour of the burden in an industrial electric furnace (Wedepohl and Barcza, 1983). An excavation was done of a 48 MVA furnace a month after it had been switched off. A mineralogical study was made of samples from various regions of the furnace to understand the reduction reaction. The aim of this study was to improve the efficiency of the process and apply knowledge gained.

In 1996 a dig-out was done on a 54 MVA ferrochrome furnace at Elkem Rana (Ringdalen *et al.*, 1996). The furnace was then left to cool for about two weeks, before some water was sprayed on the top of the furnace burden to speed the cooling. The coke on the top of the charge was still burning even after three weeks cooling. The furnace interior was investigated by core drilling in addition to a manual 'dig out'. The main purpose of the excavation was to determine the physical position and size of different zones in the furnace, and the amount and composition of ore, metal and slag in these zones. Since most of the reduction is assumed to take place below the electrode tip, the dig out focused on this part of the furnace. The main zone in this part of the furnace is the coke bed. It is defined as the part of the furnace below the electrode tip where a surplus of coke is mixed with alloy, liquid slag and partly reduced ore. The positions and extension of the different zones found during this excavation of furnace no. 1 are shown in Figure 4a. Tangstad and Olsen (2004) compiled the schematic of furnace zones of a silicomanganese furnace as shown in Figure 4b.

Ollila *et al.*, (2010) devised an innovative way to study Outokumpu Tornio no. 2 ferrochrome furnace by means of electric furnace drillings. Samples were drilled from electric furnace no. 2 with a transformer power of 75 MVA. Drilling done at different points of time and furnace conditions enables metallurgy development and optimisation of the furnace in a more detailed way. The developed sampling method provided cutting-edge knowledge of the phenomena in the SAF and can be used as a tool to further develop and optimise the SAF process. A 27.5 MW submerged arc furnace at Glencore Manganese Norway, producing silicomanganese, was excavated during a shutdown for relining in May



2017. The furnace was shut down between two taps without any meltdown of materials and excavated over a period of two weeks. Figure 4c showed the different furnace zones. (Ringdalen and Ksiazek, 2018). Figure 4d shows the results of a silicomanganese dig-out with the different furnace zones and composition (Norbo *et al.*, 2021). Coetzee *et al.*, (2021) performed a dig-out prior to a ferromanganese furnace reline. The main aim of the dig-out was to establish what caused bottom plate lifting on the furnace. It was hugely insightful to note that ferromanganese furnaces do not show sidewall wear and skull formation can be seen years into the campaign (Figure 5a). The carbon pad was mostly worn in the middle with the smelting zone being deeper from smelting with longer electrodes (Figure 5b). Figure 5c shows that the graphite pad was mostly intact, but wear was seen below electrodes. As the furnace had to be prepared for reline, the furnace demolition had to serve as a dig-out too and samples were taken from various areas on the hearth.

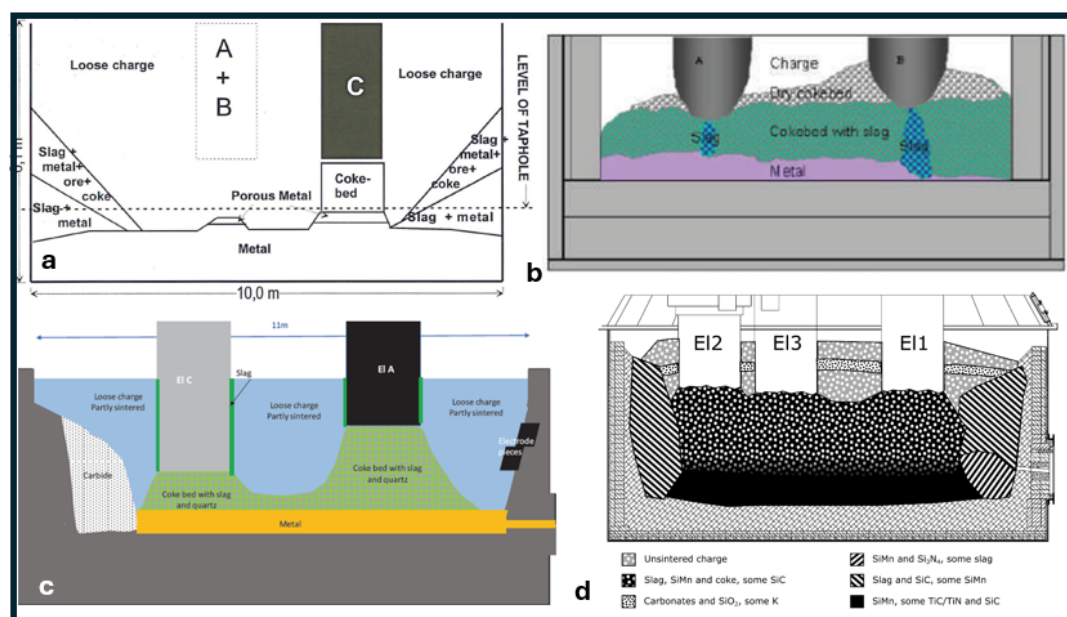


Figure 4: (a) Section through the centre of 54 MVA ferrochrome with an idealised sketch based on observations from the excavation (Ringdalen and Eilertsen, 1996); (b) zones in an excavated silicomanganese furnace (Olsen and Tangstad, 2004); (c) 27.5 MVA silicomanganese furnace observed zones and position in a cross-section through electrode A and furnace centre (Ringdalen and Ksiazek, 2018); (d) 31 MW silicomanganese furnace sketch of the different furnace zones and their composition (Norbo *et al.*, 2021).

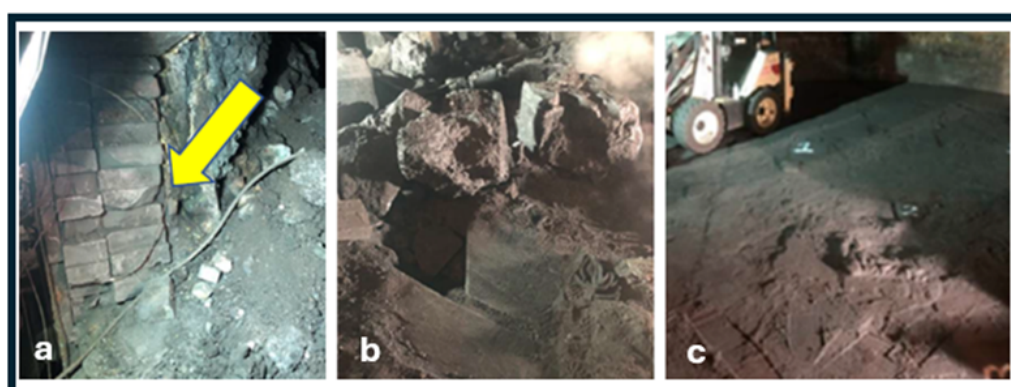


Figure 5: (a) Sidewall of a ferromanganese furnace showing very little wear and proper skull formation shown by yellow arrow; (b) the carbon pad was worn in the centre of the furnace and partial solid blocks were only seen on the outer parts of the hearth; (c) the graphite pad was mostly intact, but worn areas were seen below the electrodes (Coetzee *et al.*, 2021).

## CONVENTIONAL DEMOLITION

Furnace demolition is the process of breaking down structures and clearing all contents of a furnace prior to a reline. Normally a furnace is quenched with water or liquid nitrogen for cool down. Mechanical demolition is done using remote-controlled equipment, high powered excavators and hydraulic breakers. Thermal oxygen lancing and even controlled explosives are used to cut the shell or structures and assist when metal is too hard to break. The timeframe of demolition is normally short due to the aim being 'destruction' and not 'breaking for building'. Figure 5 shows the demolition done on a silicomanganese furnace about 27 years ago when furnaces were still built onto civils without grillage beams. In this case there was about 300 tons of hot metal below the furnaces that resulted in a run-out during salamander tapping. This however is not the norm on modern furnaces that are built on grillage beam with proper hearth cooling! In this case civil breaking was also in the Scope of Work (SOW) as well as installation of new civil base, grillage beams and new water-cooled shell.

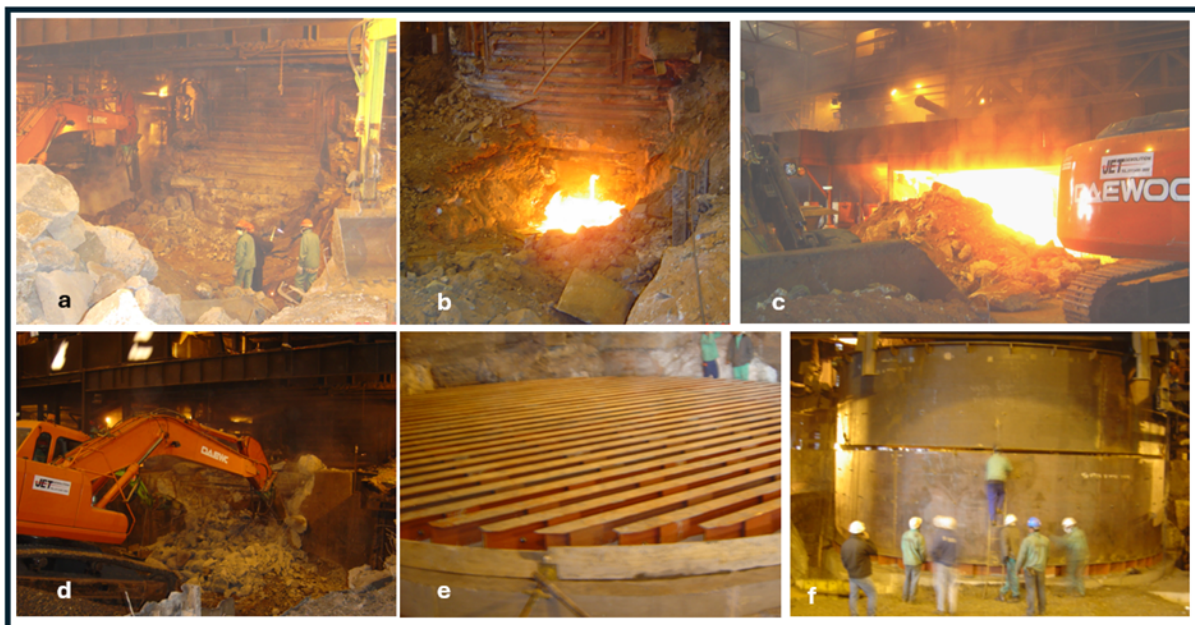


Figure 6: (a) Civil demolition being done of ladle bay; (b) lancing well below ta- hole to do a salamander tap (c) runaway tap due to metal having smelted well into civil base; (d) cleaning and breaking with excavator; (e) new civil base and grillage beams; (f) construction of a new furnace shell in two segments (Coetzee et al, 2021).

## RELINING OR REPAIR?

The decision to reline or repair a furnace is not made lightly considering the capital cost required. This decision is normally based on technical manifestations of impending lining failures, so it is a binary yes/no type of decision (Hearn *et al*, 2004). Besides for financial considerations, there is also the issue of safety risks. A furnace on the brink of burn-through would not only result in equipment damage but can also lead to personnel injury. A furnace relining is not only a capital decision, but is also dependent on commodity price, demand and market conditions. A relining is not only a disruptive process in terms of time and resources but also results in significant production losses which can never be recovered.

Previously in South Africa spare linings were kept on most smelters or at least sufficient materials for a large V-shape repair. Relines were done when the furnace reached the end of life and planned well in advance. Unfortunately, this ideal situation is not feasible for South African producers, who have a large, mostly ageing fleet of furnaces in need of relines. No producer can afford to reline multiple furnaces at the same time. What makes matters worse in South Africa is the fact that in 2025 multiple converging factors have created what industry observers describe as perfect storm conditions for South

African ferrochrome producers. Unlike previous market cycles characterised by single adverse elements, the current downturn combines energy cost escalation, global pricing pressure, and operational inefficiencies simultaneously. South African power costs represent 40-60% of total production costs. At these power costs and ferrochrome prices, producing ferrochrome is simply not profitable. Market pricing dynamics compound these challenges significantly. Global ferrochrome prices have declined below production costs for numerous South African facilities, creating scenarios where each tonne produced generates direct financial losses. This pricing environment makes operational suspension the economically rational response, despite the broader strategic implications for market share and industrial capacity. The disconnect between rising input costs and declining output prices has created an unsustainable operating environment that forces strategic production adjustments across the sector. Further complicating the picture is the introduction of carbon taxation, which adds new financial burdens to operations.

Provided that market conditions change with government committing to lower electricity prices, managing CBAM and introduction of rebates for tonnages or ferrochrome produced – only then can decisions be made to repair or reline. Loss-making furnaces cannot afford relines or repairs. In this cost competitive environment, it can be foreseen that the most cost competitive strategic assets will be relined/repared. Extending furnace lifetime through lower cost furnace repairs, not relines, can hugely benefit smelters. The main driver for any smelter now is to retain capital, not spend it. The capital expenditure necessary to upgrade ageing fleets to more efficient, environmentally compliant operations represent a significant investment that becomes increasingly difficult to justify against the backdrop of other economic pressures. Technical dig-outs to repair furnaces can add lifetime to a furnace at a cost much lower than reline. The furnace is unfortunately not only comprised of a lining. The decision to reline would also be based on structural steel integrity, auxiliary equipment such as gas plants, electrodes, transformer changes. These are all large capital expenditure where the decision to reline would be based on considering the end of life of all furnace components.

## METHODOLOGY FOR A TECHNICAL DIG-OUT FOR FURNACE REPAIR

For a concise understanding of these repair methods, the terminologies for areas mentioned are as follows:

- **V-shape:** A repair larger much larger than a tap hole repair, that would include demolition and installation with personnel and equipment inside the furnace. The exact scope of work can only be determined upon completion of the demolition.
- **Hearth repair:** This would be a repair that involves the brickwork on the floor of the furnace and might include steelwork repairs on the bottom plate and grillage beams.
- **Clover repair:** The clovers are the areas across the electrodes where the refractory wear is in a clover format. (a three-leaf clover is commonly used to explain this wear pattern).

### *Pre-requisites for a clover or hearth repair on a brick lining*

Furnace V-shape repairs (Coetzee *et al.*, 2010) have been extensively documented in literature over the past 15 years. Coetzee *et al.*, (2024) outlined the various types of V-shapes that can be done on closed and open ferrochrome furnaces. This paper explains the next phase where the V-shape is the entry point for technical demolition to get to areas of repairs on the furnace sidewalls, clovers as well as complete or partial hearth repair. Another pre-requisite is that the alumina hearth bricks below the sidewall must be intact to enable a proper tie-in between old and new brickwork. Figures 7a and 7b show examples where the metal penetration into the sidewall resulted in sidewall brickwork pushing upwards and the 'refractory sidewalls are essentially loose'. Tying into these sidewall bricks will require much more breaking to find level brickwork. The sidewalls shown in these pictures are just outside the V-shape area where the sidewall and hearth bricks interface. Figure 7c is a perfect example of sufficient integrity on the sidewall and hearth bricks to do a proper hearth repair. It must be noted that hearth and clover repairs on furnaces with castable sidewalls (Figure 7d) are significantly easier than doing repairs on



furnaces with carbon or alumina brick sidewalls. To repair a furnace hearth the sidewall must have some integrity to prevent brickwork collapse.

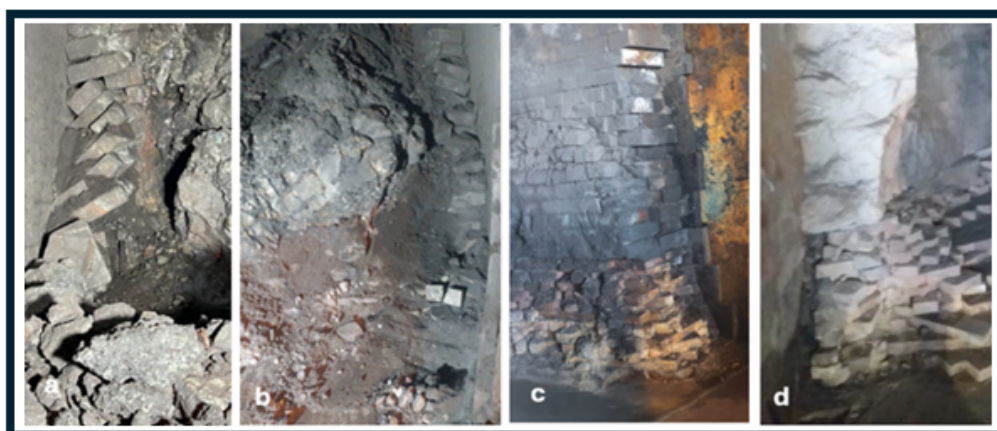


Figure 7: (a); (b) Examples where the sidewall brickwork has excessive metal and slag penetration along with brickwork pushing upwards. In such cases a hearth repair will be extremely difficult; (c) carbon sidewall bricks showing good structural integrity and alumina bricks in a good condition where a tie-in can be done on the hearth; (d) castable sidewall on top of multiple layers of alumina bricks showing a hearth repair can be done.

On older furnaces it can be expected that there will be significant metal penetration into the hearth and lower sidewalls of the lining. The most wear on the hearth would however be expected in the areas below the electrodes. A hearth repair should ideally be done before a hearth burn-through that would have resulted in significant bottom plate, grillage and even civil damage. Partial hearth repairs over the past three years however revealed that bottom plate steelwork, grillage beams and partial civils repairs can be done to get the furnace back into operation. Damaged areas typically on the metal line or below can be done by careful removal of brickwork and proper supports and propping to prevent sidewall collapse. Repairing the clovers and sidewall is much easier on a furnace where brickwork is mortared and the bricks are held or 'keyed in a ring' (Figures 8a and 8b). In the event of the sidewall and freeboard brickwork pushing upwards, there would be metal and slag penetration lower down in the sidewall. In such a case the entire clover area must be dropped to repair affected areas typically below the metal line.

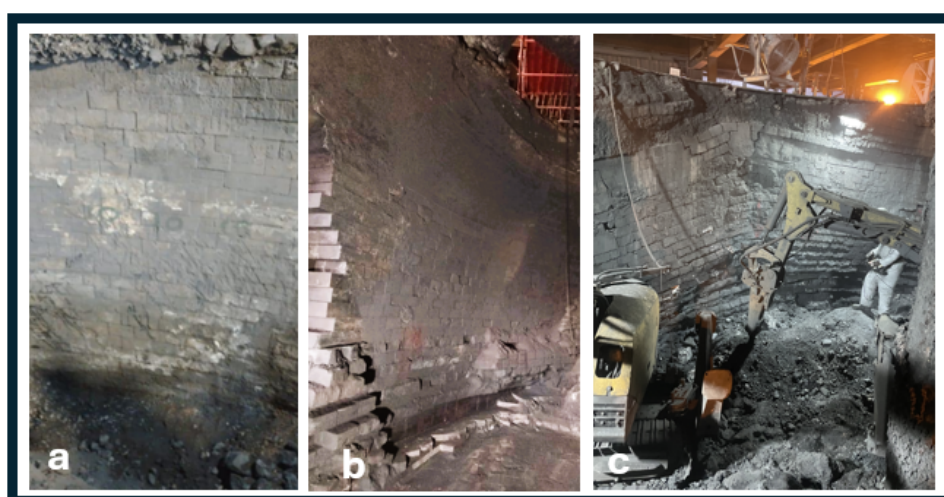


Figure 8: (a) and (b) Showing freeboard and sidewall refractories intact that is ideal for a clover repair typically on or below the metal line; (c) freeboard refractories and sidewall brickwork pushed upwards, in such a case the scope of repair will include dropping entire sidewall clover area to repair area below metal line.

### *Preparation for technical dig-outs*

To the untrained eye conducting demolition to do furnace repairs might seem like ‘just breaking’ – this however cannot be further from the truth! In the months or even years leading up to these endeavours, the furnace thermocouples, IR scans, tap hole condition and bottom plate lifting are carefully studied to determine the exact SOW. The furnace would have had significant V-shape repairs, mini-V-shape repairs and even localised repairs at the clover areas in areas of burn throughs (Coetzee *et al.*, 2024). On large, closed furnaces doing V-shapes could already have been up to the bottom plate of the furnace to include possible steel plate repairs. A mini-V-shape is defined as a smaller shell cut to do a small-scale V-shape, normally from the outside when time and costs are the main drivers to get the furnace back online.

