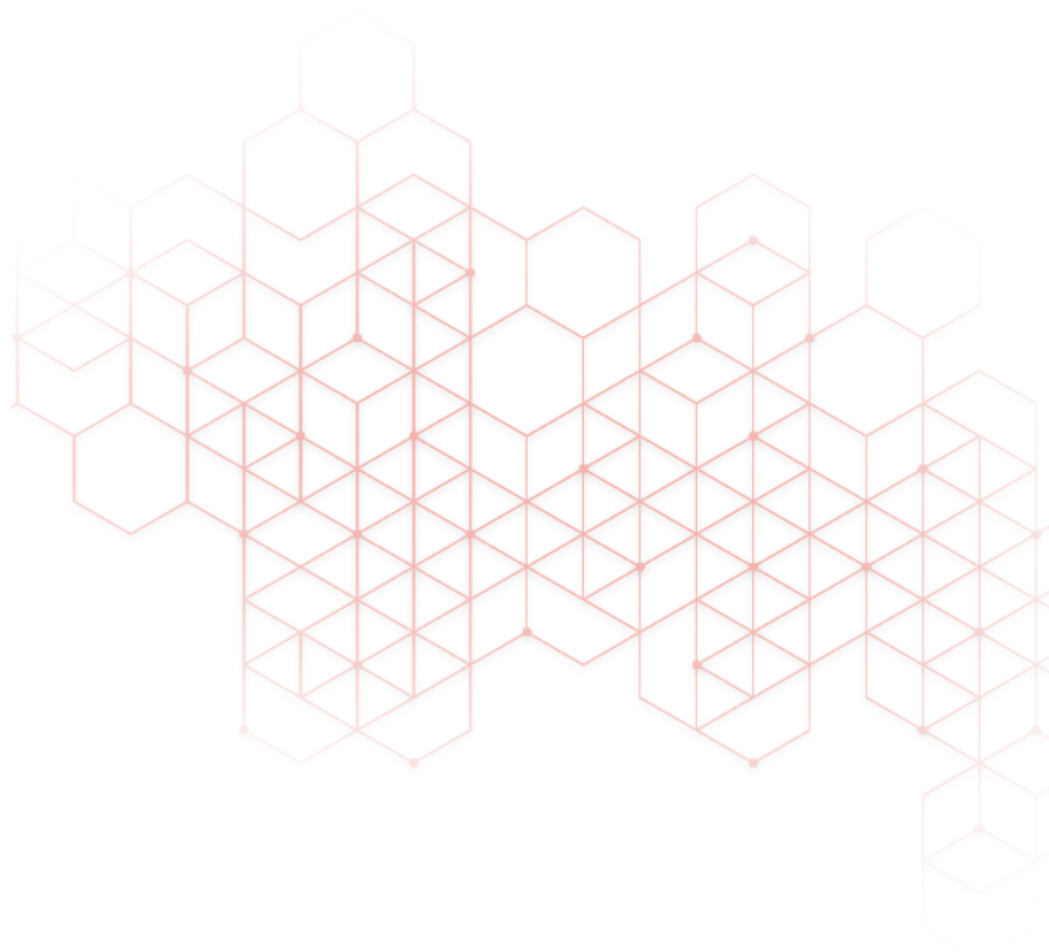


D6.3 – Report on results of the steel use case implementations

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List of abbreviations

Abbreviations	Explanation
AMI	ArcelorMittal Innovation
AMOB	ArcelorMittal Olaberria-Bergara
AMS	ArcelorMittal Sestao SL
BB	Beam Blank
BEF	Befesa Aluminio SLU
BSC	Barcelona Supercomputing Center
CA	Calcium aluminate
CAPEX	Capital expenditure
CCS	Cold crushing strength
CEL	Celsa Armeringsstål AS (former name of 7 Steel Nordic Manufacturing)
CFD	Computational Fluid Dynamics
CHT	Conjugate Heat Transfer
CTEC	Constellium Technology Center
CO	Carbon monoxide
CO ₂	Carbon dioxide
EHF	Electric arc furnace
EDX	Energy Dispersive X-ray Spectroscopy
FPDF	Filtered Probability Density Function
H ₂	Hydrogen
KPI	Key Performance Indicator
LES	Large Eddy Simulation
LHV	Lower Heating Value
MYT	Mytilinaios Anonimi Etaireia (Now MET: Metlen Energy & Metals)
NG	Natural gas
NTNU	Norwegian University of Science and Technology
NO _x	Nitrogen oxides
OEC	Oxygen enhanced combustion
OFC	Oxygen Fuel Combustion
OPEX	Operating expenses
P4P	Processes4Planet
RDI	Research Development and Innovation
RUL	Refractoriness under load
RWTH-GHI	Rheinisch-Westfaelische Technische Hochschule - Institut für Gesteinshüttenkunde, Aachen University - Institute of Mineral Engineering
SEM	Scanning Electron Microscopy
SRIA	Strategic Research and Innovation Agenda
SPE	Speira GmbH
SPIRE	Sustainable Process Industry through Resource and Energy Efficiency
SWE	Swerim AB
TEC	Tecnalia Research & Innovation
TME	Toyota Motor Europe NV
TMC	Thermal-Mechanical-Chemical
WP	Work Package
XRD	X-ray Diffraction
XRF	X-ray Fluorescence

1 Introduction to work package 6

Within the HyInHeat project, Work Package 6 focuses on the demonstration, evaluation, and validation of hydrogen-based heating technologies in representative steel production processes. The activities performed address both retrofit solutions for existing industrial assets and design concepts for future hydrogen-ready installations. The work combines industrial-scale demonstrations, pilot-scale testing, numerical modelling, materials characterization, and engineering studies in order to assess the technical feasibility, operational performance, energy efficiency, product quality implications, refractory behaviour, safety considerations, and environmental benefits associated with the use of hydrogen in industrial heating applications.

This deliverable presents the results obtained from the implementation of the steel use cases within WP6. The reported activities cover a broad range of heating technologies and combustion concepts, including hydrogen-air combustion, hydrogen-oxygen combustion, oxygen-enhanced combustion, and hybrid fuel strategies. The investigations were conducted at multiple industrial and pilot facilities across Europe and involved close collaboration between steel producers, technology providers, research organisations, and engineering partners.

The work presented in this report has four principal objectives:

- To demonstrate the technical feasibility of replacing natural gas with hydrogen in representative steel reheating and ladle preheating applications.
- To quantify the impact of hydrogen utilisation on process performance, energy consumption, emissions, product quality, and operational stability.
- To assess the influence of hydrogen-based combustion environments on steel oxidation behaviour and refractory materials.
- To provide engineering, modelling, and safety knowledge supporting the future industrial deployment of hydrogen heating technologies in the steel sector.

The deliverable is structured according to the individual tasks of Work Package 6:

- Task 6.1 presents retrofit demonstrations at ArcelorMittal Sestao (AMS) and ArcelorMittal Olaberria-Bergara (AMOB), including industrial furnace trials, combustion characterisation, CFD simulations, and metallurgical investigations.
- Task 6.2 presents pilot-scale walking beam furnace experiments investigating different hydrogen combustion modes and their effects on heating performance, energy efficiency, oxidation behaviour, and descaling quality.
- Task 6.3 addresses hydrogen-based ladle preheating through numerical simulations, industrial demonstrations, and refractory performance studies.
- Task 6.4 presents a greenfield study for a hydrogen-ready walking beam furnace, covering process design, burner technology, CFD analysis, safety assessment, and process integration.
- Task 6.5 presents a retrofit study for continuous annealing and galvanizing line furnaces, evaluating the technical requirements and implications of hydrogen implementation.

Overall, the results presented in this deliverable provide a comprehensive assessment of hydrogen-based industrial heating solutions for steel production and contribute valuable knowledge towards the decarbonisation of thermal processes within the European steel industry. The findings demonstrate both the opportunities and the challenges associated with industrial hydrogen deployment and provide a foundation for future scale-up and commercial implementation.

2 Task 6.1: Retrofit on Tunnel Furnace Demonstrator with H₂/Air Combustion

Modifications have been made to tunnel furnace line A at the ArcelorMittal Sestao steel plant so that two of its areas, specifically areas 8 and 9, can operate using hydrogen as fuel instead of natural gas. Once these modifications were completed during the maintenance shutdown in August 2025, tests were carried out on different production days during December 2025 and January and February 2026.

The main changes introduced are:

- 1) Provisional installation of a decompression skid (NIP) in the yard used to storage hydrogen trucks (*high-pressure skid*)
- 2) Installation of a 4bar pipe from the decompression Nippon skid to the final decompression skid
- 3) Installation of a final decompression skid (*low-pressure skid*) from 4 bar to consumption pressure
- 4) Variable excess air inlet
- 5) Vents in the decompression facilities
- 6) Changes in programming for monitoring and operate autonomously and remotely the burners
- 7) Installation of new thermocouples connected to the new programming

After the adaptation of the facilities to enable H₂ combustion in 16 dual NG/air/H₂ burners inside the tunnel furnace, one H₂ combustion test was carried out with the tunnel furnace empty (no-load test) and three H₂ combustion tests were carried out with slabs inside the furnace. The aim was to cover all the most common grades used at ArcelorMittal Sestao, observing their behaviour in situ and deferred (metallographic results to be obtained later in Maizières).

During all tests performed, NO_x measurements are taken at the flue gas outlet of the burners, which will be compared with the amount of NO_x generated at the burner flue gas outlet when operating with NG/air. It is expected an increasement of NO_x.

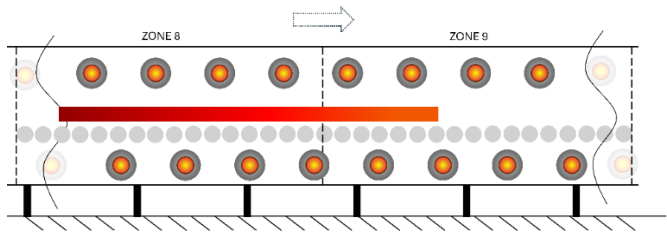


Figure 1. Illustration of the zones of the tunnel furnace

Lado Sestao	48	50	52	54	56	58	60	62	En posición superior
Lado Ría	79	81	83	85	87	89	91	93	En posición inferior

Los quemadores instalados son de dos tipos diferentes:

- Flox/flame son los quemadores de encendido: Automatic and remote turning on burners
- Flame son los quemadores de mantenimiento: Rest of burners

Potencia nominal de los quemadores			
Equipo	U	Potencia min. /máx.	Nominal
Quemador Rekumat M300	kW	160 / 240	3.840

Tabla 12

Potencia instalada de las zonas 8 y 9 es de 3.840 kW

Figure 2. Description of dual H₂/NG burners changed

Each time trials are carried out, a series of initial steps must be followed to ensure that the H₂ is securely connected to the pipe. The steps are the following ones:

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- 1) Grounding of lorries
- 2) Opening of the nitrogen system for purging in the high-pressure skid
- 3) Verification that the medium pressure line remains pressurised with nitrogen
- 4) Inerting of connecting tubes between skid and trucks
- 5) Nitrogen purge of the low-pressure skid
- 6) Connection of tubes between skid and trucks
- 7) Opening of lorry valves with skid valves closed
- 8) Restart and check of the automatic safety system for warnings and control of the high-pressure skid solenoid valve
- 9) Opening of manual valves to pressurize the medium pressure line
- 10) Final checks on low-pressure skid
- 11) Change in the section of the furnace from NG to H₂, through the by-pass
- 12) **Regulation of the 16 burners to H₂ combustion.*
- 13) **Normalization of the furnace to continue production*

**steps added after first trial*

Following the complete adaptation of the facility, one empty-furnace hydrogen combustion test and three production tests were carried out. These tests covered the main grades processed at ArcelorMittal Sestao and were designed to evaluate the thermal behaviour of the furnace, hydrogen consumption patterns, operational stability, and potential impacts on product quality. As part of the testing campaign, NO_x emissions were measured at the burner exhaust outlets to compare H₂/air combustion with conventional NG/air operation. As anticipated, an increase in NO_x formation is expected under hydrogen firing, and this behaviour will be further examined in later sections of the report.

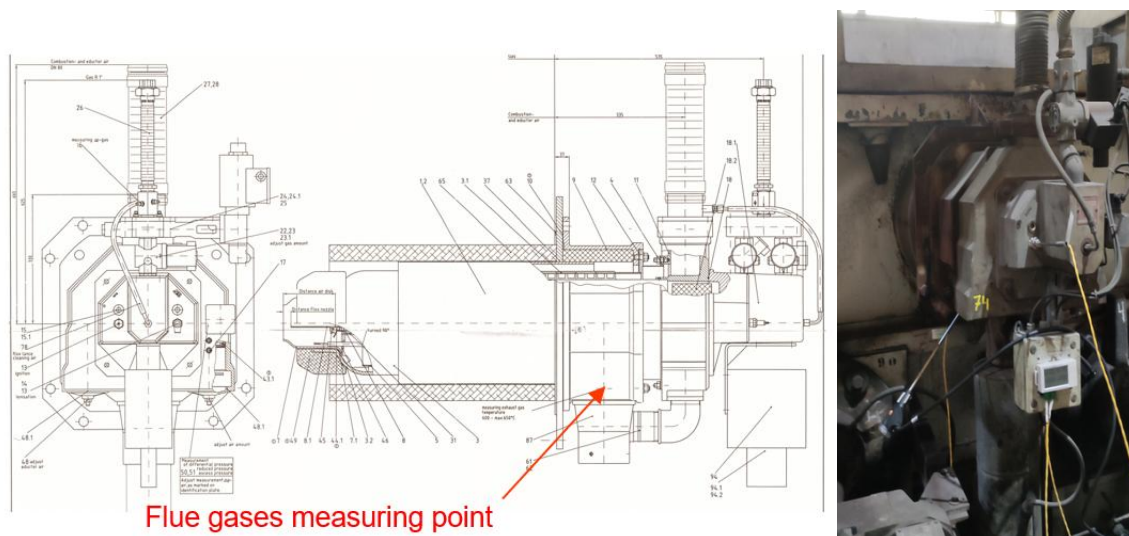


Figure 3. Measuring point in burner flue gas exhaust

2.1 TEST 0: 03.12.2025 – no-load test

This first test is carried out during one of the scheduled shutdown days, with the aim of testing the H₂ system and adjusting the burners without production in the furnace. Working temperatures are maintained in the furnace, instead of lowering it to the usual maintenance temperature. A check is carried out to ensure that the burners are performing as they should. Initially, the burners are unable to reach the set point temperature. This is because they had not been adjusted to the working conditions for H₂ (the amount of H₂ entering for equal pressures is very different from the amount of natural gas entering, requiring adjustment and variation of the excess air). After adjusting the burners, the expected set point temperature was reached.

Table 1. General data on TEST 0.

Beginning of the trial	13:00
End of the trial	16:20
Duration	3:20
Number of heats	0
Mean flow rate (Nm ³ /h)	805,30
Maximum flow rate (Nm ³ /h)	1013,84
Total consumption (Nm ³)	2694,40
Maximum burner H ₂ pressure (mbarg)	140,93
Mean burner H ₂ pressure (mbarg)	118,07
Minimum burner H ₂ pressure (mbarg)	90,63
Excess air global fan (rpm)	3000 rpm

All the flow rates and consumption have been obtained from measurements taken by a flow meter installed in the low-pressure skid. To normalise the measurement, the actual pressure and temperature of the fluid are taken into account.

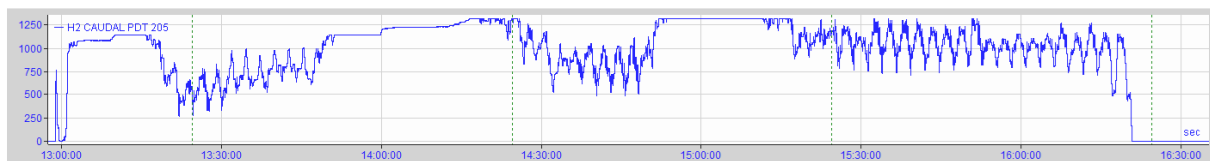


Figure 4. H₂ flow rate during test.

Table 2. Temperatures inside the tunnel furnace during TEST 0.

	8 zone	9 zone
SP maximum temperature (°C)	1150	1150
Maximum achieved temperature (°C)	1165,56	1164,41
SP minimum temperature (°C)	1050	1050
Minimum achieved temperature (°C)	1033,83	1030,44
SP mean temperature (°C)	1118,54	1118,54
Mean achieved temperature (°C)	1109,74	1113,51
Maximum demand (%)	100%	100%
Mean demand (%)	77,62%	70,42%

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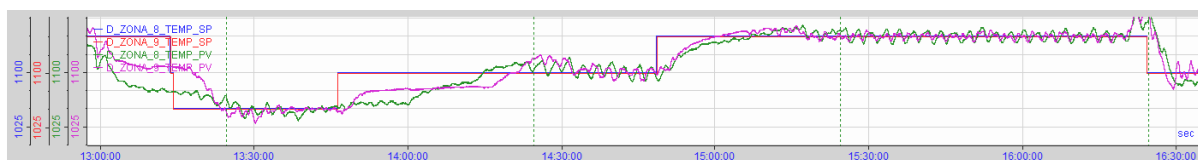


Figure 5. Temperatures (set points and achieved) during TEST 0.

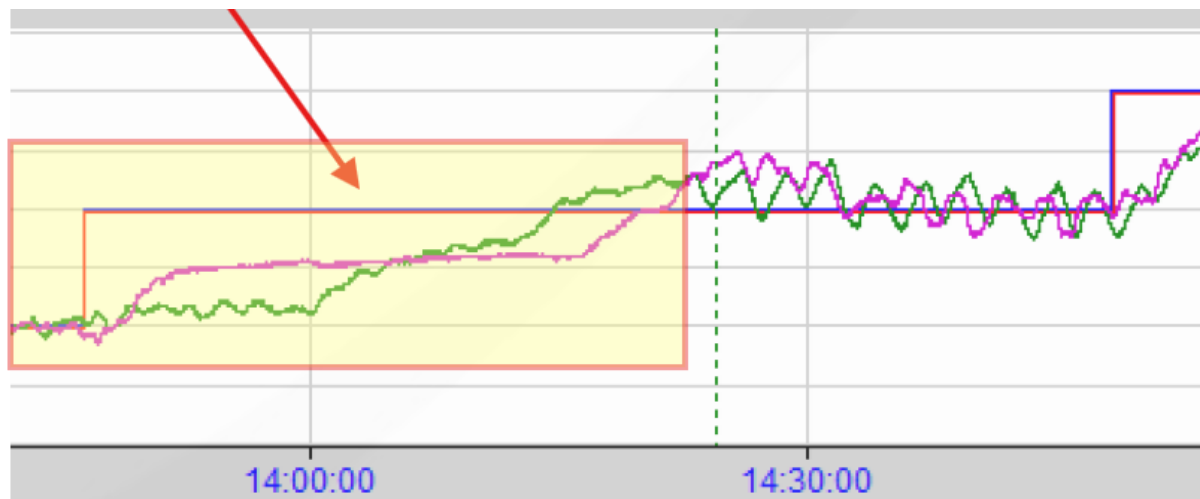


Figure 6. Highlighted in yellow shows the time in which temperatures achieved could not reach the set point due to the lack of manual adjustment.

These are the consumptions according to the H₂ supply by the trucks. At the beginning of the test, 2 trucks of 4.133 Nm³ each one were connected and simultaneously feed the main decompression skid. The invoiced amount was set by difference of pressure of H₂ in the trucks according to the remaining values:

Table 3. Invoiced amount of hydrogen gas for TEST 0.

Date:	amount [Nm ³]	Supplier:
03.12.2025	3.513,00	NipponGases

Following a successful empty test, it is decided to proceed with the plan: 3 heats of grade 3XXX, 3 heats of grade 6XXX, 4 heats of grade 9XXX.

2.2 TEST 1: 11.12.2025 – 3XXX Grade family

The test is carried out without any major surprises, with a sequence of three heats. No significant quality defects are observed in the preliminary analysis (there is an on-line inspection system for surface quality assessment). The amount of scale present does not appear to have increased considerably, nor are any new defects apparent in the coils. A sample is taken from each heat for subsequent laboratory analysis and comparison with coils of similar grade and characteristics, except from one of the heats, in which two samples are taken (from the bottom and from the beginning of the slab. This slab was retained inside the tunnel furnace, having its beginning part in the zones with H₂ and the coil out the zones. Therefore, two those samples were taken to be analysed.

At the end of the test, the atmosphere in the furnace is analysed and it is found that the amount of excess air inside the furnace could be optimised for more efficient combustion. Therefore, it is decided to reduce the speed of the global excess air fan for future tests.

Table 4. General data on TEST 1.

Beginning of the trial	10:44
End of the trial	15:37
Duration	4:53
Number of heats	3
Mean flow rate (Nm ³ /h)	634,27
Maximum flow rate (Nm ³ /h)	1088,79
Total consumption (Nm ³)	3099,09
Maximum burner H ₂ pressure (mbarg)	145,33
Mean burner H ₂ pressure (mbarg)	118,86
Minimum burner H ₂ pressure (mbarg)	97,35
Excess air global fan (rpm)	3000 rpm

Table 5. Temperatures inside the tunnel furnace during TEST 1.

	8 zone	9 zone
SP maximum temperature (°C)	1113	1113
Maximum achieved temperature (°C)	1140,63	1129,93
SP minimum temperature (°C)	1000	1000
Minimum achieved temperature (°C)	979,262	979,494
SP mean temperature (°C)	1102,60	1102,53
Mean achieved temperature (°C)	1100,29	1099,36
Maximum demand (%)	100%	100%
Mean demand (%)	42,59%	51,05%

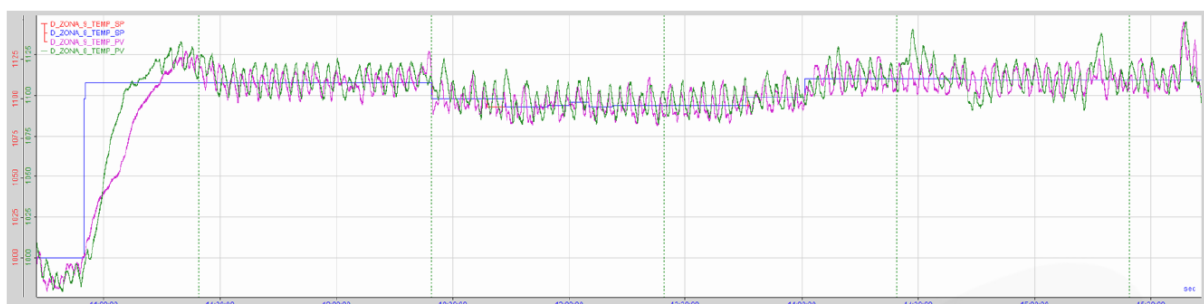


Figure 7. Temperatures during TEST 1.

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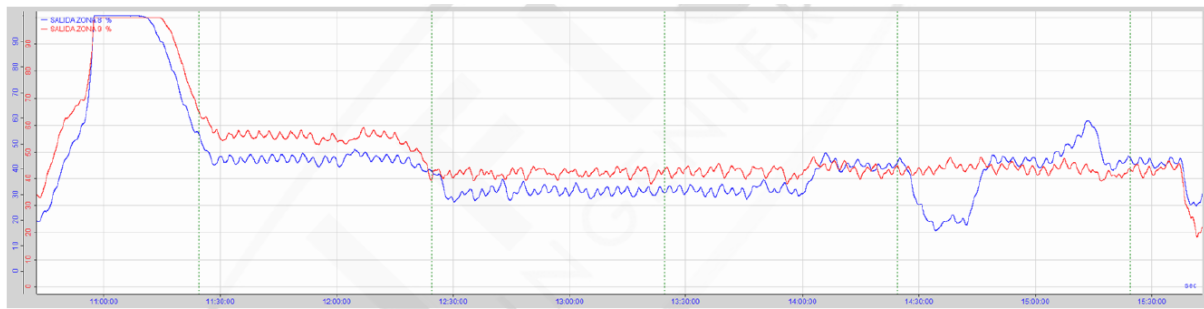


Figure 8. % of demand of burners during TEST 1.

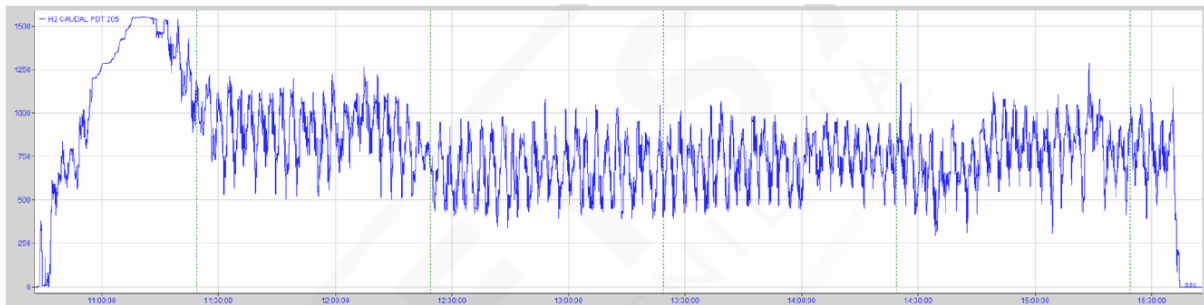


Figure 9. H₂ flow rate during TEST 1.

These are the consumptions according to the H₂ supply by the trucks. At the beginning of the test, 2 trucks of 4.133 and 4.117 Nm³ respectively were connected and simultaneously feed the main decompression skid. The invoiced amount was set by difference of pressure of H₂ in the trucks according to the remaining values.

Table 6. Invoiced amount of hydrogen gas for TEST 1.

Initial truck H ₂ pressure (barg)		207
Final truck H ₂ pressure (barg)		113,1
Date:	amount [Nm ³]	Supplier:
11.12.2025	3.197,00	NipponGases

2.2.1 Safety considerations

The connecting tube between the truck and the decompression skid must have been purged with nitrogen while being connected. When considering the regulation of the burners, during of the return from consuming H₂ to NG, care must be taken to ensure that, in the event of them being out of adjustment, there is more excess air than 'excess' fuel.

2.2.2 Flue gas analysis

In this trial, flue gases have been sampled from several burners in order to assess the emissions based on the burner itself. It has been observed high variability based on the ON-OFF burner operation. Flue gases are exhausted also when burner is OFF. When burner is ON (relatively low cycle time, from few seconds to 3-4 min), air valve opens first, increasing the flue gases exhaust rate, and then opens the fuel gas valve. It could be also observed variable CO₂ percentage in the burners, meaning that the burner exhaust not only its own produced flue gases, but also a fraction of the flue gases stream in the combustion chamber itself.

A comparison with natural gas operation, in sampling 66(1) and 62(4) show the relative impact of hydrogen firing (two-three times higher than natural gas firing). Values plot are averaged when burner is ON. A correlation with the amount of CO₂ (produced in the zones firing gas natural) can be observed.

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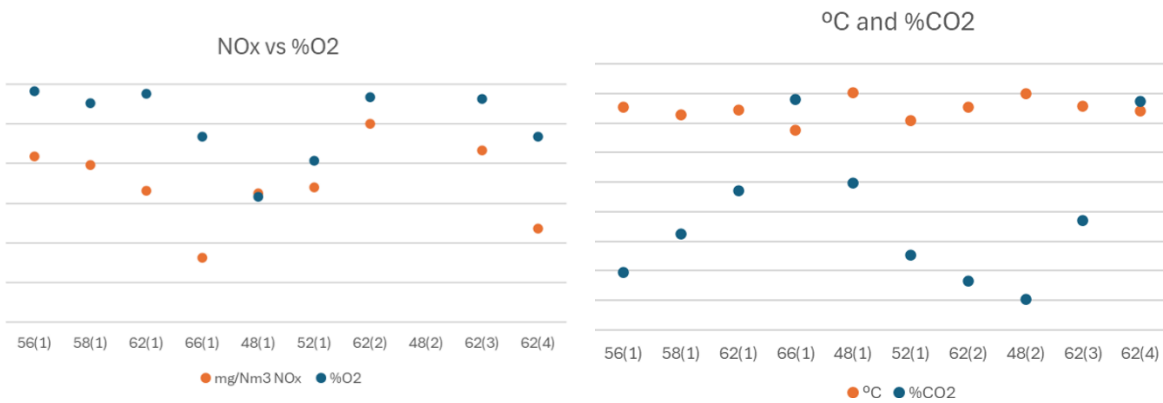


Figure 10. Emissions result during TEST 1 (relative values).

Based on these results, for the second campaign it has been agreed to select two representative burners (FLAME and FLAME-FLOX modes) and measure flue gases during a longer time period.

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2.3 TEST 2: 22.01.2026 – 6XXX Grade family

The test is carried out without any major issue, with a sequence of three heats. No significant quality defects are observed in the preliminary analysis. The amount of scale present does not appear to have increased considerably, nor are any new defects apparent in the coils. A sample is taken from each heat for subsequent laboratory analysis and comparison with coils of similar grade and characteristics.

In this test, hydrogen is supplied at 13:24h, but there is no production until approximately 15:15h due to some production incidences, at which time the furnace is at a maintenance temperature setpoint. The graphs show how, with the change in the temperature setpoint (increase from 950°C to 1.150°C at around 15:15h), the response to reach the SP is very slow, taking a long time to reach the value despite the power demand being 100%.

At this point, the fuel pressure settings on the burners are checked, slightly increasing the power of all of them to reach the correct values. It is also observed during this period that two of the burners in zone 8 are not operational. They are reset and begin to operate normally. The actions described result in an increase in power and therefore in the achievement of the temperature SP.

Table 7. General data on TEST 2.

Beginning of the trial	13:24
End of the trial	19:12
Duration	5:48
Number of heats	3
Mean flow rate (Nm ³ /h)	649,27
Maximum flow rate (Nm ³ /h)	1132,45
Total consumption (Nm ³)	3768,32
Maximum burner H ₂ pressure (mbarg)	144,85
Mean burner H ₂ pressure (mbarg)	118,15
Minimum burner H ₂ pressure (mbarg)	102,01
Excess air global fan (rpm)	2800 rpm

Table 8. Temperatures inside the tunnel furnace during TEST 2.

	8 zone	9 zone
SP maximum temperature (°C)	1150	1150
Maximum achieved temperature (°C)	1149,32	1151,08
SP minimum temperature (°C)	899	899
Minimum achieved temperature (°C)	930,82	931,70
SP mean temperature (°C)	1061,74	1062,08
Mean achieved temperature (°C)	1052,84	1054,01
Maximum demand (%)	100%	100%
Mean demand (%)	48,05%	49,07%

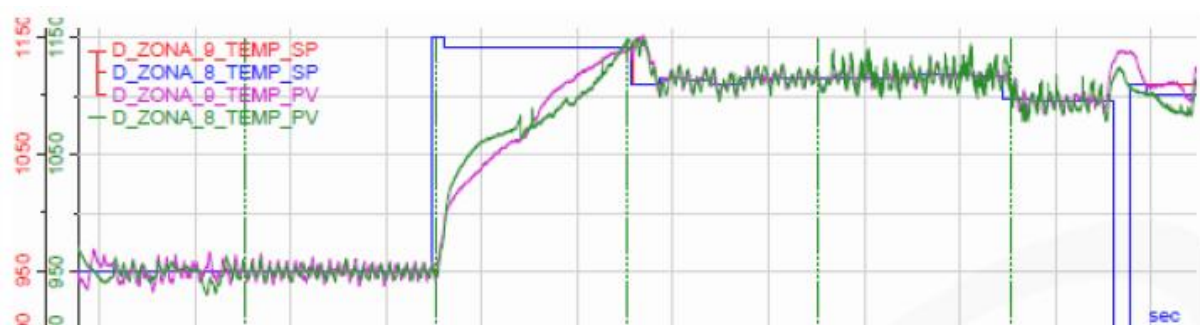


Figure 11. Temperatures during TEST 2.



Figure 12. H2 flow rate during TEST 2.

These are the consumptions according to the H₂ supply by the trucks. At the beginning of the test, 2 trucks of 4.133 Nm³ each one were connected and simultaneously feed the main decompression skid. The invoiced amount was set by difference of pressure of H₂ in the trucks according to the remaining values:

Table 9. Invoiced amount of hydrogen gas for TEST 2.