Besides for IR scans on the shell, IR scans between the grillage beams will also show the bottom plate temperatures, which is a key indicator of lining wear. In the event of increased bottom plate lifting it can already be concluded that there would be significant metal penetration into hearth refractories. Ferrochrome furnaces do not typically burn through the hearths, but when postponing relines, running longer electrodes or changes in metallurgical operation – this has unfortunately become a reality. It should however be noted that there is a direct correlation between increased bottom plate temperatures and lack of hearth air cooling. This could be due to fans being offline or a build-up of dust and debris between grillage beams. If the temperature on the bottom plate is sufficiently high and there is a lack of air flow, this dust/debris build-up can even become sintered, which makes it incredibly difficult to clean between grillage beams. In the event of doing a hearth, clover, steel plate or even partial grillage repairs – these repairs would serve no purpose unless the cooling below the furnace is addressed. High pressure cleaning of the build-up between grillage beams has proven to be a very efficient technique to ensure proper air flow and cooling below the furnace.

The technical demolition described in this section will outline demolition of the contents of the furnace to expose refractory sidewalls and hearth refractories (or lack thereof). For any technical dig-out having the furnace properly melted down with a minimum metal heel is of utmost importance (Figure 9a). The less metal, the faster the demolition would be. For this type of demolition normally 3-4 brokks would be required along with a standby brokk and highly skilled, experienced operators. During the initial stages of demolition when the furnace is red-hot, an excavator can also be used for initial breaking and maximum breaking power. All brokks must be equipped with buckets and hammers, enough moils and a 24-hour technician on site in the event of any breakdowns. The start of any demolition would be a shell cut for entry of brokks into the furnace. A lot of operators would focus only on chrome breaking in the tap hole area. This however will take a lot of time whereas the main goal of repairing the sidewalls, clovers or hearth is to get to the back of the furnace. Thus, breaking banks as shown in Figures 9b and 9c are crucial preparation steps. Note in Figure 6d the placement of L-shape steel supports to prevent freeboard brickwork collapse.



Figure 9: (a) Furnace melt down; (b) banks in front of sidewall brickwork; (c) mix, banks and chrome to be broken out; (d) steel support to prevent freeboard brickwork collapse.



### *Dropping furnace level for correct angle of breaking and brokks entry into the furnace*

Figure 10 shows that to get access into the furnace with brokks, as well as getting the furnace level lower – the entire furnace level inside must be dropped. This is achieved by first focusing on breaking all banks which are normally red-hot underneath. This strategy works perfectly to open the area of breaking and remove heat that is being contained underneath the banks. Figure 11a and 11b show examples of when the angle of breaking is too steep during breaking. Breaking at a too-steep angle is not only inefficient but does not benefit the overall demolition strategy. The aim of technical demolition is to get to the sidewalls and the hearth – thus the furnace level must be lowered first as is shown in Figure 11c.

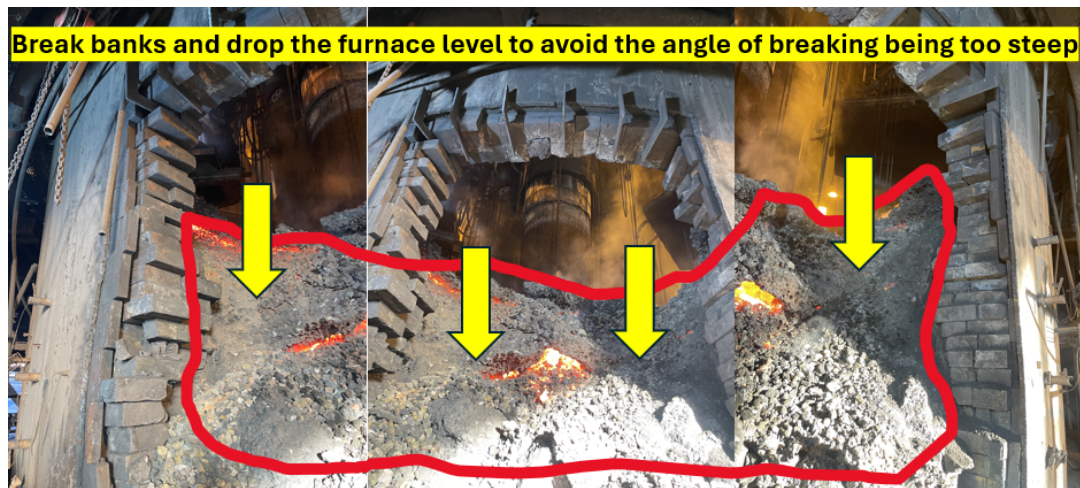


Figure 10: At the onset of demolition, it is crucial to break banks to remove heat out of the furnace butt; also drop the level at the front for brokks to enter.

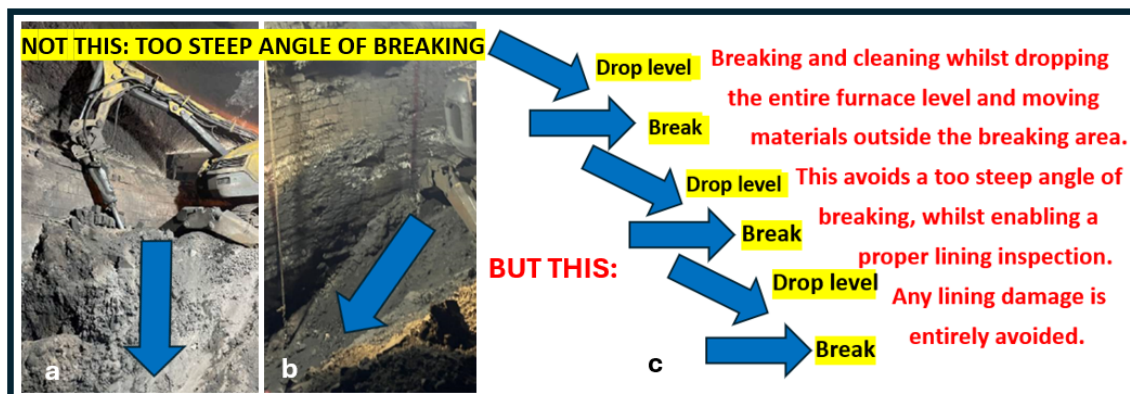


Figure 11: (a) and (b) Show the angle of breaking too steeply; this results in very inefficient breaking; (c) The correct methodology for demolition is opening the sidewalls and getting to the hearth by lowering the entire furnace level.

### *Demolition to expose sidewall and hearth refractories*

When doing technical demolition to repair furnaces the approach is called 'breaking for building'. As mentioned in the introduction, the aim is not destruction! Once the banks inside the furnace are broken, mix removed and the level is dropped, multiple brokks can enter the furnace. Whenever demolition occurs below electrodes, they must be manually broken (Figure 12a) or cut with high pressure water (Figures 12b, Figure 12c and Figure 12d). Should there be any chance of falling pieces onto personnel or equipment, the electrodes must be secured with caps and slings. Safety is the number one priority when doing this type of demolition. Figure 13 captures the demolition process in detail. Once the electrodes have been cut and secured, then multiple brokks can focus not only on further dropping the

furnace level but also focus on chrome breaking. Breaking banks was the easy part. Breaking hot chrome, especially in the case of a significant metal heel into the hearth, is not an easy feat. Using liquid nitrogen to cool the furnace down works very well but is unfortunately a very expensive option. Water must be used for cooling down, but the usage thereof should be carefully monitored so as not to flood the furnace. Should this demolition be done on a furnace with magnesite refractories, hydration of bricks would occur. On carbon linings steam will go into brick joints and pores that will lead to refractory degradation.

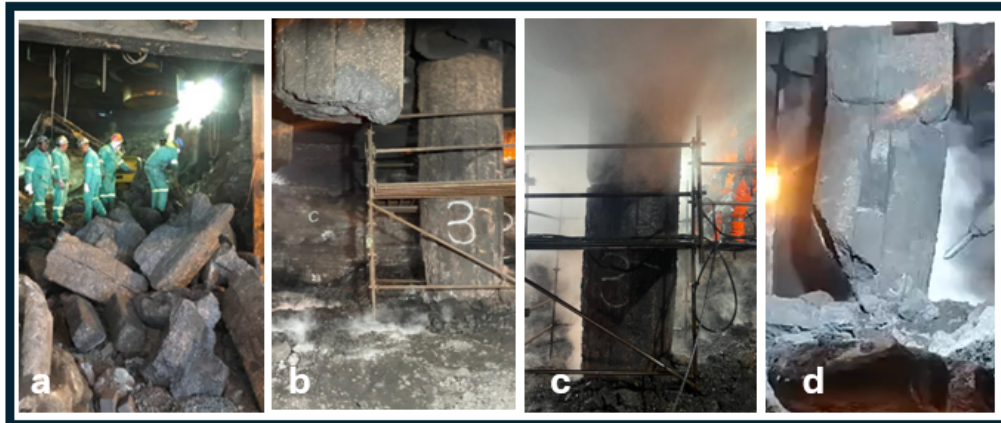


Figure 12: (a) Electrodes being manually broken with brokks; scaffolding to cut electrode 3 with high pressure water; (c) electrode cutting with high pressure water; (d) Broken piece of electrode after cutting with high pressure water.

Figure 13a and 13b show breaking of chrome at the entrance to create space for more brokks breaking inside. Figure 13c shows the brokks focusing on the area below the electrodes to remove the maximum chrome to get to the hearth. Figure 13d illustrates that once the area below the electrodes is cleared, the focus can shift to the remaining slag and chrome in front of the sidewalls. This is the trickiest part of the demolition process – break close enough to the sidewall to expose brickwork but never damage any sidewall brickwork. The final detailed breaking on the hearth and lower sidewalls must be done with utmost caution, as brick removal at this stage will be done with jackhammers and chipping hammers. Figure 13e shows the completed demolition process and Figure 13f indicates damaged bottom plate sections that must be removed and replaced.



Figure 13: (a) Breaking chrome at the entrance of furnace; (b and (c)) multiple brokks focusing on chrome breaking below the electrodes; (d) multiple brokks with buckets and moils focusing on opening up the areas in front of the sidewalls; (e) demolition complete to show bottom plate; (f) damaged bottom plates to be replaced.



### *Steelwork, clovers and hearth repair*

In the event of bottom plate damage, the damaged sections of plate must be removed to expose the grillage beams. Any debris or metal between grillage beams must be cleaned as they would be barriers to proper hearth cooling. If grillage beams are damaged, then they must be replaced (Figures 14b and 14c). Should a burn-through have occurred prior to this type of repair, there is a high likelihood of civil damage, which would also be repaired. New bottom plate sections are shown in Figure 14d, and level checks are done for installation to start.



Figure 14: (a) Cleaning metal and debris between grillage beams; (b) and (c) new grillage beam installation; (d) new bottom plate sections and level checks.

Prior to starting the clover repairs, the brickwork installation on the hearth must be finalised. The aim of this paper is not to go into exact installation details, but the complexity of such a repair must be understood. When attempting to repair two clover areas and do a V-shape on the way out, or during clover repairs, coordination and logistics are of the utmost importance. When bricks are removed from any part of the lining the structural integrity of the lining can be compromised. Clover repairs leaving freeboard bricks intact are achieved with sufficient supports keeping brickwork intact. As the Vshape area is typically the entry point for materials, steel supports should be installed, not only to hold freeboard bricks, but also the sidewalls. Even with standard V-shapes the sidewalls can pull away from the shell during demolition. Any 'cracks' in the sidewall must be filled with good quality repair type tap-hole clay. Any slag and metal in the sidewall brickwork should be removed prior to any building. Figures 15a and 15b show the type of supports that are needed when building under a 'loose sidewall'. Damaged graphite tiles and carbon bricks must be removed as is shown in Figure 15b. Figure 15d shows an excellent example of propping of the wall when 'building underneath the wall'. It is very important that grouting nozzles be placed on the shell for grouting after startup to fill any possible voids between the sidewall and steel shell. Installation of alumina refractories on the hearth is shown in Figure 16a with the completed V-shape installation shown in Figure 16b. On the furnace hearth carbon blocks, carbon ramming (Figure 16c) or even electrode paste cubes (Figure 16d) can be installed.



Figure 15: (a) Supports being put to hold the clover sidewall while installing new carbon bricks; (b) Damaged tiles are taken off the shell while the area is being supported; (c) building below the metal line at a clover; (d) Scaffold pipe supports put in place prior to removing and building bricks underneath a sidewall.



Figure 16: (a) Alumina brick installation on the hearth; (b) completed V-shape installation; (c) carbon ramming installation; (d) electrode paste cubes loaded onto hearth.

## CONCLUSION

Technical demolition utilising highly skilled personnel and equipment enables demolition of all furnace contents leaving the sidewall and hearth refractories intact. It must be noted that these types of repairs are extremely complex and carry inherent risks if not done by professionals. Doing these types of repairs instead of relines not only results in significant capital savings but also extends furnace lifetime. South African producers are at the forefront of these types of repairs that can also include civil, grillage and steelwork repairs. Producers do not have the luxury to reline multiple ageing furnaces in their fleets. In 2025 and beyond, motivating complete relines will become even more difficult to retain capital investments. With escalating energy tariffs, global market downturn and increased competitive pressures producers are struggling to operate profitably. Any initiative that can save time and money is required to save our precious ferroalloys industry. Now more than ever it is essential to have innovative thinking and collaboration between suppliers and producers. The graphs from FAPA March 2025 illustrate the reality of the situation we are facing in South Africa. We are at the highest end of the global cost curve; thus urgent measures are required.

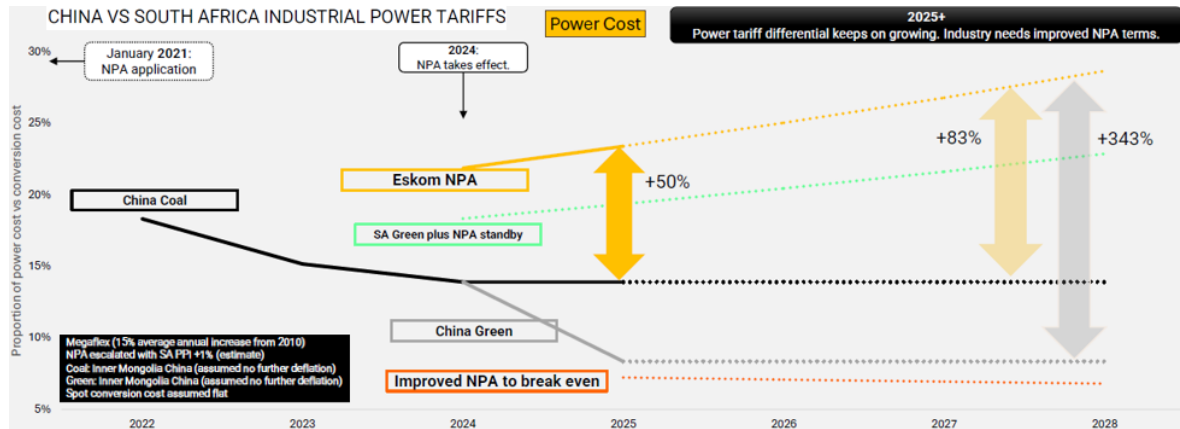


Figure 17: Explanation of South African vs Chinese industrial power tariffs and the improved NPA required to break even (FAPA, 2025).

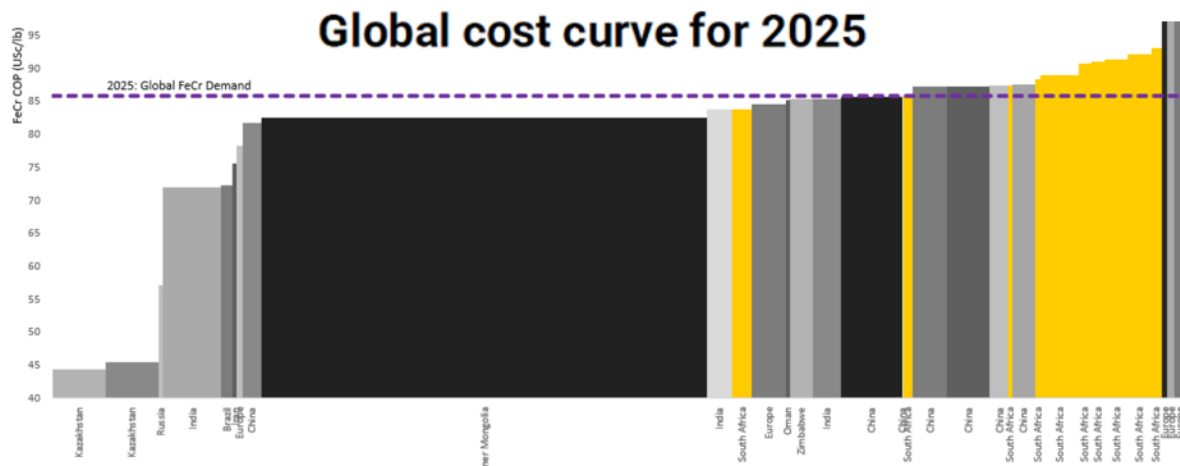


Figure 18: Global cost curve showing South Africa in yellow on the highest end (FAPA, 2025).

## ACKNOWLEDGEMENTS

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# Effect of pellet size on the reduction behaviour of chromite pellets using gaseous hydrogen as reducing agent

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Conventional ferrochrome production in Finland relies on carbothermic reduction of chromite pellets in a submerged arc furnace. This process involves melting a step which diminishes the influence of pellet size on its reduction behaviour. However, it generates substantial carbon dioxide emissions which are incompatible with decarbonisation goals. Consequently, hydrogen has been studied as a reducing agent in metals production, including the ferrochrome industry. Unlike carbothermic reduction, the hydrogen reduction process proceeds in the solid state, which places gas diffusion and particle geometry as decisive factors in the mechanism. As chromite pellets are produced within a certain size distribution as opposed to an exact and uniform size, understanding the effect of pellet size on the reduction degree achievable is essential. In this paper, various diameters of chromite pellets were reduced with hydrogen at two temperatures (1200°C and 1400°C) for four hours. In addition, the reduction degree was calculated. Results show that smaller pellets achieve higher reduction degrees than larger pellets. At 1400°C, the smallest pellet reached a reduction degree of 68.6%, while the largest pellet achieved 53.0%. A similar trend is also shown in an experiment at 1200°C. The smallest pellet reached 42.6%, while the largest pellet achieved 37.3%. Kinetic modelling also confirmed that ash diffusion layer is the rate-limiting step in this gas-solid interaction, which explains the high reduction degree of the small pellets. Therefore, these findings highlight pellet size as a key parameter in hydrogen direct reduction. Optimising its size distribution could enhance the efficiency of future ferrochrome production.

**Keywords:** hydrogen reduction, chromite, pellet, size, kinetic, reduction degree

## INTRODUCTION

Chromium is an essential alloying element in stainless steel (Cobb, 2010). The commercial ore which becomes the source of chromium is a mineral known as chromite ( $\text{FeCr}_2\text{O}_4$ ). However, as an oxide form chromite is still not suitable for a direct charge in stainless steel production. It needs to be pre-treated beforehand into a semi-finished commodity known as high-carbon ferrochrome (FeCr) (Basson and Daavittila, 2013a). Due to the increasing demand for stainless steel, reaching 58 million tonnes per year (Worldstainless, 2025), the surge request for ferrochrome's stock will follow accordingly. As an indicator, 93–320 kg of ferrochrome is normally used for producing one ton of stainless steel (Gyllenram *et al.*, 2022; Wei *et al.*, 2023a).

Conventional ferrochrome production relies on the carbothermic reduction of chromite pellets in a submerged arc furnace (SAF) (Basson and Daavittila, 2013b). There are plenty of process systems which use SAF as the heart of the process. Two systems (but not limited to) are based on a rotary kiln – submerged arc furnace (RK-SAF) and sinter belt system - submerged arc furnace (SBS-SAF) (Basson and Daavittila, 2013c; Hamuyuni *et al.*, 2021). All those technologies utilise SAF as a melting and reduction reactor. The melting step in the pellet-based SAF largely diminishes the influence of pellet size on the chromite's reduction, because the main reduction of chromium occurs when the chromite dissolves in the slag (Hayes, 2004).

This condition creates a sequence of reduction reactions, including solid-solid and solid-liquid reactions, leading to the formation of molten iron-chromium-carbon alloy. Even though it is an effective production route, the SAF process emits substantial carbon dioxide (CO<sub>2</sub>) emissions due to its dependency on coke (carbon-based material) as a reducing agent and for improving electrical conductivity of the burden. It is estimated that in one ton of ferrochrome produced, there are 1.6 to 1.82 tons of CO<sub>2</sub> (scope 1 emissions) released to the atmosphere (Wei *et al.*, 2023b; Olsen, 1998). Therefore, an alternative non-carbon reducing agent such as hydrogen is required to mitigate this challenge.

Based on a thermodynamic point of view, it is unlikely that hydrogen reduction will occur in the chromite and chromium oxide. As plotted in the Ellingham diagram (Figure 1), the lines of chromite and chromium oxide are still below the hydrogen line which indicates higher stability of chromite and chromium oxide as an oxide. Hypothetically, this might create challenges in applying hydrogen for converting chromite into metallic iron and chromium. Higher temperature might be required as shown in the Arvanitidis *et al.* (1996) and the current industrial scale of hydrogen reduction of iron as it can be seen that the FeO to Fe line is also below the hydrogen line. .