Initial truck H ₂ pressure (barg)		209
Final truck H ₂ pressure (barg)		103
Date:	amount [Nm ³]	Supplier:
22.01.2026	4.546,00	NipponGases

2.3.1 Flue gas analysis

In order to cope with the variability of flue gas analysis due to the high-frequency ON-OFF operation, it has been agreed to select two representative burners (FLAME and FLAME-FLOX modes) and measure flue gases during a longer time period.

In this testing campaign, it has been observed a high value of oxygen that explains the high level of NO_x measured. Air flow can be adjusted with a variable speed drive, but a fine tuning in the burner valves is accomplished. In this case, the adjustment has been done during normal operation, burner by burner, and the adjustment may be influenced by the operating point of the other burners in the zone.

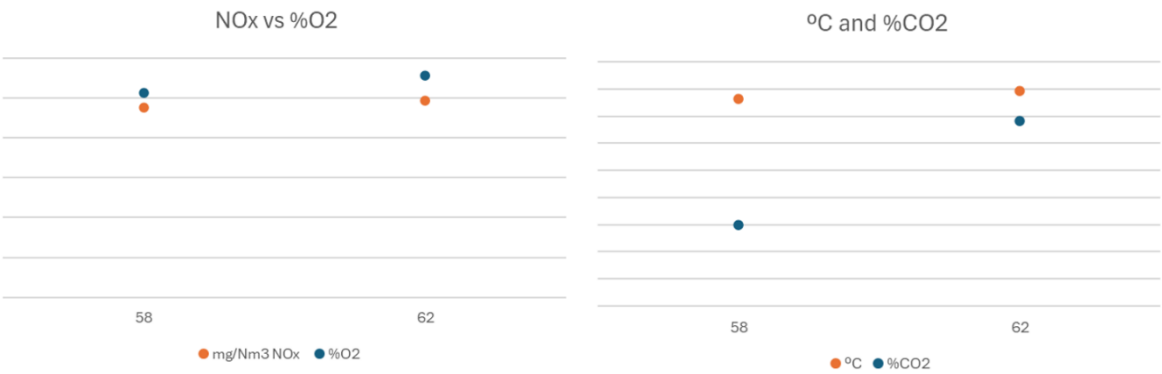


Figure 13. Emissions result during TEST 2 (relative values).

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2.4 TEST 3: 18.02.2026 – 9XXX Grade family

The test is carried out without any major surprises, with a sequence of four heats. No significant quality defects are observed in the preliminary analysis. The amount of scale present does not appear to have increased considerably, nor are any new defects apparent in the coils. A sample is taken from each heat for subsequent laboratory analysis and comparison with coils of similar grade and characteristics.

It has been decided to change the strategy for adjusting the burners. In this test, the adjustment of burners is carried out with all 16 burners On at the same time, rather than lighting them one by one. This measure is intended to optimise fuel consumption by minimising excess air.

Once the furnace is prepared to work with H₂ and begin the first sequence, there is an incident in another part of the process that prevents it from starting at that moment. The temperature SP is reduced to 900°C during the incident (approximate duration 1h15'). During this period, when the furnace is at 900°C SP, consumption of approximately 105Nm³ is recorded. Regulation of the furnace in this section was successful, and all the burners were in proper working conditions from the beginning of the test.

Table 10. General data on TEST 3.

Beginning of the trial	10:49
End of the trial	18:09
Duration	7:20
Number of heats	4
Mean flow rate (Nm ³ /h)	603,76
Maximum flow rate (Nm ³ /h)	1097,08
Total consumption (Nm ³)	4431,58
Maximum burner H ₂ pressure (mbarg)	139,53
Mean burner H ₂ pressure (mbarg)	119,96
Minimum burner H ₂ pressure (mbarg)	85,78
Excess air global fan (rpm)	2800 rpm

Table 11. Temperatures inside the tunnel furnace during TEST 3.

	8 zone	9 zone
SP maximum temperature (°C)	1171	1171
Maximum achieved temperature (°C)	1203,29	1217,70
SP minimum temperature (°C)	900	900
Minimum achieved temperature (°C)	885,67	883,87
SP mean temperature (°C)	1091,48	1091,34
Mean achieved temperature (°C)	1110,23	1091,34
Maximum demand (%)	100%	100%
Mean demand (%)	45,69%	48,58%

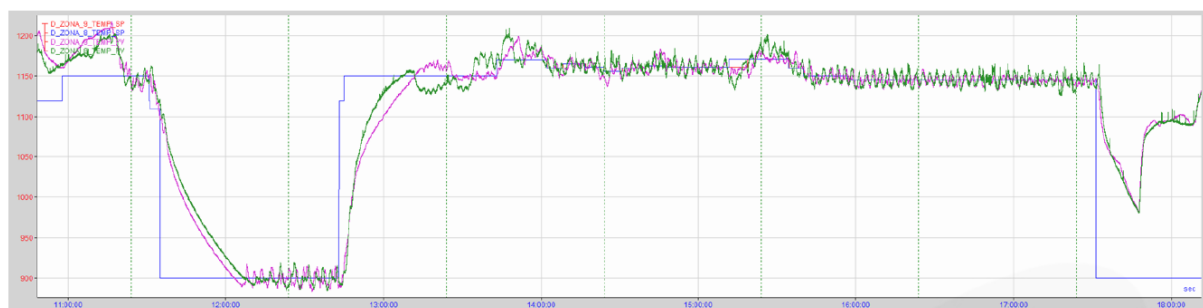


Figure 14. Temperatures during TEST 3.

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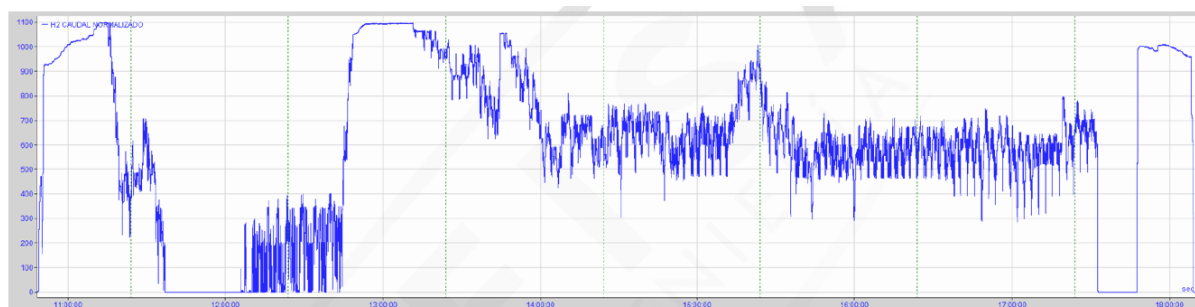


Figure 15. H₂ flow rates during TEST 3.

These are the consumptions according to the H₂ supply by the trucks. At the beginning of the test, 2 trucks of 4.133 Nm³ each one were connected and simultaneously feed the main decompression skid. The invoiced amount was set by difference of pressure of H₂ in the trucks according to the remaining values:

Table 12. Invoiced amount of hydrogen gas for TEST 3.

Initial truck H ₂ pressure (barg)		201,5
Final truck H ₂ pressure (barg)		77,3
Date:	amount [Nm ³]	Supplier:
18.02.2026	5.476,00	NipponGases

Each time trials are carried out, a series of final closing steps must be followed to ensure that the H₂ is securely disconnected. The steps are the following ones:

- 1) Return in the section of the furnace from H₂ to NG, through the by-pass
- 2) Regulation of the 16 burners to NG combustion.
- 3) Normalization of the furnace to continue production
- 4) Inerting with N₂ the pipeline and low-pressure skid
- 5) Vents on the low-pressure skid
- 6) Vents on the high-pressure skid
- 7) Locking of gas stations

2.4.1 Flue gas analysis

As explained above, for this testing campaign the procedure of fine burner tuning has been improved, in order to minimize the influence of the other burners. It has been achieved a lower excess air and therefore lower NO_x emissions. As key conclusion, spite NO_x level increases when firing hydrogen, it can be controlled if the excess air is properly adjusted. This has also a positive impact on energy consumption and scale losses reduction.

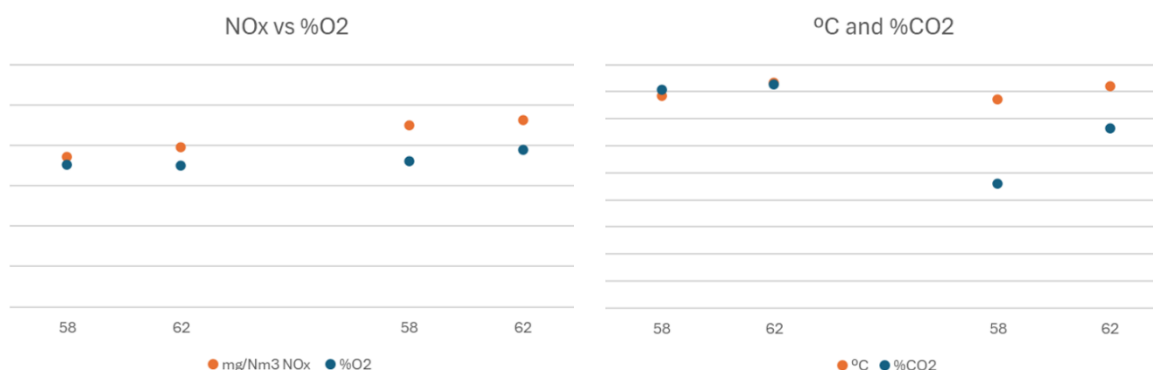


Figure 16. Emissions result during TEST 3 (relative values).

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2.4.2 Minor incident and action taken

A couple of weeks after the trials, it was noted that refractory material had fallen from part of the tunnel furnace lid in one of the areas where hydrogen combustion was tested. This caused a hole in the lid, so the lid has been removed for repair. Samples have been taken from the lid for analysis by CEIT. Following an initial inspection, it appears that the joint between the different sections of the furnace failed (possibly due to high temperatures), causing the material to fall. It is not known whether this is directly related to the trials carried out, but this is not considered the most likely hypothesis.

However, during the summer shutdown, the scheduled samples will be taken for analysis at CEIT, as planned. The results of these analyses will be added to the various project deliverables when they become available.



Figure 17 Lid broken by the loose refractory

2.5 On-site quality analysis of coils of the trials

Despite the preventive attitude regarding the potential presence of extra scale on the coils, quality analyses have revealed scale similar to that found with natural gas. Neither the real-time checks nor the subsequent analyses carried out on these coils by AM Sestao have revealed any significant or detrimental changes in the coils.

2.6 Conclusions and next steps

1. The objectives of the trial were achieved, with safety generally ensured. The system is therefore ready for a future in which hydrogen can be burned in these areas as an alternative to natural gas.
2. Forcing all burners to adjust them at the same time minimised the oxygen present. It would be also beneficial to be able to increase the skid's outlet pressure to a higher level. Nowadays, pressure drops across the non-return valve make it difficult to adjust the burners quickly, as the skid's outlet pressure cannot be raised any further due to physical constraints.
3. This project has led to the installation of burners fitted with dual valves and BCUs and a variable excess air inlet regulator. This represents a significant improvement to the burners and ensures compliance with the updated safety standards in line with the new regulations. Furthermore, it would enable the zone's holding temperature to be lowered, as the burners can be switched on and off remotely and quickly. This could lead to a reduction in natural gas consumption, bringing both financial and environmental benefits.

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4. High-cycle burner ON/OFF operation challenges the combustion tuning, but an improved methodology could be done to reduce excess air. Flue gas analysis shows a variable CO₂ level, meaning that the burner exhaust part of the flue gases from the combustion chamber, not only the generated itself.
5. There is a strong impact of the excess air on NO_x emissions. It is very important to tune properly the burner, which would have a positive impact also on energy consumption and scale losses.
6. Hydrogen firing increases NO_x by 2-3 times when firing hydrogen (depending on the excess air used), but emission level can be kept below the regulation limit.
7. No downgrading due to scale surface defect in the trial sequences. Downgraded coils in Trial 1 and 3 are due to long stay time in the furnace and out of the scope of the trial
8. The coils produced with H₂ trials, in general, do not show different behaviour compared to coils produced during same period and similar descaler pressure (qualitative comparison as quantitative is not possible)
9. For continuous hydrogen-powered operation, it is still necessary a hydrogen pipeline that reaches the plant. This is part of the Basque Hydrogen Corridor project, which is currently delayed compared to the original 2026 target.

3 Task 6.1: Retrofit on Tunnel Furnace Demonstrator with H₂/Air Combustion – CFD simulations by Tecnia

3.1 CFD simulations on tunnel furnace demonstrator with H₂/air

The tunnel furnace demonstrator of AMS will be retrofitted with H₂/air combustion in two independent heating zones, zones 8 and 9, without modifying the actual burners.

The main objective is to analyse the effect of H₂/air use on the behaviour of these zones in comparison with the current situation by means of CFD simulations.

The first approach was to simulate the two heating zones, 8 and 9, including the translation of the thinslab.

And due to the difficulty of including the PID control for the on/off sequence of the burners in the model, it was decided to define a specific number of burners that will be on all the time, and which are off.

A UDF to fix the temperature of the thinslab constant (1,100°C) for the part of the thinslab located outside of zone 8 was developed and the speed of thinslab is set up to meter 4 of zone 8 to 4.8 m/min and then 50 m/min.

Under these conditions the simulation for the NG/air case was performed, but the results obtained for the furnace zones temperature and thinslab temperature were not realistic. The set-point zone temperature could not be corrected.

Different transient situations happen during thinslab movement across the heating zones, see Figure 18.

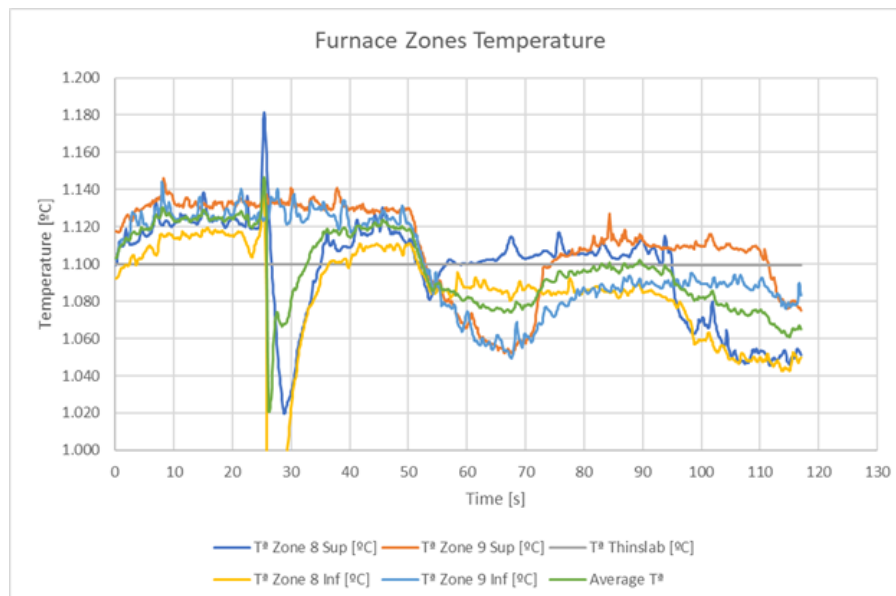


Figure 18. Temperature evolution of zones during thinslab movement of CFD simulation for NG/air case.

With these results we decided to change the initial approach and perform transient simulations with the thinslab occupying the entire length of zones 8 and 9 without moving it, with a fixed distribution of burners on, for both cases for a better comparison of the effect of the use of hydrogen.

So, two simulations under the same operation conditions have been performed for zones 8 and 9 of the furnace.

3.1.1 Model set-up

To perform this work, we have used different tools from ANSYS, like SpaceClaim, FluentMeshing and FLuent of version 2023R2.

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With the installation drawings and the information collected by AMS, a 3D model of the furnace interior of zones 8 and 9 was made, including the burners in simplified form, from the gas outlet nozzle.

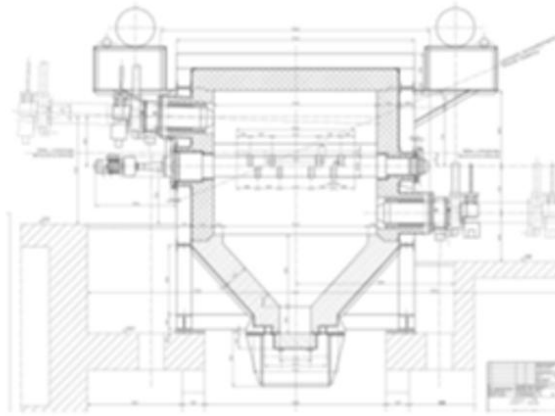


Figure 19. Drawing of tunnel furnace section.

The model has a polyhedral mesh of 3,504,098 cells for fluid and the thinslab. The total length of zones 8 and 9 are 26 meters with 17 burners of 0.24 MW each.

The dimensions of the thinslab are 58x1,250x34,010 mm and have been positioned to occupy the two zones (see Figure 20).

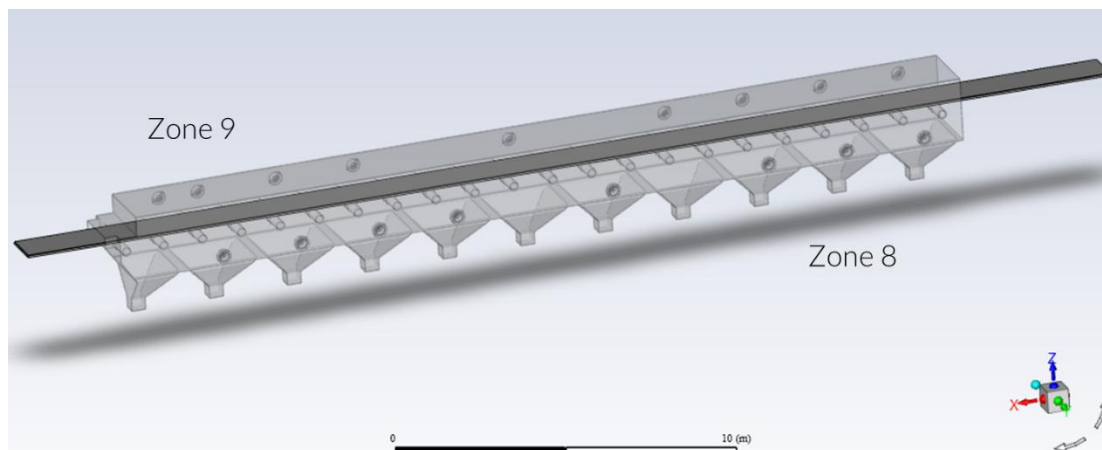


Figure 20. 3D model of the tunnel furnace zones and the thinslab.

For the set-up of the model, we have used two different approaches to solve the combustion process, the reason of this is because with the first approach the results obtained for the NO_x were not in line with the values measured by AMS in another project in its facilities lab. With the second approach more accurate results were obtained.

Set-up:

- Transient simulation of 31,2 s, time taken to pass the thinslab through the zones 8 and 9 at 50 m/min
- Energy, SST-K-Omega Turbulence model, Radiation DO
- Non-Premixed Combustion, Chemical Equilibrium, Non-Adiabatic, PDF for NG with preheated air (386 °C) and H₂ with preheated air (440 °C) and $\lambda = 1,05$
- NG(CH₄) LHV: 9.94 kWh/Nm³; H₂ LHV: 2.97 kWh/Nm³
- Steel grade of thinslab: 3101
- Uniform temperature of thinslab of 1,100 °C as initial condition
- Temperature of zones 8 and 9: 1,105 °C as initial condition
- Burners On: 52, 54, 56, 60, 62, 79, 81, 83, 85, 87 and 91 with constant mass-flow rates
- Input power: NG/air, 2,643 MW and H₂/air, 2,539 MW. 3.94% lower for H₂/air

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- Furnace walls: Natural convection and radiation with ambient, and shell conduction for insulation thickness (2 layers)
- Rolls surface: Heat flux, -300 W/m^2

3.1.2 CFD Results

All the figures with contours correspond to the final time of the simulation.

Velocity

The velocity of flue gases in a longitudinal plane through the centre of the furnace is shown on for both cases in Figure 21 and Figure 22. For visualization purposes the maximum value is fixed to 5 m/s. The values of higher velocities are in the same areas, the average velocity in the fluid domain is 2 m/s for the NG/air case and 1.96 m/s for the H₂/air case.

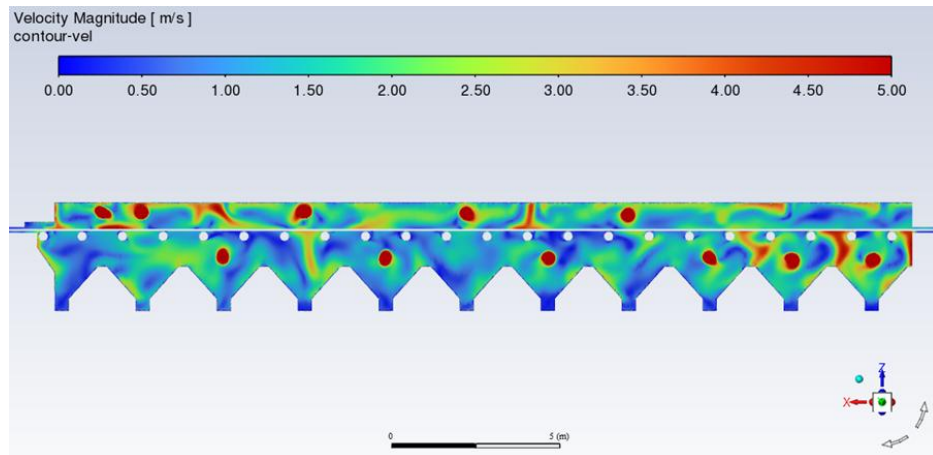


Figure 21. Contour of velocity for NG/air case.

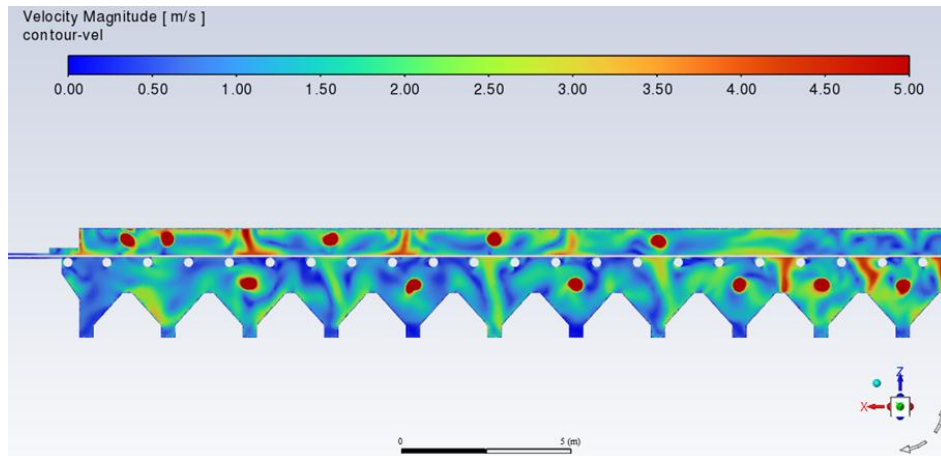


Figure 22. Contour of velocity for H₂/air case.

As an example of the velocity profile and movement of the flue gases around the thinslab in Figure 23 and Figure 24 are shown the velocity contour on cross section planes at burner n° 54 and n° 85 for the NG/air case and for the H₂/air case.

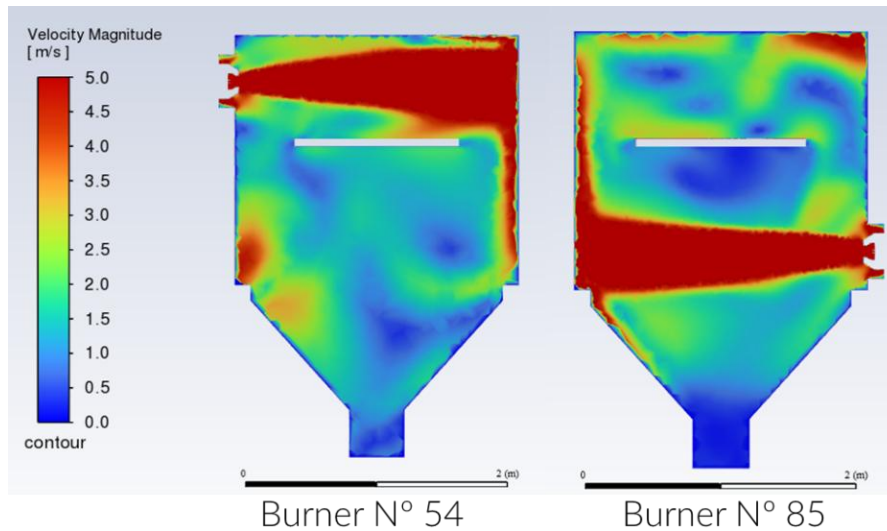


Figure 23. Velocity contours in cross sections at burner n° 54 and 85 for NG/air case.

There are some differences at both locations, the zones of higher velocities appear in the same positions but for the case of H₂/air the higher values are more dispersed, with higher values near the top face of the thinslab.

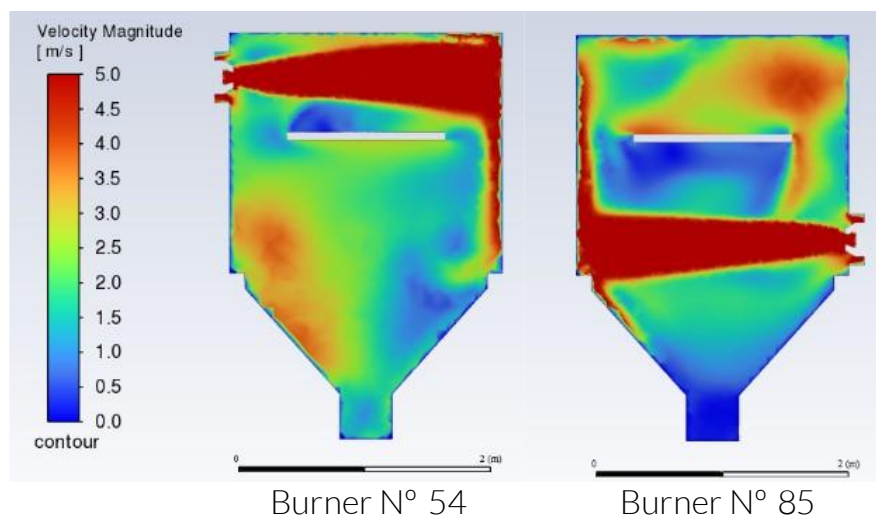


Figure 24. Velocity contours in cross sections at burner n° 54 and 85 for H₂/air case.

To compare the different velocity profile along the axis of the burner for NG/air and H₂/air in Figure 25 is plotted the velocity from burner wall to opposite wall for burners n° 54 and n° 85. The big difference in velocity is observed at the first 400 mm from the natural gas tip.

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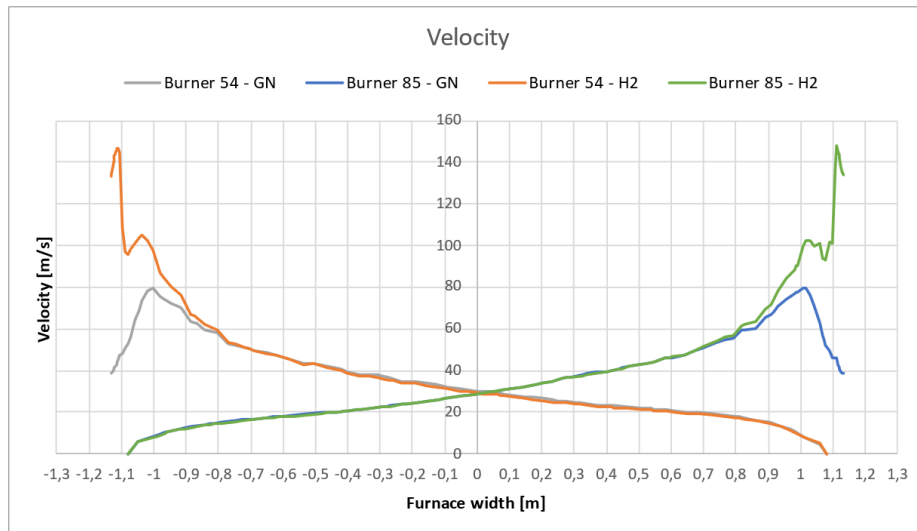


Figure 25. Velocity profile along axis of burners 54 and 85.

Furnace Temperature

For the temperature, the results obtained are quite similar for both cases, the average temperature on both zones for NG/air is 1,110.97 °C and 1,112.73 °C for the H2/air case, just an increment of 1.76°C. The same distribution of temperatures can be observed in a general view as is shown in Figure 26 and Figure 27. In both cases the thinslab divides the furnace in two temperature zones, the region below, with lower temperature and the region above the thinslab with high temperature.

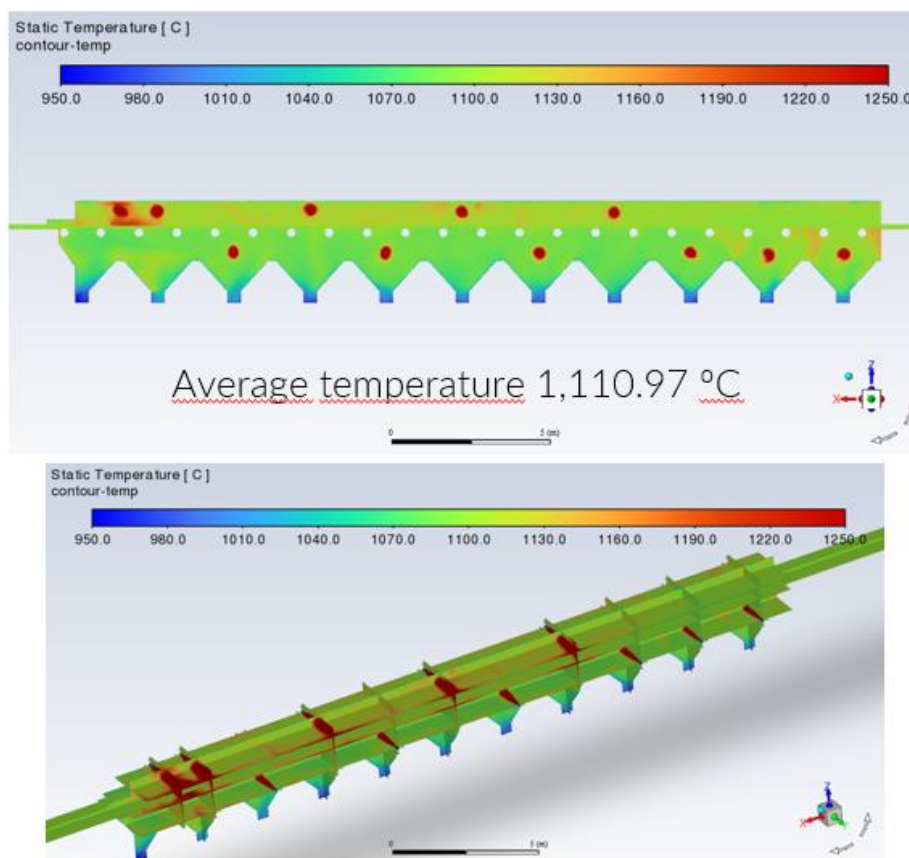


Figure 26. Temperature distribution in zones 8 and 9 for NG/air case.