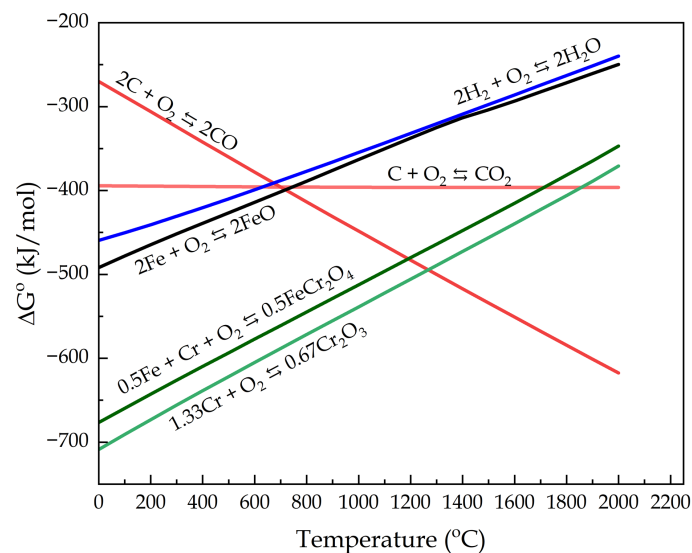


Figure 1. The Ellingham diagram of different metal oxides constructed using HSC chemistry ver 10.

Additionally, the hydrogen direct reduction process is different from the carbothermic process. Hydrogen gas-based reduction technology typically performs the reduction below the metal's melting point, as shown by the HYBRIT initiative and Molybdenum Company (Shields, 2013; Remus *et al.*, 2013). The reactions proceed there as solid-state reactions between the feed and hydrogen gas. Within the context of ferrochrome, these are chromite pellets and hydrogen gas. The solid-state nature of the reduction reaction implies the importance of the pellet size. In the case of iron ore, increasing the pellet's size lowers the reduction degree due to the diffusion of gaseous reactants through pellet becoming rate limiting (Spreitzer and Schenk, 2019). Additionally, Beheshti *et al.*, (2015) also showed that the smaller iron ore pellet's diameter increased the metal's reduction degree respective to the gas-based reduction process.

There are plenty new studies dealing with the optimisation of the ferrochrome production. Due to the current ferrochrome production route involving the use of arc furnaces, either RK-SAF or SBS-SAF, the objectives are more related to energy consumption. Many recent studies on chromite pellets have mainly focused on its agglomeration behaviour (Visuri *et al.*, 2021) electrical conductivity (Heikkilä *et al.*, 2021), and chemistry (Erwee *et al.*, 2021). Indeed, these features are relevant, because those can significantly affect the energy consumption of the smelting process, which is the main cost of the process.

On the contrary, there are not many studies focusing on the influence of pellet size, as this is considered irrelevant to the current process. Xiao *et al.*, (2004) tried to explore pellet size effect on the degree of reduction, but their study was limited to carbon monoxide (CO)-based reduction. For hydrogen reduction Khama *et al.*, (2026) used self-made chromite pellet and even Coertzen *et al.*, (2026) used an industrial pellet of similar size to this study. However, both studies used multiple pellets in one batch-test approach and focused on the reduction degree of the chromite and its main driving force without mentioning the role of individual pellet size. Current industrial chromite pellet size in the range of 14 to 16 mm may be considered optimal in the current carbothermic process, but the optimal chromite particle size for gas-based process is still unknown. In addition, many researchers also observed the use of chromite fines in their study of hydrogen reduction. Although, these studies mentioned the particle size distribution or range of the chromite fines' size, the relation of the size ranges or distribution in the gas-based reduction process were also not highlighted (Wu *et al.*, 2023), (Ma *et al.*, 2025).

Therefore, there is a noticeable gap in research regarding the influence of pellet size on the reduction degree of chromite reduced by hydrogen. This gap becomes more critical by looking at the mature technology for hydrogen-based reduction, which are shaft furnaces and fluidised bed reactors, all of which operate below the product's melting temperature and relies mainly on the solid-gas interactions during the reduction process.

Furthermore, this issue becomes even more apparent by considering the nature of pellet production itself. Pellets are produced using an agglomeration process known as pelletising (Yang, 2014). Instead of one single size, the resulting sizes of pelletising follow a certain distribution (defined by mass or volume) in like manner to other solid bulk material processing. This principle also applies to the industrial chromite pellets, suggesting that size variability can play a major role in the chromite's reduction behaviour if the ferrochrome industry shifts toward hydrogen gas-based reduction technologies.

Therefore, the aim of this paper was to study how pellet size affects the reduction degree of chromite pellets in hydrogen direct reduction using a laboratory-scale furnace. Understanding the effect of size is essential for evaluating the reduction behaviour of this CO<sub>2</sub>-lean process. This insight either provides how chromite pelletising should be adapted for its implementation by finding optimum size distribution or how the hydrogen direct reduction process should adjust the residence time for this particular characteristic of pellet size distribution.

## MATERIALS AND METHODS

Pelletised chromite from Outokumpu Stainless Oy was used in the reduction experiments. The sample was taken after the pelletising process line. Specifically, it was gathered by shovelling different piles in the cold pellet storage until representable of sample was obtained. Then, 80 kg of pellets sample was sent by Outokumpu to the University of Oulu. There, the sample was further divided with a rifle-splitter device. The X-ray fluorescence (XRF) analysis was provided by Outokumpu Stainless Oy too, and the result is shown in Table I, calculated as oxides.

Table I: Chemical composition of the pellets

| Content (wt.%)                 |                                |       |                                |                  |      |
|--------------------------------|--------------------------------|-------|--------------------------------|------------------|------|
| Fe <sub>2</sub> O <sub>3</sub> | Cr <sub>2</sub> O <sub>3</sub> | MgO   | Al <sub>2</sub> O <sub>3</sub> | SiO <sub>2</sub> | Rest |
| 25.63                          | 43.40                          | 11.75 | 13.07                          | 4.33             | 1.82 |

Furthermore, the phase structure of the pellets was analysed using X-ray diffraction (XRD). This analysis was able to identify the main probable phases in the chromite pellet. By using PDXL2 software, the main phases were determined to be magnesiochromite and hematite (see Figure 2).

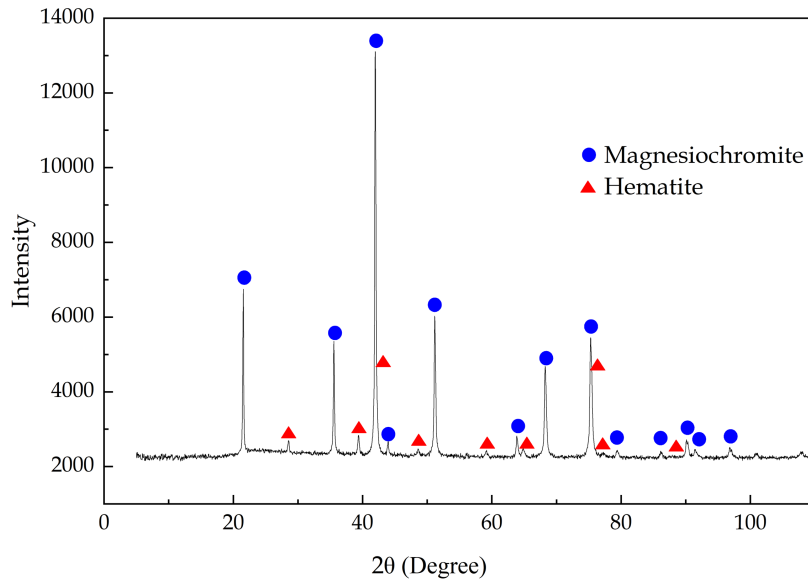


Figure 2. XRD analysis of the chromite pellet showing the main phases available.

The pellet was subjected to sieve analysis using the hand-dry sieving method using 16 mm, 14 mm, 11.2 mm and 8 mm sieve size. The sample mass used during this sieving test was 9.4 kg. The pellets' particle size distribution in the form of weight retained fraction is shown in Figure 3. Some pellets were selected from fraction 14.2/11.2 mm, 16/14.2 mm and +16 mm for the sample test in the single-pellet reduction test.

The reduction test was conducted using a thermogravimetric analyser called LINSEIS STA PT 1600. Each set of reduction experiments used one single pellet as the sample whose initial diameter, as shown in Table II, was measured manually using a calliper by considering nine different measuring points (Vitikka *et al.*, 2025). In addition to diameter, pellet's true density ( $\rho_t$ ) was also measured by using gas pycnometer (Micromeritics Accupyc II 1340) device.

For the reduction experiment, the sample was put in the reaction chamber and contacted with the reducing gas during the process. The only reducing gas used in the trial is hydrogen gas (purity of 99.9990%) and it is injected into the chamber with a flow rate of 2 standard litres per minute (1 atm and 0°C). The experiments were designated as a constant-temperature process, so the holding temperatures were set at 1200°C and 1400°C. Nitrogen gas (purity of 99.9990%), 2 litre per min of flow rate, was employed during preheating with 10 degree per min of heating rate until the temperature reaches the holding temperature, to prevent the initiation of reactions below the designated temperature.

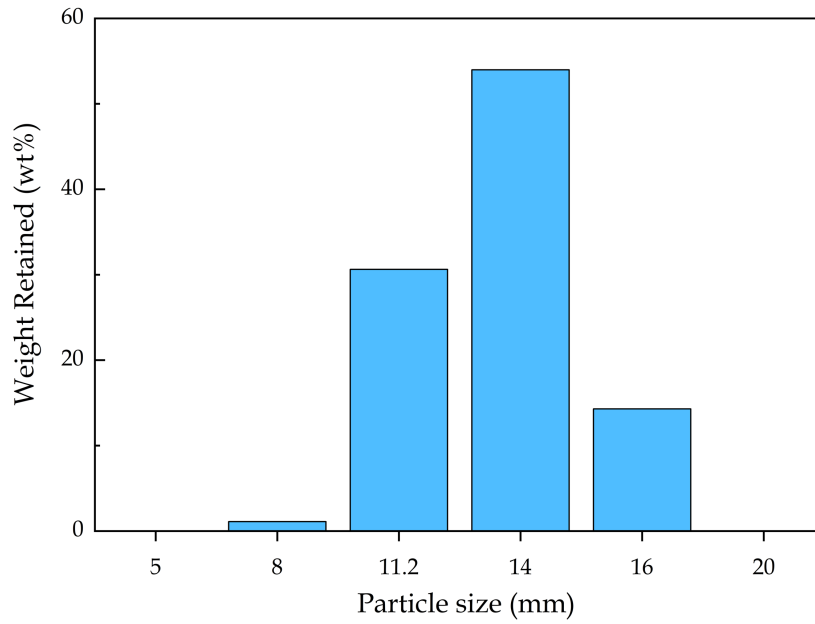


Figure 3. Particle size distribution of the chromite pellet, displayed as population density.

The weight loss during the experiments was recorded and became the output of the LINSEIS STA PT 1600. This data was employed for the construction of the reduction degree graph. After four hours of isothermal reduction, the cooling step was initiated by re-injecting the nitrogen gas. The pre-heating, iso-thermal and cooling step cycle is shown in Figure 4.

Table II: The initial diameter of the raw pellets for the reduction test.

| Item                                               | D1    | D2    | D3    | D4    | D5    | D6    | D7    | D8    | D9    | D10   | D11  |
|----------------------------------------------------|-------|-------|-------|-------|-------|-------|-------|-------|-------|-------|------|
| <b>Initial diameter <math>d_0</math> (mm)</b>      | 13.93 | 14.03 | 14.93 | 15.59 | 15.64 | 15.97 | 15.92 | 17.81 | 15.97 | 16.55 | 17.4 |
| <b>Reduction temperature <math>T_R</math> (°C)</b> | 1400  |       |       |       |       |       |       |       | 1200  |       |      |

Subsequently, the reduced chromite pellets were placed in the epoxy mounting and cut to polished section specimens. These specimens were prepared for microstructure observation along with the polished section of the unreduced chromite pellets. The microstructural image was then observed using a field emission scanning electron microscope (FESEM), the Zeiss Ultra Plus. The energy dispersive X-ray spectroscopy (EDS), which is coupled in the FESEM device, was employed to determine the elemental composition of the existing phases in the unreduced and reduced pellets.



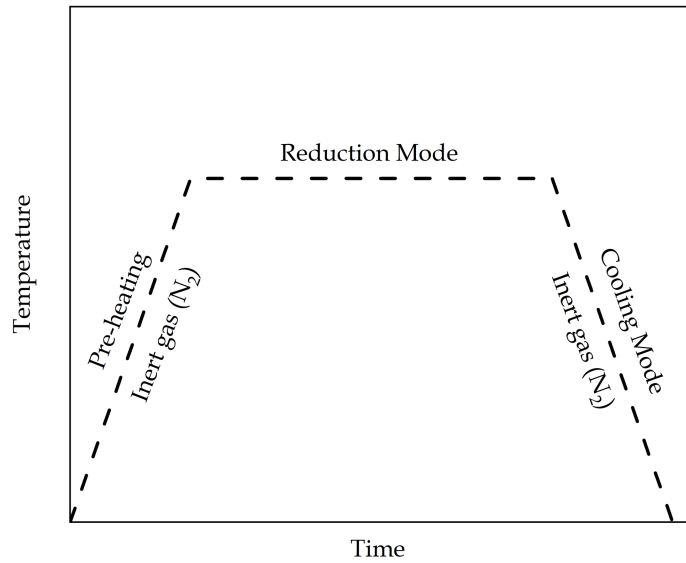


Figure 4. Experimental cycle for one set of reduction test.

## RESULTS AND DISCUSSION

### SEM Observations and Phase Identification

SEM observations revealed distinct microstructural features of the raw material (feed) and reduced pellets (product). In the unreduced pellet, a Widmanstätten pattern was observed across multiple chromite grains displayed in Figure 5a. It was a sign of the formation of new secondary oxides different from the initial chromite grains. Van Staden *et al.*, (2024) explained that the Widmanstätten pattern corresponded to hematite phases formed by spinel decomposition during the high temperature pelletising process. This finding is supported by the XRD result shown in Figure 1 and EDS analysis in Table III, which confirms the presence of hematite-rich phase and displayed higher iron content compared to the chromite grains.

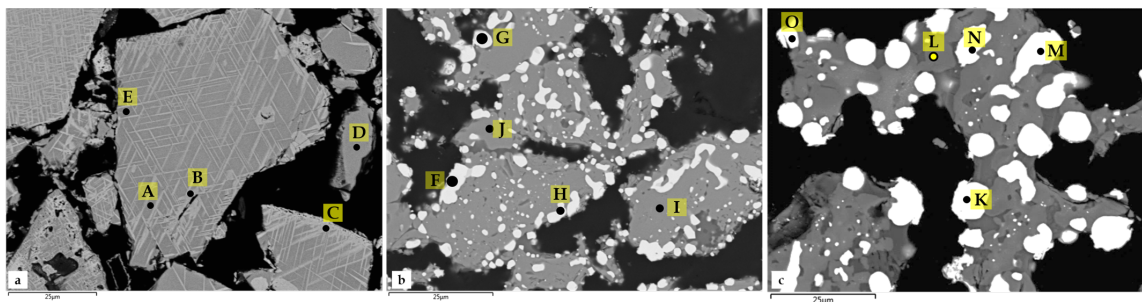


Figure 5. Microstructure images of (a) unreduced chromite pellets (feed), (b) the chromite pellets reduced at 1200°C, and (c). the chromite pellet reduced at 1400°C.

In contrast, the Widmanstätten hematite pattern completely disappeared in the reduced pellets, as seen in Figures 5b and 5c. Instead, the microstructure of reduced pellets exhibited multiple bright phases, suggesting the formation of metallic phases due to hydrogen reduction. In addition, the EDS analysis (see Table IV in point F, G, H, K and M) confirmed that the metallic iron and chromium are the main components in the metallic phases. In the sample reduced at 1400°C, other phases are also observed. The backscattered electron identified it as grey phases inside the bright phases (metallic zone). These zones contained a high amount of chromium, with the chromium to iron (Cr/Fe) ratio exceeding five.

Table III: The elemental composition of the raw pellets (feed) in Figure 4a, based on EDS analysis

| No | Possible Phases  | Composition (wt.%) |       |      |       |       |      |       |
|----|------------------|--------------------|-------|------|-------|-------|------|-------|
|    |                  | Fe                 | Cr    | Mg   | O     | Al    | Ti   | Rest  |
| A  | Magnesiochromite | 12.40              | 28.99 | 8.03 | 26.78 | 8.61  | 0.20 | 14.99 |
| B  | Hematite-rich    | 18.26              | 29.21 | -    | 25.79 | 10.14 | 0.42 | 16.18 |
| C  | Hematite-rich    | 20.88              | 31.47 | -    | 28.28 | 7.25  | 0.52 | 11.60 |
| D  | Magnesiochromite | 11.86              | 31.05 | 8.66 | 26.07 | 6.78  | 0.23 | 15.35 |
| E  | Magnesiochromite | 12.91              | 28.75 | 7.6  | 26    | 8.31  | 0.19 | 16.24 |

The remaining unreacted chromite grains can be seen as dark grey phases in the reduced pellets shown in Figure 4b and 4c. Based on its EDS point analysis in Table IV (point G I, J, and L), it still held significant amounts of chromium and oxygen, indicating that in the pellets, the whole chromium oxide component was not completely reduced. Nevertheless, the iron content in these grains is lower than its iron content in the unreduced pellet. It had 0.60 to 0.80 wt.% and around 0.56 wt.% of Fe on the sample reduced at 1200°C and 1400°C respectively, as shown in Table IV. This implied that the iron component in the chromite was reduced easier than the chromium oxide component.

Table IV: The elemental composition of the reduced pellets in Figures 4b and 4c, based on EDS

| No | Possible Phases      | Composition (wt.%) |       |       |       |       |      |      |      |       |
|----|----------------------|--------------------|-------|-------|-------|-------|------|------|------|-------|
|    |                      | Fe                 | Cr    | Mg    | O     | Al    | Si   | V    | Ti   | Rest  |
| F  | Fe-rich and Cr-metal | 55.95              | 30.3  | -     | -     | -     | 0.50 | -    | -    | 13.25 |
| G  | Fe-rich and Cr-metal | 56.99              | 30.58 | -     | -     | -     | 0.48 | -    | -    | 11.95 |
| H  | Fe-rich and Cr-metal | 54.74              | 31.06 | -     | -     | -     | 0.48 | -    | -    | -     |
| I  | Magnesiochromite     | 0.80               | 32.46 | 10.63 | 26.99 | 10.00 | -    | -    | 0.64 | 18.78 |
| J  | Magnesiochromite     | 0.60               | 31.01 | 11.16 | 28.19 | 11.45 | -    | -    | 0.52 | 17.07 |
| K  | Fe-Cr metal          | 49.28              | 48.76 | -     | -     | -     | 1.95 | -    | -    | -     |
| L  | Magnesiochromite     | 0.56               | 19.92 | 15.46 | 34.64 | 24.41 | -    | 0.21 | 0.73 | 4.08  |
| M  | Fe-Cr metal          | 50.99              | 47.10 | -     | -     | -     | 1.93 | -    | -    | -     |
| N  | Cr-rich and Fe metal | 10.60              | 77.37 | -     | -     | -     | -    | -    | -    | 12.03 |
| O  | Cr-rich and Fe metal | 10.8               | 77.56 | -     | -     | -     | -    | -    | -    | 11.64 |

Overall, SEM analysis hinted that hydrogen gas reduced the iron oxide and chromium oxide component in the chromite into its metallic form. A study by Arvanitidis *et al.*, (1996), which used synthetic chromite, also reported the presence of iron and chromium at a lower reduction temperature (~1100°C). On the other hand, the hydrogen gas could only partially reduce the chromium oxide, while it reduced the iron oxide component in the chromite grains into less than 0.80 wt.% under the scope of experiments studied.

However, either literature or experiment results show occurrence that is not observed according to Ellingham diagram. In Ellingham diagram, thermodynamically wise, it is displayed that there will not be chromium metallic in the equilibrium system. This deviation is caused by the fact that Ellingham diagram works correctly for pure metal as pure metal activity is always assumed as 1 (Epifano and Monceau, 2023). Because ferrochromium is categorised as alloys, the activity of both chromium metal and iron metal is lower than 1. It creates a condition that can lower the stability of the oxide, especially in the interface area between oxide and metallic (Young, 2008). As a result, the chromium oxide, which is the surface of metallic iron, tends to form chromium metallic under rich-hydrogen atmosphere and its metallic phase was spotted in the SEM observation. This implies that gaseous hydrogen reduction of chromite pellets can serve as pre-reduction step.

#### Reduction Degree and Reduction Kinetics

As polymetallic ore, chromite contains two valuable metals: iron and chromium. SEM observation

successfully identified the presence of both elements in their reduced or metallic state. Therefore, the reduction degree (RD) is displayed as the composite reduction degree (CRD) of iron and chromium. The following equation 1 is used to calculate the CRD by assuming that the weight loss is fully contributed by the removal of oxygen from iron and chromium oxide during the reduction process:

$$CRD(\%) = \frac{W_L}{O_C + O_F} \cdot 100\% \quad [1]$$

where  $W_L$  represents the weight loss recorded by the thermogravimetric analyser. Meanwhile,  $O_C$  and  $O_F$  represents the oxygen mass in the chromium oxide component and the iron oxide component, respectively.

As the pellet reduction works within the framework of solid-gas reaction, a shrinking core model was used to understand the reduction behaviour. Ash layer diffusion and chemical reaction control equation which belong to the shrinking core model (Levenspiel, 1998a) were selected to determine the kinetic of reaction and the rate-controlling step existing in the hydrogen reduction of chromite process by fitting it with the experimental results. By assuming the spherical shape of the pellet, the equation of ash layer diffusion and chemical reaction are displayed in the equations 2 and 3, respectively, where  $k$  is the observed kinetic value (%/minute) obtained from fitting,  $t$  is time (minute) and  $RD$  is the composite reduction degree (%).

|                                               |  |     |
|-----------------------------------------------|--|-----|
| $t = 1 - 3(1 - RD)^{\frac{2}{3}} + 2(1 - RD)$ |  | [2] |
| $RD = 1 - (1 - kt)^3$                         |  | [3] |

In addition to the equations under shrinking core model framework, the Rosin-Rammler equation were used for the fitting too. The application of Rosin-Rammler equation could model phenomena affected by particle size (Vinnett and Waters, 2020a) as this scope of study used different sizes of pellets for the isothermal reduction tests. The Rosin-Rammler equation is shown in the equation 4 where  $RD$  is the composite reduction degree (CRD),  $k_{RR}$  is the observed kinetic or reaction rate and  $a_{RR}$  is the fitting parameter related to the distribution of the observed kinetic value.

$$RD = 1 - \exp(-(k_{RR}t)^{a_{RR}}) \quad [4]$$

Figures 6 shows the reduction degree curves of individual pellets derived from experimental result at 1400°C which is fitted with ash layer diffusion equation. It is seen that smaller pellets exhibited a higher composite reduction degree. After four hours of reduction time, the composite reduction degree of 13.93 mm and 14.03 mm pellets was 68.39% and 68.60%, respectively. On the other hand, the large pellets (17.81 mm) only achieved a composite reduction degree of 52.96 %.

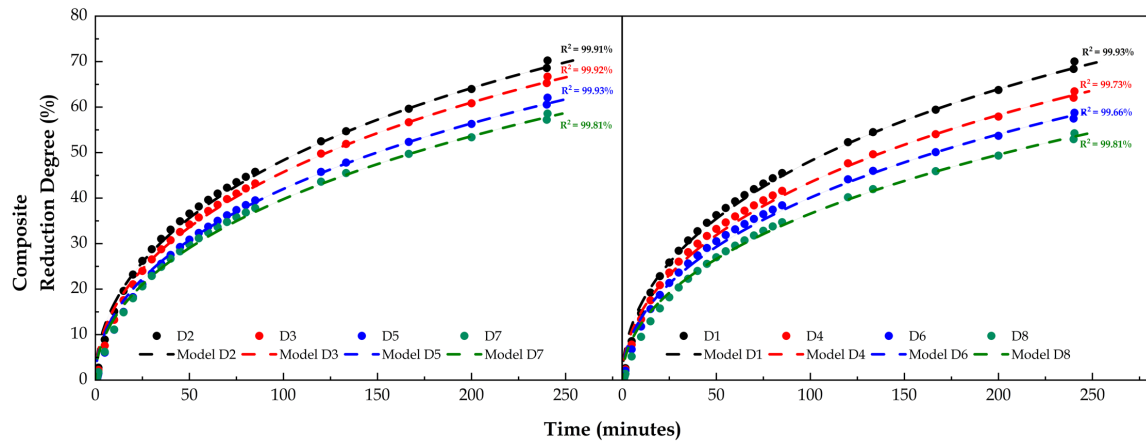


Figure 6. The composite reduction degree of iron and chromium in the chromite reduced at 1400°C, combined with a kinetic fitted model (D1 – D8).

Meanwhile, Figure 7 displays the experimental result of reduction test at 1200°C fitted with ash layer diffusion equation. The same trend of having a high degree of reduction on smaller pellets can also be observed too in the graph. The larger the pellet, the lower the reduction degree of the pellet. The pellet with 17.41 mm size exhibited a reduction degree of 37.28%. This is 5.2 percentage points lower than the reduction degree of the pellet with a diameter of 15.97 mm.

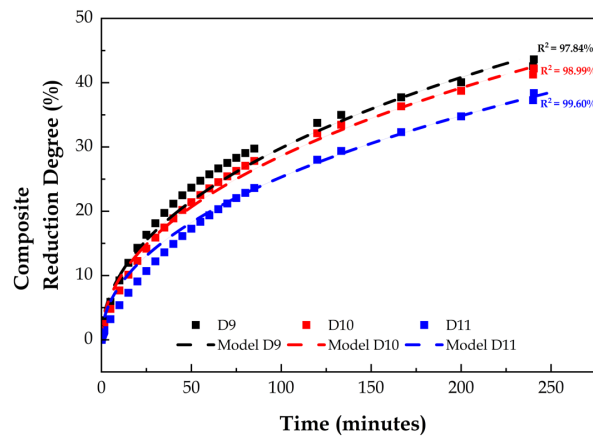


Figure 7. The experimental data of composite reduction degree of iron and chromium in the chromite reduced at 1200°C combined with kinetic fitted model.

Ash layer diffusion kinetic equation was selected to be displayed because it has the highest degree of correlation to all experimental results. It performs better compared to the chemical reaction control equation for all pellets tested on the temperature of 1400°C and 1200°C indicated by R-square ( $R^2$ ) value shown in Table V. This finding suggests that the chromite formed non-porous product layer (metallic iron chromium alloy), which inhibits either the diffusion of incoming hydrogen gas or inhibits the discharge of water vapour molecule, during the hydrogen reduction process. Therefore, the time required to penetrate this product layer become the rate limiting step for the hydrogen reduction of chromite. This concept is similar to the CO-based chromite reduction as reported by Kekkonen *et al.*, (1995) although Xiao *et al.*, (2001) explained that mixed of diffusion and chemical reaction control dominates the CO-based reduction behaviour.

Table V: The goodness of fit shown by the  $R^2$  Value (%) of each individual kinetic equation

| Sample Code | Reduction Temperature | $R^2$ Value (%)     |                           |               |
|-------------|-----------------------|---------------------|---------------------------|---------------|
|             |                       | Ash Layer Diffusion | Chemical Reaction Control | Rosin-Rammler |
| D1          | 1400°C                | 99.93               | 70.95                     | 99.88         |
| D2          |                       | 99.91               | 69.89                     | 99.87         |
| D3          |                       | 99.92               | 71.57                     | 99.87         |
| D4          |                       | 99.73               | 65.85                     | 99.84         |
| D5          |                       | 99.93               | 73.93                     | 99.82         |
| D6          |                       | 99.66               | 65.65                     | 99.79         |
| D7          |                       | 99.81               | 69.03                     | 99.79         |
| D8          |                       | 99.81               | 70.75                     | 99.72         |
| D9          | 1200°C                | 97.84               | 48.28                     | 99.40         |
| D10         |                       | 98.99               | 62.39                     | 99.45         |
| D11         |                       | 99.60               | 76.71                     | 99.47         |

Moreover, unexpectedly, Rosin-Rammler equation also shows a good fit on the experimental results. Figure 8 displays that Rosin-Rammler and ash layer diffusion kinetic equation has a similar degree of correlation level, while the chemical reaction control equation does not fit well. From experimental results shown in Figures 6 and 7, pellet size indeed has a considerable effect on the reaction kinetics. The smallest pellets in each set of experiments (D1, D2 and D9) exhibit the highest reduction degree. Conversely, the large pellets (D7, D8 and D11) consistently exhibit the lowest reduction degree in each respective set of experiments. This relationship may explain why the Rosin-Rammler equation performs well for the reduction degree's curve fitting. However, this equation is proposed initially for kinetic model fitting by assuming that the observed kinetic value is a distributed value rather than a singular value which is affected by the size distribution of the test sample (Vinnert and Waters, 2010b). Therefore, it would be interesting to perform the model for further work involving the packed-bed pellet reduction test as it would be a realistic representation of an industrial operating condition.

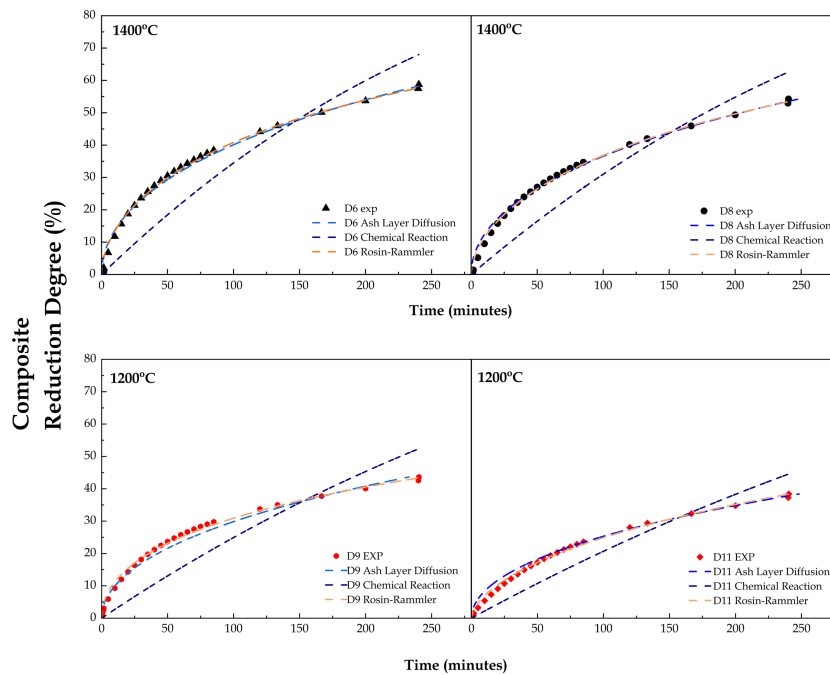


Figure 8. Visualization of three kinetic equations fitted on experimental results of D6, D8, D9 and D11.

In the framework of shrinking core model, especially the ash layer diffusion control, the correlation between pellet size and the observed kinetic is depicted by equation 5 (Levenspiel, 1998b). Parameters in this equation represent radius (R), observed ash layer product kinetic (k) listed in the Table VI, molar density of solid reactant ( $\rho_B$ ), stoichiometric coefficient of solid reactant (b), effective diffusivity of the gas reactant through the ash layer product ( $D_e$ ), and bulk gas reactant's molar concentration ( $C_{ag}$ ).

$$k = \frac{6bD_e C_{ag}}{\rho_B R^2} \quad [5]$$

The value of the bulk gas molar concentration can be determined using ideal gas law concept as shown in the equation 6. The P represents the partial pressure of the gas reactant which equal to 1 atm or 101.325 kPa. Meanwhile, R is the gas constant equal to 8.314 Joule. mol<sup>-1</sup>. K<sup>-1</sup> and T is set to be the holding temperature of the experiment (in Kelvin). In addition, the stoichiometric value of the solid reactant is assumed to be 0.5 by following the reaction displayed in the equation 7. This reaction was chosen because the presence of metallic iron and chromium was confirmed as discussed in the previous discussion related to the microstructure observation.

|                                                                                                                                                                                           |     |
|-------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------|-----|
| $C_{ag} = \frac{n}{V} = \frac{P}{RT}$                                                                                                                                                     | [6] |
| $1.5\text{FeCr}_2\text{O}_4(\text{s}) + 3\text{H}_2(\text{g}) \rightarrow 1.5\text{Fe}(\text{s}) + \text{Cr}(\text{s}) + \text{Cr}_2\text{O}_3(\text{s}) + 3\text{H}_2\text{O}(\text{g})$ | [7] |

Table VI: The kinetic value obtained from kinetic model fitting using Equation 2 for each pellet size

| Item                                                          | D1     | D2    | D3    | D4    | D5    | D6    | D7    | D8    | D9     | D10   | D11   |
|---------------------------------------------------------------|--------|-------|-------|-------|-------|-------|-------|-------|--------|-------|-------|
| initial diameter (d°-mm)                                      | 13.93  | 14.03 | 14.93 | 15.59 | 15.64 | 15.97 | 15.92 | 17.81 | 15.97  | 16.55 | 17.4  |
| Kinetic parameter (x10 <sup>-3</sup> - minute <sup>-1</sup> ) | 1.007  | 1.016 | 0.894 | 0.796 | 0.736 | 0.662 | 0.650 | 0.539 | 0.345  | 0.314 | 0.242 |
| reduction temperature (T <sub>R</sub> )                       | 1400°C |       |       |       |       |       |       |       | 1200°C |       |       |

Figure 9 shows the correlation of the pellet diameter with the observed kinetic rate of ash layer diffusion. By fitting the observed kinetic rate with the equation 5, the effective diffusivity of hydrogen gas to the chromite pellet at the holding temperature of 1400°C and 1200°C could be determined as shown in the Table VII.

Table VII: The kinetic value obtained from kinetic model fitting using Equation 2 for each pellet size

| Holding Temperature (°C) | Effective Diffusivity (De) in m <sup>2</sup> .s <sup>-1</sup> |
|--------------------------|---------------------------------------------------------------|
| 1400                     | 6.964 x 10 <sup>-7</sup>                                      |
| 1200                     | 2.730 x 10 <sup>-7</sup>                                      |

In addition, it can be also be seen from the Figure 7 that the effect of pellet size is significant in the experiment performed at 1400°C. On the other hand, the experiment conducted at 1200°C shows that the observed kinetic rates have lower sensitivity toward size dependence. It is probably a sign that the kinetic regime has started to be a mixture of ash layer diffusion and chemical reaction control due to lower energy available, or the amount of ash layer product (metallic layer product) is much less than the pellet reduced at 1400°C. These two probabilities may also be the reason why the ash layer diffusion fitting line has a generally lower degree of correlation (R<sup>2</sup> value) on the result of the experiments performed at 1200°C (Figure 7) compared to the experiments performed at 1400°C. For the experiment

at 1200°C, with the increasing pellet size, the degree of correlation also increases. This behaviour indicates the ash layer diffusion became the sole rate limiting step again once the pellet size was big enough to make the time required for the hydrogen gas to diffuse into the pellet's core longer than the time required for hydrogen gas to react chemically in the pellet's surface. However, this observation needs to be elaborated further by conducting more experiments at 1200°C and even lower temperature as well as more size variation of pellets.

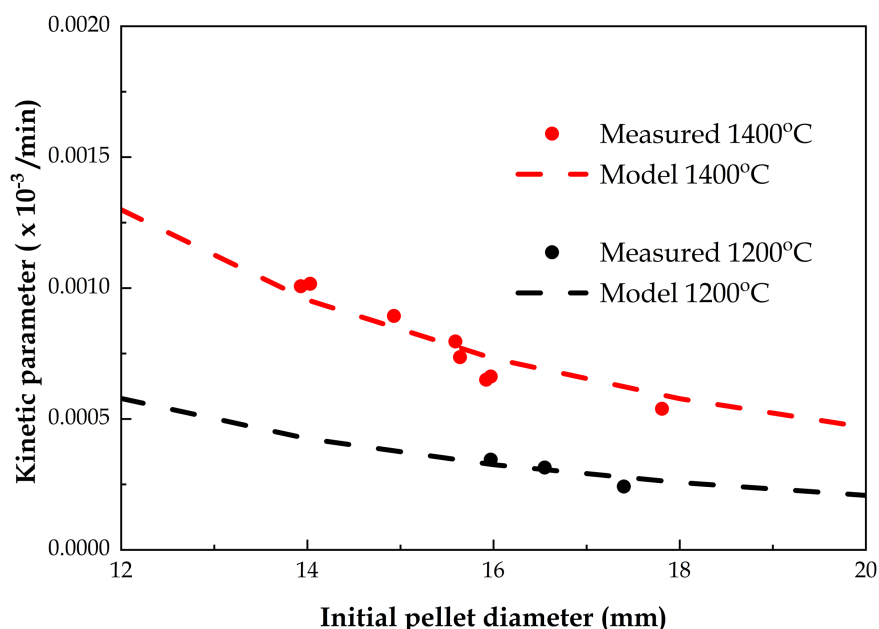


Figure 9. Model and measured observed ash layer diffusion kinetic as a function of initial pellet diameter.

## CONCLUSIONS

This paper aimed at studying the effect of pellet size on the hydrogen reduction of chromite pellets using a laboratory-scale furnace. The results of the reduction experiments conducted at 1400°C and 1200°C indicate that the smallest pellets consistently achieved high CRD values while the largest pellets exhibited the smallest CRD, highlighting the role of particle size in solid-gas interaction. Based on SEM observations of samples from the experiments, the gaseous hydrogen successfully reduces the iron completely and partially the chromium part into their metallic phase at 1400°C and 1200°C. The bright phases observed in the SEM images confirm the generation of the metallic iron-chromium phase. Furthermore, EDS point analysis indicates that the pellet reduced at 1400°C contains more chromium than the reduced pellet in the 1200°C experiments.

The ash layer diffusion function effectively describes the rate-limiting step in the hydrogen reduction of the chromite pellets. This model enables the determination of individual reaction rate constants for various pellet sizes. Furthermore, by estimating the effective diffusivity of hydrogen toward the chromite pellet, a correlation between pellet diameter and ash layer diffusion kinetic rates can be observed for pellets reduced at 1400°C and 1200°C under hydrogen gas environment. These findings underscore the importance of feedstock size in optimising reduction performance and reactor design.

In addition, the Rosin-Rammler function provides a good fit to all experiment results. This approach offers a promising framework to describe the effect of chromite pellet size distribution on the distribution of the observed kinetic rates. Thus, further packed-bed pellets experiments are required to elucidate this relationship further. This may be an important addition for future works as the kinetic rates will be presented as distributed values affected by size distribution instead of just a singular deterministic value, which is a more realistic depiction of an industrial operating condition.



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# Fundamental principles of electric furnace start-ups: Theory, applications and case study

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Start-up is a critical phase during any electric furnace project that requires careful planning as they are challenging and very different from normal operations. Ensuring a successful furnace start-up that maintains long-term furnace integrity requires a clear understanding and a strict adherence to fundamental start-up principles and philosophy.

This paper presents and discusses fundamental start-up principles for the various phases of a typical electric furnace start-up. A variety of topics are discussed for each phase:

- Start-up Preparation (management, commissioning, sacrificial instrumentation, initial charge, diverter chutes)
- Burner Preheat (freeboard target temperature and rise rates)
- Electric Preheat (refractory temperature rise rates and hearth brick joint gap closure)
- Slag Pool Formation (pool formation and growth)
- Feed Addition (feed matte/metal bearing materials to reach tapping levels)
- Ramp-up to Normal Operation (reach nameplate capacity).

These start-up principles can be applied to a variety of commodities (ferronickel, platinum group metals, etc.), furnace types (AC vs DC, circular vs rectangular) and start-up types (green hearth with pool formation via solid slag, green hearth with bath formation via molten slag charge, frozen heel reheats after sidewall rebuilds, etc.).

This paper discusses at a high level how fundamental start-up principles were adapted and applied in recent start-ups performed by Hatch to ensure successful start-ups. The paper also presents a detailed case study of the recent start-up of a platinum group metals electric furnace at Sibanye-Stillwater Metallurgical Complex in Montana, USA. Data from the start-up is presented and analysed and lessons learned are summarised.

**Keywords:** Electric Furnace, Start-up, Platinum Group Metals, Pyrometallurgy, Furnace Integrity, Safety

## INTRODUCTION

An electric furnace start-up is a significant deviation from normal operating practices as they are performed infrequently and the overall process is outside of typical operating windows. This requires specialised expertise to ensure a safe and successful start-up. Hatch has over 50 years of experience designing and starting up electric furnaces over a wide range of commodities. This paper provides an overview of Hatch's latest methodology and best practices for electric furnace start-ups. A case study has been included for a recent platinum group metals (PGM) furnace start-up at Sibanye-Stillwater Metallurgical Complex (SSMC) in Columbus, Montana, USA.