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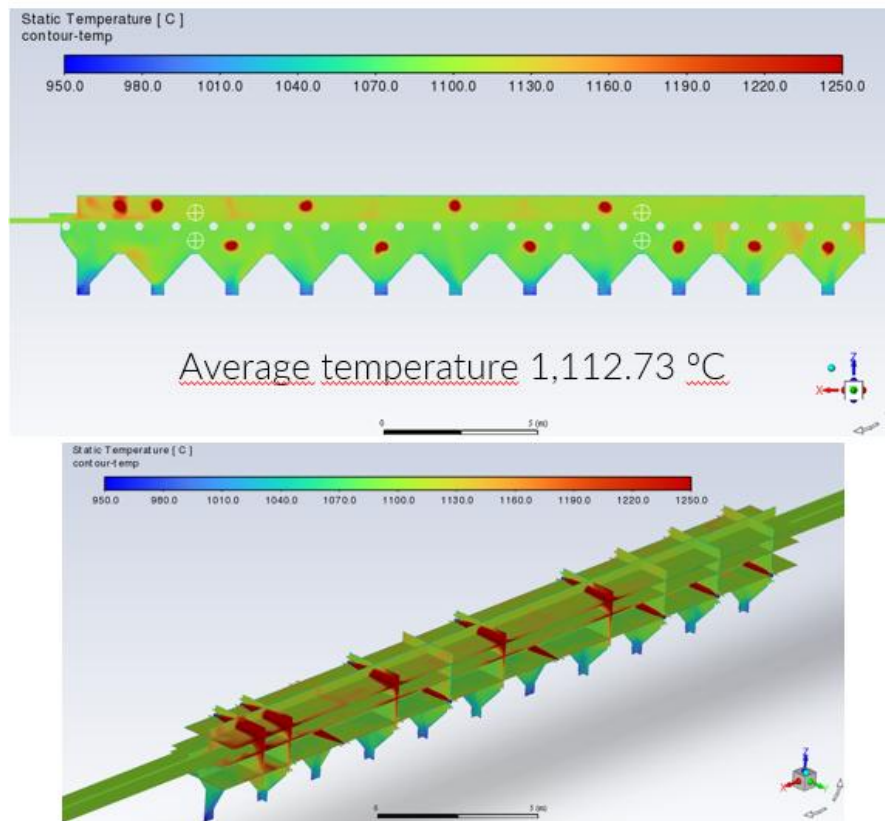


Figure 27. Temperature distribution in zones 8 and 9 for H₂/air case.

But if we plot the temperature profile in a cross section, as in the case of the velocity, the differences around the thinslab appears. For the H₂/air case the temperature above the thinslab and close to it is higher than for the NG/air case.

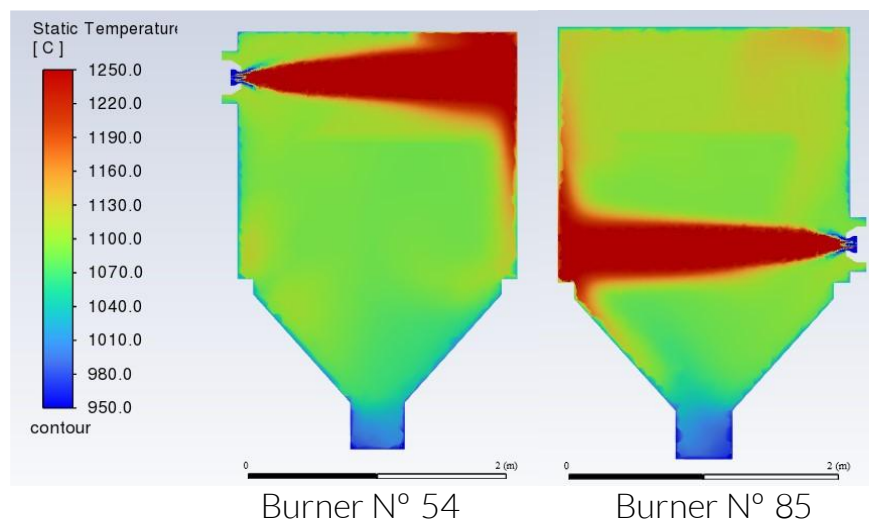


Figure 28. Temperature contours in cross sections at burner n° 54 and 85 for NG/air case.

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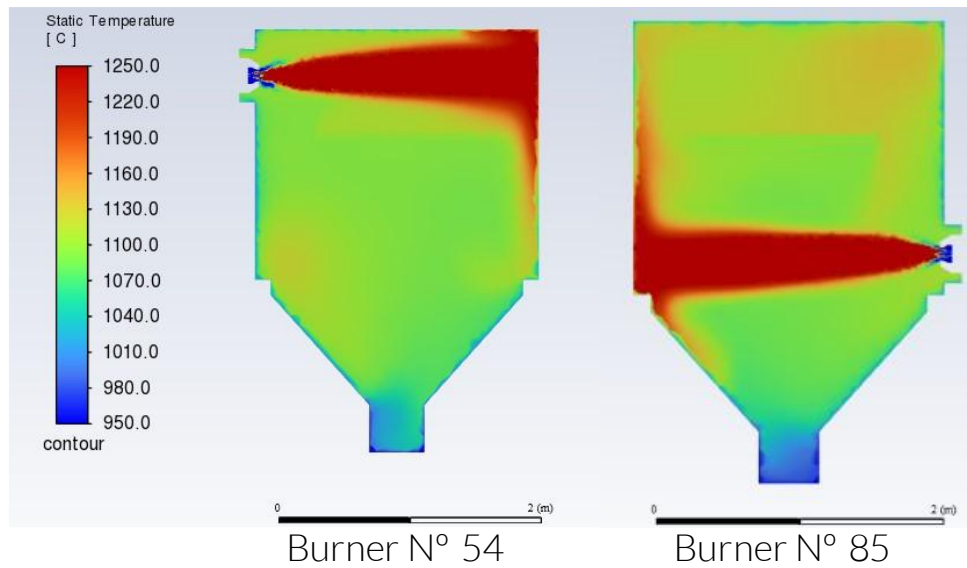


Figure 29. Temperature contours in cross sections at burner n° 54 and 85 for H₂/air case.

To show the difference of the combustion temperatures for both cases in Figure 30 is plotted the temperature along the axis of burners n° 54 and n° 85 from wall to wall of the width furnace, showing the same temperature for both cases starting from the centre of the furnace.

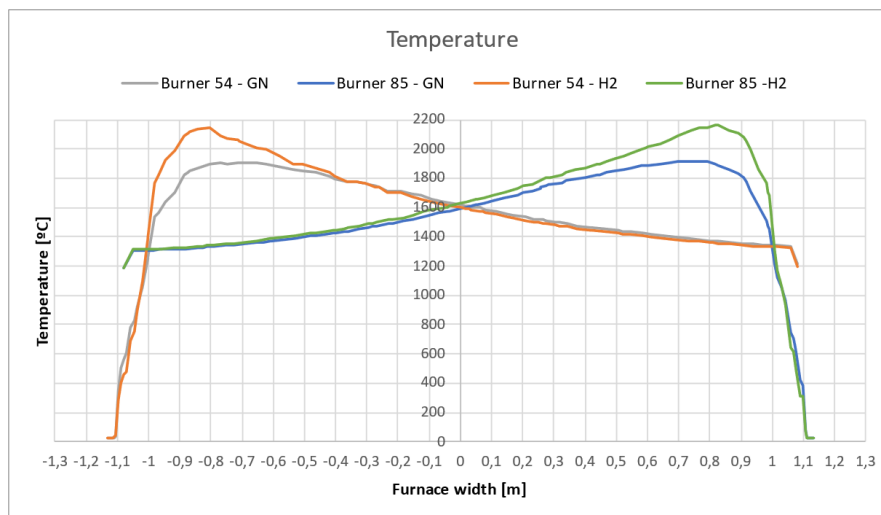


Figure 30. Temperature along axis of burners N° 54 and N° 85.

Another region of interest is a cross section at a roll, the thinslab is supported by the roll. To show the temperature profile we have selected the roll n° 13 that is in the middle of the model, Figure 31, again a region with a slightly higher temperature is located close to the top surface of the thinslab.

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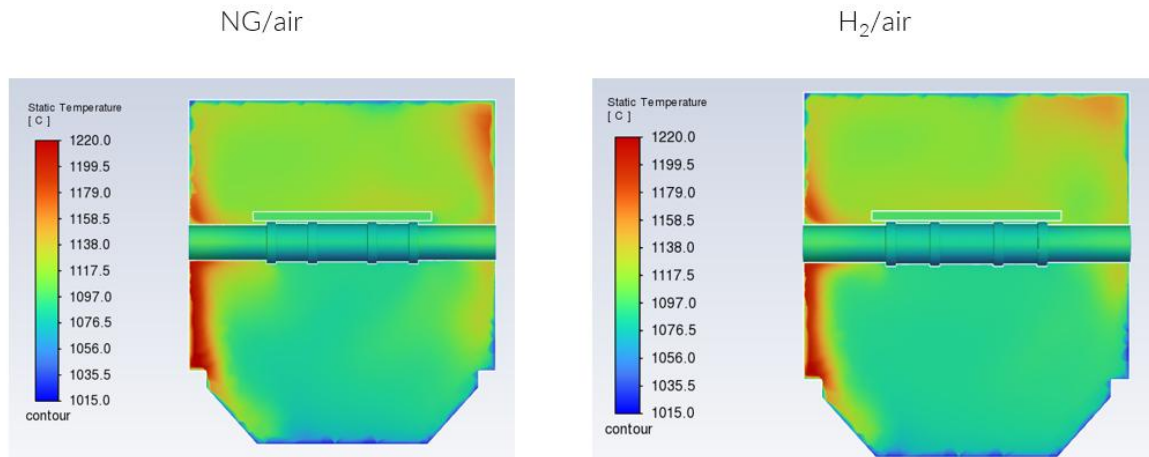


Figure 31. Temperature contours on cross section at roll n° 13 for both cases.

During the transient simulation of 31.2s the temperature above and below the thinslab have been recorder. The locations of the monitor points are displayed in the top image of Figure 27. These values and the average temperature of the two zones are plotted on Figure 32.

The average temperature of the two zones is slightly higher for H₂/air case, and at the locations of the monitors of both zones the average temperatures are higher for the H₂/air except for Z9-Bottom. The numerical values are summarized on Table 13.

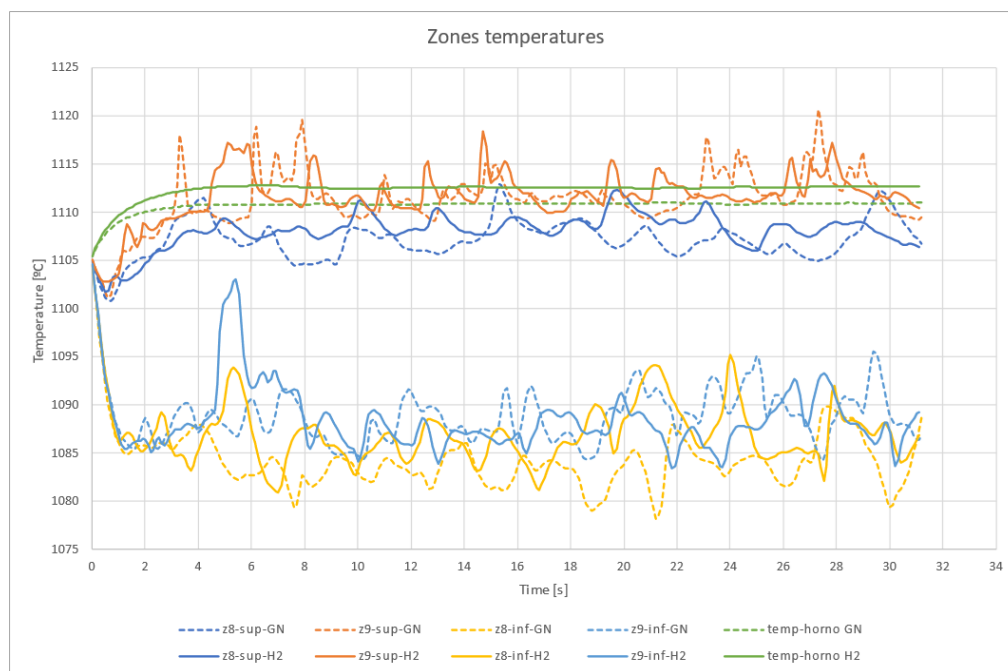


Figure 32. Zones temperature evolution for both cases.

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Table 13. Summary of monitor temperatures for both cases.

Temperature °C	NG/air	H ₂ /air
Z8 – Top	1,107.14	1,108.06
Z8 – Bottom	1,084.18	1,087.04
Z9 – Top	1,111.39	1,111.64
Z9 – Bottom	1,088.82	1,088.51
Furnace zones	1,110.97	1,112.73

Thinslab Temperature

Regarding to the effect of the use of H₂/air on thinslab temperature under the conditions simulated, the same profile of heating across the length of the thinslab is obtained for both cases, with slightly higher temperatures for H₂/air, Figure 33. The lines thinslab-sup (orange) and thinslab-inf (grey) are located at 2 mm from the top and bottom surface of the thinslab and the thinslab-centre (blue) at the centre and all at thinslab axis.

Also, the effect of the burners on is clear on both sides of the thinslab and the roll effect on bottom surface and in the centre.

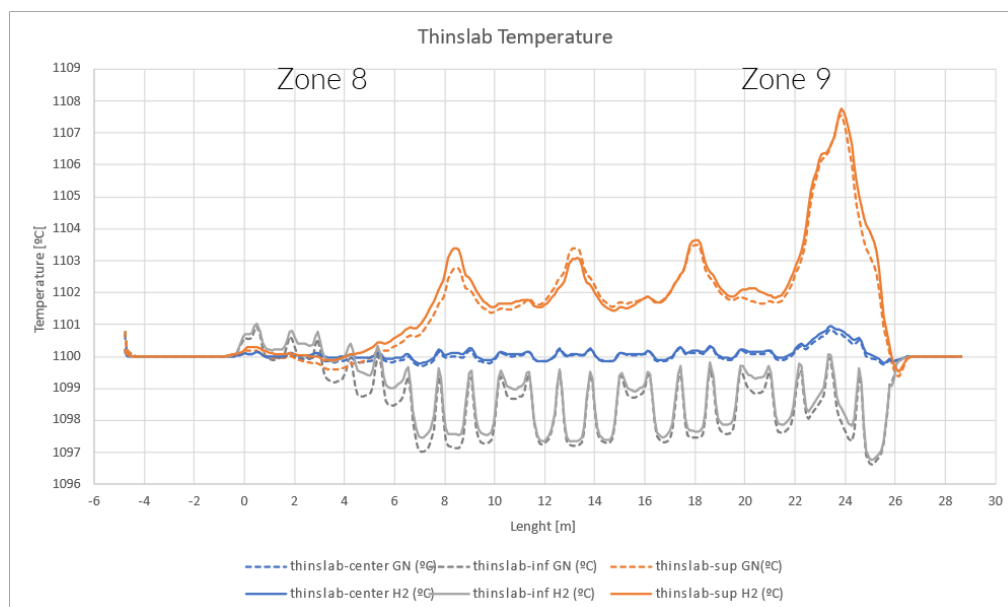


Figure 33. Temperature along thinslab length for both cases

To show more clearly the effect of the burners on over the surface temperature of the thinslab in Figure 34 and Figure 35 are shown the final temperature distribution on top and bottom surface for both cases at the end of the simulation.

The results obtained, again are quite similar for both cases.

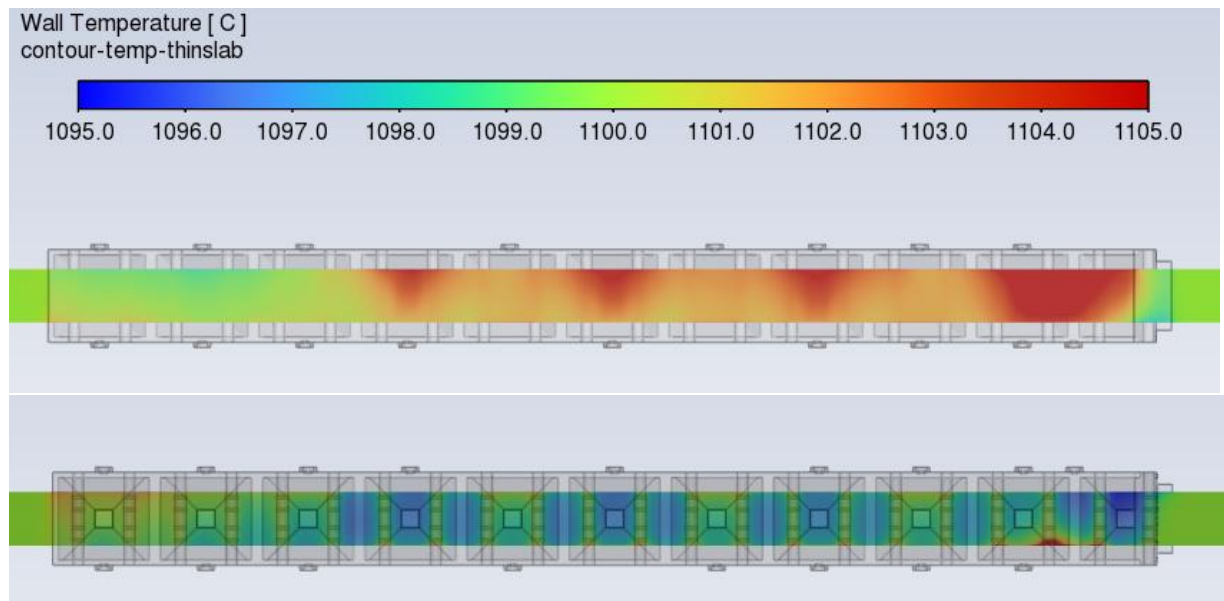


Figure 34. Temperature distribution on thinslab top and bottom surface for NG/air case.

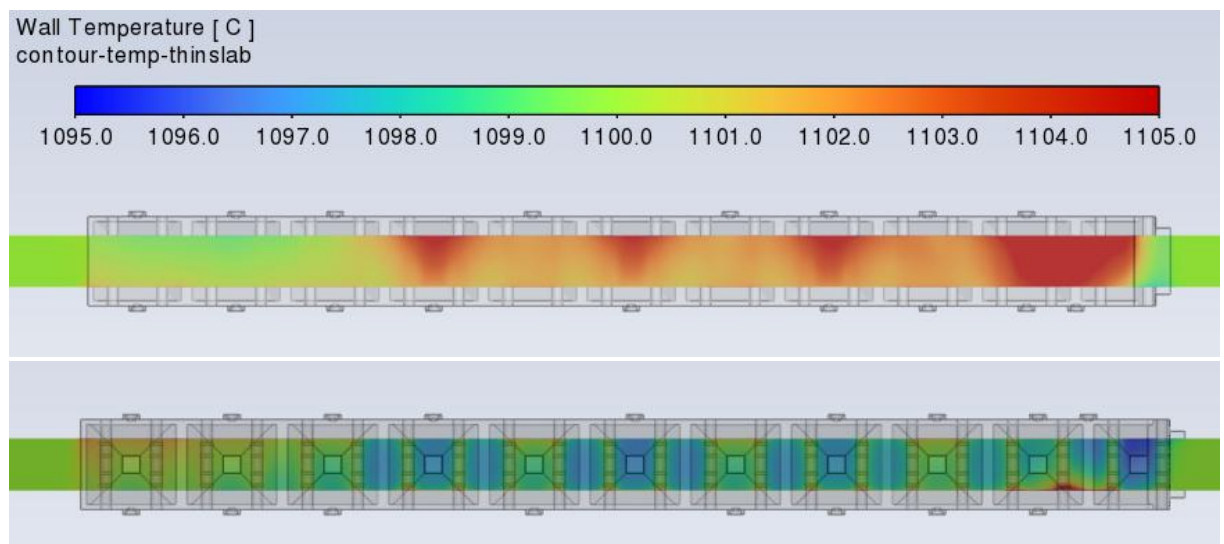


Figure 35. Temperature distribution on thinslab top and bottom surface for H₂/air case.

Combustion species

For the combustion's species, higher levels of NO and H₂O with the use of H₂/air are obtained.

3.1.3 Conclusions

The comparison of results for the NG/air and H₂/air cases under the simulation conditions defined shows that:

- The behaviour of the heating zones 8 and 9 are quite similar for both cases
- The temperature distribution inside the furnace is practically identical and there aren't hot spots for the H₂/air case
- The possible change in the movement of the flue gases inside the furnace doesn't affect the heating of the thinslab
- Under the conditions simulated the heating of the thinslab follows the same pattern, for the case of H₂/air, the temperature gained by the thinslab is slightly higher than for the NG/air
- Higher levels of NO and H₂O with the use of H₂/air

Remark that the power input for the H₂/air case is 3.94% lower than the NG/air case.

4 Task 6.1: Retrofit on Walking Beam Furnace Demonstrator with H_2/O_2 Combustion

4.1 Temperature monitoring tests in the AMOB's walking beam furnace

With the two new burners installed and the H_2 line connected and checked, several tests were carried out by TECNALIA. A description of these two burners is included in the deliverable 6.1 – “Demonstrators of the steel use cases”.

Temperature monitoring tests consist of measuring the temperature of the steel product (beam blank, BB) throughout a complete heating cycle inside the reheating furnace (RHF) of ArcelorMittal Olaberria-Bergara (AMOB). Three tests were performed with different fuel (natural gas or hydrogen) and oxidising agents (air or oxygen) combinations programmed in the two new burners:

- 1) 2025/10/16 – H_2/O_2
- 2) 2025/11/12 – NG/ O_2
- 3) 2026/02/09 – NG/air

The rest of the burners of the furnace worked with NG/air, and with an oxygen enrichment of 6% in zones 1 to 4 (preheating and heating).

The temperature was monitored by a data logger, with a capacity of 20 channels, housed in a box welded to the BB2. The data logger was protected by a thermal barrier and covered with thermal blanket to insulate it from the high temperatures. The thermocouples connected to it were distributed along a BB2 of 6000 mm, in three sections as shown in Figure 36.

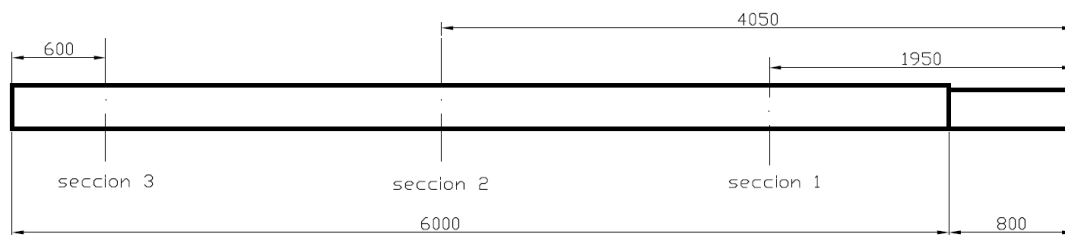


Figure 36. Drawing with the sections of the BB2.

In each section, six points were measured (Figure 37) on the BB2 at different depths (Table 14). In addition, in section 2, the temperature of the air was measured above and below the BB2. The measurement below the BB2 was taken by inserting the thermocouple through the hole 7 (T7), which was drilled only in that section. The identification of the 20 points is detailed in Table 15.

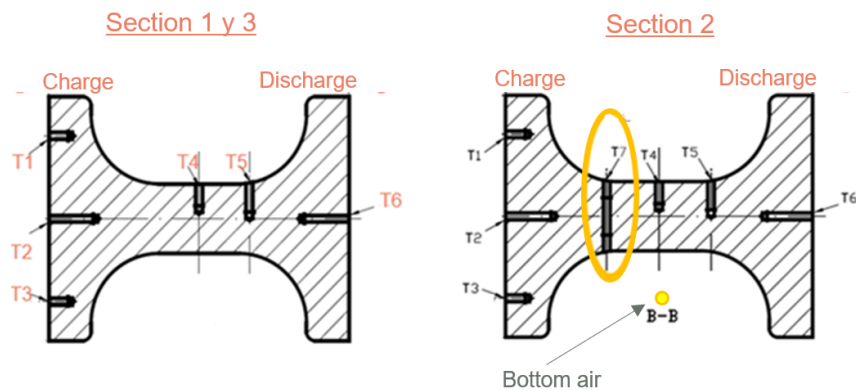


Figure 37. Drawing with the position of the thermocouples in each section of the BB2.

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Table 14. Depth of the holes.

Hole	Depth (mm)
T1	25
T2	55
T3	25
T4	35
T5	40
T6	55
T7	Through hole

Table 15. Name and distribution of the thermocouples.

Thermocouple name	S1	S2	S3
25_sup	1	7	13
55_carga	2	8	14
25_inf	3	9	15
35	4	10	16
40	5	11	17
55_desc	6	12	18
Air Top		19	
Air Bottom		20	

The three tests have been performed with the same distribution of thermocouples, and with the BB2 located in the same position inside the furnace (Figure 38).

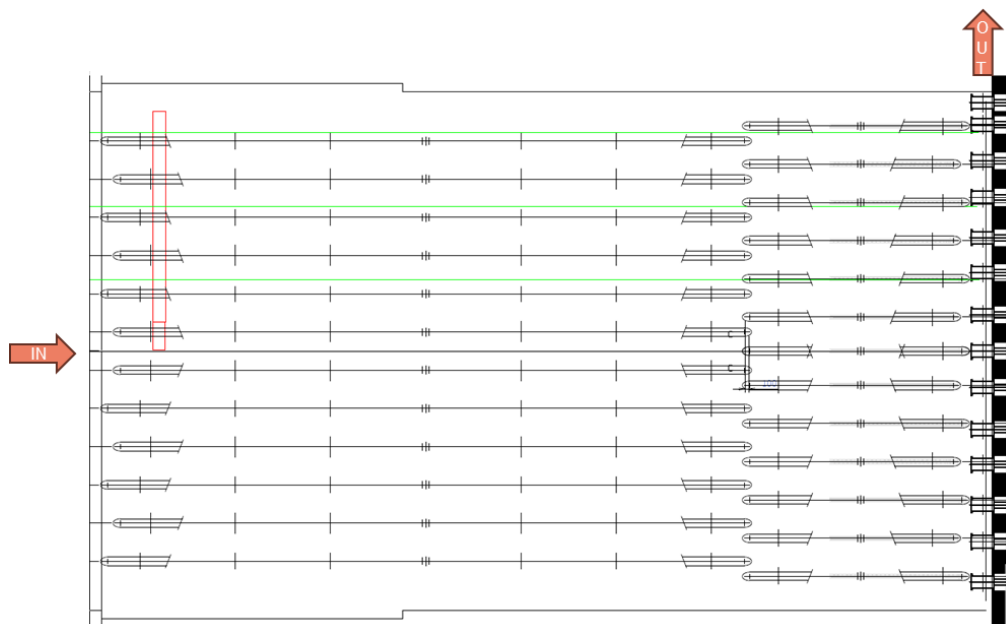


Figure 38. Plan view of the furnace, with the BB2 positioned on the beams.

The box containing the data logger was almost in the centre of the furnace, with the BB2 covering the left-hand side of the furnace, and with the left-hand extreme at around 795 mm from the furnace wall (Figure 39).

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Figure 39. BB2 at the entrance of the furnace, positioned on the beams.

A picture of the monitored BB2 before and after entering the reheating furnace is shown in Figure 40. Before entering the furnace, the 20 thermocouples are installed in the BB2, and connected to the data logger. Additionally, thermal protection is placed to insulate all the equipment.



Figure 40. Monitored BB2 before and after entering the reheating furnace of AMOB.

After the heating cycle inside the RHF, the BB2 with the register was removed from the rolling mill to recover the data logger. A slice sample was also taken from a second BB2 heated close to the monitored BB2 to be analysed by CEIT in their laboratories.

The data logger collects data that yields 20 temperatures versus time curves. However, as the duration of the tests varies depending on the specific circumstances of each production day, the curves are presented in a format that is independent of time, i.e. temperature versus furnace length, to enable the comparison between tests.

Figure 41, Figure 42 and Figure 43 show the graphs of the 20 heating curves recorded during the three performed tests. The grey vertical lines indicate the limits of the zones within the furnace. Additionally, the advance speed of the BB2 (m/min) is shown.

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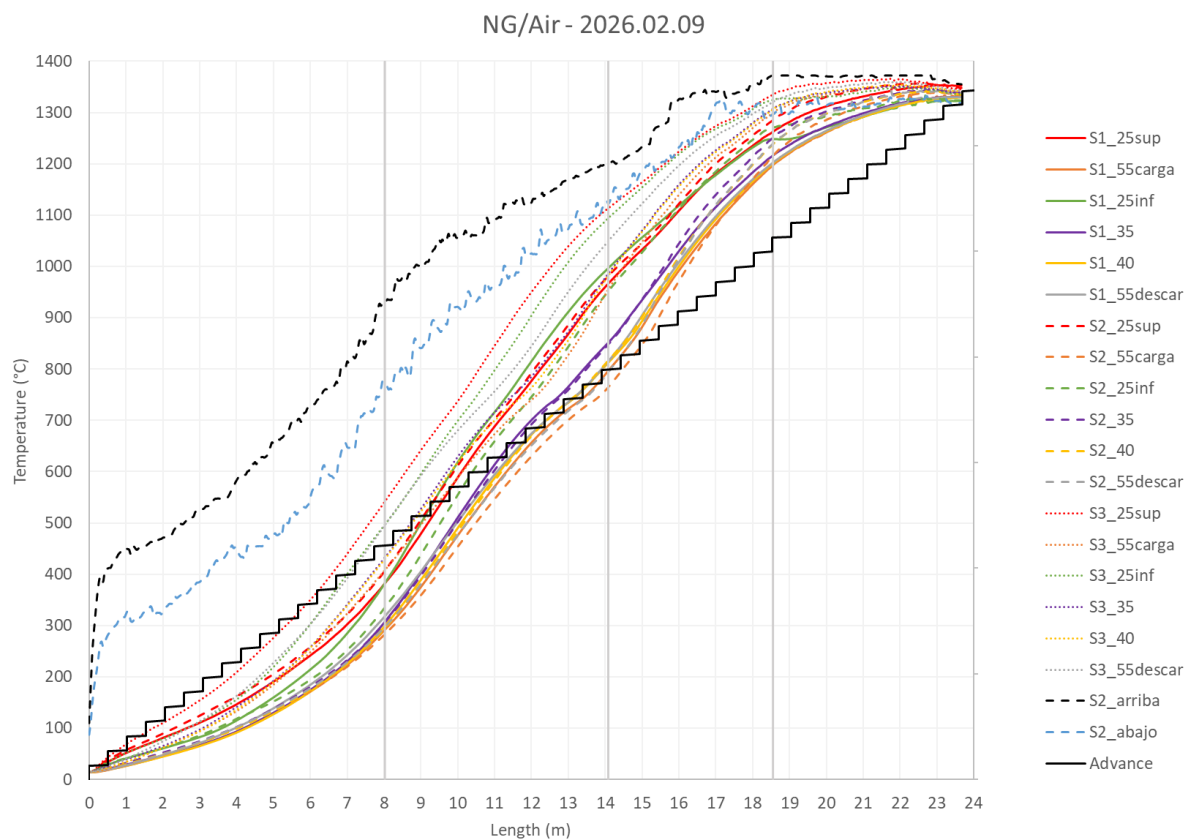


Figure 41. Heating curves of NG/air test.

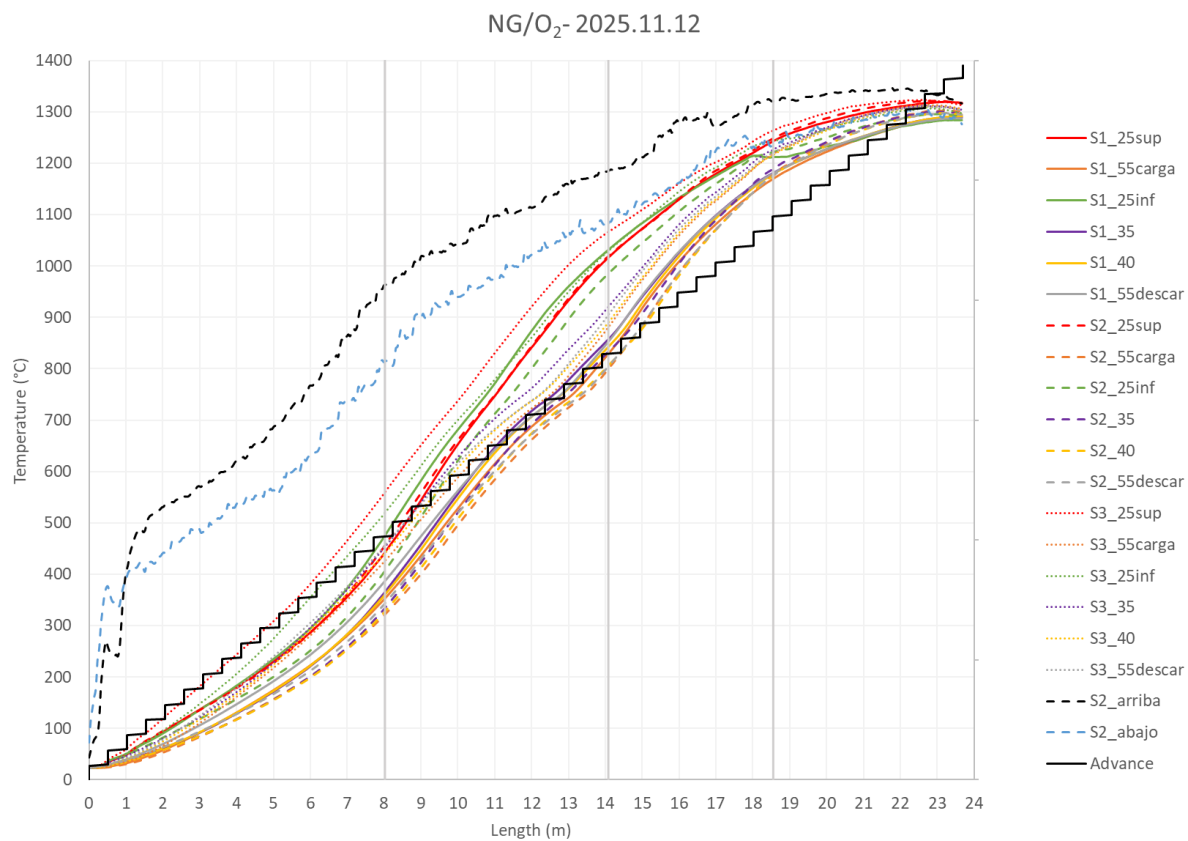


Figure 42. Heating curves of NG/O₂ test.

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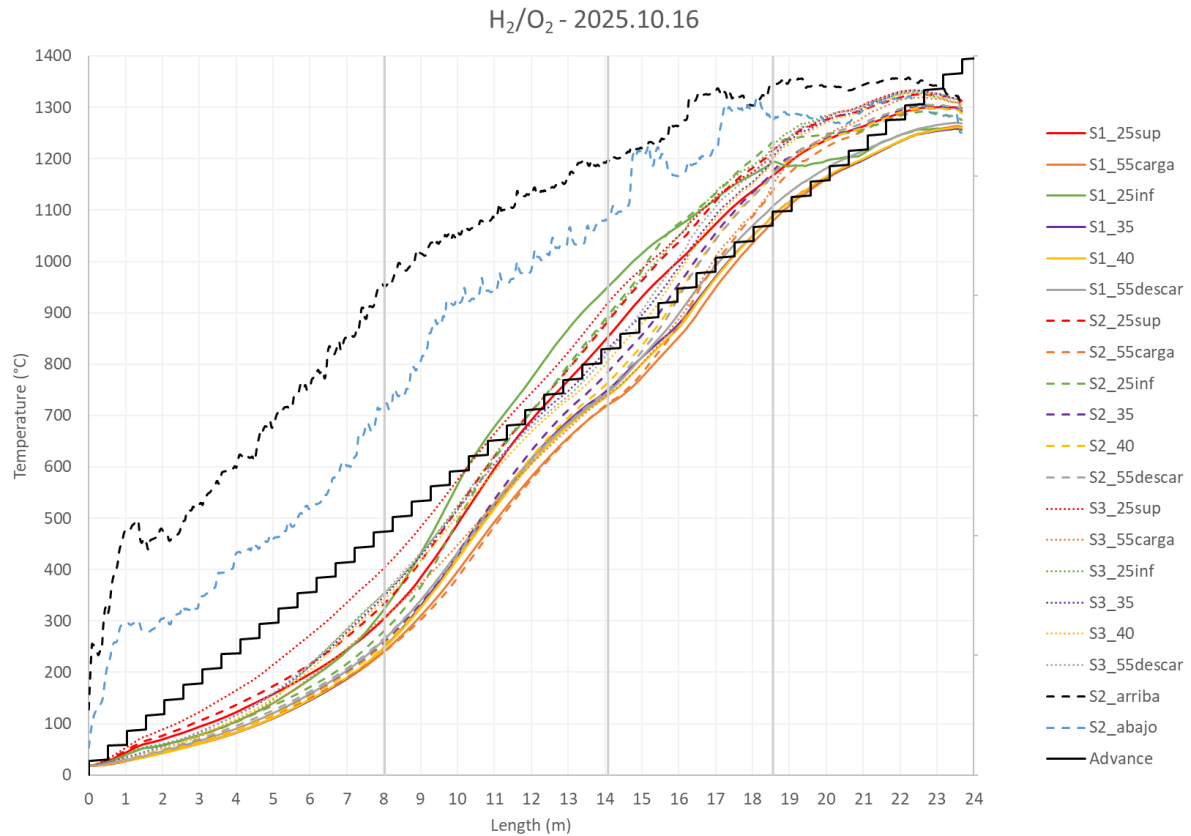


Figure 43. Heating curves of H₂/O₂ test.

After each test, the 20 curves are thoroughly studied alongside the data collected by the steel plant, such as wall temperatures, flow rates and pressure. The curves are compared by section, position and depth for each test. Comparisons between tests are also made. These studies are too extensive to be included in this deliverable. As a summary, some comparative graphs and tables showing temperatures are presented.

It should be noted from the outset that, although similar conditions have been set for the tests, there are factors that depend on the specific circumstances of each production day, and that make each test different.

Firstly, to make the comparison easier, the heating curves of the NG tests are presented in Figure 44 and the heating curves of the oxy-combustion tests are presented in Figure 45. Also, to cover all the cases, the comparison between NG/Air and H₂/O₂ is presented in Figure 46.

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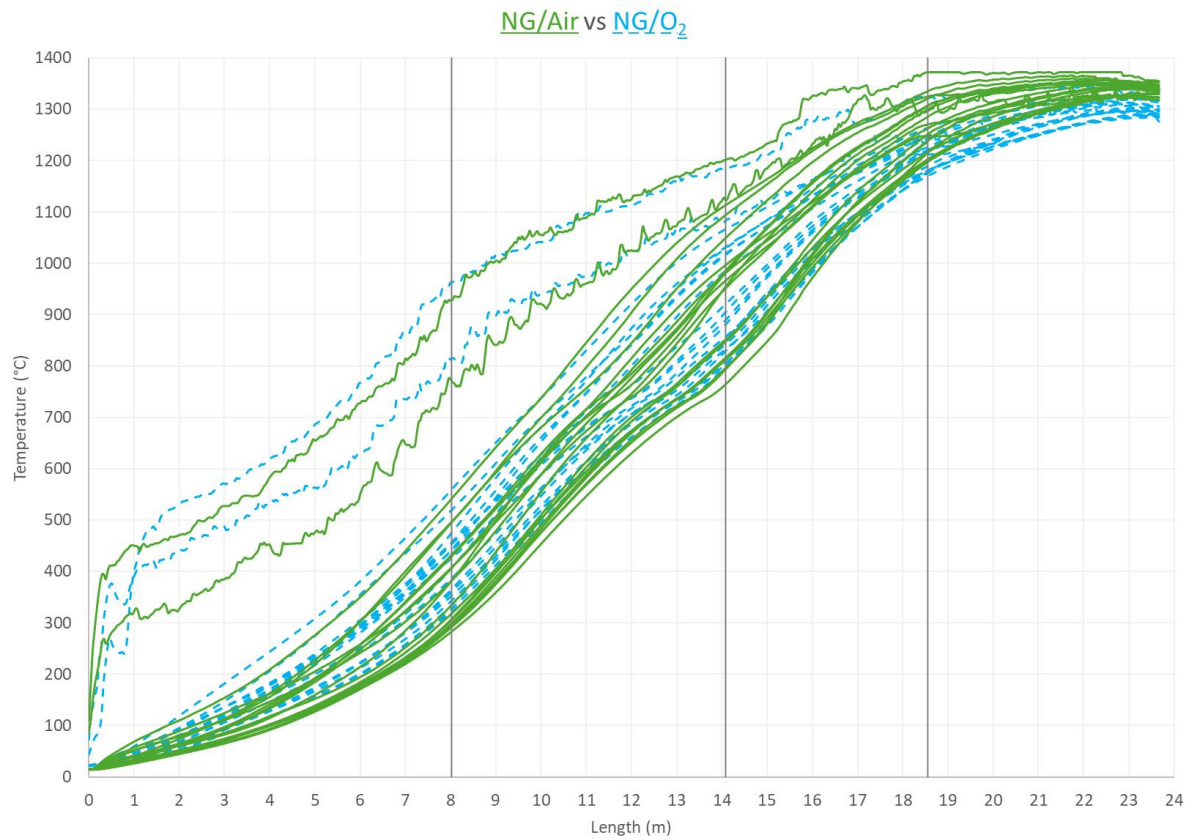


Figure 44. Heating curves of NG/air (green) vs. NG/O₂ (blue).

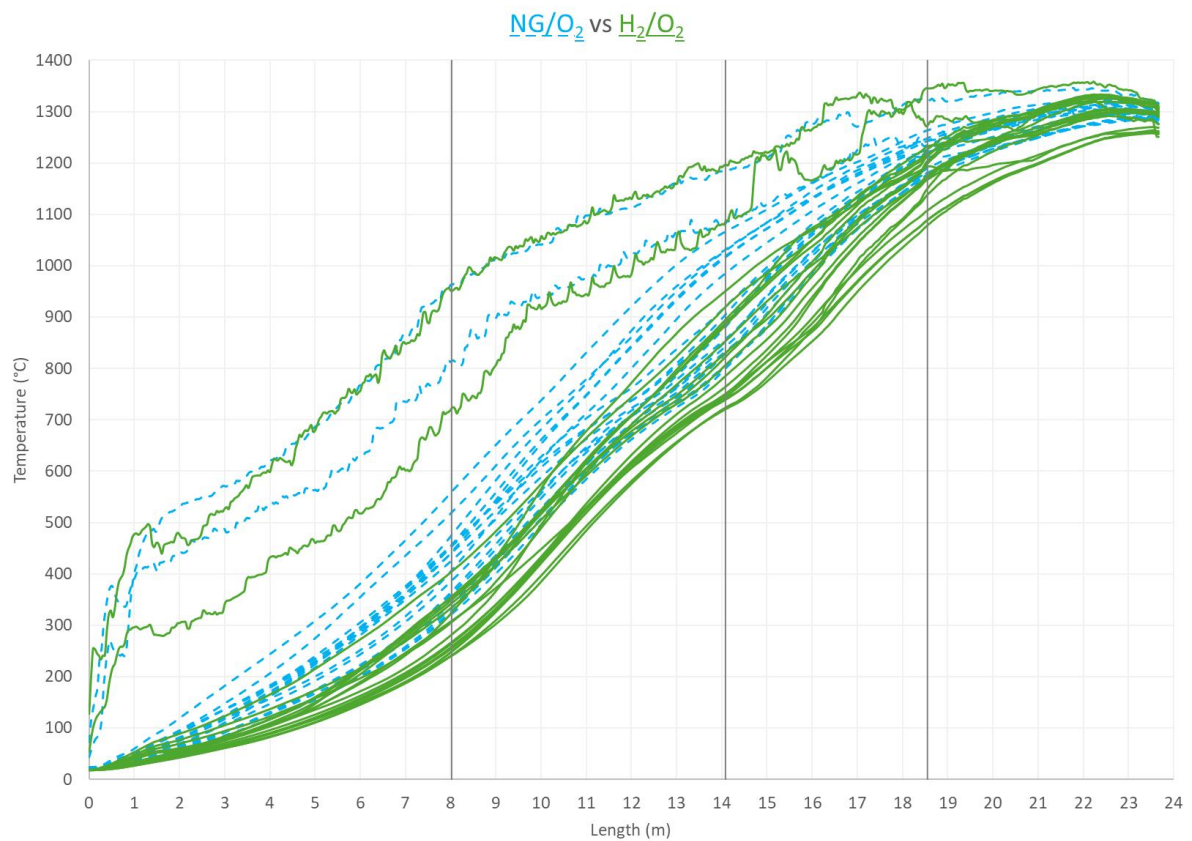


Figure 45. Heating curves of NG/O₂ (blue) vs. H₂/O₂ (green).

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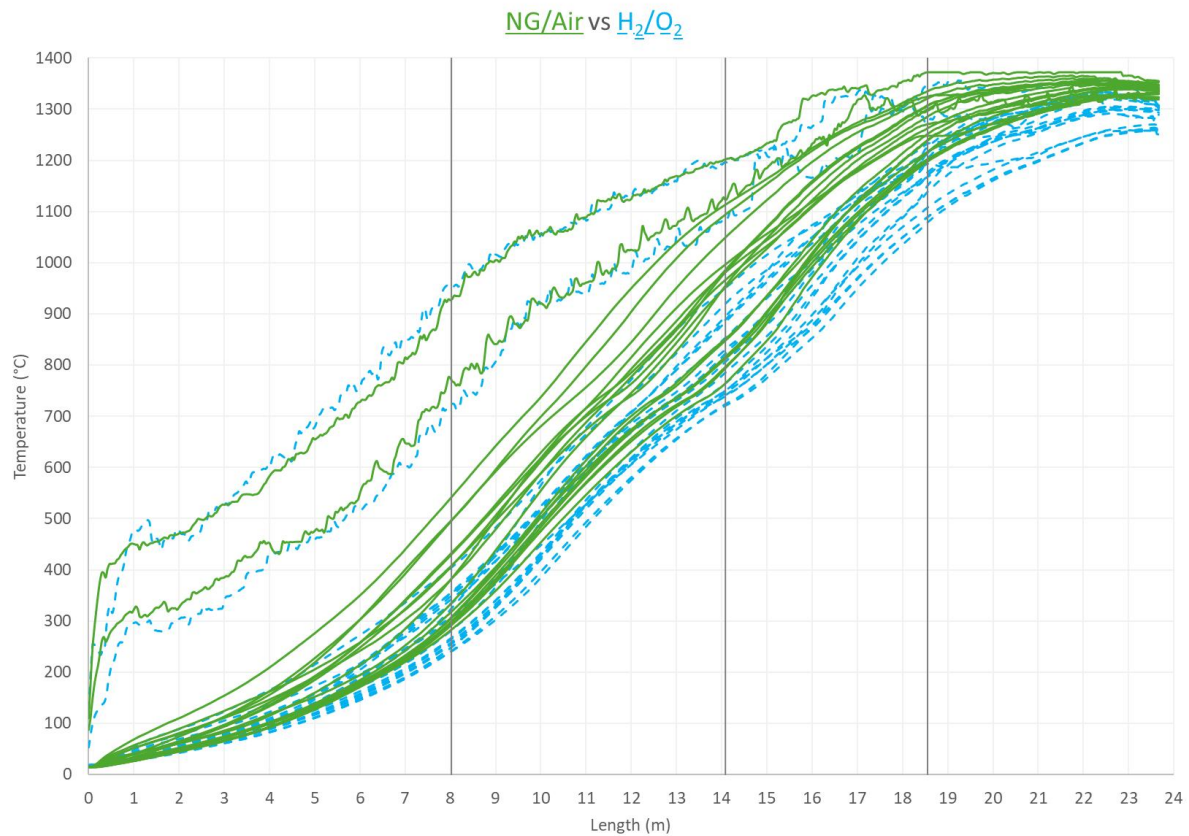


Figure 46. Heating curves of NG/Air (green) vs. H₂/O₂ (blue).

Comparing the NG tests of Figure 44, both have similar temperature profiles. However, at the end of the furnace, in the NG/air (green) test, the temperature of the entire BB2 was higher. If we look at the air curves, in both cases the upper thermocouple was the hotter one, and the lower thermocouple was the cooler one. When comparing the tests, there was less difference in the air-top curves than in the air-bottom curves. In the bottom area of the BB2, there was a difference in the behaviour of the flue gases, which means that in the NG/Air case they are cooler. The difference is greater in the initial zone of the furnace, which is where the flue gases from the entire furnace converge.

Comparing the oxy-combustion tests of Figure 45, in general terms the NG/O₂ test was hotter than the H₂/O₂ one. At the end of the furnace, both BB2s were at similar temperature. In this case, the air-top curves of both tests are quite similar, but comparing the air-bottom curves, the H₂/O₂ test is cooler than the NG/O₂ test. The difference is also greater in the initial zone of the furnace.

Comparing the third combination of Figure 46, the NG/Air test was hotter but less homogeneous than the H₂/O₂ test. This trend is also remarkable at the end of the furnace. Regarding the air curves, the comparison gives different results from the previous cases. Both, air-top and air-bottom curves between tests are similar.

These results can also be observed by analysing the temperatures at the furnace exit. Table 16 presents the temperatures of the BB2 during the last minute inside the furnace.

Table 16. Temperatures of the BB2 at the last minute inside the furnace.

	NG/Air	NG/O ₂	H ₂ /O ₂
Average temperature	1337.1	1300.1	1292.3
Maximum temperature	1352.6	1318.3	1317.0
Minimum temperature	1322.7	1284.0	1257.7
Maximum ΔT	29.9	34.3	59.3

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The average temperature considers the points measured in the BB2 (six per section). The maximum temperature was recorded at point 25sup_section1 for both NG tests, and at point 25sup_section2 for the H₂/O₂ test. The minimum temperature was always recorded at point 25inf_section1. The maximum ΔT indicates the temperature dispersion along the BB2.

According to the results shown in Figure 44, Figure 45 and Figure 46, it can also be observed in Table 16 that in the NG/air test the temperatures were the highest, and also, the BB2 was more homogeneous in temperature (lowest ΔT). The H₂/O₂ test had the lowest temperatures, and the least homogeneity at the exit.

The differences between the three tests are due to several factors that depend on the specific circumstances of each production day. Table 17 summarises some of the process data influenced by the production programme in the steel plant.

Table 17. Process data for each test.

	NG/Air	NG/O ₂	H ₂ /O ₂
Test time	1h34'40"	1h25'50"	1h30'40"
Material	275JR	S275JR	S235
O ₂ enrichment (Z1-Z4)	6%	6%	6%
Discharging rate (seg/BB)	120	110	110
Production (t/h)	177	201	195

In addition to the temperature profiles, gas consumptions have been analysed. The total gas consumptions for the entire furnace during the tests have been calculated and are presented in Table 18.

Table 18. Gas consumptions of the entire furnace during the tests.

Consumptions (m ³)	NG/Air	NG/O ₂	H ₂ /O ₂
NG	8 644	8 840	7 854
H ₂	-	-	1 348
Air	71 465	68 497	66 880
O ₂	3 793	4 922	4 498

The study of the consumptions alone is not enough to compare tests. For this reason, several calculations have been performed to obtain more normalised data. Table 19 presents the energy consumption in several forms.

Table 19. Energy calculations for the tests.

Calculations	NG/Air	NG/O ₂	H ₂ /O ₂
Energy (MW)	NG	57.0	64.2
	H ₂	-	-
Consumption per weight (m ³ /t)	NG	30.9	30.7
	H ₂	-	-
Total energy (MW)	57.0	64.2	56.8
Total energy per weight (kWh/t)	321.6	319.4	284.5

The concepts in Table 19 that are useful for comparison are the “consumption per weight” and the “total energy per weight”. Both figures have been calculated according to **Fehler! Verweisquelle konnte nicht gefunden werden.** and Eq. 2, where the test time and the production have been considered.

$$\text{Consumption per weight} \left(\frac{\text{m}^3}{\text{t}} \right) = \frac{\text{Consumption (m}^3\text{)}}{\text{Production} \left(\frac{\text{t}}{\text{h}} \right) \cdot \text{time (h)}} \quad \text{Eq. 1}$$

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$$\text{Energy per weight} \left(\frac{kWh}{t} \right) = \frac{\text{Consumption} (m^3)}{\text{time} (h)} \cdot \frac{LHV \left(\frac{kWh}{m^3} \right)}{\text{Production} \left(\frac{t}{h} \right)} \quad \text{Eq. 2}$$

Regarding “consumption per weight”, only the NG tests can be compared. The savings of the NG/O₂ test with respect to the NG/air test are 0.6%. According to the “total energy per weight”, the savings, depending on the reference, are those presented in Table 20.

Table 20. Comparison between tests.

Calculations	NG/Air	NG/O ₂	H ₂ /O ₂
Consumption per weight (m ³ /t)	Ref.	0.6%	-
Total energy per weight (kWh/t)	Ref.	0.7%	11.5%
	-	Ref	10.9%

The savings of the NG/O₂ test with respect to the NG/air test are very low, around 0.6-0.7%. The savings of the H₂/O₂ test with respect to the NG/O₂ test show better values, around 11-11.5%. This comparison has been obtained under furnace conditions depending on the specific circumstances of each day, causing the furnace to operate under different stability conditions.

The analysis presented in this report up until now has been conducted on the entire furnace. However, the mentioned three different fuel (natural gas and hydrogen) and oxidising agent (air and oxygen) combinations have been applied to the two modified new burners. One of the new burners is in the zone 7 and the other new burner in the zone 8, both in the soaking zone, one in front of the other. In each zone (zone 7 and zone 8), there are two burners, and the new ones are the burner number two (the burners closer to the furnace exit).

To conduct a more localised study, the consumptions of these two burners have been calculated and presented in Table 21.

Table 21. Gas consumptions of the two new burners during the tests.

Consumptions (m ³)		NG/Air	NG/O ₂	H ₂ /O ₂
Z7_B2	NG	317.5	223.9	-
	H ₂	-	-	674.0
	Air	3 141.2	9.1	89.3
	O ₂	0.02	470.0	326.3
Z8_B2	NG	288.4	229.1	-
	H ₂	-	-	673.7
	Air	2 853.8	231.6	947.2
	O ₂	0.19	481.3	332.4

The most notable data in Table 21 is the high value of consumption of air in the oxy-combustion tests. It appears that the Z8_B2 burner has an uncontrolled combustion air inlet. The possibility of an incorrect reading by the flow meter has also been considered. This is currently being investigated.

In summary, the three tests were carried out successfully, and the two new burners work correctly. The oxy-combustion tests showed fuel savings. However, more realistic conclusions can only be drawn once process data covering a longer period is collected. The collection of more detailed data is currently being planned.

4.2 CFD simulations of the AMOB's walking beam furnace with H₂/O₂ combustion

In this task TEC performed CFD simulations of the AMOB's walking beam furnace. The objective of these simulations was to analyse the effect of the new hydrogen and natural gas OFC (oxyfuel combustion) burners on the behaviour of the furnace. The new burners were installed at the bottom zone, at the end of the soaking zone of the furnace.

The results obtained in the previously explained temperature monitoring tests were used as input data for the CFD model. Simulations were therefore carried out for the three combustion conditions (NG/air, NG/O₂ and H₂/O₂).

4.2.1 Model set-up

In this work, different ANSYS tools, including SpaceClaim, FluentMeshing and FLuent (version 2022R1) were used.

Based on installation drawings and the information provided by AMOB, a 3D model of the walking beam furnace was created (Figure 47). The furnace has a total length of 25.83 m and includes 61 burners. The zone and burner distribution were described in Deliverable D6.1.

The model includes a total of 45 beam blanks (BB2), each with a 13.52 m length, with a spacing of 515 mm between them. A polyhedral mesh with 4,658,825 cells was used for the fluid domain.

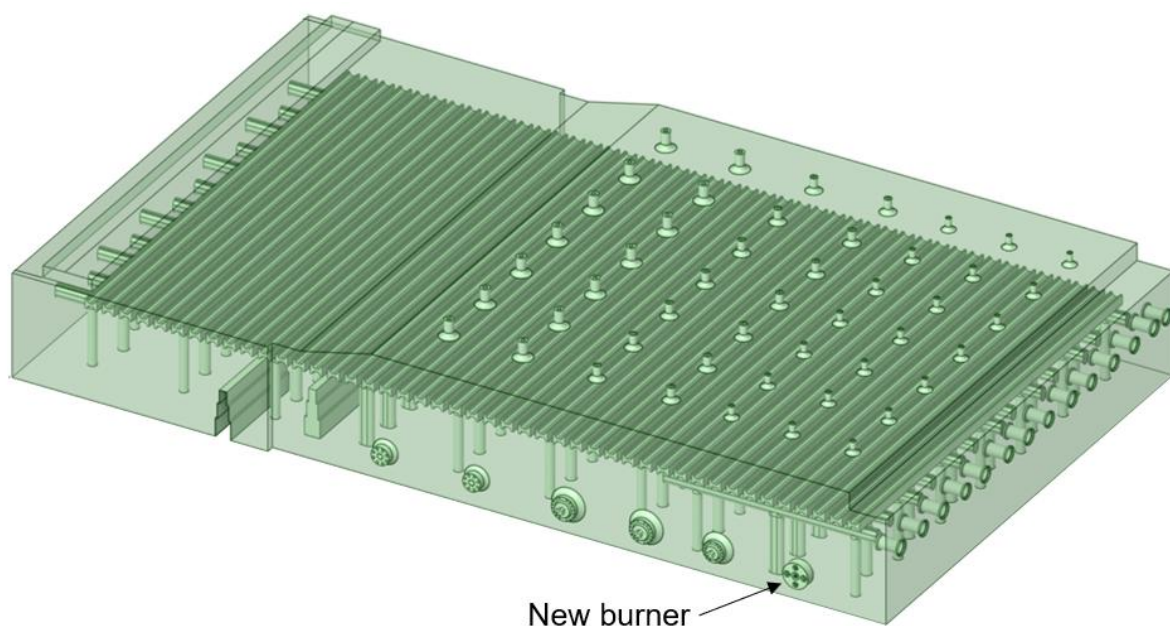


Figure 47. 3D model of the AMOB's walking beam furnace.

Since the computational cost of simulating the movement of the BBs was too high, steady-state simulations were performed. The following models were used: Energy, SST k- ω turbulence, Radiation (DO), and Species Transport with Reactions.

For the furnace walls, different layer configurations and thicknesses were defined depending on the zone. As the discharge rollers, walking beams and skid rails are water-cooled, the heat absorbed by the water was calculated using measured flow rates and temperature variations from the tests. The resulting heat flux was imposed as a boundary condition.

The temperature of the BBs was not solved directly in the model. Instead, the average temperature curve obtained from the monitoring tests was used. A trendline of the experimental data was calculated and implemented as an input. Figure 48 shows an example for the H₂/O₂ case.

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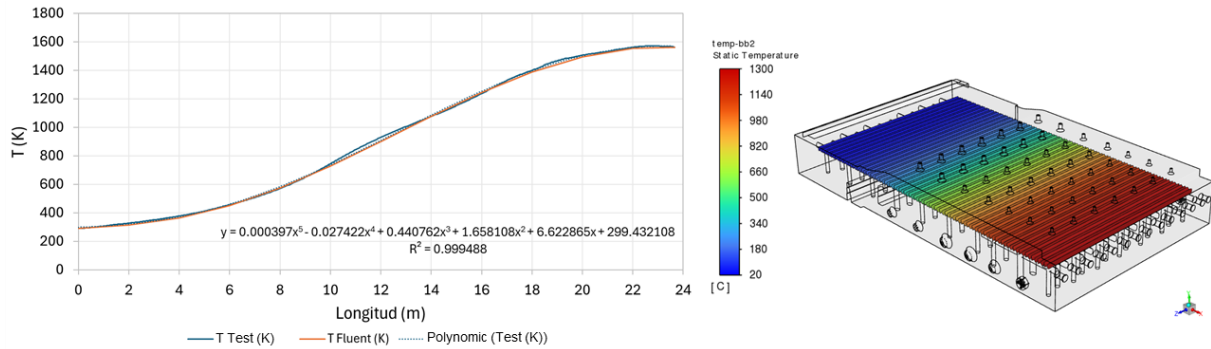


Figure 48. Set-up of the BB temperature for the H_2/O_2 case.

The species considered were O_2 , H_2 and NG. For NG, the composition was estimated based on the density and the lower heating value (LHV) measured during the tests. Average flow rates from each test were used. Using these values and LHV, the input power of each test was calculated as follows: 65.21 MW for the NG/air trial, 64.96 MW for the NG/ O_2 trial, and 63.49 MW for the H_2/O_2 trial. Inlet temperatures were set to 20 °C for H_2 , NG and O_2 , and of 300 °C for preheated air.

4.2.2 CFD results

Velocity

Figure 49, Figure 50 and Figure 51 show flue gas velocity profiles under NG/air, NG/ O_2 and H_2/O_2 combustion conditions, respectively. The profiles include a central vertical plane, vertical planes along the axes of both old and new burners in the soaking zone, and a horizontal plane through the lower burners and outlet duct. For visualisation purposes, velocity values were capped at 10 m/s.

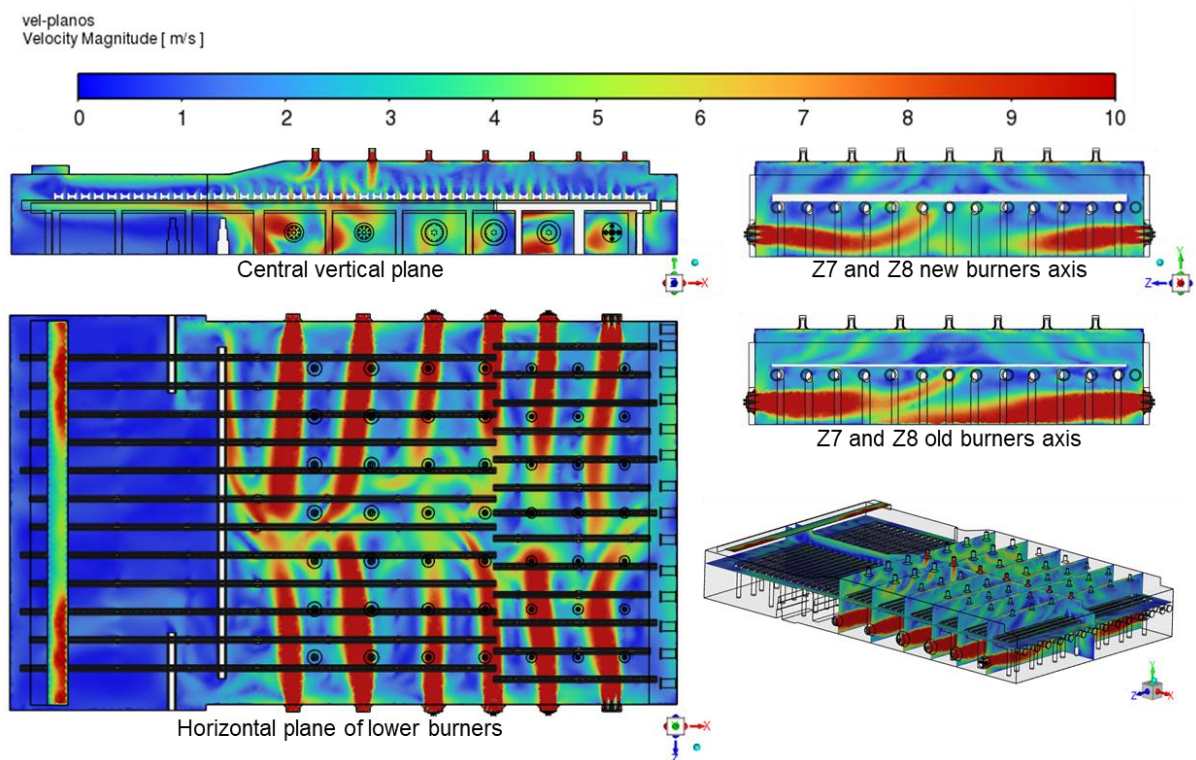


Figure 49. NG/air case velocity profiles.