This smelter has been operating for over 25 years with two Hatch designed furnaces. The primary focus of this paper is on electric furnace start-ups on a green (new) hearth that requires the slag pool to be built outwards from the electrodes by smelting solid slag, but other start-up variations are also highlighted.

### *Electric Furnace Start-up Objectives*

It is crucial to highlight the objectives of an electric start-up as they define the priority of actions that are taken throughout a start-up. The main objectives of any electric furnace start-up include:

- Ensure that furnace integrity is maintained to not impact the campaign life by having a controlled heat up rate
- Achieve zero safety incidents by completing Job Hazard Analyses (JHAs) for critical tasks and enforcing a safety culture where all personnel are aware of the furnace risk and status at all times
- Minimise delays to the start-up schedule by planning ahead, aligning all involved parties on the status of the furnace and ensuring commissioning activities are completed well before systems are needed.

A furnace start-up inherently is a different mode of operation compared to normal operations and there will typically be unforeseen delays. The goal is always to have mitigations present to minimise these delays while not compromising on furnace integrity or safety.

## DESCRIPTION OF ELECTRIC FURNACE START-UP METHODS & PROCEDURES

### *Overview*

The main methodology for completing a furnace start-up is to gradually heat and expand the working refractory lining to the extent that all the brick joints are closed before they are exposed to molten material. This minimises the risk of hearth infiltration. Once the bricks are tight, a molten bath can slowly be grown to normal operating levels. Every start-up is unique and requires a custom start-up plan based on the commodity, site and type of furnace. However, a typical Hatch start-up plan for a green hearth furnace utilising solid slag for pool building can be broken down into the following phases:

- **Start-up Preparation:** Various activities to support the start-up, including pre-start-up checklists, installation of start-up charge, and installation of sacrificial hearth heat-up monitoring instrumentation.
- **Burner Preheat:** Burners are ignited to commence start-up and provide initial heating of the start-up charge and the crucible.
- **Electric Preheat:** Controlled heating of the central hearth by arcing on a coke bed.
- **Slag Pool Formation:** Slow growth of the molten slag pool to the edges of the furnace by feeding slag at or near the electrodes.
- **Feed Addition:** Feed the furnace with typical feed material to grow bath levels to allow for tapping of slag and matte/metal.
- **Ramp-up to Normal Operation:** Increase furnace power levels and associated feed rates to reach nameplate capacity.

The control and operation of the furnace varies for each phase and the decisions to switch from one phase to the next are made based on continuous observations of conditions in the furnace (via visual inspections and monitoring of temperature, bath level and expansion). Each phase is described in greater detail in the sections below. It should be noted that the procedure provided herein is an updated version of that which was presented by Hatch in 2009 (Stober, *et al.*, 2009); this new procedure accounts for the lessons that Hatch has learned over the past 16 years.

### *Start-up Safety*

The tasks performed during a furnace start-up deviate significantly from typical day-to-day operations of a furnace. As such, it is critical that all personnel involved take special precautions to ensure their safety and the safety of others. This involves performing systematic safety reviews like JHAs for every

activity that will be performed by the start-up team. Hazard mitigating actions need to be taken before and during the start-up and constantly be reassessed to decide how best to address any safety concerns that arise.

In particular, furnace inspections are a critical task for completing a start-up but have inherent risks due to looking inside a furnace. Hatch has mitigated the risk of hot gases ejecting through inspection ports in the past by a combination of installing safety shields, wearing tappers PPE, increasing the furnace draft, locking out power, and shutting off the feed system.

#### ***Start-up Management Structure***

Due to the complex nature of a furnace start-up, an integrated team of the client, consultant and burner contractor personnel is recommended for a successful start-up. Each team has dedicated roles to fill as the client has in-depth information about their site and are responsible for operating all the equipment, whereas the consultant and contractors have specialised expertise on start-up of their furnace.

It is typically recommended to split up the management structure into a Start-up Team and a Steering Committee. The Start-up Team is comprised of client, consultant and key contract personnel that are on site and responsible for the day-to-day monitoring and operation of the furnace start-up. The Steering Committee is comprised of senior personnel that can act as decision-makers during key milestones and can be consulted when unusual circumstances arise or deviations from the plan are required.

#### ***Start-up Preparation Activities***

Several prerequisite tasks and activities must be completed before the start-up begins. It must be ensured that all systems, equipment and material required for the successful start-up and operation of the furnace are commissioned and available prior to milestone dates for which they are required to progress start-up and avoid holding delays. It is important that these tasks are completed and documented before heat-up to allow proper monitoring of the furnace and mitigate damage to the furnace during the critical phases. These start-up preparation activities typically include:

- Completing the construction and commissioning pre-start-up checklist for all furnace and ancillary components (e.g., cooling water is leak-pressure tested and running, critical instrumentation is online). This document will be formally handed over to the start-up team with a punchlist of outstanding items prior to lighting the burners.
- Installing sacrificial thermocouples on the top of the upper hearth working lining and within the lower hearth safety lining. The primary purpose of the upper hearth thermocouples is to monitor temperature rise rates with a lower time lag compared to permanent hearth thermocouples, while the lower hearth thermocouples provide redundancy when the upper hearth thermocouples fail and provide an indication of temperatures achieved for expansion paper burn out.
- Applying start-up ram to protect the hearth and its sacrificial instrumentation from mechanical damage and/or erosion from exposure to superheated slag during start-up.
- Placing start-up charge in the furnace in a manner which allows for the commencement of electric heating and the later addition of material to form a slag bath. This typically includes steel cans, steel rails, coke and graphite blocks directly under the electrodes to provide the required conductivity for electric preheat and then either slag or scrap metal (depending on the commodity's pay metal generation rate) surrounding the electrodes to provide a berm to contain the initial slag pool formation.
- Installing temporary diverter chutes to allow for redirection of coke and solid slag directly underneath the electrodes to replace it as it is consumed.
- Installing steel chains that hang in the furnace and provide a fixed reference point to track electrode tip position as the electrodes are consumed.
- Adjusting binding loads to the correct pre-start-up working loads and the as-built refractory measurements are completed to establish a baseline and sounding references.
- Installing a larger diameter tapping hole, water-cooled copper nozzle (i.e., slag insert or slag 'monkey') in the slag tapblock to allow for easier initial slag taps during the start-up, until the slag reaches normal physical and chemical properties.



- Starting the air and water cooling systems as it was determined that keeping these systems off had limited benefits in terms of accelerating heat transfer and introducing water flow to hot coolers leads to steam generation and the risk of significant pressure surges.
- Ensuring that grounding straps are disconnected until a metal/matte bath is formed to limit stray current to earth.

Note that special care needs to be taken to ensure that all tasks are completed in a way that avoids hydration of the furnace refractory and start-up materials that could lead to furnace damage.

### ***Burner Preheat***

The first phase of the start-up involves heating up the crucible with burners. This will dry-out/cure the refractory/castables and start-up materials. This will also begin heating of the furnace more generally, including the electrodes and peripheral refractory of the furnace. Heat is supplied by burners to increase the freeboard temperature gradually (typically 10°C/h). The freeboard temperature is typically held at approximately 120-200°C and 500°C for removal of moisture and bound water, respectively. The freeboard temperature can then be increased to the target setpoint, which should typically be limited to 800°C as significant electrode oxidation tends to occur at higher temperatures due to burner combustion gases reacting with the carbon electrodes based on the Boudouard reaction.

For furnaces with Soderberg electrodes, the burners provide the primary source of heating to bake the electrode paste until sustained operation with high current is achievable; the importance of electrode baking in reducing the likelihood of electrode breakage has been noted by Hatch (Stober, *et al.* 2009) and others (Shu and Dai 1995). From past Hatch experience, the freeboard temperature needs to surpass 500-600°C before Soderberg electrodes can be slipped at a rate of 25-50 mm/h to reach the coke bed from the start-up charge. For furnaces with prebaked or graphite electrodes, it is not necessary to increase the temperature above the 500°C point needed for final curing of castable materials.

Once the freeboard temperature holds are complete and the electrodes are resting on the coke with sufficient available stroke (i.e., ability for electrodes to move down within mechanical limits), the electrical preheat phase can begin. Since burner heat is the only method of heating the furnace crucible except for directly underneath the electrodes, it is critical to plan for burners to be available until molten slag reaches the furnace sidewalls to prevent cooling of the furnace periphery. The method of burner gas extraction also needs to be determined prior to start-up as the primary off-gas systems may not be capable of handling the burner gas flows or temperatures, with many operations opting to use emergency stacks until burners are shut off.

### ***Electric Preheat***

During the electric preheat phase, the goal is to arc on coke to gradually heat the refractory in the centre of the furnace underneath the electrodes and close the hearth brick joints before they are exposed to molten material. This phase continues until the upper hearth brick joints in the centre of the furnace have closed; this is inferred based on a predicted theoretical temperature and expansion monitoring.

The furnace (specifically the hearth refractory) is slowly heated by manually operating the furnace electrodes with low power levels and using coke to create a conductive path. The electrodes are operated in manual to allow for tight control of electrical parameters and to reduce the risk of electrodes boring into the hearth. The freeboard temperature is maintained at the desired setpoint using a combination of electrical energy and heat from the burners. The electrode power is manually adjusted to maintain a gradual temperature rise rate of the upper hearth sacrificial thermocouples to allow proper soaking of heat throughout the hearth (typically 3-5°C/h underneath the electrodes). These average rise rates are targeted as there will occasionally be local excursions above the average and this minimises the chance of rise rates exceeding typical castable limits of 20°C/h. Care must be taken to ensure that all areas of the furnace are rising at approximately the same rate to minimise impacts of differential expansion. To achieve these rise rates, intermittent power is typically required at the beginning of this phase followed by continuous power later in the phase. When using intermittent power, monitoring the temperature rise rate (1<sup>st</sup> derivative) and the temperature acceleration (2<sup>nd</sup> derivative) can help determine when to

turn the power on and off to avoid significantly overshooting the target temperature rise rate. The temperature acceleration acts as a leading indicator and may indicate that power needs be shut off even when rise rates are low.

It is also vital to know the electrode tip position at this point in the start-up as proximity to the hearth also affects heat-up rate. Based on past Hatch experience, the electrode tips should be maintained at least 750 mm above the top of hearth invert (i.e., top of working lining refractory at the centre of the furnace) for the majority of electric preheat and then reduced closer to 500 mm prior to slag feed to reduce the likelihood of the slag bath spilling to the sidewalls. Chains hung in the furnace are used as a fixed reference point from the hearth invert to determine the electrode tip position as the electrodes are consumed. The electrode tips are slipped to line up with the ends of the chains and the stroke remaining denotes the amount of available travel towards the hearth invert. The chains can be seen in the case study pictorial timeline (

Figure ). When necessary, coke is replenished underneath the electrodes by manually feeding it through the diverter chutes to raise the operating electrode tip position and hoist position to have available stroke.

As the hearth heats up, it will typically shrink as expansion papers burn out, and then it will expand as the bricks become tight. Prior to starting slag bath formation, any binding loads must be increased to at least 90% of working load to achieve tight brick joints in preparation for molten bath formation. Note that a furnace with expansion papers installed may not show significant physical growth before molten material is formed in the furnace, as the region which has reached the target temperature only nominally closes the gaps created by the burned-out papers.

Electrical preheat ends once all upper hearth under electrode thermocouples reach the target temperature (typically 800-1000°C), the electrode tips are in the desired position, and positive expansion has been seen, if achievable.

### ***Slag Pool Formation***

During the slag pool formation phase, solid slag is fed at the electrodes to form molten slag pools underneath each electrode. Relative to the power level at the end of the electric preheat phase, the initial pool formation typically requires higher power to form the initial pool, and then a lower power level for controlled pool growth and to reduce the likelihood of slag breaching the start-up berm to the sidewalls. As energy is supplied through the electrodes to the molten slag, the slag pools spread to form a single connected pool and then grow to the sidewalls. Electrodes are recommended to be slightly immersed in the slag bath at this time (about 100-200 mm below top of slag) for proper control of the power input into the bath. Arcing has been proposed by other authors to control freeboard temperatures (Taleghani and Elsawi 2025), but is typically not recommended as the ratio of arc to bath power is difficult to determine during the transient nature of start-up leading to less control of power into the bath. The periphery sacrificial thermocouples rate of rise is used to control the pool growth and hearth expansion continues as the slag pool expands.

Upper hearth temperatures will continue to maintain the target rise rates as defined in the electric preheat phase, within reason, as a molten slag bath can drive higher rise rates. Most upper hearth sacrificial thermocouples will be destroyed by the end of this phase due to contact with molten slag. Freeboard temperature continues to be maintained at the desired setpoint through a combination of electrical energy and burner heat. However, once the slag pools expand far enough to reach the skew, the burners will only supplement freeboard temperature, particularly during power off, mainly to mitigate electrode integrity risks. They will be removed from the furnace shortly after this point.

The slag pool formation phase is complete and pay metals bearing feed addition can begin, once all of the slag pools are connected and the slag bath has reached the skew level.

### ***Feed Addition***

During the feed addition phase, feed containing the desired pay metals is added to the furnace, raising the slag bath and beginning development of the matte or metal bath. Power may be gradually increased along with feed rate to grow the molten bath. The hearth expansion and temperature are monitored for indication that the hearth is stabilised and sounding measurements are performed regularly to monitor bath levels and get a slag sample to monitor the extent of reduction to decide on fluxing requirements. Soundings will also confirm when a fully connected metal or matte pool has been formed underneath all electrodes, and electrode control can be switched into automatic at this point as a short circuit will occur if the electrodes touch the metallic phase and mitigate hearth damage risks. Some sites will feed high grade metalics at the start of this phase to accelerate matte/metal bath growth and be able to quickly achieve automatic operation and first tap.

First slag tap and first matte/metal tap will occur during this phase. Slag and matte/metal will be tapped once the bath level is sufficiently above the slag taphole and matte/metal taphole, respectively. The hearth power density (defined as the total furnace power level divided by the overall hearth area) typically needs to be at least 60 kW/m<sup>2</sup> for slag tapping to occur but may need to be increased if the slag flow during tapping is poor due to low temperatures or a viscous slag chemistry. One of the greatest risks to the furnace at this point in the start-up is the inability to tap slag, since the bath levels could rise high enough to damage the upper sidewall. Therefore, it is critical to have a good operating factor to maintain heat during this timeframe and ensure that slag tapping equipment is properly functioning to allow for frequent removal of slag. Prior to the matte/metal level reaching the skew, it is recommended to limit the hearth power density to less than 90 kW/m<sup>2</sup> due to the potential for slag attack on the hearth. Power can then be ramped up for tapping of matte/metal.

### ***Ramp-up to Normal Operation***

Ramp-up of the furnace is typically governed by furnace conditions and availability of upstream and downstream systems at the time. Depending on the maturity of the operation, the furnace power is typically ramped up anywhere from 2-5 MW per day. Especially for greenfield smelters, there could be many hurdles with ancillary equipment that require an engineering solution to mitigate and continue ramp up. This is also the time to keep track of furnace process parameters and expansion measurements to have a baseline that can be used for predicting and extending campaign life.

### ***Other Considerations***

Day-to-day furnace operation is complex and provides numerous unique challenges. Furnace start-ups happen infrequently and involve many activities that are not typical for day-to-day operation. As such, it is very common for unexpected incidents to occur during furnace start-ups (power outages, equipment failures, issues with start-up charge or start-up materials). When these incidents occur, it is critical to have the support of a qualified start-up team to react to the situation and adapt accordingly.

Formal and informal risk analysis before a start-up and continuous risk assessment can help to predict and prepare for potential incidents (spare parts, contingency plans, etc.). Further, formal reporting of previous start-ups allows the team to take advantage of previous lessons learned in order to maximise the chances of future start-up success.

## **RESULTS & DISCUSSION FROM A FURNACE START-UP CASE STUDY**

Every start-up is unique and requires a custom methodology; there is no one-size-fits-all approach. Further, every start-up will present unique challenges which need to be carefully considered and overcome. The following sections describe a case study for a recent green hearth start-up with slag pool formation via solid slag addition. More details on different types of start-ups can be found in the ELECTRIC FURNACE START-UP VARIATIONS section.

### Case Study - Overview

The start-up of Electric Furnace #2 (EF2) occurred from 17 April to 7 May, 2025, at SSMC in Columbus, Montana. This start-up was performed after the recent rebuild of the furnace. EF2 is a 6 MW three-electrode rectangular furnace with graphite electrodes. The furnace typically runs with a combination of concentrate and catalyst to generate a slag and PGM rich matte. The slag is skimmed into a pit and returned to the concentrator. The matte is granulated, dried, processed in a Top Blown Rotary Converter to remove iron, granulated/dried, and then refined on site to form the final product.

An overall summary of the EF2 start-up key performance indicators (KPIs) (e.g., freeboard temperature, hearth temperature, furnace power) and milestones (e.g., power on, first slag tap) are shown in Figure 1. The materials fed to the furnace over the course of the start-up are shown in Figure 2. An overall pictorial timeline for the start-up, showing photos of the furnace during different phases is provided in Figure 3. These will be discussed in more detail in the following sections. Due to the location of the smelter, the furnace start-up plan and all operating data were provided in imperial units. The figures have therefore all been presented in imperial units for clarity, but SI units have been included in the text for comparison to the sections above.

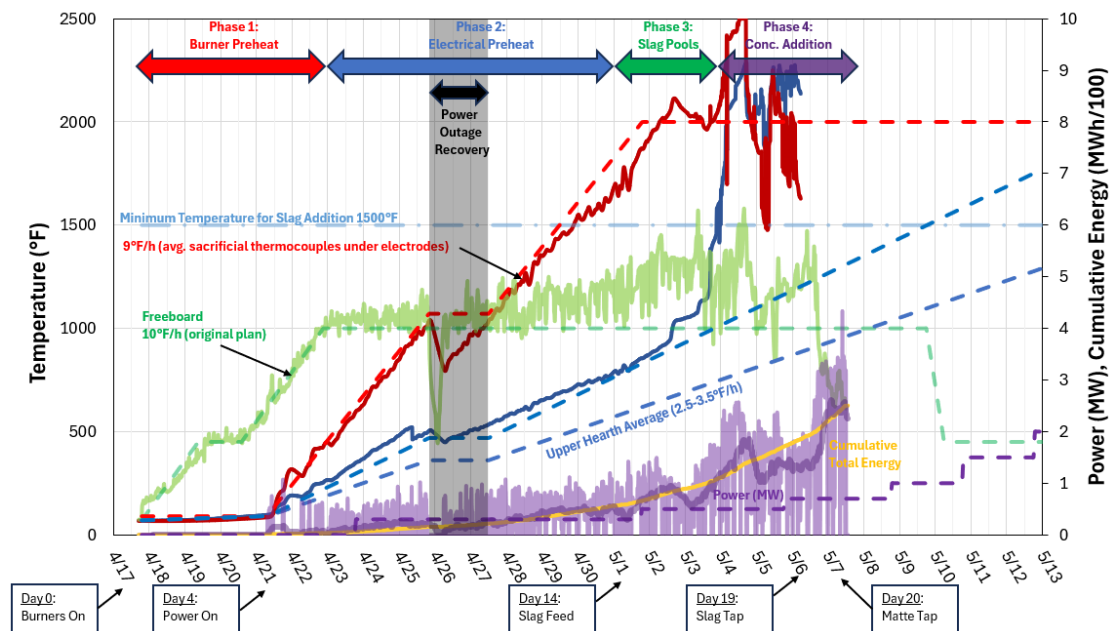


Figure 1: SSMC EF2 Start-up Case Study KPI Overview Graph.

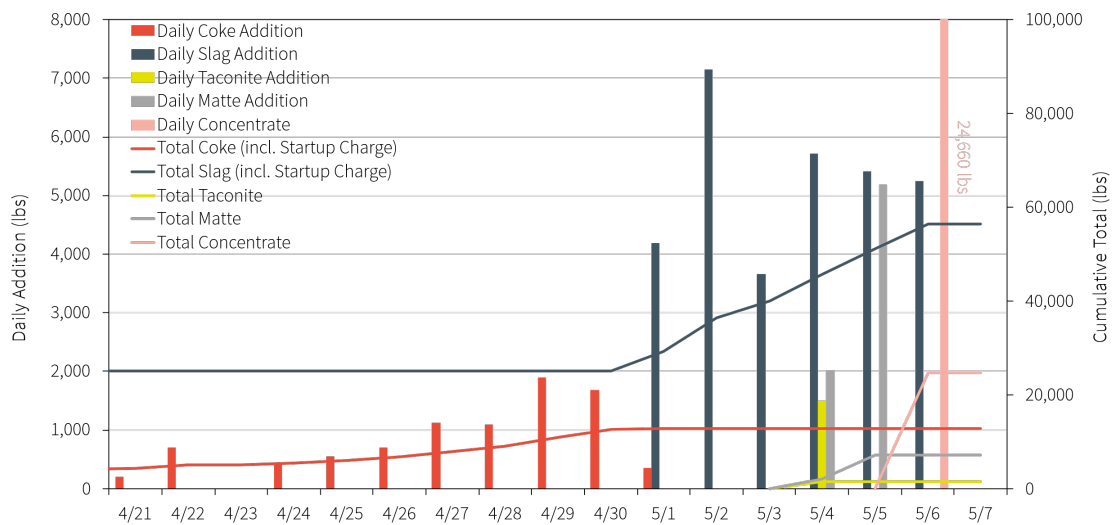


Figure 2: SSMC EF2 Start-up Case Study Start-up Feed.