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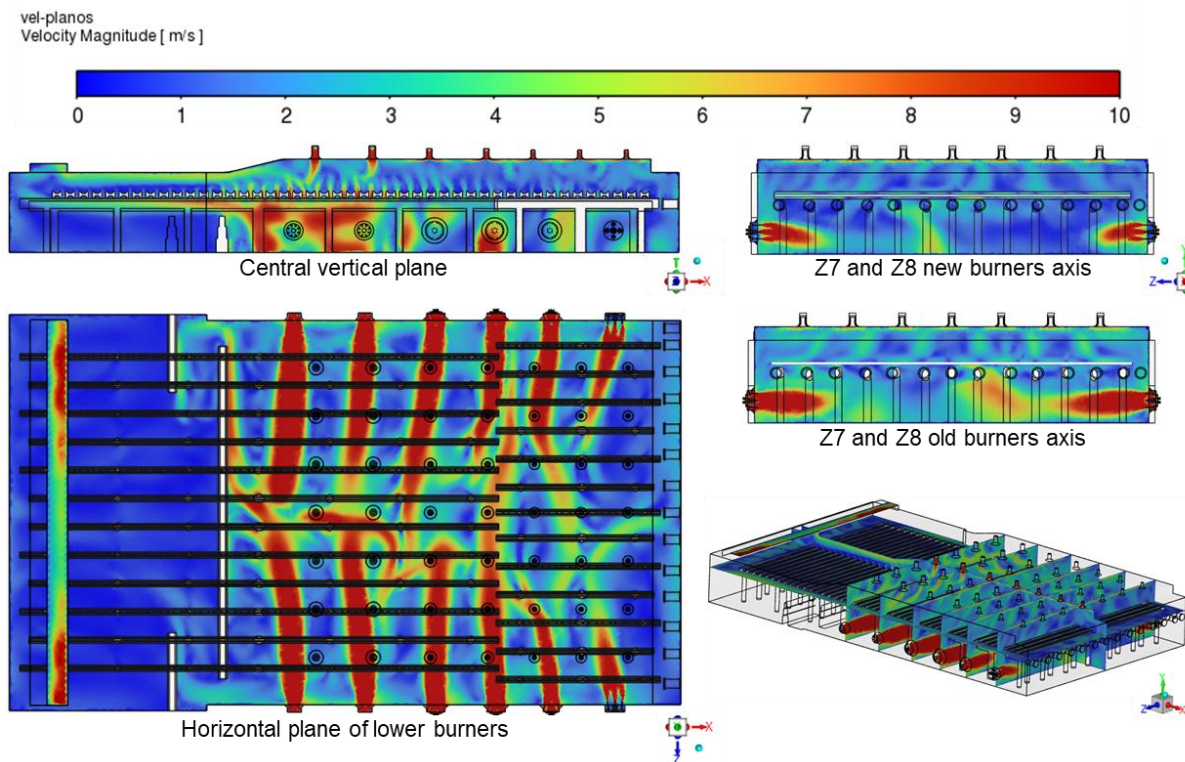


Figure 50. NG/O₂ case velocity profiles.

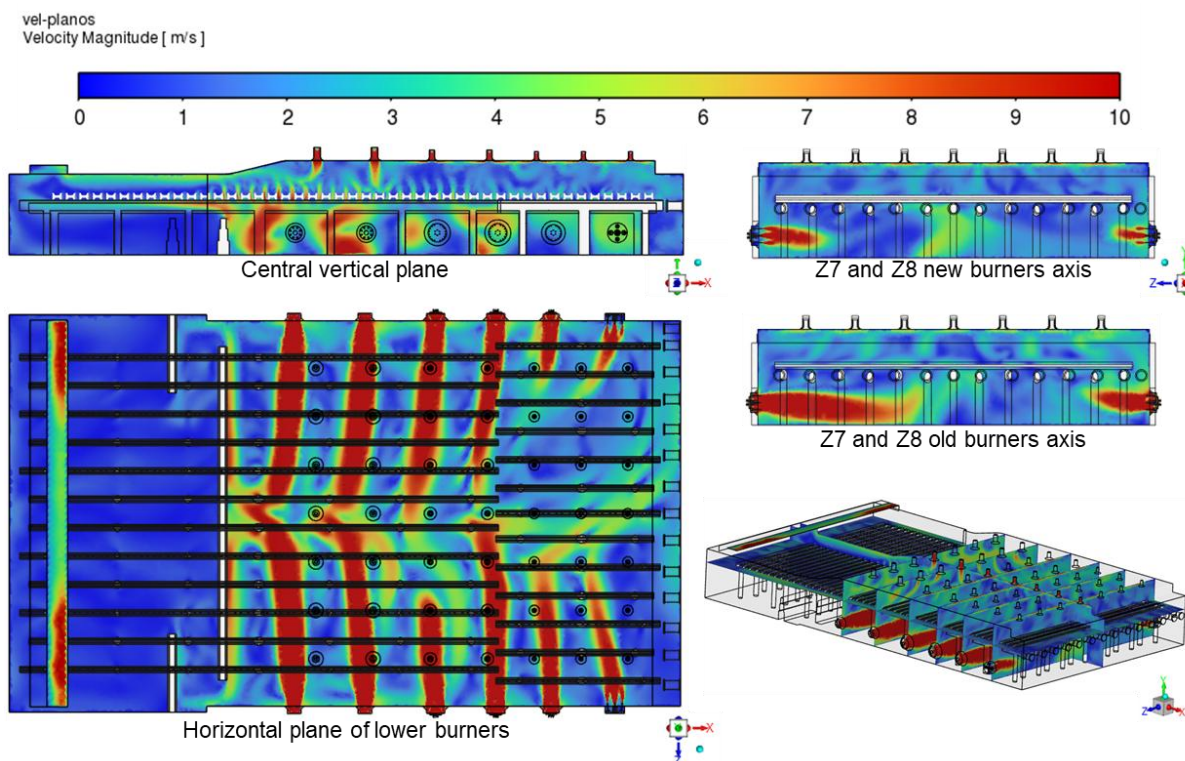


Figure 51. H₂/O₂ case velocity profiles.

Overall, furnace behaviour was similar across the three cases. However, analysing the behaviour in the new burners, in the NG/air case, velocities above 10 m/s penetrated further into the furnace, while in the OFC cases the highest velocities only extend around 2 metres into the furnace from the wall.

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To better compare the cases, velocity profiles along the axis of the new burners were analysed (Figure 52). Except at the furnace centre, velocities were higher in the NG/air case. The maximum velocity occurs near the wall in all cases. This value is higher in the NG/air condition due to the presence of a central air inlet, which is only used in that case. Between the NG/O₂ and H₂/O₂ trials the maximum velocity value is higher in the H₂ case, due to the higher flowrates.

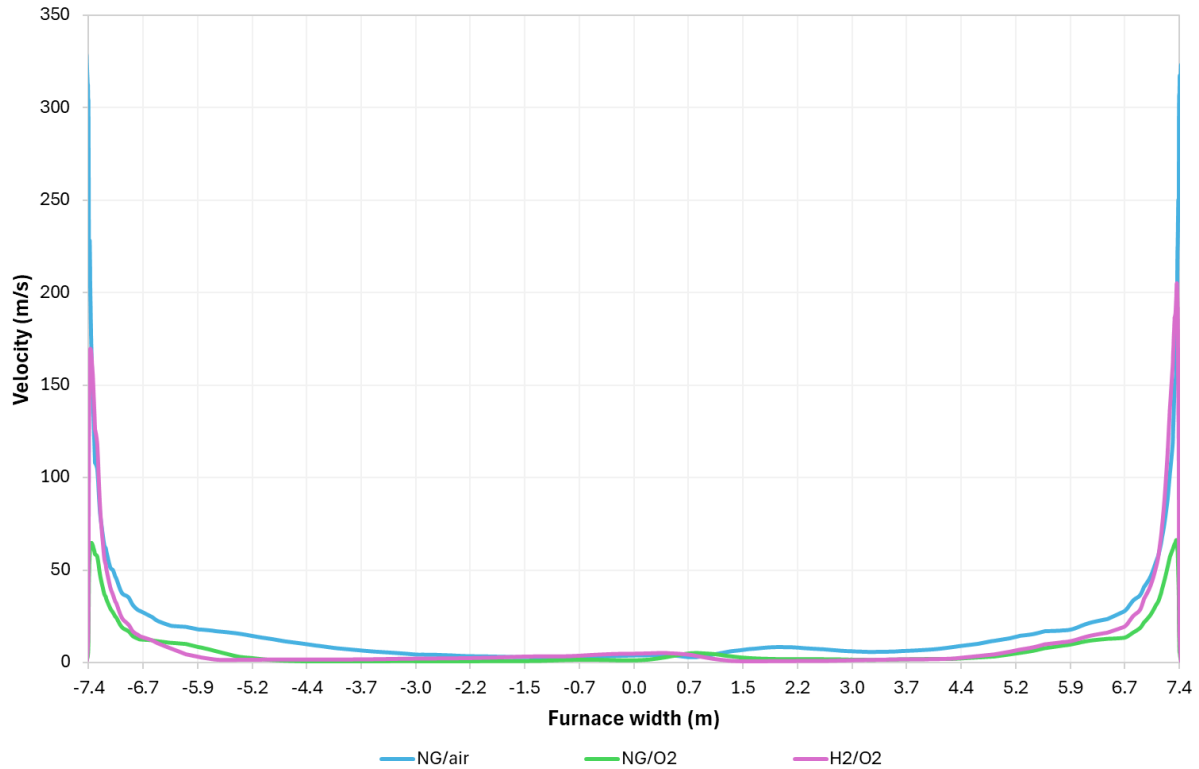


Figure 52. Comparison of the velocity in the axis of the new burners for each combustion condition.

Regarding the average outlet duct velocity, the values were similar in the three trials: 5.92 m/s for NG/air, 5.67 m/s for NG/O₂ and 5.64 m/s for H₂/O₂.

Temperature

Figure 53, Figure 54 and Figure 55 show temperature profiles under NG/air, NG/O₂ and H₂/O₂ combustion conditions, respectively. The profiles include a central vertical plane, vertical planes along the axes of both old and new burners in the soaking zone, and a horizontal plane through the lower burners and outlet duct. For visualization purposes the temperature range displayed was 50 °C to 1800 °C.

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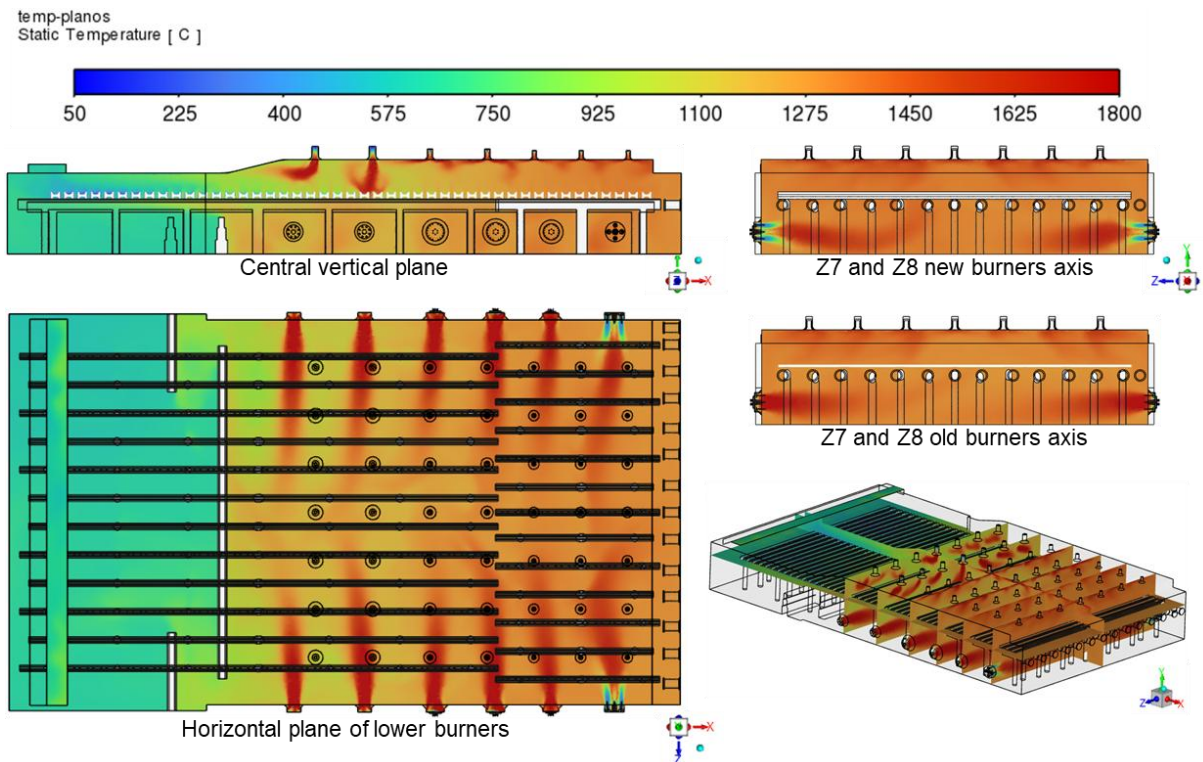


Figure 53. NG/air case temperature profiles.

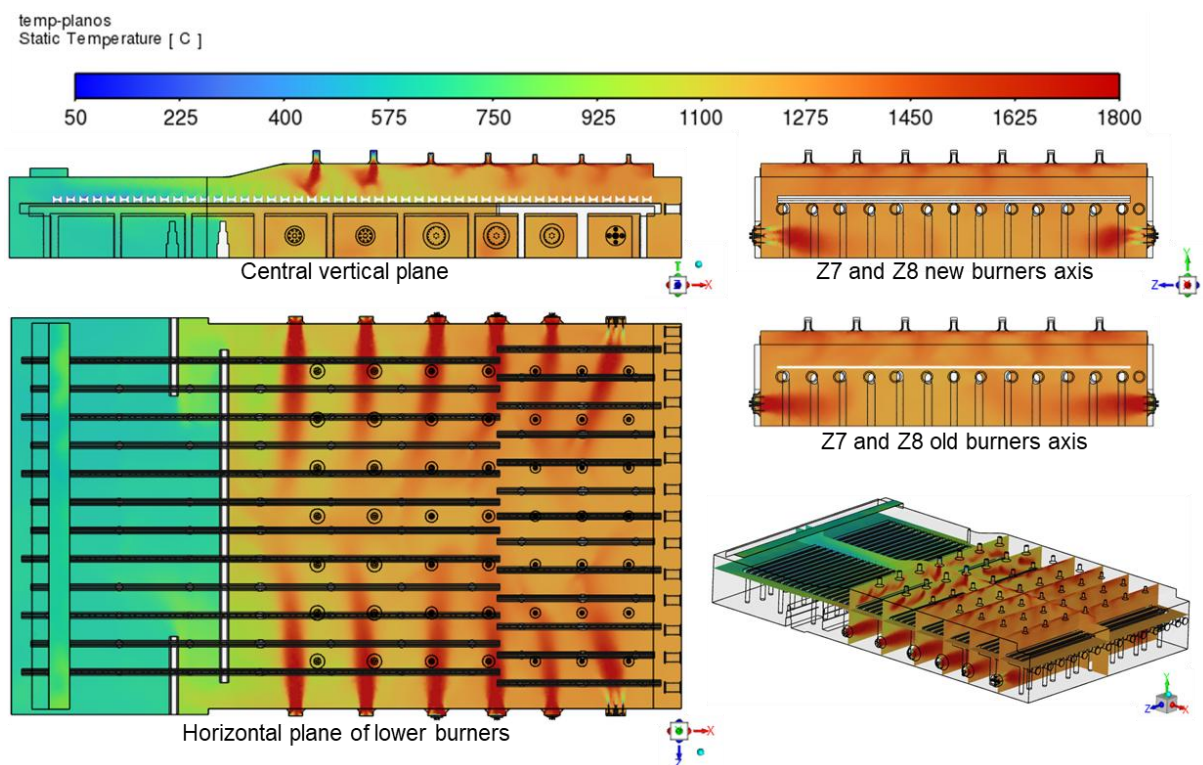


Figure 54. NG/O₂ case temperature profile.

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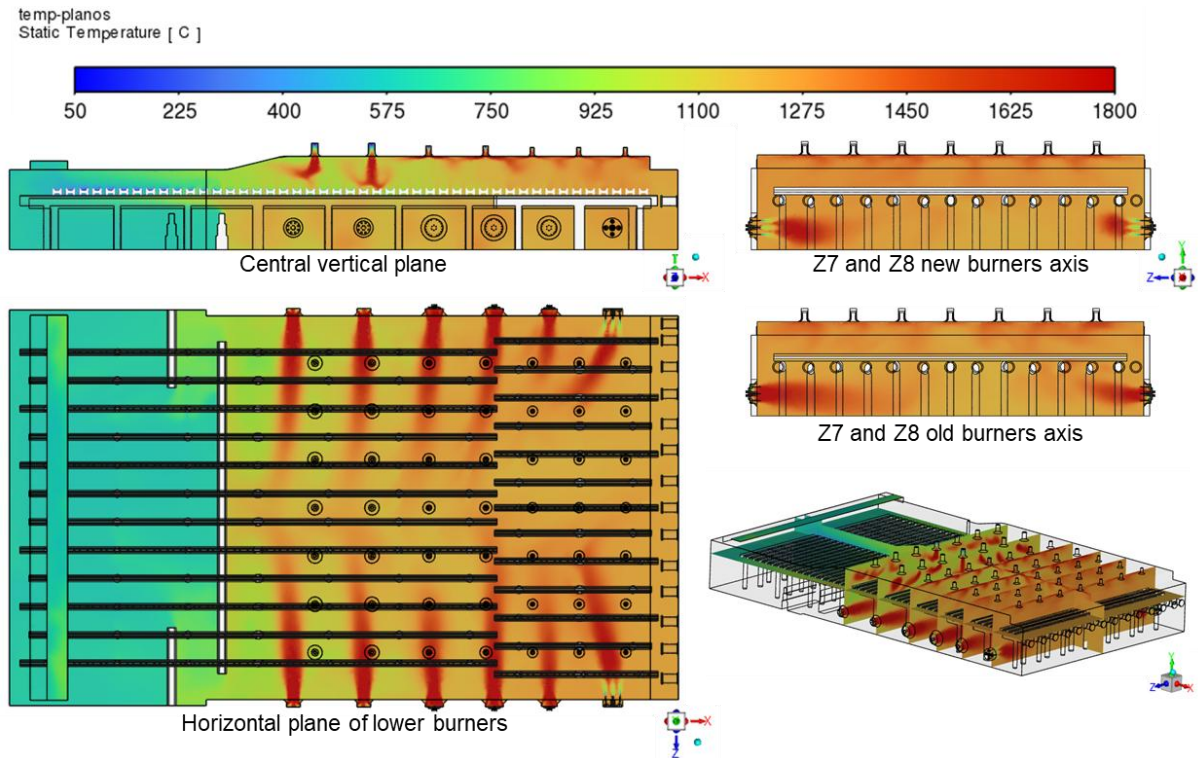


Figure 55. H_2/O_2 case temperature profile.

In general, the behaviour of the furnace was similar for the three combustion cases, with no significant differences observed between the combustion modes. Average furnace temperatures were 1091.98 °C for NG/air, 1083.73 °C for NG/ O_2 , and 1050.77 °C for H_2/O_2 . These differences are likely due to variations in total input power rather than combustion type.

To better analyse the temperature differences between the three trials, Figure 56 shows a horizontal plane comparison of the zones of the new burners. In this case, the temperature ranges from 1500 °C to 1800 °C.

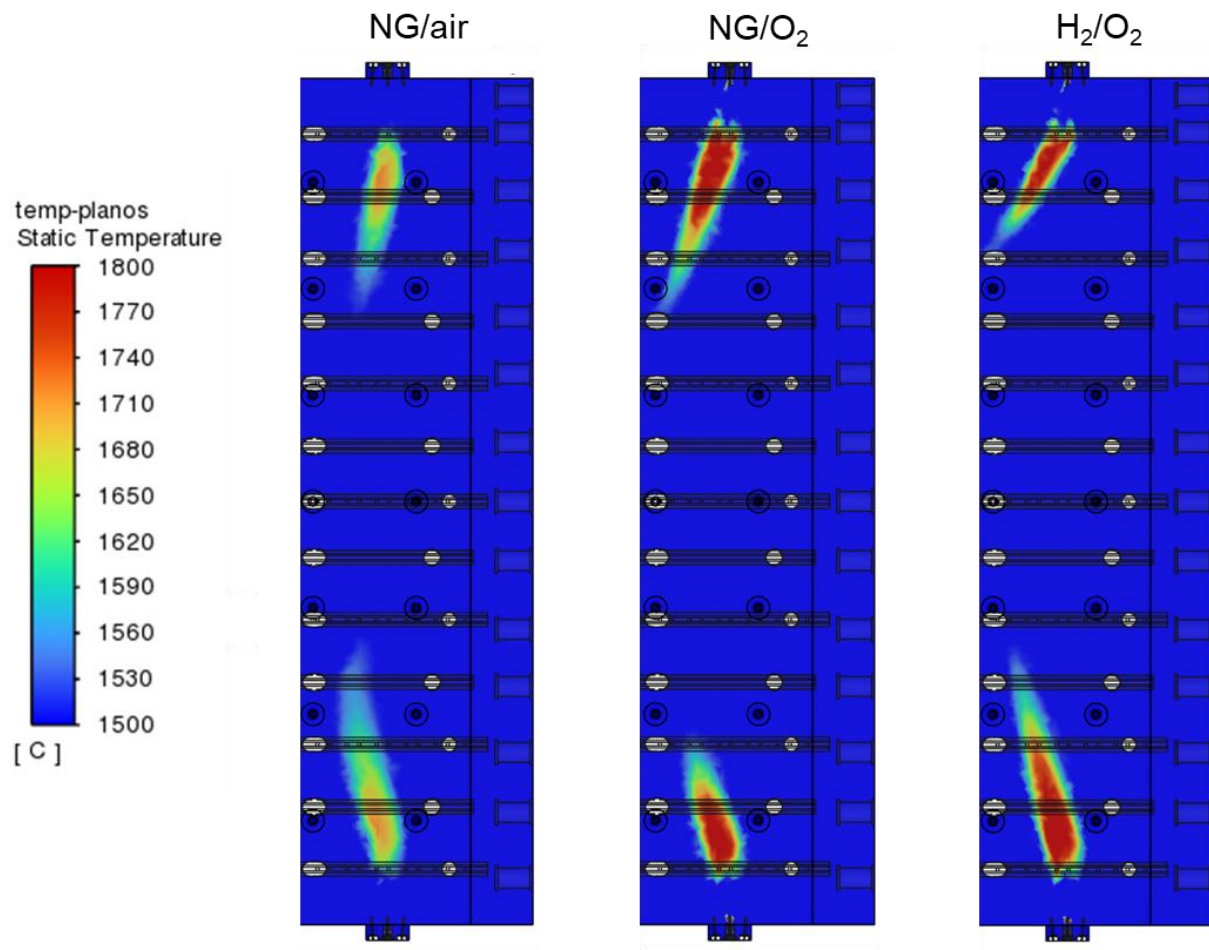


Figure 56. The new burners horizontal plane temperature profiles for each combustion condition.

Although the temperature distribution shape is similar, it can be seen how the OFC flames are hotter than de air combustion flame. Thus, the maximum temperature was higher in the OFC cases than in the NG/air case; specifically, the maximum values were 2453.12 °C for NG/air, 3641.05 °C for NG/O₂, and 3876.92 °C for H₂/O₂.

To better compare the temperature in the three combustion cases, temperature profiles were plotted along the axis of the new burners within the width of the furnace (Figure 57). It should be noted that the flame from the new burners is not straight, which is why the maximum temperature is not recorded at the burner axis. The two OFC profiles are quite similar, with a clear maximum peak around 1.5 metres from the wall. In contrast, the NG/air profile is smoother, with a smaller temperature difference between the maximum and the centre of the furnace.

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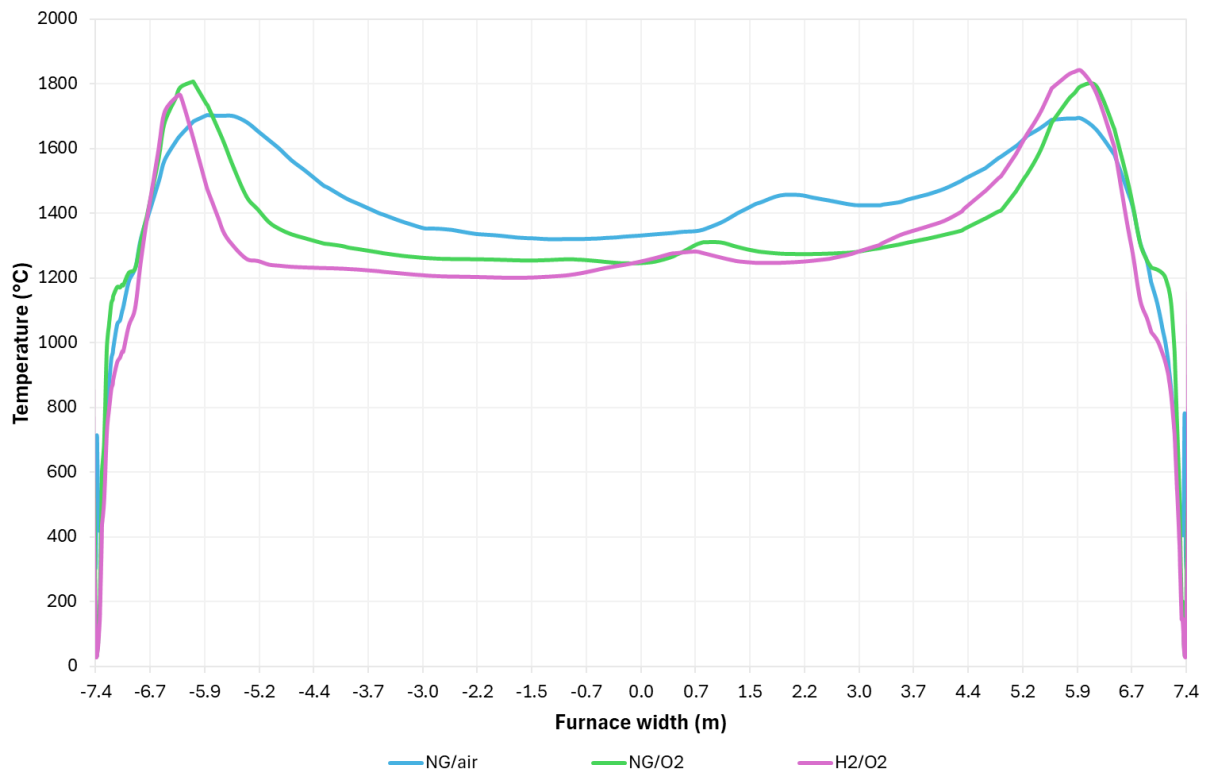


Figure 57. Comparison of the temperature in the axis of the new burners for each combustion condition.

Regarding the average temperature at the outlet duct, the values were similar in the three conditions: 691.87 °C for NG/air, 688.10 °C for NG/O₂ and 669.19 °C for H₂/O₂. The small differences are related to the total power and, consequently, to the average temperature inside the furnace.

Combustion species

Table 22 shows the mole fractions of the major species. These values represent the average of the total volume of the furnace. The H₂O values are somewhat higher for the OFC cases, and the CO₂ values are slightly lower in the case for H₂/O₂, but overall, the values obtained for the three cases are quite similar. It should be noted that only two of the furnace's out of 61 burners have been modified, so significant differences in the species were not expected.

Table 22. Furnace average mole fractions for each combustion case.

Mole fraction	NG/air	NG/O ₂	H ₂ /O ₂
CO ₂	0.1039	0.1174	0.0997
H ₂ O	0.2007	0.2265	0.2269
O ₂	0.0104	0.0066	0.0171
N ₂	0.6844	0.6484	0.6559
Others	0.0006	0.0011	0.0004

Validation of the model

To validate the model, Figure 58, Figure 59 and Figure 60 show a comparison between the temperature measured during the test using the thermocouple exposed to the furnace atmosphere above the BB and the temperature obtained in the simulations at the same position.

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In all three cases, the model fits quite well. The greatest difference occurs in the first few metres of the furnace. This is probably because the thermocouple entered the furnace at room temperature in the tests and had a reaction time, whereas in the CFD model the temperature was that of a steady-state condition. Conversely, the peaks visible around the 12-metre mark in the simulation profiles are due to the effect of the top burner flame at that precise point. During the tests, the thermocouple was in motion and did not remain stationary; therefore, it did not record that peak.

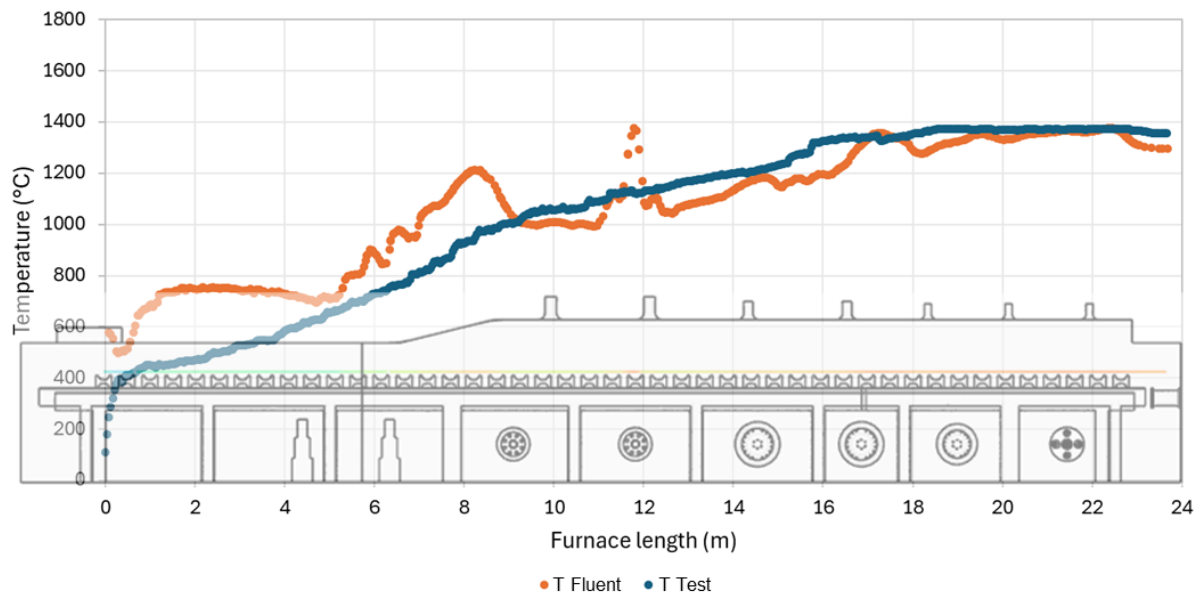


Figure 58. Comparison of the temperature recorded above the BB during the test and the obtained in the simulation for the NG/air case.