Figure 3: SSMC EF2 Start-up Case Study Pictorial Timeline.

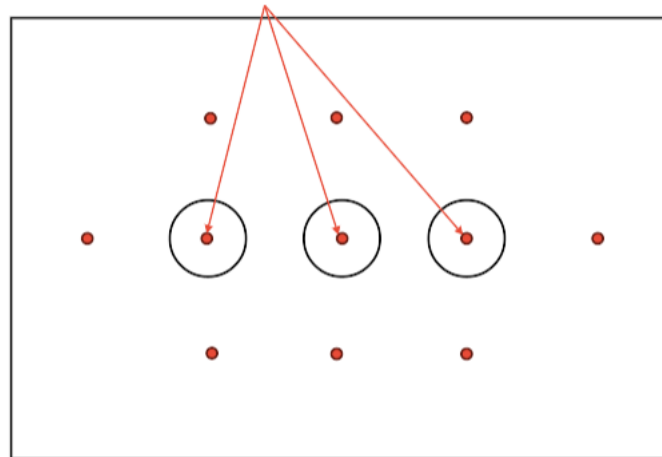
The success of this start-up was due to a variety of factors. However, one very important factor to highlight was the close integration between the Hatch start-up team and the SSMC team. The clear communication and cooperation of the two entities was critical for success. This collaboration allowed the combined start-up team to leverage the different resources and skills possessed by both parties.

#### ***Case Study - Start-up Preparation Activities***

Prior to the start-up, roles were defined for the personnel on the Hatch and SSMC team. Daily meetings were held in the weeks prior to the start-up with the objective of completing the pre-start-up checklist. All critical construction and commissioning activities were completed prior to the decision to light the burners.

As a part of the pre-start-up activities, sacrificial thermocouples were installed on the hearth and covered by ram (sacrificial thermocouple approximate locations are shown in Figure 4). The team then placed the initial charge materials (rebar, graphite blocks, cans, coke, slag, etc.) inside the furnace along with diverters to help with feeding coke and solid slag during the start-up. Chains were also hung from the roof of the furnace in between the electrodes to provide a measurement reference point against which to align the electrode tips (visible in Figure 3). The section view and a photo of the start-up charge are shown in Figure 5.

### Under Electrode Sacrificial Thermocouples



\*Sacrificials are in same location within the safety lining and above the working lining

Figure 4: Locations of Sacrificial Thermocouples.

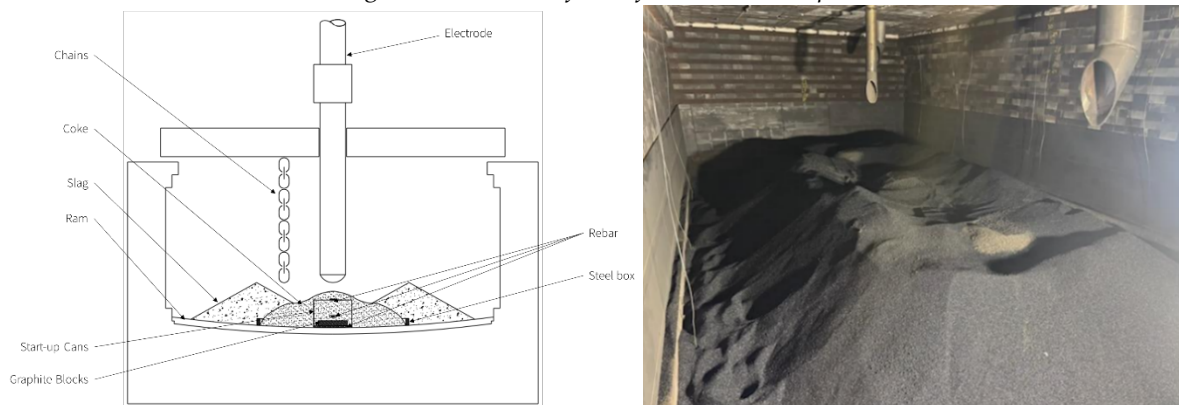


Figure 5: SSMC EF2 Start-up Case Study – Initial Start-up Charge and Diverters.

### Case Study - Burner Preheat

Removable natural gas burners were used to heat the furnace during the burner preheat phase and were managed by a burner contractor. The heat-up plan was closely followed during this phase (10°F/h (5.6°C/h) rise rate, soak at 450°F (230°C), and hold at 1000°F (540°C)) – this is clearly indicated on the start-up overview graph in green (Figure 1). No additional time was required for electrode baking as this furnace uses graphite electrodes. A significant temperature rise in hearth thermocouples was not observed during the burner preheat phase due to the charge covering the majority of the hearth.

### Case Study - Electric Preheat

The furnace was run intermittently and mainly with single-phase power during the early phases of the electrical preheat to control the under electrode sacrificial thermocouple rise rate. As the furnace approached the minimum temperature for slag feed (1500°F (820°C) upper hearth under electrodes) the furnace was run in three-phase mode more frequently and run more continuously. The furnace online time based on phases and power level is shown in Figure 6.

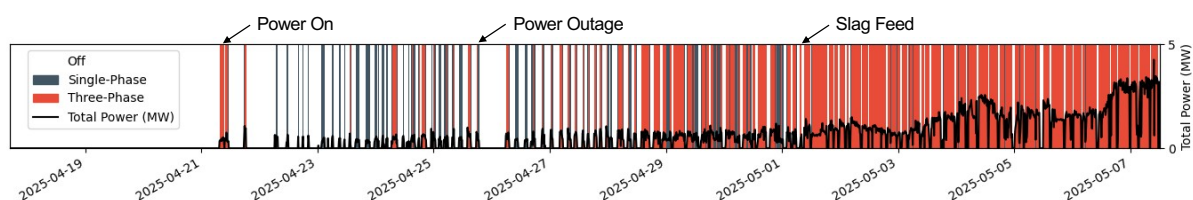


Figure 6 Furnace Power Online Time and Power Level

Note that a power outage occurred on 25 April 2025, which accounts for the uncharacteristically low power at that point in time, this is also shown in the overview figure (Figure 1) where the background of the figure is dark grey. This is one example of unexpected incidents affecting a start-up. The power outage delayed the start-up schedule, since the temperature in the furnace dropped during the period the power was out. However, there was no damage to equipment and no further delays due to the reaction from the start-up team and decisions made by the integrated start-up team in order to adapt and recover from the incident.

The target temperature rise rate was selected as 9°F/h (5°C/h) for the upper hearth sacrificial thermocouples under the electrodes (Figure 1 in red). This results in roughly a 3.5°F/h (1.9°C/h) average temperature rise rate for all the upper hearth thermocouples (Figure 1 in blue). This was closely adhered to apart from during the power outage when the temperature dropped by 200°F (110°C), delaying the schedule by roughly two days. In order to know when to increase, decrease, and turn off the power, the temperature rise rates and the rate of change of the temperature rise rate (i.e., temperature acceleration) are monitored. Figure 7 shows that the temperature acceleration acts as a leading indicator for the temperature rise rate as changes in the temperature acceleration will be seen before changes in temperature rise rate. This helps maintain good control over the temperature rise rate and helps to avoid instances where the temperature rise rate is far above target.

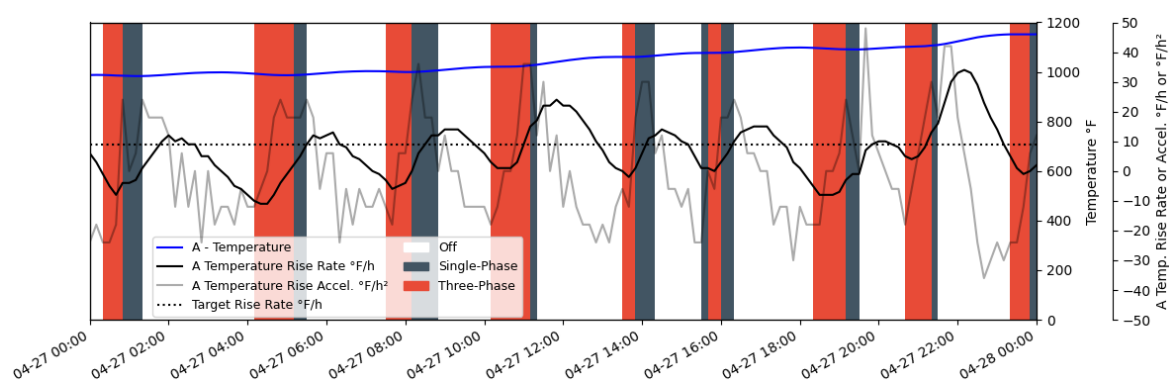


Figure 7: Temperature Rise Rate and Rise Acceleration Under Electrode A over a 24 h Period.

Total power did not exceed 1 MW during the electrical preheat phase. The average power values and cumulative power can be seen in the overview plot in purple (Figure 1). The power outage on 25 April is clearly shown here by the prolonged 0 MW average power reading.

During the electrical preheat phase, 9000 lbs (4100 kg) of coke were consumed and replenished by manually feeding the diverter chutes. The amount of coke fed each day is seen in Figure 2 in red.

When possible, the furnace interior was inspected and photos were taken. This helped monitor quality of the electrodes and allowed the team to see if any of the electrodes were becoming pencilled (i.e., narrowing at the tip). It is important to monitor electrodes with a pencil tip shape that will result in significantly higher electrode resistance than expected; this has been observed and noted by others as well (Shoko, *et al.* 2022). Some pencilling of the electrodes can be observed in Figure 3.

### Case Study - Slag Pool Formation

Solid slag was fed to the furnace once the temperature under the electrodes had reached 1700°F (930°C), well above the 1500°F (820°C) minimum temperature (Figure 1). Lower hearth sacrificial thermocouples were reading temperatures that exceeded expansion paper burn out and positive expansion of the hearth was observed prior to this point in time. The quantities of slag fed during this phase are shown in Figure 2 in navy blue.



The slag was piled near the electrodes and then melted by the electric heat from arcing on the coke, forming the initial conductive molten slag pools. Once stable pools had formed, the electrodes were slightly immersed in the slag pool, and dip tests were completed regularly at low voltage tap to determine electrode tip depth in the slag. After slag pools were formed, regular furnace soundings were performed and samples were assayed to determine the composition of the slag. During this phase coke is no longer fed to the furnace, as any additional coke and even residual coke from the electrical preheat phase will reduce iron from the slag, lowering the slag iron content. Taconite was added to compensate for this phenomenon (see Figure 2 in yellow). The additional iron increased the conductivity of the slag and allowed for the electrodes to be slightly immersed in the slag bath for better distribution of heat throughout the furnace.

Solid slag continued to be added to the furnace and power was increased, growing the size of the slag pools until the slag surpassed the slag berms from the start-up charge or the berms dissolved. One day after slag pool formation some of the peripheral sacrificial thermocouples showed indication of heat from the slag bath reaching these locations with no indication of slag 'over-topping' the berm. Slag reached the skew four days after the initial slag feed. Pictures of this time period are shown in the pictorial timeline (Figure 3).

#### ***Case Study - Feed Addition***

Once slag reached the skew of the hearth, granulated matte was added to the furnace to accelerate formation of the matte layer, followed by concentrate addition. The amount of matte and concentrate added throughout the start-up is seen in Figure 2. Soundings were continued during this time to monitor matte and slag levels. Furnace power control was switched from manual to automatic mode once a verified matte bath was established underneath all electrodes.

Once the slag level had reached 5 inches (127 mm) above the slag tap-hole, the first slag skim was performed. After matte was 2 inches (51 mm) above the matte tap-hole level, the first matte tap was performed 20 days after the burners were ignited. This signified the end of start-up and it occurred three days ahead of schedule with no safety incidents. After first slag and matte tap, operation continued to be gradually ramped up until EF2 was operating at its nominal rate.

## **ELECTRIC FURNACE START-UP VARIATIONS**

The following sections highlight the impacts of different site conditions, commodities, and furnace types that create variations of the Hatch start-up methodology compared to a furnace with a green hearth and solid slag feed as discussed above.

#### ***Site Conditions - Green Hearth Furnace with Molten Slag Available***

When a large volume of molten slag is available on site that can be fed to an electric furnace, this provides the opportunity to reduce the overall start-up duration by preheating the furnace only with burners with no primary charge followed by a molten slag charge. Compared to the start-up methodology mentioned in the sections above, start-up ram is no longer installed as this would insulate hearth bricks. Typically only a small amount of coke and steel underneath each electrode is added to facilitate contact if not made with molten slag. The typical methodology for the start-up is an extended burner preheat where the freeboard temperature rises at 5-10°C/h to the desired soaking point of 800-1200°C. The furnace is held at this temperature for several days until the lower hearth sacrificial thermocouples surpass the expansion paper burn-out temperature around 400°C, where confirmation of burn-out is indicated by furnace expansion measurements. Power is then initiated and molten slag is fed to the furnace immediately after. Feed addition can then begin to raise the slag and metal/matte bath to achieve the first tap.

This type of start-up is primarily used on slag cleaning furnaces as there is always a significant quantity of molten slag available upstream. SSMC completed this type of start-up with Hatch's assistance on Electric Furnace #1 in 2017 when it was running as a slag cleaning furnace. The start-up was completed



in 14 days compared to 20 days when the slag pool was built using solid slag. Figure 8 shows a summary of the major KPIs of this start-up (note that the figure uses imperial units).

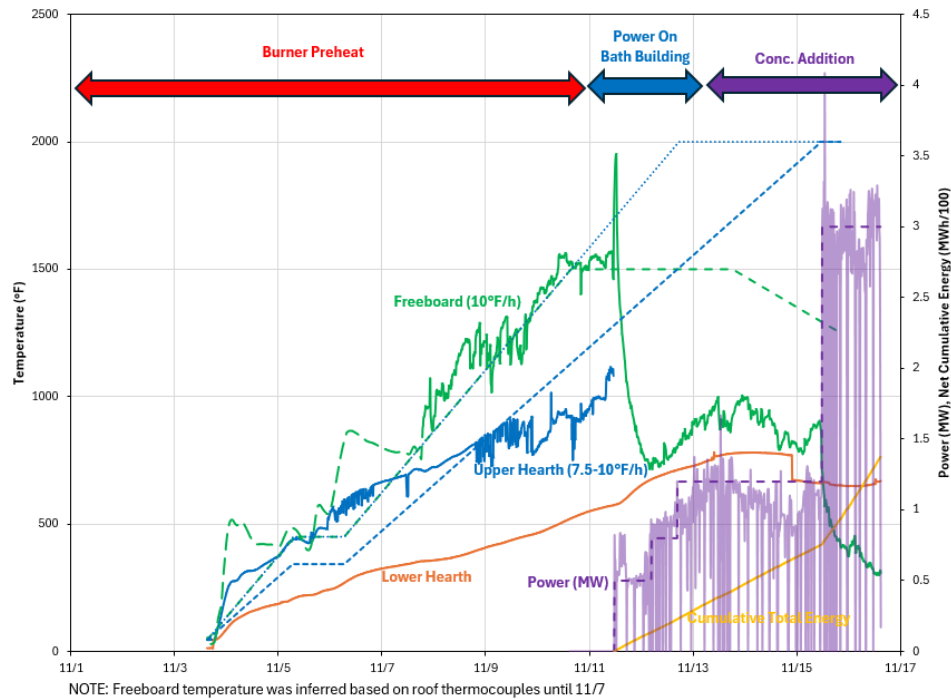


Figure 1: SSMC EF1 Green Hearth Start-up with Molten Slag KPI Overview.

#### Site Conditions – Furnace Reheat with a Frozen Slag Bath

A furnace reheat after the slag bath completely freezes over is required for a variety of reasons, with the main ones being either after a sidewall/endwall rebuild or if there was a power failure that required the electrodes to be lifted out of the bath. Since sacrificial thermocouples cannot be installed on the hearth for this scenario, permanent hearth thermocouples are used as the KPI with a target rise rate of approximately 1-2°C/h back to nominal operating temperatures to minimise thermal stress on the bricks and allow for closure of any gaps that may have formed prior to molten bath exposure. Since frozen slag is not conductive, a layer of coke and steel are required as a start-up charge to allow for current to travel between electrodes until the top of the slag bath melts.

After the initial arc, power is controlled to form and grow out the pool while maintaining the target rise rates. Feed can be restarted once the pool is completely melted outwards to the sidewalls and down to the hearth (as much as feasible depending on typical heel thickness). The duration of this type of start-up typically depends on the downtime as the heat lost from the furnace needs to be replenished.

SSMC completed this type of start-up with Hatch's assistance on Electric Furnace #1 in 2024 after an endwall replacement caused the bath to freeze. The Hatch site team mobilised quickly to complete this start-up and it only took nine days once power was re-established to get the first matte tap. Figure 9 shows the permanent hearth thermocouple temperature readings after the power failure and the rise rates during the reheat (note that the figure uses imperial units).

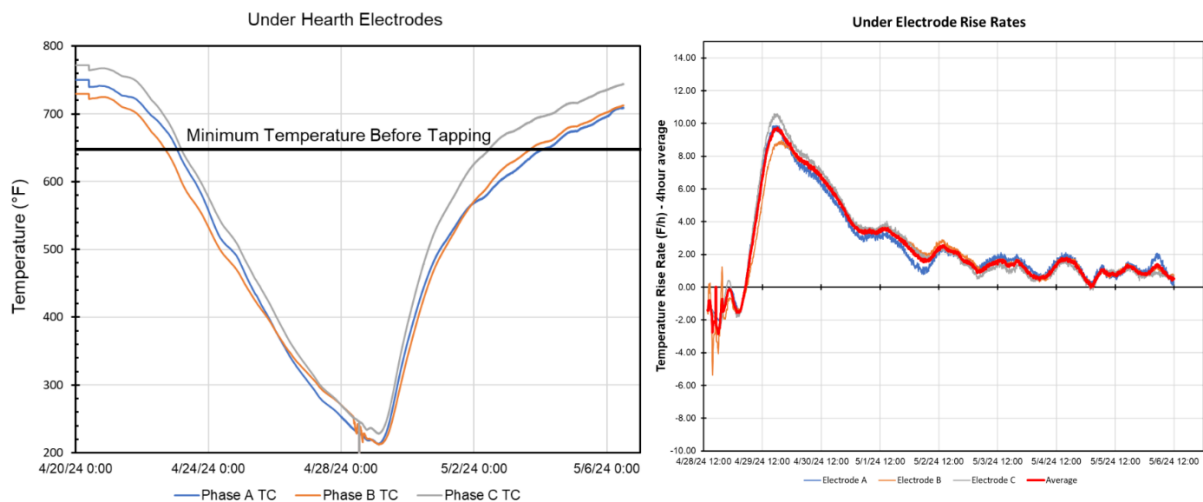


Figure 2: Permanent Hearth Thermocouple Temperatures and Rise Rates for SSMC Furnace Reheat.

### Impact of Different Commodities

The type of commodity can have an influence on the start-up charge and the overall approach for start-up. In particular, the generation rate and density of the matte/metal phase have a major impact on the overall start-up duration and influences the composition of the start-up charge. For ferronickel furnaces, the low metal fall (i.e., percentage of metal in feed) from calcine and high density of ferronickel would require several weeks to build the required levels for tapping. Due to this fact, the start-up charge typically uses steel shot, scrap metal or crushed FeNi as the outer berm instead to accelerate the formation of a metal bath. The amount of steel added will dilute the first several metal taps with iron and a trade-off is required to determine the potential time-saving from the charge vs the number of taps that are off specification. For PGM furnaces and other commodities that generate a matte, the duration to build the matte bath is typically not as long as FeNi due to most concentrates having a high matte fall. In this case, slag is usually used as the berm for the start-up charge but occasionally metallic reverts or recycled matte is fed to the furnace during start-up to shorten the overall duration.

### Furnace Type – Circular vs Rectangular

The overall start-up methodology for a circular vs a rectangular furnace does not vary significantly. There are some nuances for each furnace type though. For example, circular furnaces require a start-up charge where the coke and steel rails form a delta arrangement to be underneath each electrode, whereas rectangular furnaces have a straight arrangement. Binding adjustments are more frequent for rectangular furnaces and require significant planning with the maintenance team to ensure springs are at the desired working loads prior to start-up and before slag pool formation.