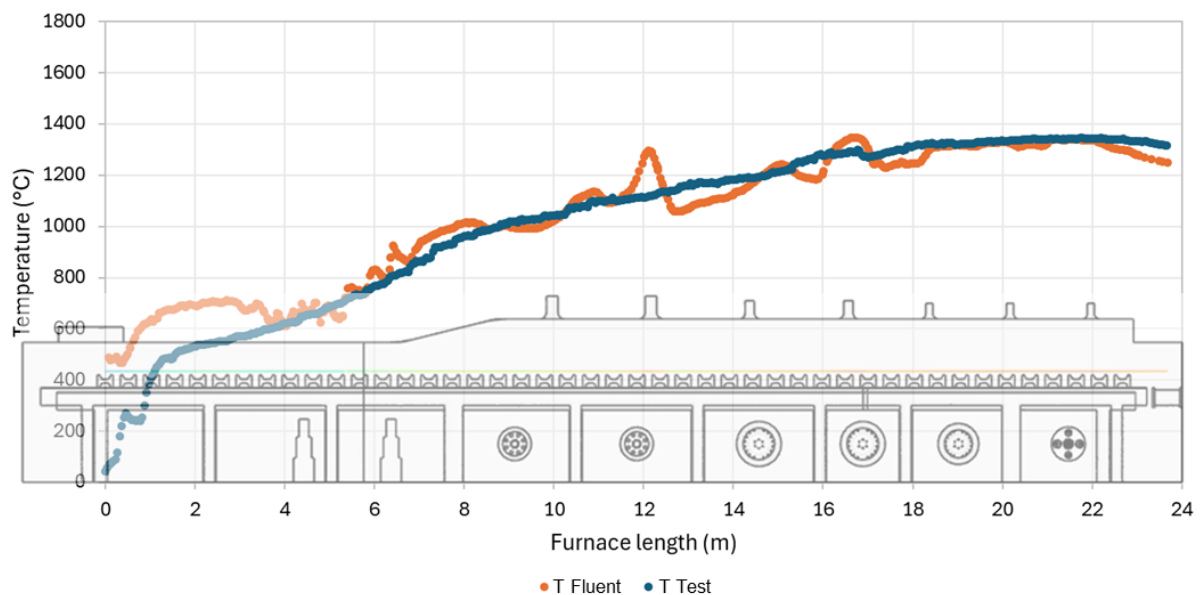


Figure 59. Comparison of the temperature recorded above the BB during the test and the obtained in the simulation for the NG/O₂ case.

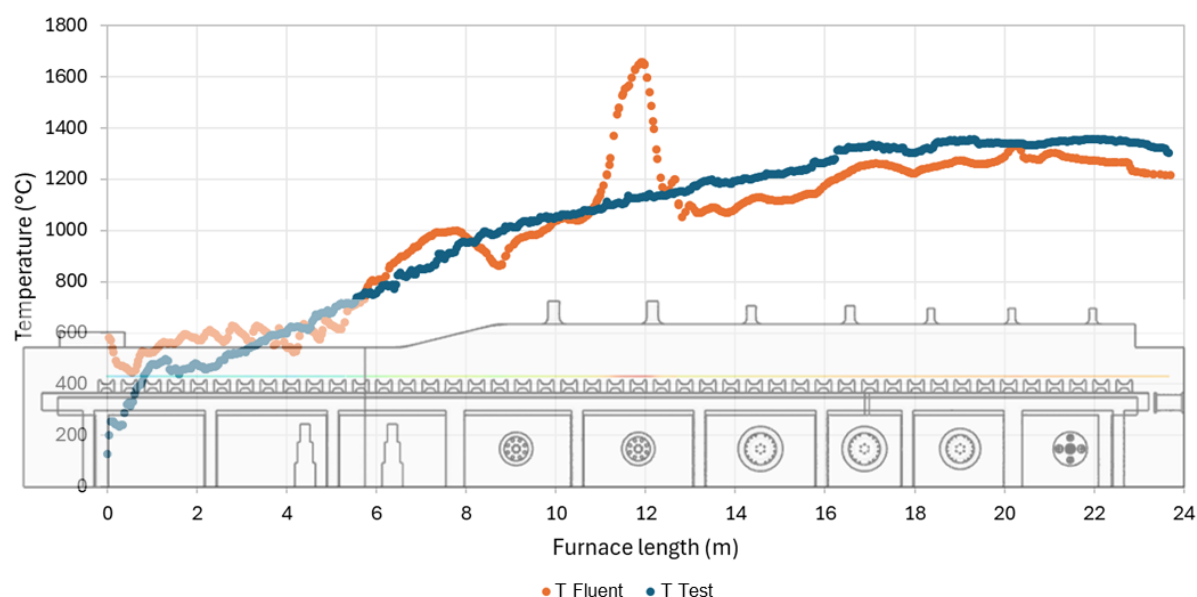


Figure 60. Comparison of the temperature recorded above the BB during the test and the obtained in the simulation for the H_2/O_2 case.

4.2.3 Summary and conclusions

The CFD model shows good agreement with the tests. The main conclusion is that the combustion type does not significantly affect overall furnace behaviour. Table 23 shows a summary of the main parameters obtained in the simulations for the three combustion cases.

Table 23. Summary of the main parameters obtained in the simulations.

	NG/air	NG/O ₂	H ₂ /O ₂
Power (MW)	65.21	64.96	63.49
T outlet duct (°C)	691.87	688.10	669.19
v outlet duct (m/s)	5.92	5.67	5.64
T furnace (°C)	1091.98	1083.73	1050.77
T max. furnace (°C)	2453.12	3641.05	3876.92
Mole fraction CO ₂	0.1039	0.1174	0.0997
Mole fraction H ₂ O	0.2007	0.2265	0.2269
Mole fraction O ₂	0.0104	0.0066	0.0171
Mole fraction N ₂	0.6844	0.6484	0.6559
Mole fraction others	0.0006	0.0011	0.0004

4.3 Characterization of the materials from the tests in SAR's combustion chamber located at the TEC facilities with material from AMOB

The main objective of the work carried out is to evaluate the quality of AMOB steel grades resulting from the implementation of H₂ combustion and oxy-combustion during the reheating process before hot rolling. To this end, different tests were conducted in SAR's combustion chamber located at the TEC facilities with material from AMOB. The tests were performed in the context of WP2 (Task 2.4.) in which TEC conducted thermal treatments in three different combustion conditions: NG/air, NG/O₂ and H₂/O₂. Duplicated tests were conducted for each atmosphere: one to quantify mass loss at TEC and another for subsequent microstructural analysis at CEIT.

The microstructural analysis carried out includes the following:

- Macroscopic scale morphology and adherence.
- Microstructural characterisation by OM and FEG-SEM, studying the following aspects:
 - On the oxidised surfaces: study of the morphology of the oxidised layer.
 - On transversal section: oxide and the interface characterization. Scale thickness. Oxide adhesion. Internal oxidation thickness. EDS compositional analyses have allowed: chemical mappings in sections covering the different scale/interface/matrix layers and point analysis in regions of interest including oxidised layers, internal oxidation and matrix.
 - Electron backscatter diffraction (EBSD) analyses have allowed the identification of the main constituent phases of the oxide, the crystallographic relationships and the main oxidation mechanism related to the applied atmospheres.

4.3.1 Material and experimental conditions

The studies have been carried out on structural S355JR steel supplied by AMOB. The material is supplied as 400 mm long BB1. Figure 61 shows the beam blanks after testing received for microstructural characterization at CEIT.

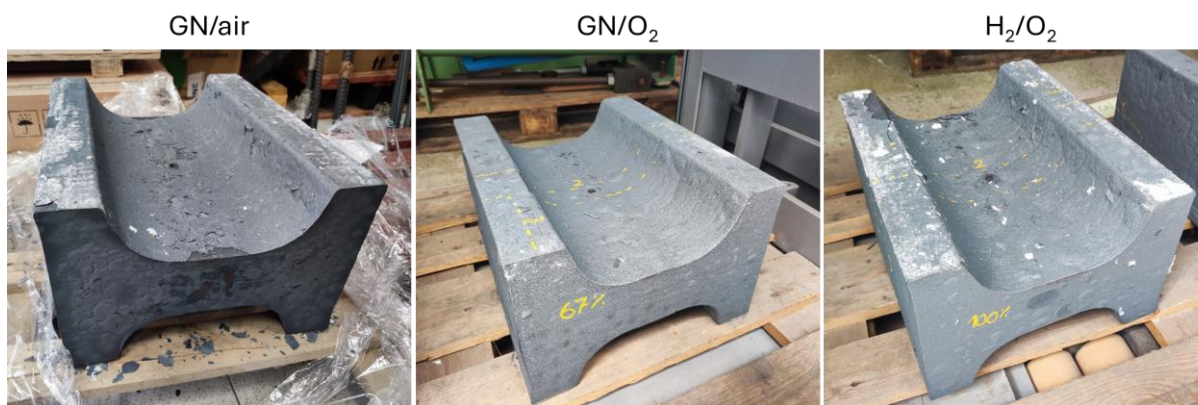


Figure 61. S355JR beam blanks 1 received for microstructural characterization after testing under conditions NG/air, NG/O₂ and H₂/O₂.

Characterisation was carried out at five different locations in the beam blanks: the web and flange, at various distances and heights from the burner as detailed in Figure 62. The positions T1-T4 were selected based on the temperature control carried out using thermocouples in the tests, as detailed in the Deliverable 2.5 of this project. In addition, position T5 was selected as an extra position in the region of the radius between the flange and the web.

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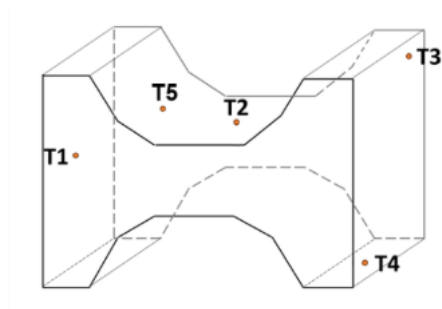


Figure 62. Diagram detailing the five positions where microstructural characterization was carried out.

The set of conditions and positions characterized in the context of this task is summarized in Table 24. The thermal cycle was designed to replicate the industrial thermal cycle applied to structural steel in the AMOB reheating furnace, which reaches 1200 °C in 70 minutes—the time required for the material to walk through the entire furnace. A detailed description and analysis of the thermal cycle is available in Deliverable 2.4 of this project.

Table 24. Summary of conditions and positions characterized.

Steel grade	Geometry	Combustion conditions	Position characterized				
S355JR	BB1	NG/air	T1	T2	T3	T4	T5
		NG/O ₂	T1	T2	T3	T4	T5
		H ₂ /O ₂	T1	T2	T3	T4	T5

4.3.2 Results and discussion

The characterization of the beam blanks was initiated in all cases by optical inspection and the acquisition of macro images of the regions of interest. Figure 63 shows some of the images taken at each of the positions. These images reveal that, at the macro level, the surfaces are characterized by heterogeneous oxide formations, where blisters and irregularities (striations) can be observed. In some cases, the oxide layer has peeled off. This is particularly evident in the NG/air condition. However, in NG/O₂ and H₂/O₂ conditions, the detached oxide seems thicker than in NG/air and, when spalling occurs, it does in the form of large flakes. Despite this, the oxide present in the beam blanks exhibits strong adhesion in general terms.

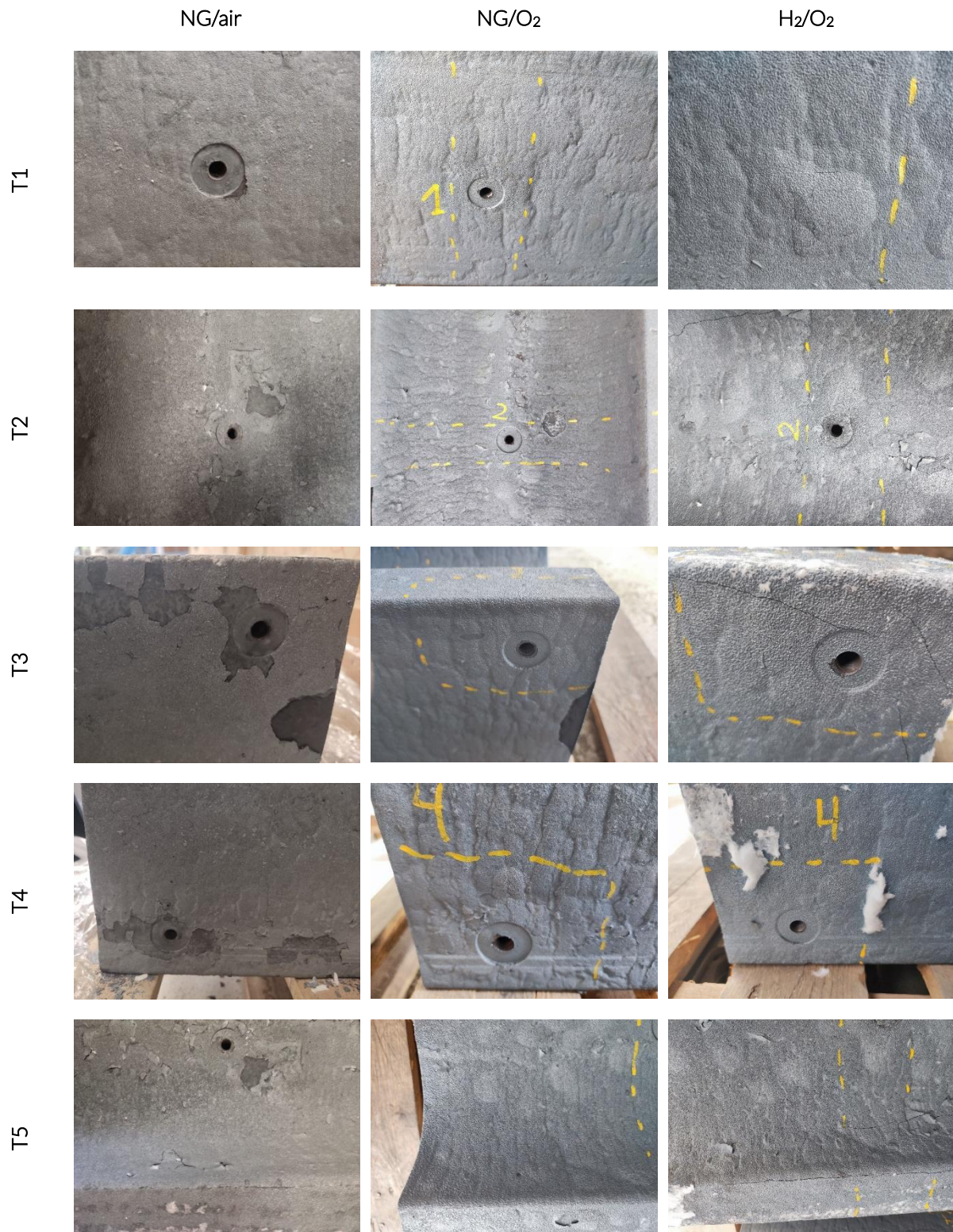


Figure 63. Macro images of defined positions for beam blanks tested under NG/air, NG/O₂ and H₂/O₂ conditions.

Detailed characterization of developed oxide in the different positions and conditions tested was then carried out by means of FEG-SEM. In this characterization, regions with partially detached oxide were examined when present, revealing that the scale tends to spall off while leaving different underlying sublayers exposed. These sublayers exhibit markedly different morphologies, as can be observed in the example shown in Figure 64.

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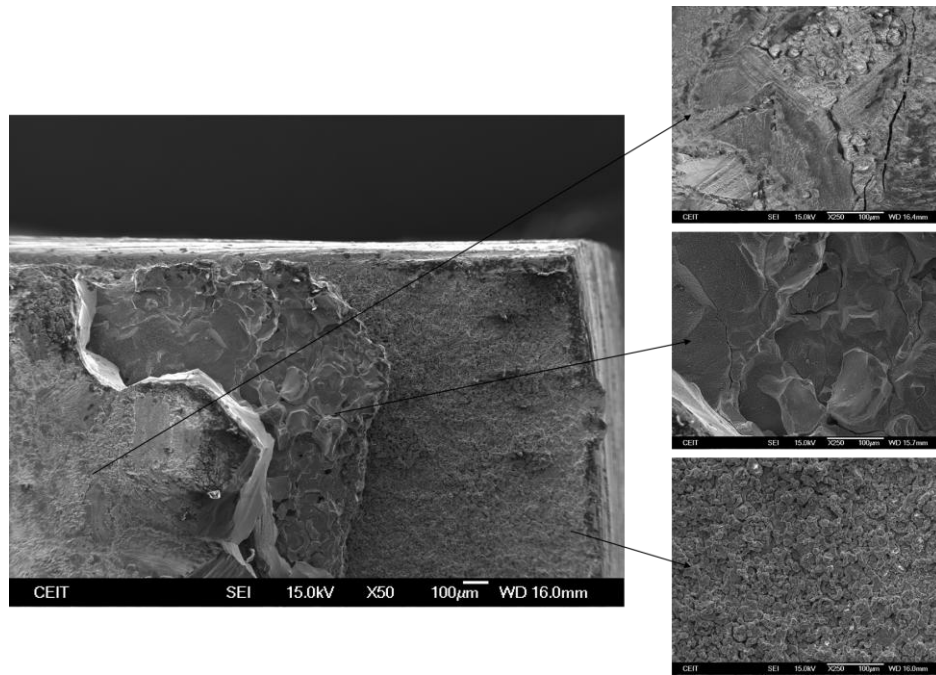
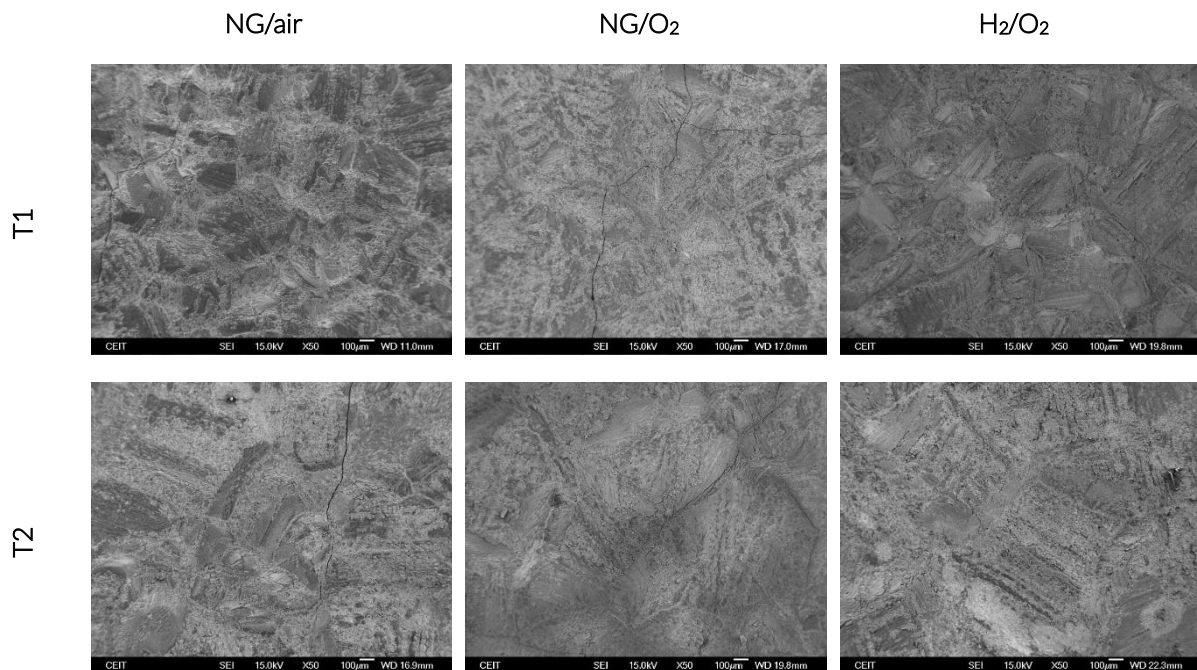


Figure 64. Example of FEG-SEM characterization of a spalled zone. Position T2, condition NG/air.

When the oxide is fully adherent, the characterization revealed that oxide is composed of coarse crystallographic units with a rough appearance. The morphology in all positions and heating conditions is similar; however, it has been found that grain size tends to be coarser at positions T2 and T5 than at the others. Moreover, it is also noteworthy that in general terms coarser grain sizes are obtained in oxy-combustion scenarios compared to NG/air, see Figure 65.



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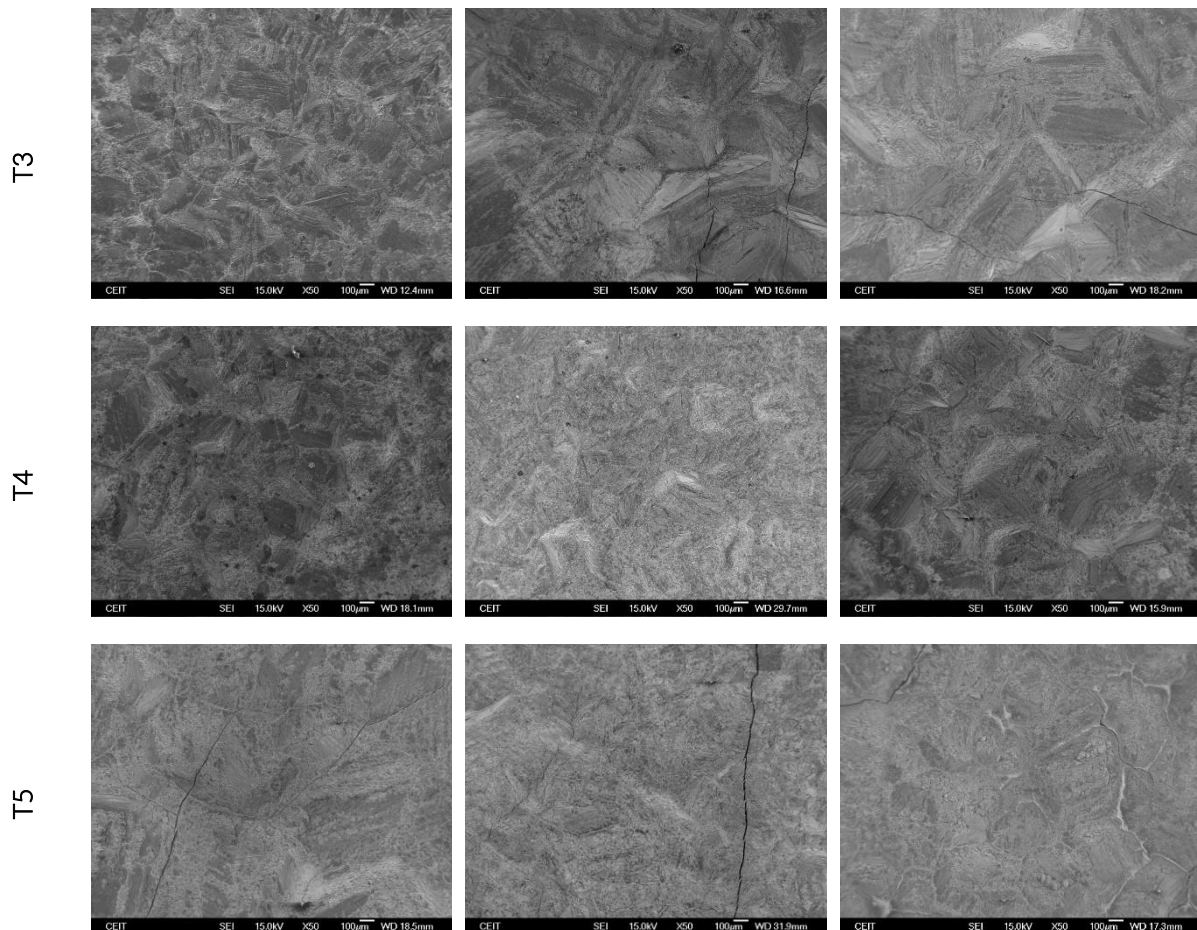


Figure 65. FEG-SEM images obtained from the oxide surface at defined positions for beam blanks tested under NG/air, NG/O₂ and H₂/O₂ conditions.

After surface characterization, the samples were sectioned, mounted, and prepared in cross-section following the metallographic grinding and polishing route using different water-based diamond suspensions. Subsequently, some samples were polished with a colloidal silica solution to facilitate phase revelation to perform electron backscatter diffraction (EBSD) measurements.

Figure 66 shows the cross-sectional images of the oxide layer for all the cases under study. The contrast in the FEG-SEM images obtained using backscattered electrons makes it possible to clearly differentiate the matrix (with brighter contrast) from the generated scale. Although the thickness of the developed oxide and local characteristics vary depending on the position and condition under analysis, the results obtained show the morphology of the oxide scale is similar in all cases. Thus, it is observed that the oxide is mainly composed of a predominant phase identified as wüstite. In addition, several defects can be observed within the oxide layers, which may have formed during oxidation, cooling, or sample preparation. These defects were identified as cracks or pores. Pores are characterized by approximately spherical morphologies, whereas cracks tend to be elongated. In some cases, depending on the size of the pore or crack, their identification is not straightforward. Cracks may form at any stage of the process for various reasons, such as gaps remaining between columnar grains during growth, thermal stresses generated during heating or cooling or as a result of sample handling itself. Most samples present a discontinuity along a large portion of the matrix/oxide interface, which is attributed to cooling, where the difference in thermal expansion coefficients between the oxide and the matrix causes separation of the two media. It is possible that this discontinuity became more pronounced during sample preparation. Pores, on the other hand, form exclusively during oxidation. As can be seen, there is a significant number of small pores in the region near the interface. Their number tends to decrease and their size to increase as we move toward the outer region, which seems to indicate that pore migration and coalescence occur during the growth of the oxide scale.

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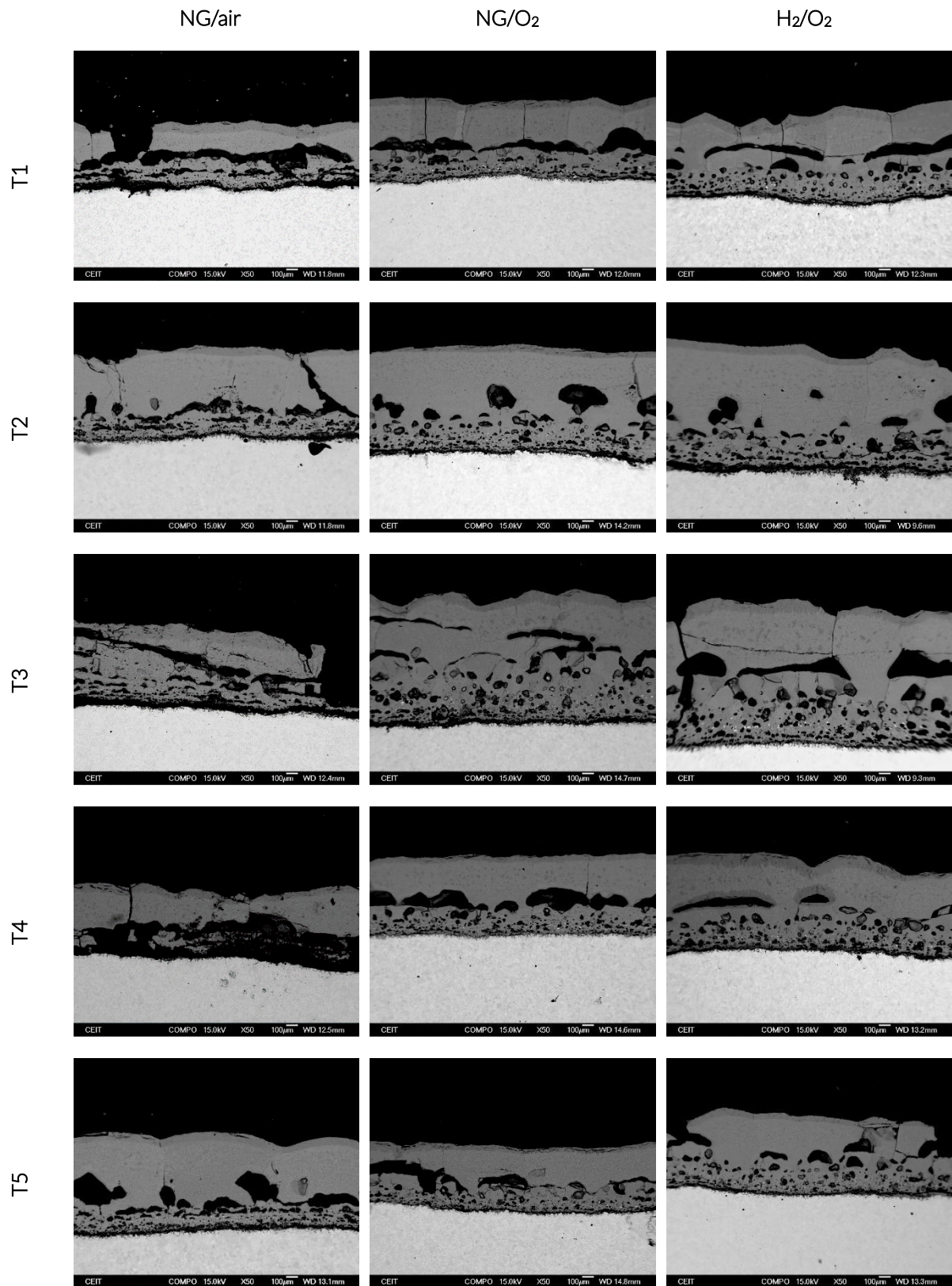


Figure 66. FEG-SEM images obtained from the cross-sections at defined positions for beam blanks tested under NG/air, NG/O₂ and H₂/O₂ conditions.

A more detailed characterization was carried out on the outer and inner regions of the oxide layers, as well as at the interface between the oxide and the matrix. These regions were considered of interest based on previous studies (see Deliverable 2.1 of this project). The characterization performed on the set of samples

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revealed that the same oxidation mechanism and phase formation take place independently on position or condition applied, therefore, the most significant results obtained from the characterization are presented below as representative examples.

The outer region of the samples is characterized by the presence of a magnetite layer, whose thickness varies depending on the position and condition under analysis. In all cases, this layer exhibits an irregular morphology at the interface with the wüstite. At the outermost part of the magnetite layer, localized hematite can occasionally be found. Likewise, the formation of magnetite in the form of islands within the large wüstite grains can also be observed. Figure 67 shows, as an example, a detailed image obtained from this region.

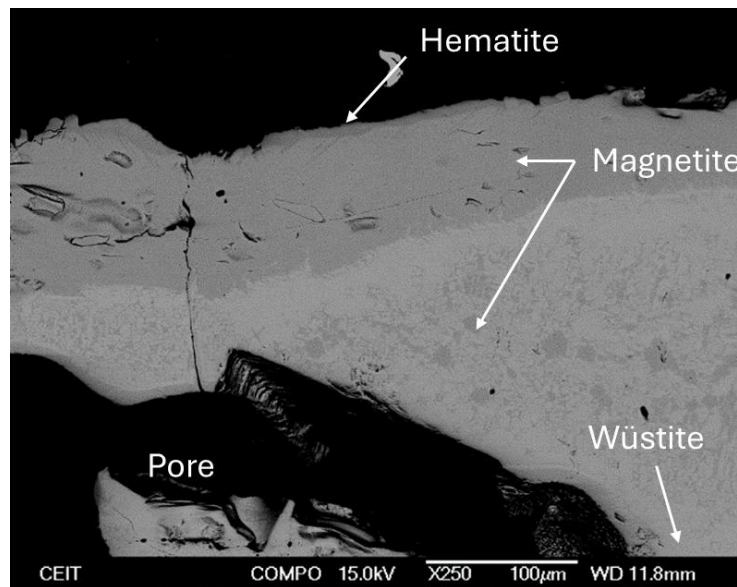


Figure 67. Examples of FEG-SEM image of the outer region. Position T1, condition NG/air.

The EBSD studies revealed that both the magnetite layer and the islands nucleated within the wüstite grains exhibit the same crystallographic orientation as the wüstite grains on which they grow. This can be clearly observed in the example shown in Figure 68, where the phase distribution is displayed in the phase map, while the orientation can be analysed using the Inverse Pole Figure (IPF). Wüstite is not stable below 570 °C and tends to transform into magnetite. Therefore, these findings suggest that its formation takes place during cooling, with the layer being mainly composed of wüstite at high temperature. According to the studies carried out, this type of oxidation mechanism has been identified as Type II or crystalline, in contrast to the Type I or three-layered which is characterised by the presence of an outer thick, smooth region.