### Furnace Type – AC vs DC Electrical System

The vast majority of electric furnaces use an AC electrical system where the start-up methodology mentioned above applies in that the slag bath is formed before the metallic phase. DC furnaces typically require the metallic phase to be formed first to ensure current path from the electrodes (cathodes) to the anodic hearth. Frozen slag is a risk to the current path and is added later in the process once a molten metal bath is present to reduce the risk of freezing. The start-up charge used is a large pile of metal, typically as shot or scrap. The rest of the start-up methodology still applies as the goal is still to slowly heat up the hearth and ensure the hearth refractory gaps are closed prior to seeing molten metal.

## CONCLUSIONS

Hatch's latest framework for multiple variations of electric furnace start-ups has been summarised in this paper. As mentioned, start-ups occur infrequently and require specialised expertise compared to

typical operations. Hatch develops customised procedures for each start-up as there is no one-size-fits all approach. This methodology was applied at SSMC's EF2 start-up to achieve a successful outcome due to the close collaboration between the Hatch and SSMC team. Hatch also has experience in several other types of start-ups including flash furnaces, converters, and novel electrolytic cell applications.

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Jordon is a process engineer with over 10 years of pyrometallurgy experience with Hatch. He has dealt with a range of processes including copper, ferronickel, platinum group metals, magnesium, and ferrochrome. His experience primarily includes the start-up and operation of electric furnaces, as well as equipment design of smelters.

Jordon has been the Furnace Start-up Manager for several electric furnace start-ups throughout his tenure at Hatch. He was responsible for managing a multidisciplinary team of engineers to ensure that each start-up was completed on schedule while minimizing risks to furnace integrity.

# PGM furnace hearth growth: A thermal perspective for operators

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Continuous hearth growth remains a persistent risk in platinum group metal (PGM) six-in-line furnaces, with direct implications for shortened refractory campaigns, unplanned shutdowns, and increased likelihood of loss of containment. This paper investigates the thermal mechanisms underlying continuous growth by linking cooling efficiency, bottom-plate trip protection, and solidus-isotherm movement within the hearth refractory. Finite-element thermal simulations, supported by plant thermocouple data, demonstrate that compromised bottom cooling—either total loss of forced airflow or partial deterioration across the furnace width—drives irreversible downward migration of the 650°C solidus isotherm into the lower hearth refractory. The 650°C solidus temperature corresponds to the approximate solidification temperature of Fe-S-Ni-Cu-Co matte relevant to PGM smelting operations. In contrast, high matte-temperature cycles show minimal influence on solidus position due to the hearth's strong thermal inertia. Compromised cooling scenarios produce a calculated 60°C increase in refractory operating temperature and significant isotherm bowing, reinforcing the need for hearth cooling-system monitoring and robust trip protection. The solidus isotherm position defines the maximum depth to which liquid matte can remain mobile within the refractory; deeper isotherm migration therefore creates the thermal precondition for matte infiltration into the lower hearth. Recommendations for operators include improved instrumentation of hearth cooling efficiency, robust trip protection at 250°C, and proactive airflow integrity management. The findings present a thermally grounded mechanism for understanding continuous hearth growth, with coupled thermomechanical analysis identified as the logical extension.

## INTRODUCTION

Platinum group metal (PGM) smelting operations process PGM concentrates in large electric furnaces, typically six-in-line rectangular configurations with installed capacities ranging from approximately 40 MW to over 68 MW. These furnaces operate continuously for multi-year campaigns, during which the hearth—the lower refractory structure containing the molten matte bath—is subject to progressive mechanical and thermal degradation.

Continuous hearth growth is widely observed across PGM rectangular furnace operations which may shorten furnace campaign lives and introduce molten material safety concerns (Shaw, *et al.*, 2013; Joubert, *et al.*, 2024). As the furnace operates, mass from the matte layer accumulates within the hearth region, penetrating refractory layers and increasing the risk of premature failure through upper hearth brick flotation (Palmer, *et al.*, 2005).

Despite its ubiquity, the underlying mechanism is poorly understood in industry practice. Empirical growth data provide limited diagnostic capability due to their sensitivity to measurement noise, campaign disturbances, and operational interruptions.

This paper proposes a thermal mechanism that explains continuous hearth growth in terms of hearth cooling efficiency and solidus-isotherm penetration.

## FURNACE GEOMETRY AND MATERIAL CONFIGURATION

The furnace considered in this study is a typical six-in-line rectangular smelting furnace whose hearth refractory assembly consists of three distinct layers from top to bottom: upper hearth brick, lower hearth brick, and infill, as shown in Figure 1 (McDougall, *et al.*, 2012). A copper cooler is installed at the upper sidewall and the model domain corresponds to the hearth cross-section in Figure 1, with thermocouple positions TC1 and TC2 at the centreline and off-centre locations respectively. Furnace outer dimensions, hearth layer thickness, and matte bath height are given in Table I. The thermophysical properties assigned to each material layer are listed in Table II and were held constant throughout all simulations; matte properties were taken from Hundermark *et al.*, (2020) and refractory layer properties from manufacturer data sheets. The thermal resistance of the hearth stack is concentrated in the brick layers, meaning solidus isotherm position is most sensitive to changes in heat extraction at the brick–bottom plate interface – the thermal basis for the cooling efficiency scenarios examined in the model results.

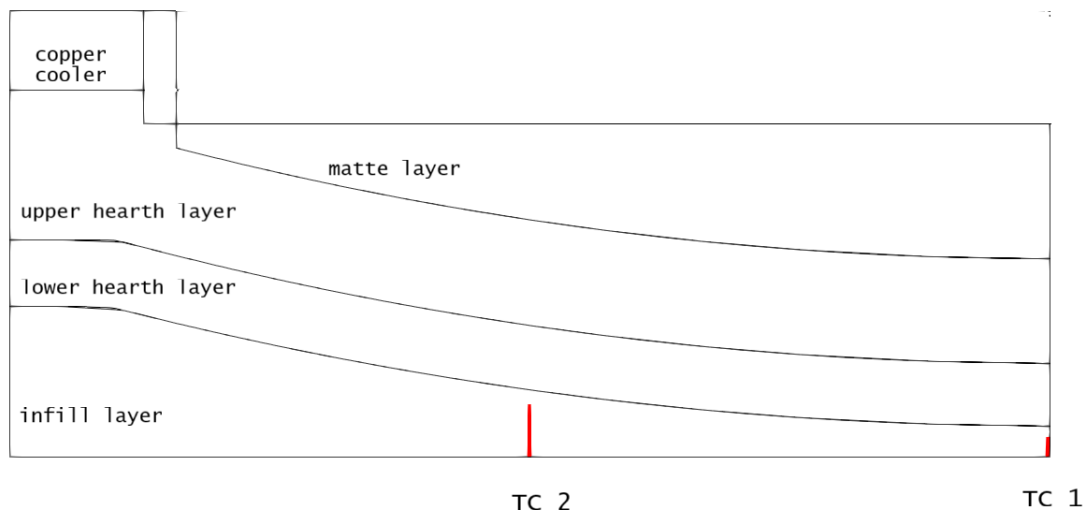


Figure 1. Simplified furnace geometry showing material layers and thermocouple locations at TC1 and TC 2.

Table I. Dimensions and material layer thicknesses of a sixin-line furnace considered

| Item                                              | Dimension (m) |
|---------------------------------------------------|---------------|
| Furnace total (outer) length                      | 28            |
| Furnace total (outer) width                       | 9.5           |
| Hearth refractory layer thickness (at centreline) | 0.9           |
| Height of matte bath                              | 0.6           |

Table II. Thermophysical properties of materials used in the thermal transfer model

| Material     | Density<br>(kg m <sup>-3</sup> ) | Heat Capacity<br>(J kg <sup>-1</sup> K <sup>-1</sup> ), | Thermal<br>conductivity<br>(W m <sup>-1</sup> K <sup>-1</sup> ). | Reference                                     |
|--------------|----------------------------------|---------------------------------------------------------|------------------------------------------------------------------|-----------------------------------------------|
| Matte        | 4600                             | 890                                                     | 17                                                               | (Snyders, 2008; Sheng , <i>et al.</i> , 1998) |
| Upper hearth | 2900                             | 880                                                     | 3                                                                | Data sheet                                    |
| Lower hearth | 2900                             | 960                                                     | 8                                                                | Data sheet                                    |
| Infill       | 2800                             | 650                                                     | 20                                                               | Data sheet                                    |

## CHARACTERISATION OF CONTINUOUS GROWTH

Daily growth measurements on the furnace hearth are taken and tracked by the plant binding operators using defined references. The measured hearth-growth profiles frequently show periods of positive and negative growth (Figure 2). The apparent ‘negative growth’ does not mean the hearth is shrinking. Extended power-off periods result in lower hearth temperatures which cause refractory contraction, and small gaps can form which can be infiltrated by matte; when the furnace reheats, this appears as positive growth (Shaw, *et al.*, 2013). Given the time lag and dampening of the thermocouple temperature readings, it is difficult to relate physical measurements to hearth growth.



Figure 2. Cumulative transverse (furnace width growth relative to furnace perimeter illustrating typical transverse hearth growth on a PGM rectangular furnace.

## EMPIRICAL ASSESSMENT OF LONGITUDINAL AND DEPTH-WISE THERMAL ASYMMETRY

Thermocouple temperature data were evaluated along the furnace length, from the matte end to the slag end to assess variation in cooling performance along the furnace length. Cooling airflow is directed along this same axis; longitudinal temperature differentials therefore provide a direct indication of effective heat extraction. Figure 3 presents the time-series of the inter-depth temperature differential ( $\Delta T = TC2 - TC1$ ) at three positions along the furnace length—matte end, middle, and slag end—over the campaign period. TC2 is the deeper off-centre thermocouple and TC1 is the shallower centreline thermocouple at corresponding longitudinal positions (Figure 1).

The slag end consistently exhibits the largest depth contrast, as shown in Figure 3, implying that the deeper off-centre region remains increasingly hotter relative to the shallower centreline thermocouples. This behaviour is consistent with progressive cooling deterioration and/or modification of effective thermal resistance along the furnace length. From mid-2019, the slag end separated from the other sections, indicating increasing depth-wise thermal loading at that location. From mid-2020, the middle section began separating from the matte end, further evidence of progressive spatial divergence.

A growing temperature differential between the two measurement depths reflects a reduced capacity to remove heat from the deeper refractory regions and is a more sensitive indicator of local heat extraction efficiency than absolute temperature alone. Several mechanisms could account for this progressive divergence: cooling air channels may accumulate particulate over time, reducing effective airflow at the slag end; matte infiltration into refractory brick may increase local effective thermal resistance (Hundermark, *et al.*, 2020); or disproportionate accumulation of frozen material along the furnace length could alter the local thermal boundary. The thermal model addresses cooling deterioration directly through reduced heat-transfer coefficient scenarios, and the longitudinal trend in Figure 3 provides empirical motivation for those scenarios.

This longitudinal differential is operationally relevant because it identifies spatial cooling imbalance before it becomes visible in dimensional growth measurements. The correlation between the spatially biased thermal loading shown in Figure 3 and the asymmetric transverse growth observed in Figure 2 supports the interpretation that localised cooling deterioration drives preferential growth at specific positions along the furnace length.

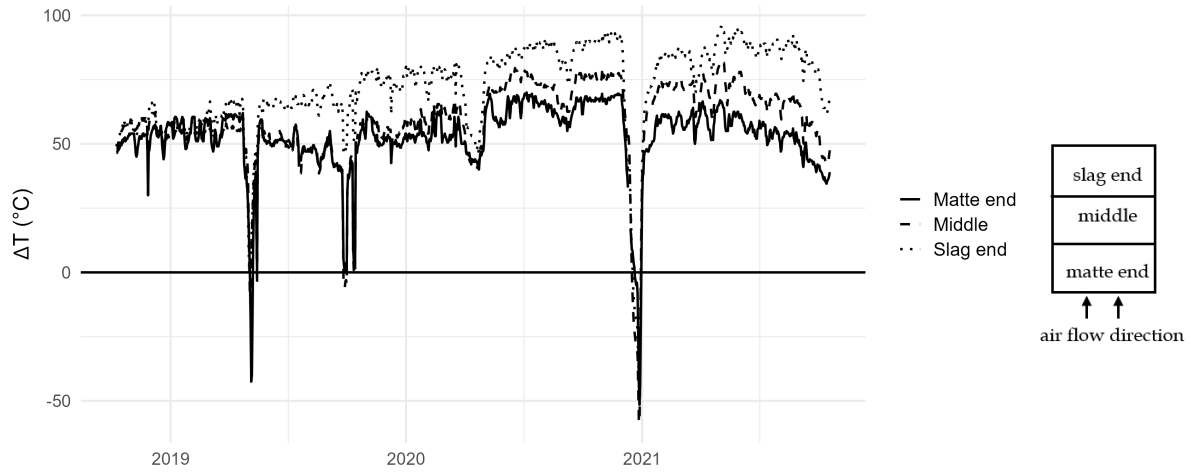


Figure 3. Time-series of inter-depth temperature differential ( $\Delta T = TC2 - TC1$ ) at the matte end, middle, and slag end positions over the campaign period. Progressive separation of the slag end from mid-2019, and subsequent divergence of the middle position from mid-2020, indicate evolving spatial imbalance in cooling performance.

## APPROACH

The thermocouple data analysis establishes that the hearth thermal field is spatially non-uniform and evolves progressively over the campaign. A transient model is required to capture time-dependent solidus migration under varying cooling boundary conditions—specifically the migration time-scales following a cooling event, which are operationally relevant to trip protection and intervention windows. A steady-state model alone would not provide this information.

A three-dimensional transient heat-conduction model of the furnace hearth was developed using FlexPDE v6.51 ( (PDE Solutions Inc, 2015)). The governing equation is transient conduction in Equation 1.

$$\rho c_p \frac{\partial T}{\partial t} = \nabla \cdot (k \nabla T) \quad [1]$$

where  $\rho$  is density ( $\text{kg m}^{-3}$ ),  $c_p$  is specific heat capacity ( $\text{J kg}^{-1} \text{K}^{-1}$ ),  $T$  is temperature (K),  $t$  is time (s), and  $k$  is thermal conductivity ( $\text{W m}^{-1} \text{K}^{-1}$ ).

A steady-state solution was first obtained under nominal cooling conditions and used as the initial condition for transient simulations. The literature consistently describes thermomechanical modelling as a two-stage process (Schant, 2004).

The key modelling assumptions are:

- Prescribed matte-surface temperature as a Dirichlet boundary.
- Quarter-furnace symmetry, which is appropriate for the uniform and half-width cooling scenarios and ensures the model remains computationally tractable—the half-width scenario represents localised asymmetric cooling on one side of the domain rather than a geometrically precise furnace dimension.
- Temperature-independent material properties. This final assumption simplifies computation at the cost of not capturing the conductivity variation in matte-infiltrated brick reported by Hundermark *et al.*, (2020). The effect is that the model likely underestimates the long-term thermal shift under progressive infiltration; results should therefore be interpreted as conservative estimates of isotherm migration under the simulated boundary conditions.

Surface heat extraction at the bottom plate, endwall, and sidewall is represented by a convective boundary condition in Equation 2.

$$-k \frac{\partial T}{\partial n} = h_i (T - T_\infty) \quad [2]$$

heat-transfer coefficient for surface zone  $i$  ( $\text{W m}^{-2} \text{K}^{-1}$ ), and  $T_\infty$  is the ambient sink temperature (K). The subscript  $i$  denotes distinct boundary zones, each with independently specified  $h_i$  and  $T_\infty$  values. The mesh and boundary condition zones are illustrated in Figure 4. Key hearth dimensions are indicated in Figure 4 and are discussed with reference to specific simulation results in the sections below.

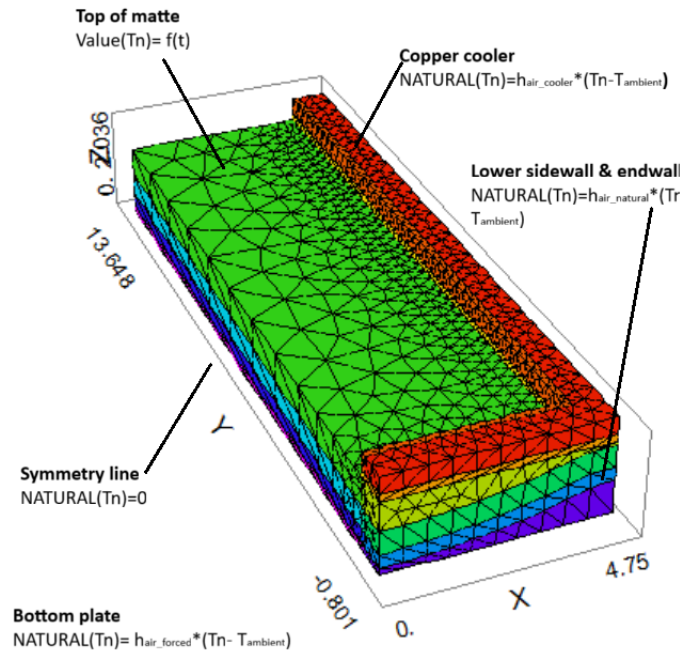


Figure 4. Model mesh and boundary conditions applied.



Thermomechanical gap formation and matte transport through refractory pore channels are not modelled; these represent the logical next step and are identified as future work.

## MODEL RESULTS

Thermocouple positions, TC 1 and TC 2, were explicitly defined in the model and temperature histories were extracted for comparison with imposed surface-temperature cycles. Solidus position was tracked using the 650°C isotherm and quantified as the vertical distance from the upper hearth hot face. Solidus position was tracked using the 650°C isotherm, which corresponds to the approximate solidification temperature of PGM smelting matte with representative Fe-S-Ni-Cu-Co composition (Li Kao & Garbers-Craig, 2025). Below the solidus temperature, matte is fully solidified and immobile; the solidus isotherm therefore defines the maximum depth to which liquid or partially liquid matte can be present within the refractory at any instant. The model does not include latent heat of solidification; this simplification means that the thermal response in the vicinity of the phase transition may be slightly faster than in reality, tending toward a marginally conservative (deeper) estimate of isotherm penetration depth under degraded cooling conditions.

### Impact of Cooling Efficiency

The hearth bottom is cooled predominantly through externally forced airflow and conductive contact between the bottom plate and refractory assembly. Figure 5 illustrates the hearth temperature evolution following a simulated reduction in cooling efficiency.

The range  $h = 5 - 50 \text{ W m}^{-2} \text{ K}^{-1}$  represents the plausible operating envelope when considering forced-air flow, shell contact conditions and cooling-channel configuration. Across this range, the  $25 \text{ W m}^{-2} \text{ K}^{-1}$  case consistently produced a temperature distribution that agrees with the known refractory temperature gradient and the typical thermocouple behaviour recorded under stable operation. The value therefore represents a realistic aggregate of the actual thermal resistances present in the installed hearth cooling system, without requiring explicit modelling of the airflow or shell geometry.

Using this coefficient as the baseline ensures that subsequent transient and sensitivity studies are anchored in a physically meaningful cooling performance.

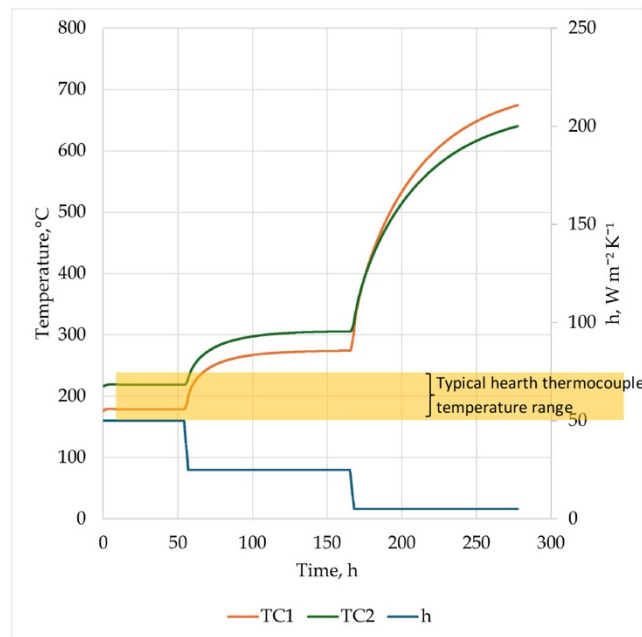


Figure 5. The evaluation of different bottom cooling coefficients demonstrates that the furnace response is highly sensitive to the magnitude of heat extraction at the shell-hearth interface.