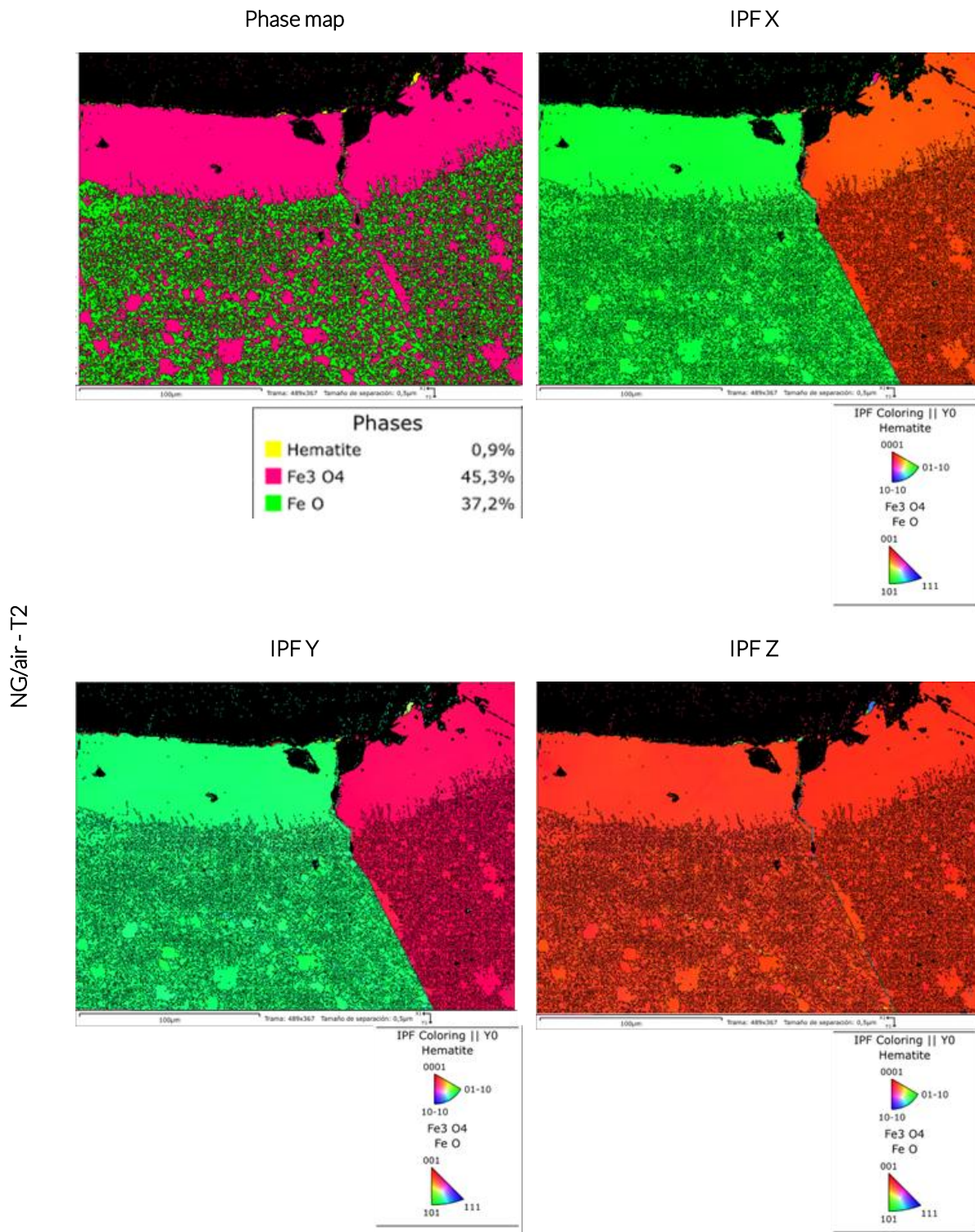


Figure 68. Examples of EBSD images (Phase Map and IPF) obtained from the outer region. Position T2, condition NG/air.

As we move toward the interior of the scale, the grain size of the wüstite tends to decrease, as does the size of the pores. Similarly, the fraction of magnetite islands decreases, although this phase can still be observed nucleating around pores and grain boundaries. On the other hand, the appearance of a dark-contrast intergranular phase is observed in this region, which was identified as fayalite by means of EDS. Likewise, a bright-contrast phase identified as occluded matrix is also present, tending to become enriched in elements more noble than iron (mainly Cu and Ni), as shown in the example in Figure 69.

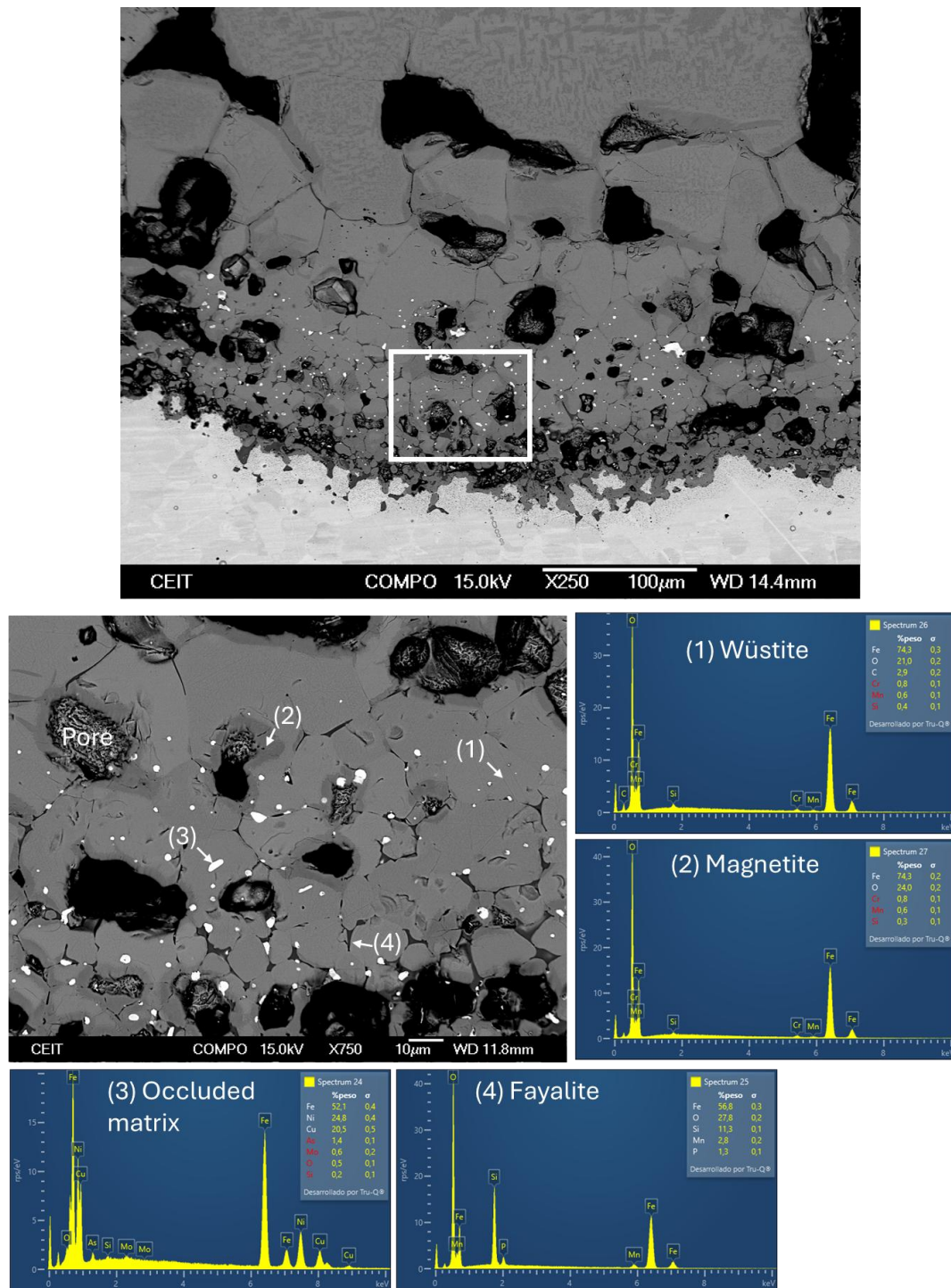


Figure 69. Examples of FEG-SEM image and EDS characterization of the inner region. Position T5, condition NG/O₂.

The study of the samples was completed through the characterization of the interface between the oxide and the matrix. This region is of particular interest since it is expected to have a direct impact on descaling properties and on the possible embrittlement phenomena that may be activated during subsequent forming stages. In addition, this region typically exhibits a high fraction of small oxides within the matrix close to the oxide layer, constituting the internal oxidation zone.

In this way, an exhaustive analysis was carried out with the aim of identifying and describing the region in detail, as shown in the example of Figure 70. The phases identified were wüstite/magnetite and globular fayalite. Regarding internal oxidation, the EDS analyses revealed the presence of complex oxides mainly

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composed of Fe, Mn, Cr, and Si (and in some cases Ti). In addition, small bright particles identified as Cu are occasionally observed. In no case was Cu enrichment at the interface found to be significant or to occur along grain boundaries, suggesting that it will not activate embrittlement mechanisms during rolling.

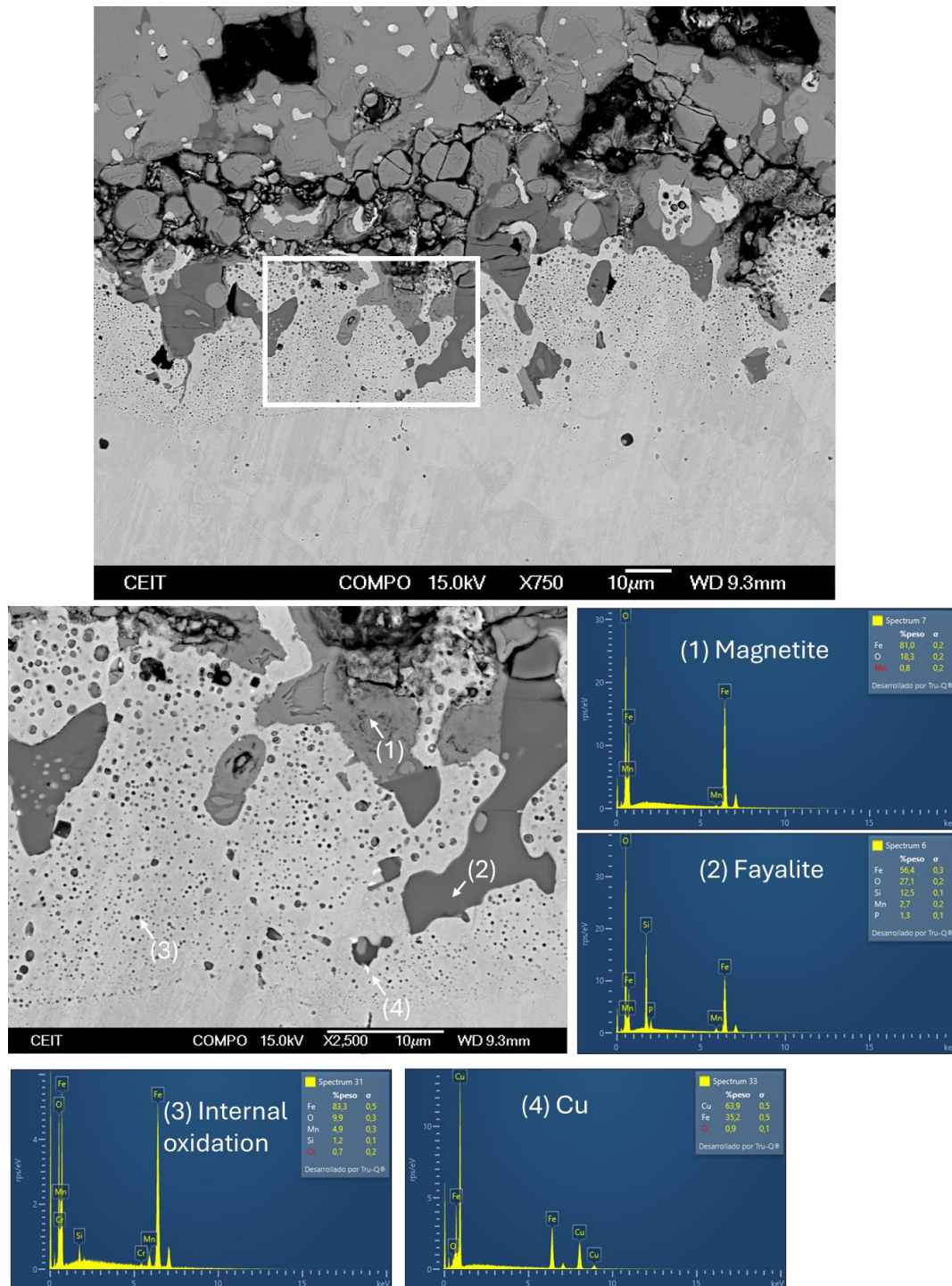


Figure 70. Examples of FEG-SEM image and EDS characterization of the interface. Position T3, condition H₂/O₂.

In addition to the microstructural characterization, quantitative metallography measurements were carried out in order to quantify the effect of oxidation on the thickness of the developed oxide layers. Figure 71 presents the oxide thickness results obtained at each of the positions of interest, together with the average value obtained for each condition.

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As can be observed, there is a certain degree of variability in the values obtained, especially under oxy-combustion conditions. Thus, the oxide thickness under the NG/air condition ranges between 540 and 760 μm , under the NG/O₂ condition between 580 and 1200 μm , and under the H₂/O₂ condition between 660 and 1150 μm . These results are consistent with the analysis of the thermal cycles presented in Deliverable 2.5, where it was indicated that NG/air combustion proved to be the most effective achieving uniform temperatures across all monitored points by the end of the treatment. Hence, the greater thermal uniformity results in lower variability in thickness. It is also interesting to note that the temperature difference between T3 and the other thermocouples was more pronounced in NG/O₂ and H₂/O₂ than in NG/air test, which is in line with the thicker oxide layers measured in this position under oxy-combustion conditions.

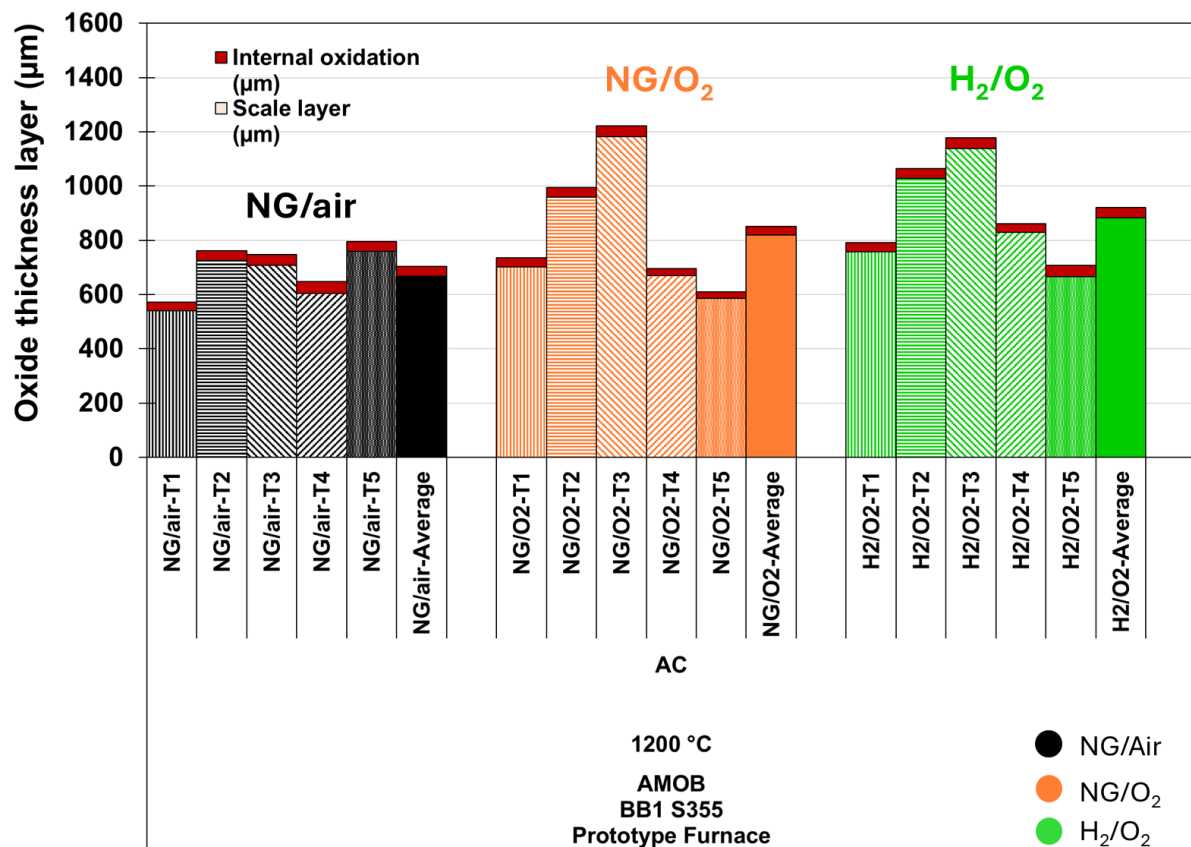


Figure 71. Measurements of oxide thickness (scale and internal oxidation) for the sets of samples and conditions analysed.

The final average oxide thickness values were:

- 667 μm for NG/air combustion.
- 819 μm for NG/O₂ combustion.
- 884 μm for H₂/O₂ combustion.

The incremental increase in thickness

- 152 μm from NG/air to NG/O₂, which corresponds to 22.79% increase.
- 217 μm from NG/air to H₂/O₂, which corresponds to 32.53% increase.

In general terms, the average measurements indicate that the oxide thickness tends to increase in the oxy-combustion conditions, NG/O₂ and H₂/O₂, whereas the layer is thinner under the NG/air condition. This effect is likely caused by differences in thermal cycling and/or by accelerated oxidation kinetics under oxy-combustion conditions. It is not possible to discriminate between the two effects since the thermal cycles and combustion conditions vary simultaneously (see summary of results in Table 25). Regarding the internal oxidation, its average depth into the matrix is around 35 μm in all cases.

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Based on the oxide thickness data, it is possible to estimate the resulting weight loss for the 400 mm long BB1. These data by CEIT, along with key variables such as residual O₂ and weight loss measurements reported in Deliverable 2.5 by TEC are summarised in Table 25. It should be noted that the results obtained in the characterization and estimations performed are consistent with the weight loss measurements.

Table 25. Weight loss measurements and estimations carried out specifically for the 400 mm long BB1.

Combustion conditions	Position	t in which T>1150 °C (min)	Average residual O ₂ (%)	Weight loss (%)	Oxide thickness (μm)	Estimated weight loss (%)
NG/air	T1 (55mm)	13.0	1.46	0.97	667	0.99
	T2 (40mm)	13.3				
	T3 (25 mm)	24.8				
	T4 (25 mm)	16.2				
NG/O ₂	T1 (55mm)	24.3	2.38	1.26	819	1.22
	T2 (40mm)	23.2				
	T3 (25 mm)	42.3				
	T4 (25 mm)	21.3				
H ₂ /O ₂	T1 (55mm)	-	2.25	1.58	884	1.32
	T2 (40mm)	19.2				
	T3 (25 mm)	39.0				
	T4 (25 mm)	18.0				

4.4 Characterization of the materials from AMOB's walking beam furnace trials

The main objective of the work carried out is to evaluate the quality of AMOB steel grades resulting from the implementation of H₂ combustion and oxy-combustion during the reheating process before hot rolling. To this end, material from three different industrial trials conducted at AMOB's walking beam furnace was extracted from the semi-finished products and sent to CEIT for characterization. In these trials 2 out of 61 burners in the soaking zone of the furnace operated NG/O₂ and H₂/O₂ combustion in each trial. The remaining burners operated under the standard NG/air operating conditions. A detailed description of the furnace retrofitting, and its operation has been provided in section "Retrofit on the AMOB's walking beam furnace with H₂/O₂ combustion (SAR/NIP)" of this deliverable.

The same characterization methodology described in section "Characterization of the materials from the tests in SAR's combustion chamber located at the TEC facilities with material from AMOB" of this deliverable was applied in the present study. Main findings are presented below.

4.4.1 Material and industrial conditions

The studies have been carried out on structural S235 steel produced at AMOB. The material is supplied as 100-300 mm long BB2. Figure 72 shows the beam blanks received for microstructural characterization at CEIT.

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Figure 72. Beam blanks 2 produced in AMOB under NG/air, NG/O₂ and H₂/O₂ combustion programme with the two new burners.

For consistency, the same sample positions, and designation criteria previously described in section: “Characterization of the materials from the tests in SAR’s combustion chamber located at the TEC facilities with material from AMOB” were maintained throughout this study. The set of conditions and positions characterized in the context of this task is summarized in Table 26. A detailed description and analysis of the industrial thermal cycle is available in section “4.1 Temperature monitoring tests in the AMOB’s walking beam furnace” in this deliverable.

Table 26. Summary of conditions and positions characterized.

Steel grade	Geometry	Combustion conditions	Position characterized				
			T1	T2	T3	T4	T5
S235	BB2	NG/air	T1	T2	T3	T4	T5
		NG/O ₂	T1	T2	T3	T4	T5
		H ₂ /O ₂	T1	T2	T3	T4	T5

4.4.2 Results and discussion

The characterization of the beam blanks was initiated in all cases by optical inspection and the acquisition of macro images of the regions of interest. Figure 73 shows some of the images taken at each of the positions. These images reveal that, at the macro level, the surfaces are characterized by heterogeneous oxide formations, where blisters and irregularities (striations) can be observed. In different regions, the oxide layer has peeled off which could be related to sample handling. In general terms, the oxide is considered to exhibit good adhesion. In general terms, the macroscopic condition of the surfaces does not show significant differences.

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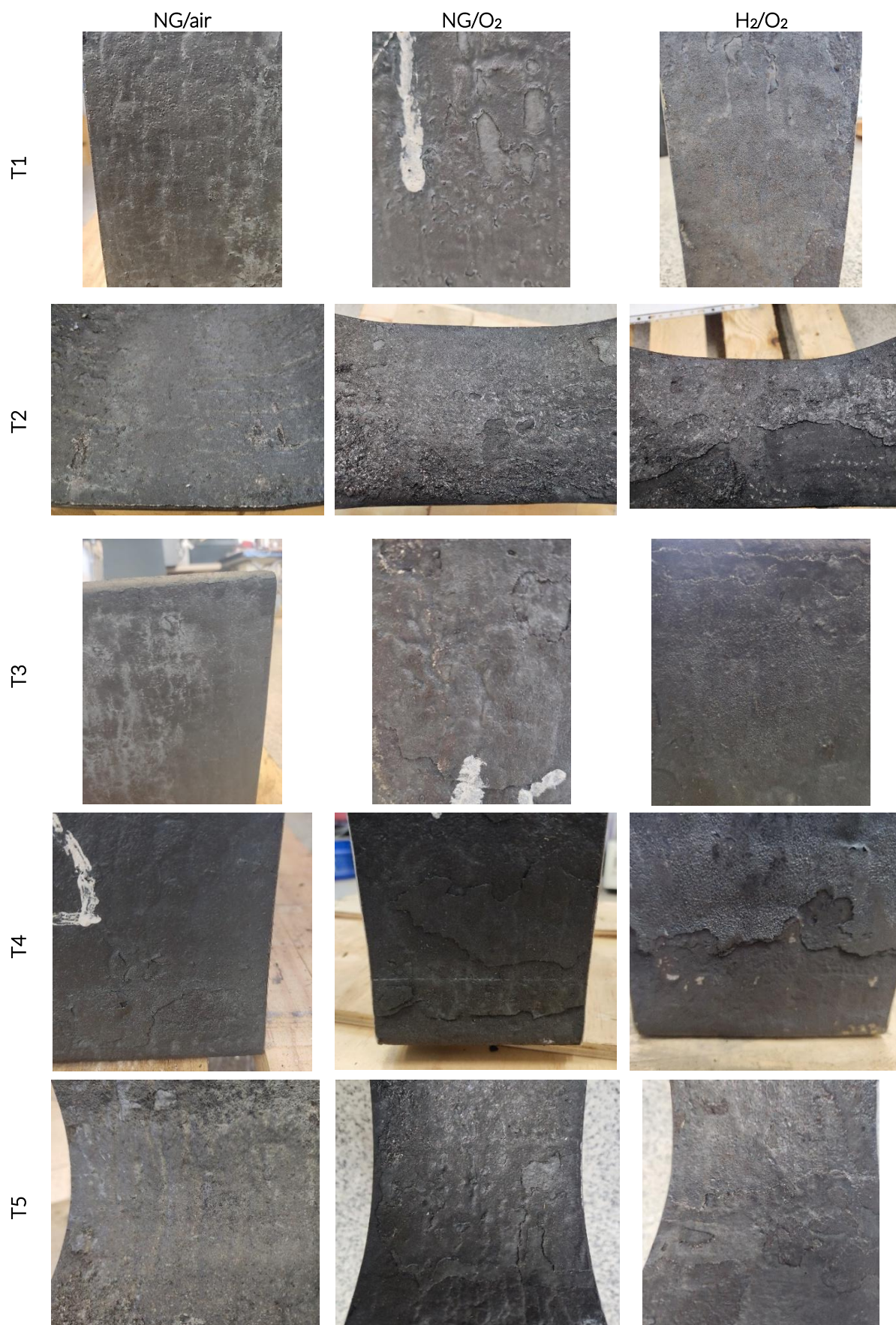


Figure 73. Macro images obtained at defined positions for beam blanks from NG/air, NG/O₂ and H₂/O₂ industrial trials.

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Detailed characterization of developed oxide in the different positions and conditions tested was then carried out by means of FEG-SEM. The characterization revealed that oxide is composed of coarse crystallographic units with a rough appearance. The morphology in all positions and heating conditions is similar. In general terms it seems grain size tends to be coarser in NG/air trial.

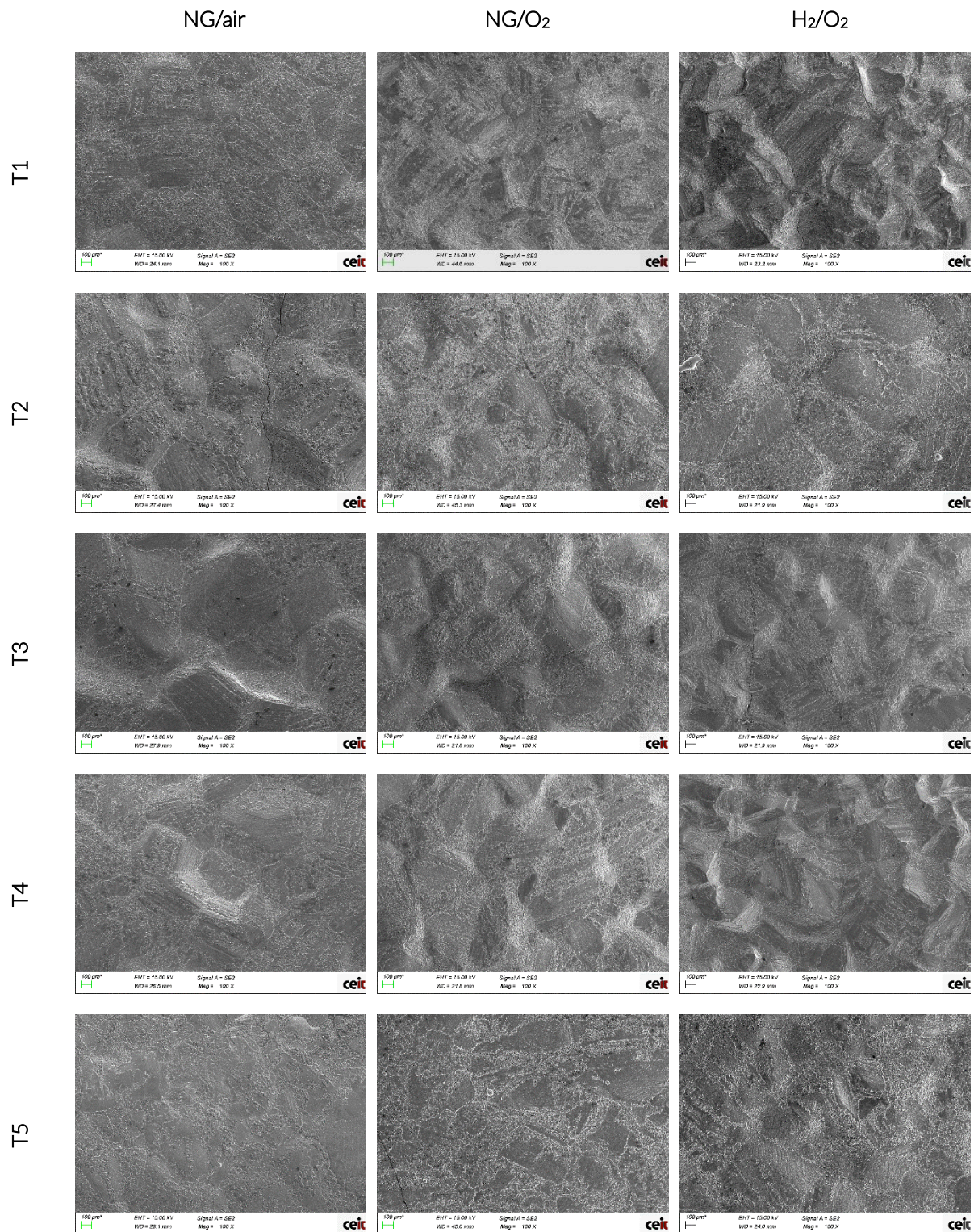


Figure 74. FEG-SEM images obtained from the oxide surface at defined positions for beam blanks from NG/air, NG/O₂ and H₂/O₂ industrial trials.

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After surface characterization, the samples were sectioned, mounted, and prepared in cross-section following the metallographic grinding and polishing route using different water-based diamond suspensions. Figure 75 shows the cross-sectional images of the oxide layer for all the cases under study.

The backscattered-electron FEG-SEM images provide a clear contrast between the brighter metallic matrix and the oxide scale formed on the surface. Despite variations in oxide thickness and local features depending on the analysed position and condition, the overall morphology of the oxide layer remains comparable in all cases. The scale is predominantly composed of a major phase identified as wüstite.

The defects observed within the oxide layers are similar to those previously identified in the prototype samples. Features, including pores, cracks, and partial discontinuities along the matrix/oxide interface, were detected in all analysed conditions. As reported previously, the interfacial discontinuities are attributed to thermal expansion mismatch between the oxide scale and the metallic substrate during cooling, while the pore distribution suggests pore migration and coalescence during oxide growth.

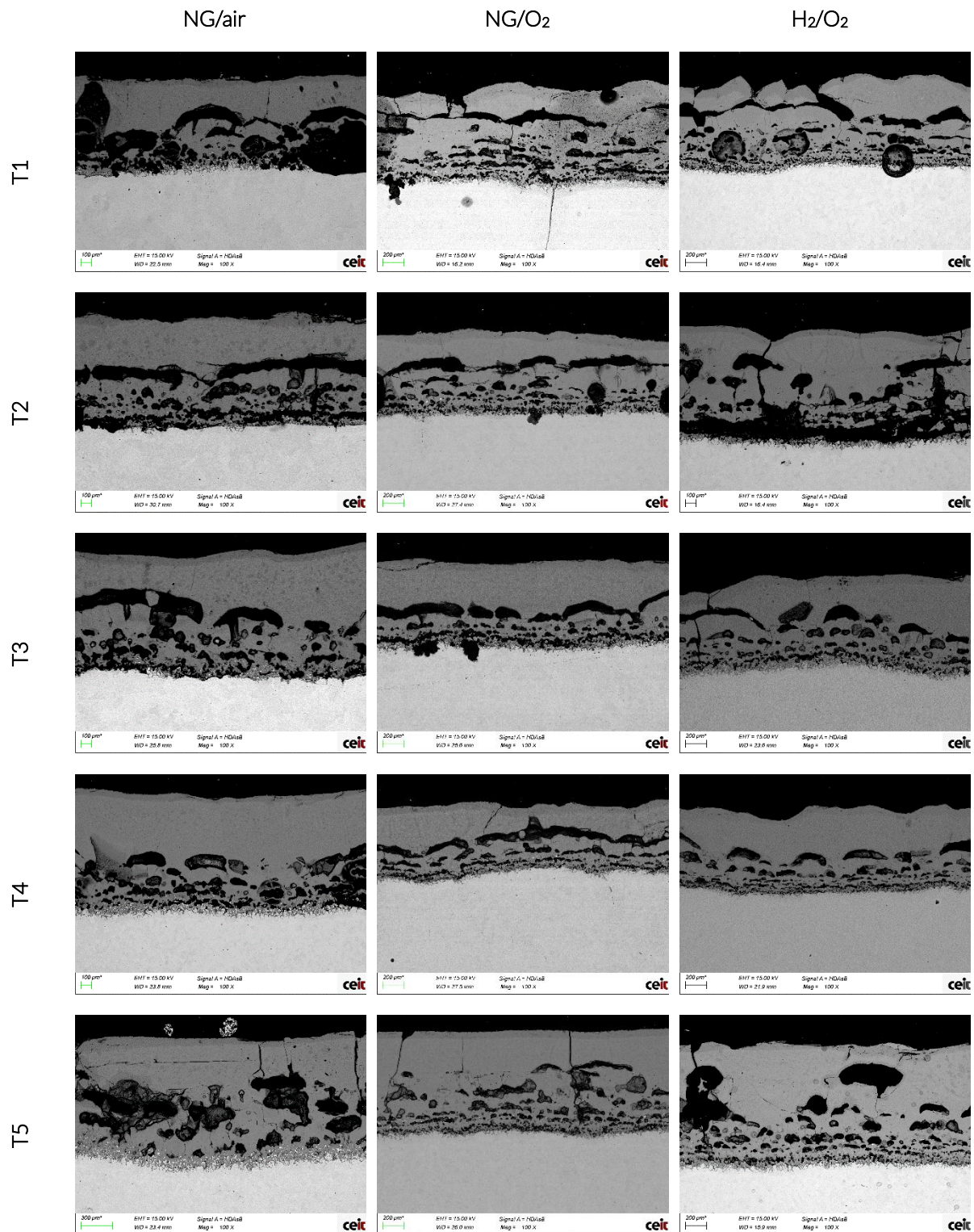


Figure 75. FEG-SEM images obtained from the cross-sections at defined positions for beam blanks from NG/air, NG/O₂ and H₂/O₂ industrial trials.