### Impact of Loss of Forced Cooling

To simulate complete loss of forced airflow, the bottom boundary condition was changed from  $h = 25 \text{ W m}^{-2}\text{K}^{-1}$  to natural convection ( $h \approx 5 \text{ W m}^{-2}\text{K}^{-1}$ ). A furnace trip condition—representing automatic furnace power switch off—was applied at a bottom-plate temperature of  $250^\circ\text{C}$ , consistent with installed protection settings. When the trip fires, the matte-surface boundary condition transitions from the prescribed operating temperature to a no-flux (adiabatic) condition, representing cessation of power input and progressive bath solidification.

Figure 6 shows the transient temperature response following complete cooling loss. The temperature at TC1 and TC2 locations rise sharply after the cooling loss, reaching a peak before declining as the bath cools and solidifies post-trip.

Under nominal conditions, the solidus and liquidus isotherms are located at approximately 360 mm and 240 mm from the upper hearth hot face, respectively (Figure 3a). At the point of trip, these positions shift marginally to 370 mm and 250 mm—a 3% displacement—confirming that the thermal profile at shutdown remains close to nominal (Figure 3b). The solidus isotherm subsequently migrates to the upper-lower hearth interface within 58 h of trip and remains there for approximately 47 h (Figure 3c) before the solidifying bath causes retreat toward the hot face (Figure 3d).

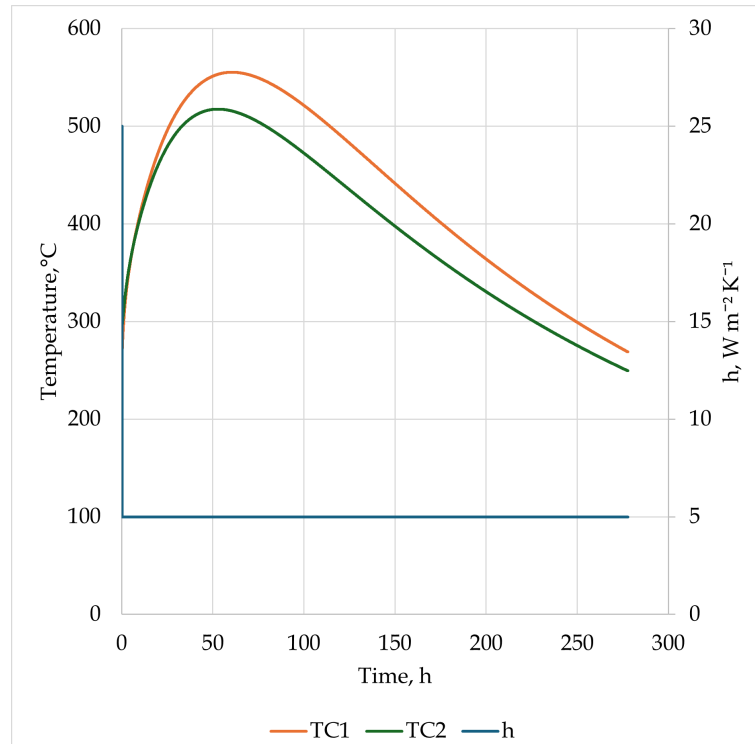


Figure 2. Transient temperature response following complete loss of forced cooling. Natural convection is insufficient to stabilise the hearth, causing the thermocouples to reach  $550^\circ\text{C}$ .

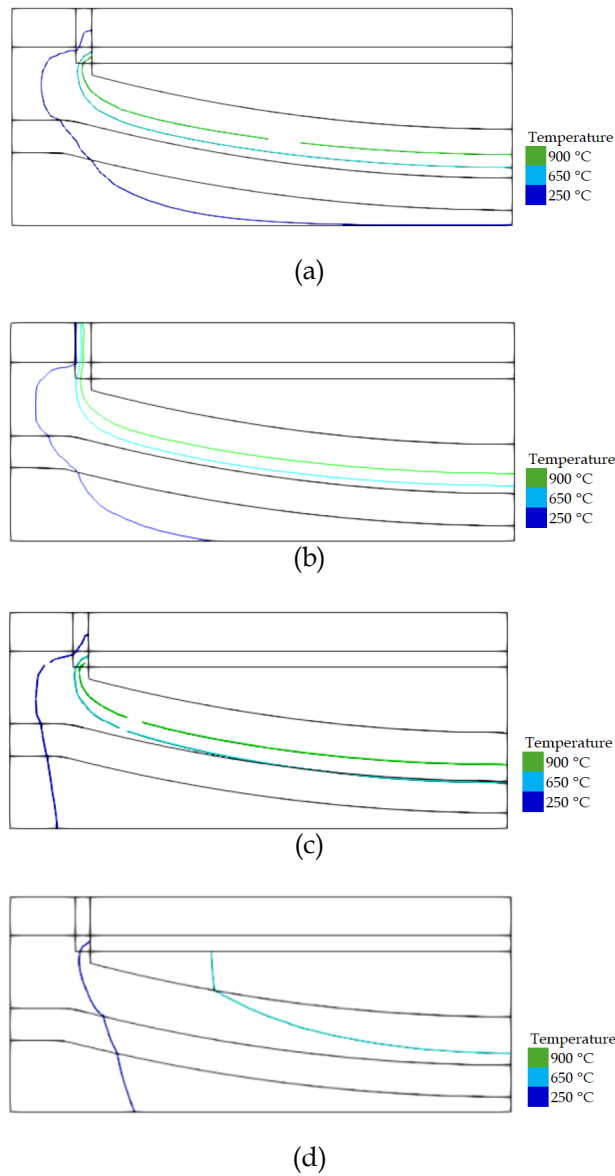


Figure 3. Solidus and liquidus isotherm migration following cooling loss. (a) Initial conditions (b) Solidus isotherm migrates to lower hearth 58 h from trip. (c) All material below solidus after 94 h.

Without bottom-plate trip protection, Figure 8 shows the solidus isotherm reaching the upper-lower hearth interface within 36 h – 22 h earlier than in the protected case. A further 48 h is required for the solidus to reach the lower hearth cold face, indicating thermal conditions consistent with the potential onset of lower-hearth infiltration. The deepest liquidus position in this scenario is 350 mm from the hot face, 110 mm deeper than at nominal conditions.

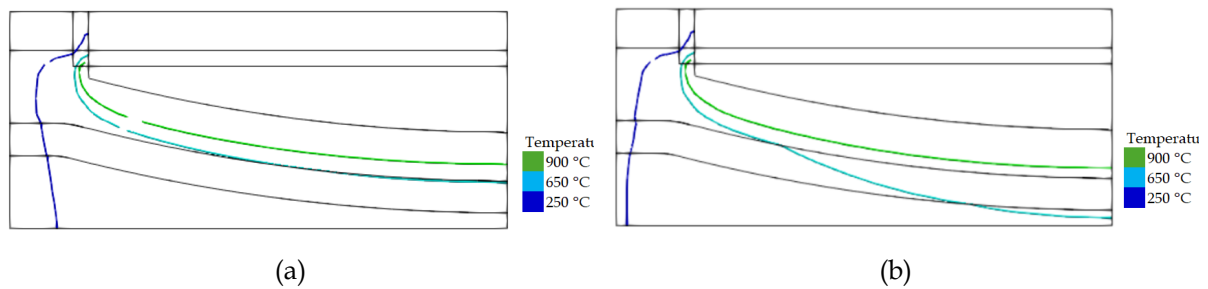


Figure 1- Isotherm contours under total loss of hearth cooling ('no bottom plate trip' condition). The 650°C solidus isotherm advances beyond the upper-lower hearth interface within 36 h (a) and reaches the infill after 84 h, (b) indicating possibility of irreversible matte penetration of the lower hearth.

### Effect of High Matte Temperature

A transient matte-temperature cycling scenario was simulated to represent periods of elevated bath temperature associated with deep electrode operation or high-chrome feed management. The matte-surface boundary temperature was cycled between 1400°C and 1800°C. TC1 showed a delayed and heavily damped response relative to the imposed surface variation (Figure 5). The centreline thermocouple (TC1) exhibited a lag of approximately 34 h relative to the imposed matte temperature cycle; this lag is consistent with the thermal diffusivity and thickness of the refractory domain. This quantified lag has a direct practical implication: embedded thermocouple readings reflect the thermal state approximately 34 h prior, not the current furnace condition

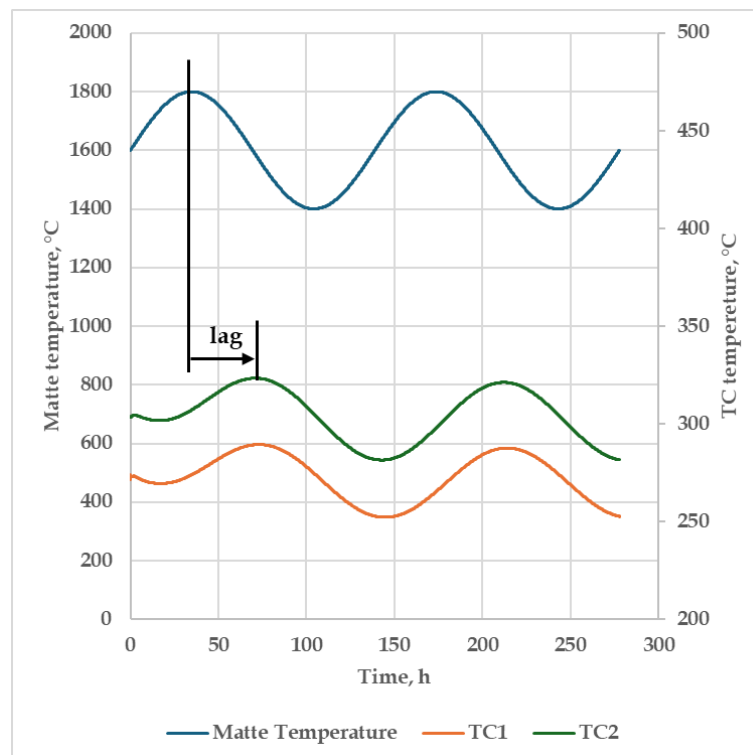


Figure 5. Imposed matte surface temperature cycle and thermocouple response showing 34 h lag and dampening.

The solidus isotherm moved only 5 mm over the full 400°C matte temperature swing (Figure 6), confirming that matte temperature cycling alone does not materially drive continuous hearth growth.

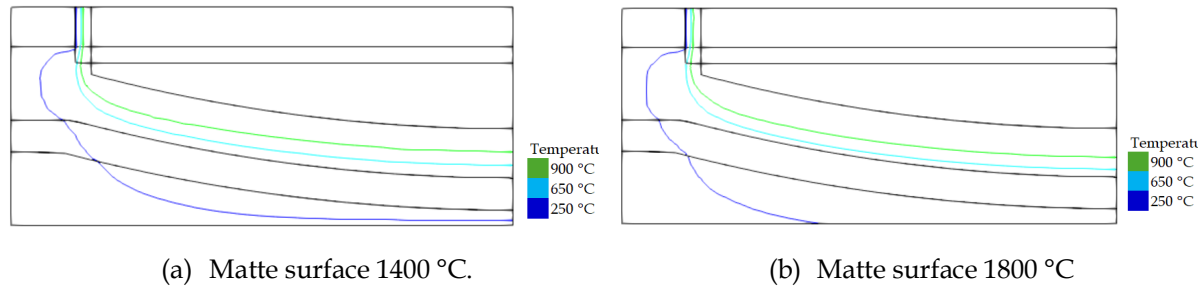


Figure 6. Isotherm positions during cycling matte surface temperature spanning 1400°C to 1800°C.

### Effect of Compromised Cooling with Matte Temperature Cycling

A half-width reduced-cooling scenario was simulated by reducing the local heat-transfer coefficient from  $h = 25 \text{ W m}^{-2}\text{K}^{-1}$  to  $h = 10 \text{ W m}^{-2}\text{K}^{-1}$  on the outer half of the hearth width, while retaining the matte-temperature cycling boundary. This scenario represents localised cooling deterioration, partial airflow blockage, degraded contact beneath one side of the hearth, or asymmetric channel fouling—applied to one lateral half of the quarter-domain. Results should be interpreted as indicative of asymmetric thermal loading rather than as a geometrically precise furnace replica.

The off-centre thermocouple (TC2) showed a steady-state temperature increase of approximately 60°C (from 33°C to 390°C) relative to the uniform-cooling baseline. Figure 7 shows the resulting thermal field comparison. The downward bowing and thickening of the 650–900°C isothermal band on the compromised side demonstrates that spatially localised cooling degradation produces asymmetric thermal loading even when global operating conditions—average power input and nominal bath temperature—remain unchanged.

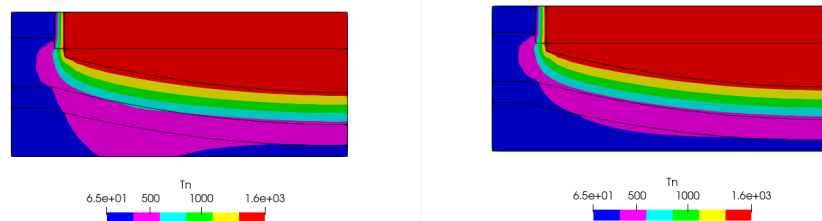


Figure 2. Comparison of thermal profiles at peak matte surface temperature for compromised cooling ( $10 \text{ W m}^{-2} \text{ K}^{-1}$  on half-width) (left) and uniform efficient cooling on the  $25 \text{ W m}^{-2} \text{ K}^{-1}$  (right).

## DISCUSSION

Cooling efficiency governs the vertical position of the 650°C solidus isotherm within the hearth refractory. Reduced bottom heat extraction displaces the solidus deeper into the upper hearth brick, while short-term matte temperature fluctuations produce only minor and reversible isotherm movement. The quantified thermocouple lag of approximately 34 h confirms that embedded thermocouple measurements reflect delayed thermal state rather than instantaneous furnace conditions—a factor that must be accounted for when interpreting thermocouple trends as real-time indicators of cooling performance.

The solidus isotherm defines the lower boundary of the thermally mobile matte zone. Below the solidus

temperature, matte is fully solidified and cannot flow into refractory pore channels. Downward migration of the solidus therefore expands the volume of refractory brick that is thermally accessible to liquid or partially liquid matte, establishing the necessary thermal precondition for deeper infiltration. This is a physical consequence of the solidus as the phase boundary, not a claim derived solely from this model.

Solidus isotherm migration alone does not constitute proof of growth. Growth requires the thermal precondition demonstrated here—liquid-phase matte present at depth—combined with a mechanical pathway for that matte to enter the refractory, driven by capillary pressure, hydraulic head, or thermomechanically generated gap formation (Shaw *et al.*, 2013). Thermomechanical gap formation and matte transport are outside the scope of this model.

A ratcheting mechanism can however be inferred from the thermal results combined with the conductivity data of Hundermark *et al.*, (2020). When cooling degrades and the solidus migrates downward, matte infiltrates newly accessible brick porosity. When cooling is restored, this infiltrated matte solidifies in place. Hundermark *et al.*, (2020) report that matte-infiltrated brick has modified thermal conductivity in the 100–450°C range. This conductivity change shifts the local thermal field such that the solidus does not fully return to its pre-event depth after cooling recovers. Successive degradation events therefore begin from a progressively worse thermal baseline, providing a physical basis for the cumulative character of continuous hearth growth observed in Figure 2.

Localised cooling degradation—as shown in the half-width scenario—produces asymmetric isotherm bowing consistent with the spatial patterns of unilateral growth documented in plant observations. This finding implies that asymmetric hearth growth can develop without any change in global operating parameters, with no single centreline thermocouple necessarily registering an alarm.

## CONCLUSION

This study demonstrates that solidus-isotherm position in the PGM furnace hearth is controlled primarily by bottom cooling performance. A three-dimensional transient heat-conduction model indicates that reducing the bottom heat-transfer coefficient from  $25 \text{ W m}^{-2}\text{K}^{-1}$  to  $10 \text{ W m}^{-2}\text{K}^{-1}$  across the outer half of the hearth width displaces the 650°C solidus isotherm asymmetrically, raising off-centre refractory temperatures by approximately 60°C. Matte surface temperature cycling over a 400°C range produces only 5 mm of isotherm movement, confirming that short-term matte temperature variation is not a material driver.

Under total loss of forced cooling with trip protection at 250°C, the solidus isotherm migrates to the upper-lower hearth interface within 58 h. Without trip protection, this occurs within 36 h and the isotherm reaches the infill layer within 84 h, indicating thermal conditions consistent with the potential onset of lower-hearth infiltration.

The thermocouple response lag of approximately 34 h implies that embedded measurements reflect the thermal state from roughly a day and a half earlier.

These findings identify cooling performance as the principal thermal variable governing solidus-isotherm position and thereby the thermal precondition for matte infiltration into the hearth. The paper addresses the thermal stage of the growth mechanism. Quantifying the mechanical stage—thermomechanical gap formation, matte infiltration rate, and the role of refractory permeability—requires coupled thermomechanical modelling and is proposed as the continuation of this work.

## RECOMMENDATIONS

Effective management of hearth growth risk requires continuous monitoring of bottom cooling efficiency using appropriate instrumentation and routine performance verification. Bottom-plate trip protection at or below 250°C is a critical safeguard: the model results show that its absence accelerates solidus migration to the upper–lower hearth interface by 22 h under total cooling loss. Thermocouple coverage across a substantial fraction of the hearth base—including off-centre locations—is necessary to detect spatial cooling asymmetry not visible from a single centreline thermocouple. Routine inspection of cooling air pathways is required to identify blockages, flow restrictions, or degraded contact. Trends in the inter-depth temperature differential ( $\Delta T = TC2 - TC1$ ) provide earlier and more sensitive indication of localised cooling deterioration than absolute temperature values alone and should be integrated into standard furnace integrity monitoring practices.

## NOMENCLATURE

| Symbol     | Description                                           | Units                              |
|------------|-------------------------------------------------------|------------------------------------|
| T          | Temperature                                           | K or °C                            |
| t          | Time                                                  | s or h                             |
| k          | Thermal conductivity                                  | W m <sup>-1</sup> K <sup>-1</sup>  |
| $\rho$     | Density                                               | kg m <sup>-3</sup>                 |
| $c_p$      | Specific heat capacity                                | J kg <sup>-1</sup> K <sup>-1</sup> |
| $h_i$      | Convective heat-transfer coefficient, boundary zone i | W m <sup>-2</sup> K <sup>-1</sup>  |
| $T_\infty$ | Ambient (sink) temperature                            | K or °C                            |
| n          | Outward surface normal                                | —                                  |
| $\Delta T$ | Inter-depth temperature differential (TC2 – TC1)      | °C                                 |

## ACKNOWLEDGEMENTS

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Large Language Model, including Anthropic Claude and OpenAI ChatGPT, were used as a proofing tool to fix spelling, grammar, register, tone, and style.

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I am a process engineer and metallurgical professional with over a decade of experience in smelting operations, process optimisation, and furnace integrity management within the platinum group metals (PGM) industry. My career has developed at the intersection of technical problem-solving and operational leadership, where I focus on achieving safe, reliable, and cost-effective performance in high-risk metallurgical processes.

I currently serve as Plant Manager for the Slag Cleaning Furnace (SCF) and matte granulation section at Valterra Platinum. In this role, I am accountable for delivering production targets while ensuring disciplined safety practices and stable furnace operations. I place a strong emphasis on structured management routines, team engagement, and proactive risk control, which has enabled me to drive both performance improvements and a culture of accountability.

Earlier in my career, I managed the safe retrenchment and transition to care-and-maintenance of a smelter operation. This was a complex assignment requiring sensitive leadership, rigorous cost control, and operational discipline. I successfully balanced the needs of employees, contractors, and stakeholders while ensuring safe decommissioning and continuity in reporting and governance structures.

My technical expertise extends to high-temperature modelling and furnace performance improvement. I am currently pursuing a Master's degree on continuous hearth growth in PGM furnaces, using finite element analysis (FlexPDE) to study thermal behaviour during start-up and steady-state conditions. This research focuses on the location of the matte solidus isotherm and its implications for furnace stability and integrity – insights that are critical for extending furnace life and reducing operational risk.

Beyond furnace modelling, I have also contributed to drying technology improvements, where I developed a roadmap to address underperformance in throughput and reliability. I have been actively involved in strengthening tapping safety, streamlining and resourcing initiatives for the annual Tapping Conference to ensure industry-wide impact and practical follow-through.

I hold a degree in Chemical Engineering and an Honors in Metallurgical Engineering and continue to broaden my knowledge through postgraduate study and professional engagement. I am particularly interested in the link between plant-level decisions and broader corporate and financial strategy in mining, as I see this integration as essential for sustainable value creation in the sector.

Through my work, I aim to combine technical depth, leadership, and strategic thinking to address challenges facing smelting operations. My commitment to safe, innovative, and efficient practice continues to guide my contributions to Valterra Platinum and to the wider metallurgical community through platforms such as SAIMM.