A more comprehensive characterization was performed on the outer and inner regions of the oxide scale, together with the oxide/matrix interface. These areas were selected due to their relevance identified in previous investigations (see Deliverable 2.1 of this project). The analyses carried out across all samples showed that the same fundamental phenomena occur regardless of the analysed position or applied

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condition. Consequently, the most representative and relevant results obtained during the characterization are presented below.

The outer region of the samples is distinguished by the presence of a magnetite layer whose thickness changes according to the analysed position and operating condition. In every case, this layer displays an irregular interface with the underlying wüstite. Hematite can occasionally be detected at the outer surface of the magnetite layer. In addition, magnetite precipitates appearing as islands within the large wüstite grains are also observed. Figure 76 presents a representative detailed micrograph of this region.

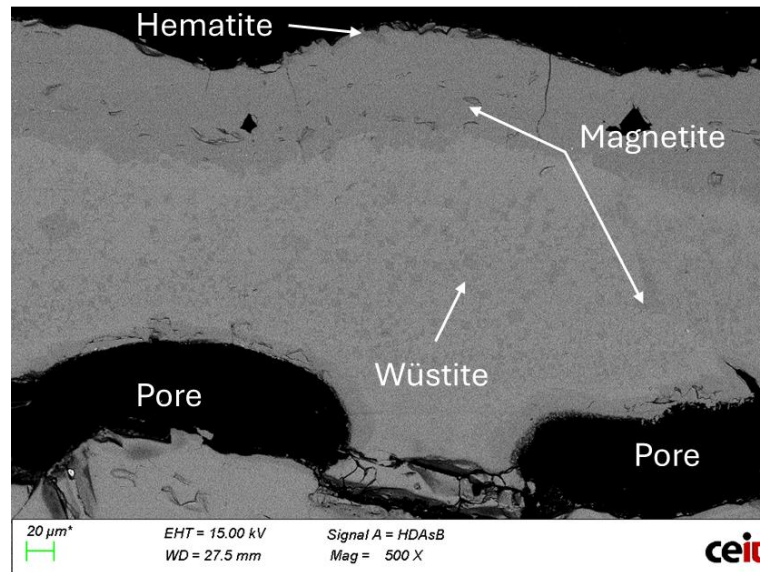


Figure 76. Examples of FEG-SEM image of the outer region. Position T2, condition NG/O2.

The EBSD analysis showed that both the continuous magnetite layer and the magnetite islands formed inside the wüstite grains share the same crystallographic orientation as the parent wüstite grains on which they form. This relationship is clearly illustrated in Figure 77, where the phase distribution is presented in the phase map and the crystallographic orientation is evaluated through the Inverse Pole Figure (IPF) maps. Since wüstite becomes unstable below 570 °C and progressively transforms into magnetite, these observations indicate that the transformation occurs during cooling, while at high temperature the oxide layer is predominantly composed of wüstite. Based on the investigations performed, this oxidation behaviour has been classified as a Type II, or crystalline, oxidation mechanism.

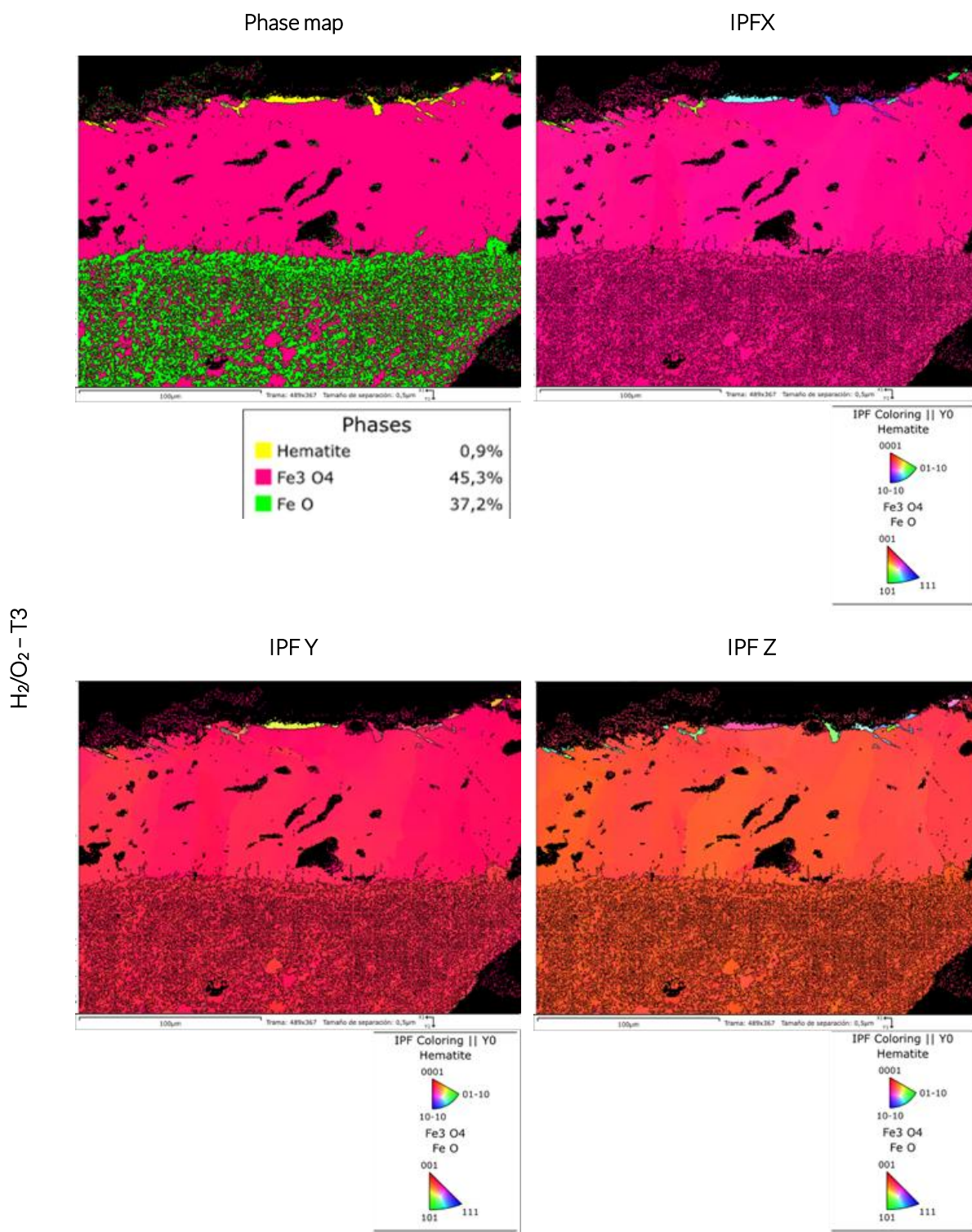


Figure 77. Examples of EBSD images (Phase Map and IPF) obtained from the outer region. Position T3, condition H_2/O_2 .

Toward the inner regions of the oxide scale, both the wüstite grain size and the pore size progressively decrease. In the same way, the amount of magnetite islands becomes lower, although magnetite can still be found preferentially forming around pores and along grain boundaries. In this region, a dark intergranular phase is also detected. It was identified by EDS as fayalite. Additionally, areas with brighter contrast corresponding to occluded metallic matrix are observed; these regions show enrichment in elements like Cu and Ni, as illustrated in Figure 78.

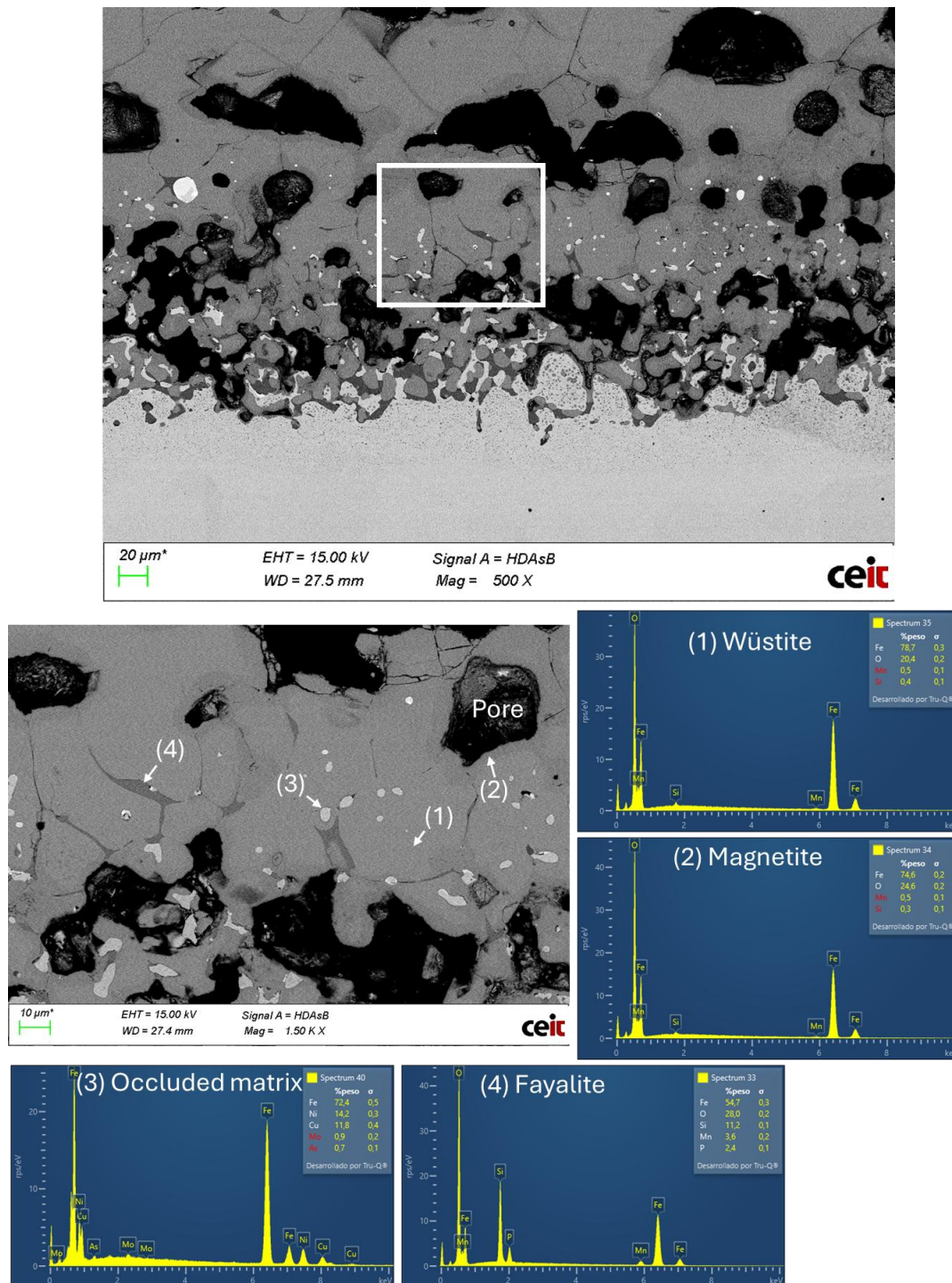


Figure 78. Examples of FEG-SEM image and EDS characterization of the inner region. Position T4, condition NG/O₂.

The investigation of the samples was further complemented by the characterization of the oxide/matrix interface. This area is especially relevant because it is expected to strongly influence descaling behaviour as well as potential embrittlement mechanisms that could be triggered during subsequent rolling operations. Moreover, this region commonly contains a high density of fine oxides dispersed within the matrix adjacent to the oxide layer, constituting the internal oxidation zone.

It is interesting to note that the morphology of the interface may vary from one position to another in the different trials, as shown in Figure 79. In some areas, a honeycomb-like structure is observed, formed by occluded matrix, fayalite, and wüstite/magnetite. In other regions, the interfacial morphology appears

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simpler. This type of morphological variation was observed in some cases during the laboratory tests (see Deliverable 2.1). EDS analyses showed that the same phases are present in all cases. Regarding internal oxidation, the EDS analyses revealed the presence of complex oxides mainly composed of Fe, Mn, Cr, and Si. In addition, small bright particles identified as Cu are occasionally observed. In no case was Cu enrichment at the interface found to be significant or to occur along grain boundaries, suggesting that it will not activate embrittlement mechanisms during rolling.

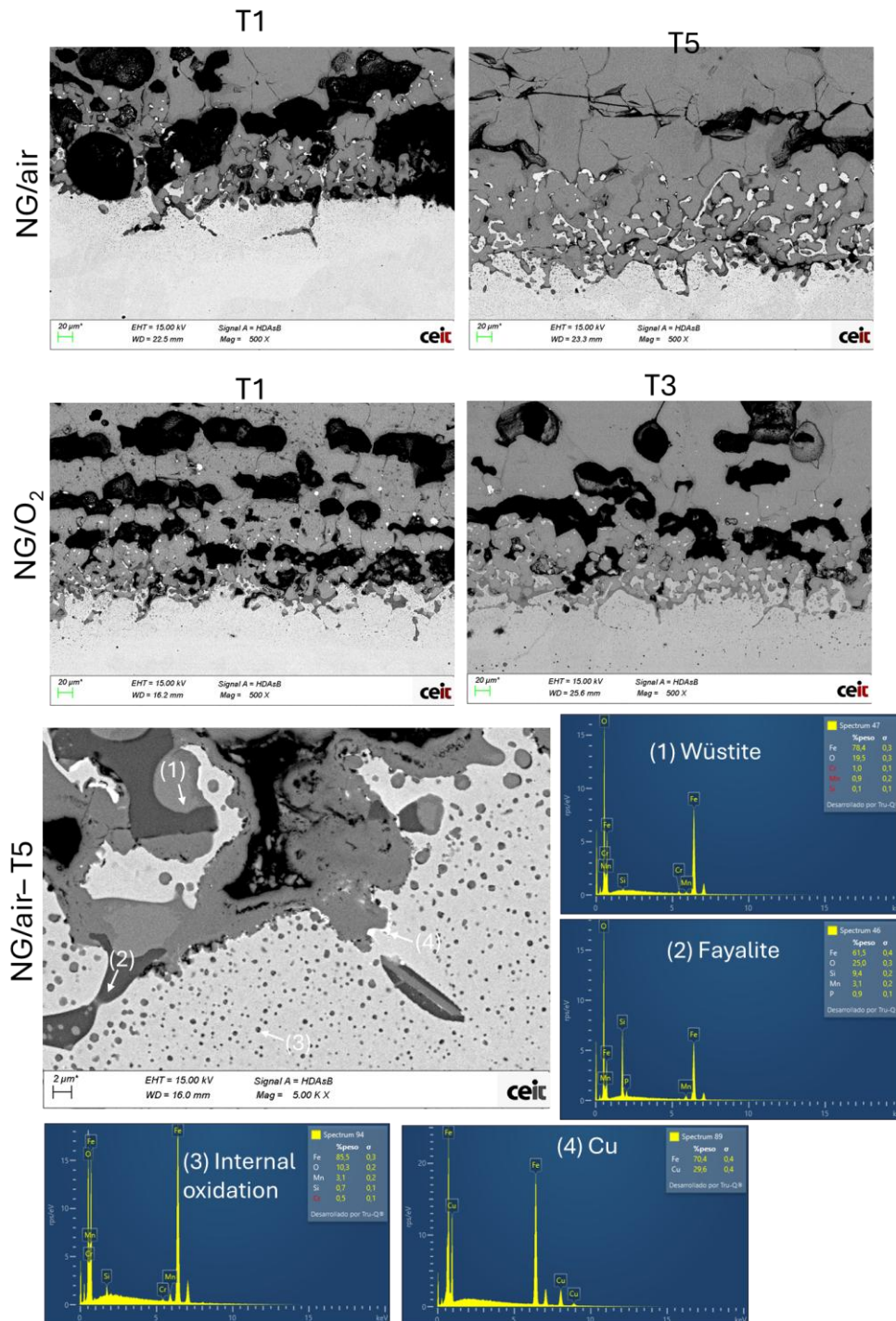


Figure 79. Examples of FEG-SEM image and EDS characterization of the interface at different positions.

The results obtained show oxide characteristics similar to those observed in the tests carried out in SAR's combustion chamber (see section "Characterization of the materials from the tests in SAR's combustion

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chamber located at the TEC facilities with material from AMOB²). It should be noted that the steel grades tested in each case are different (S355JR in the prototype chamber and S235 in the industrial trials). However, these are grades with similar chemical compositions. The laboratory studies performed and reported in Deliverable 2.1 had already suggested that the oxidation behaviour of both materials was similar.

Alongside the microstructural analysis, quantitative metallographic measurements were performed to assess the influence of oxidation on the thickness of the oxide layers formed. Figure 80 summarizes the oxide thickness values measured at the different positions of interest, together with the corresponding average thickness for each condition.

As can be observed, there is a certain degree of variability in the values obtained. The oxide thickness under the NG/air condition ranges between 900 and 1200 μm , under the NG/O₂ condition between 700 and 950 μm , and under the H₂/O₂ condition between 800 and 1200 μm . The oxide thickness in T5 is slightly greater than in the other zones; however, given the variability observed, it is difficult to draw definitive conclusions. Regarding the average values, the oxide thickness was found to be highest in the NG/air trial, followed by H₂/O₂ and NG/O₂. This appears to indicate that modifying only two burners in the industrial furnace does not have a significant influence on the oxidation behaviour of the semi-finished products. In fact, the observed differences are associated with variations in the actual thermal cycles taken place in the industrial furnace. The analysis of the curves shows that higher temperatures for longer times were applied in the condition NG/air (see section “4.1 Temperature monitoring tests in the AMOB’s walking beam furnace”). This is also consistent with the internal oxidation layer analysis, which shows greater penetration depths for the NG/air trial (93 μm on average) compared with those measured for the NG/O₂ and H₂/O₂ trials (68 and 61 μm , respectively).

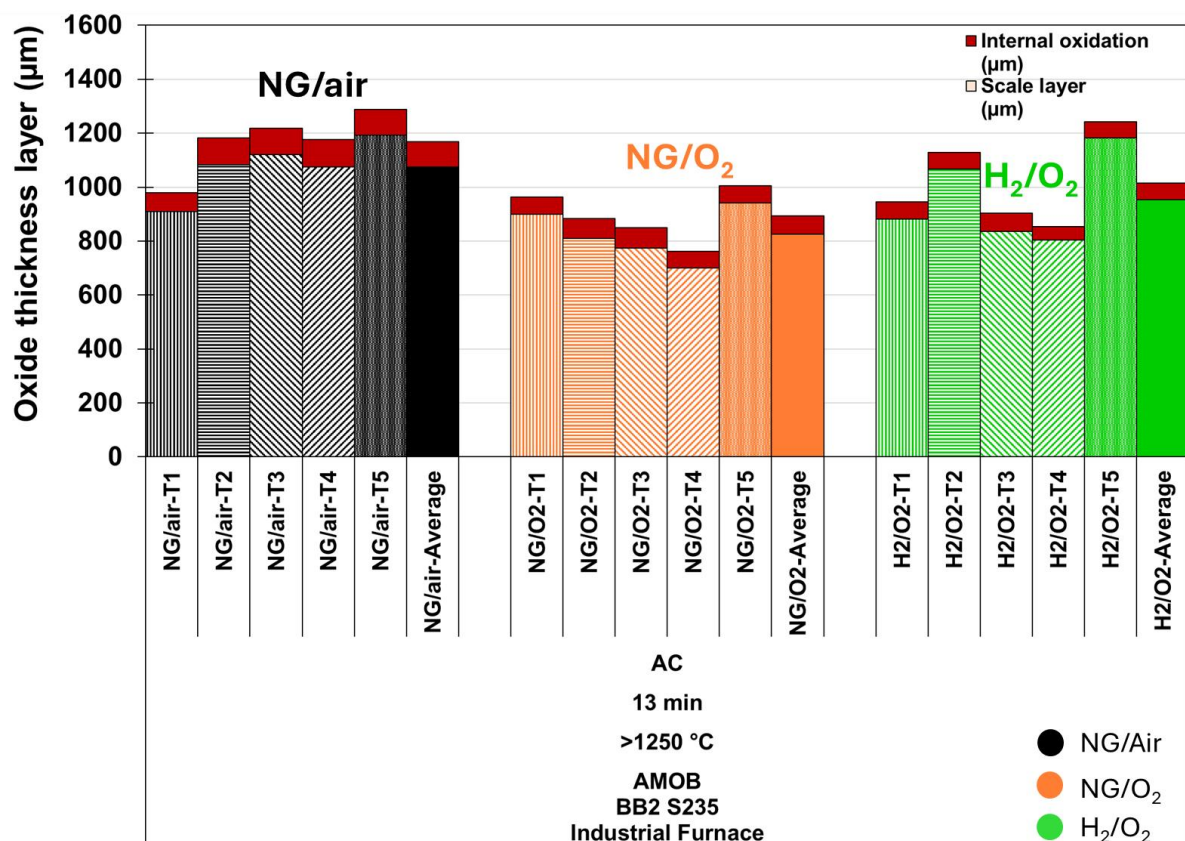


Figure 80. Measurements of oxide thickness (scale and internal oxidation) for the sets of samples and conditions analysed.

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Figure 81 shows the different thermal cycles applied throughout the project. These include:

- Isothermal laboratory TGA tests (CEIT), in which the oxidizing atmosphere is only present during the 15-minute isothermal stage at 1250 °C. Heating stage in Argon.
- Heat treatments reproducing the industrial thermal cycle (CEIT), where the reheating cycle is simulated at laboratory scale.
- Tests in SAR's prototype combustion chamber (TEC).
- Trials in the AMOB's walking beam furnace (SAR/NIP/TEC)

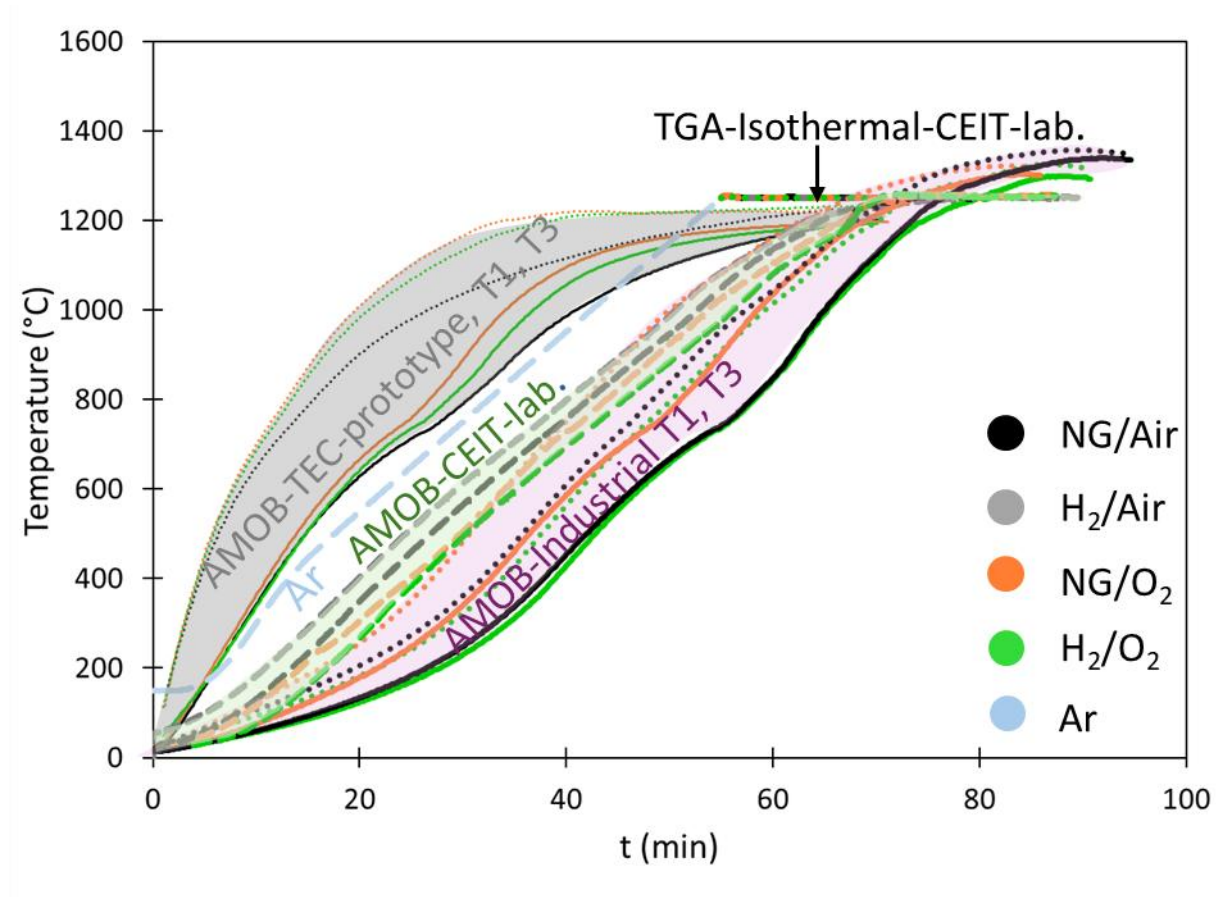


Figure 81. Summary of thermal cycles applied throughout the project on AMOB material.

Figure 82 summarizes the results obtained for oxide thickness measurements throughout the project for the AMOB steel grades. It should be noted that the thermal cycles applied in each type of test differ significantly from one another. Nevertheless, the isothermal TGA tests exhibited the lowest oxide thicknesses, which is consistent with the limited exposure time to the oxidizing atmosphere during these experiments. Regarding the thermal cycle applied during the laboratory-scale heat treatments, these cycles were longer and involved a more extended holding time at high temperature than those applied in the prototype furnace, which is consistent with the greater oxide thicknesses obtained for the S355JR grade. In turn, the industrial thermal cycle shows greater similarity to that applied in the laboratory-scale heat treatments for the S235 steel grade, although higher temperatures were reached under industrial conditions. This would explain the larger oxide thicknesses obtained, particularly under the NG/air industrial trial.

With respect to the effect of the atmosphere, drawing definitive conclusions from the prototype furnace and industrial trials becomes more complex due to the simultaneous variation of both combustion conditions and thermal cycles and as a consequence to the limited implementation of the new combustion conditions at industrial scale. The laboratory-scale results, however, appear to indicate higher oxidation kinetics under oxy-combustion conditions for AMOB steel grades.

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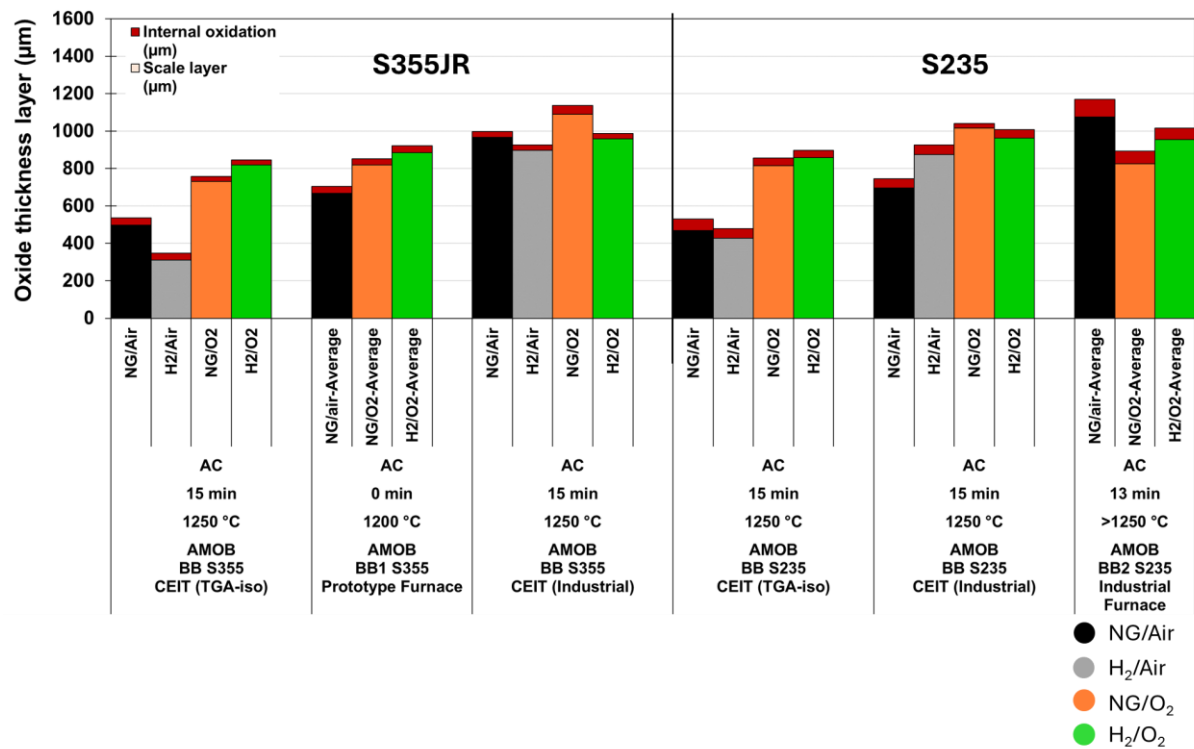


Figure 82. Measurements of oxide thickness (scale and internal oxidation) obtained in the different tests performed throughout the project for AMOB steel grades.

5 Task 6.2: Retrofit on Walking Beam Furnace Demonstrator with H₂/Air, H₂/OEC, and H₂/O₂ Combustion

5.1 Method

5.1.1 Experiments and equipment

Swirim’s key role in the project is to use its pilot-scale walking-beam furnace (WBF) as one of eight demonstrators. The WBF is equipped with advanced flow-control trains, flue-gas dampers, and control and safety systems to enable operation on hydrogen as the sole fuel. The furnace is 2.2 meters wide and 9 meters long, divided into three heating zones and an unfired preheating zone, figure 6.2.1. Each heating zone is equipped with two wall-mounted dual-fuel burners (light fuel oil or hydrogen) and a central hydrogen lance flanked by two oxygen lances, all positioned above the burners. When the furnace operates with oxygen-enhanced combustion (OEC or OFC), it operates in flameless mode, with oxygen and hydrogen supplied via separate lances. Using air-fuel combustion (AFC), the combustion air is preheated using a central recuperator, resulting in a temperature of ~225-300 °C. Furnace control is managed on a zone-by-zone basis using wall- and roof-mounted thermocouples and oxygen sensors.

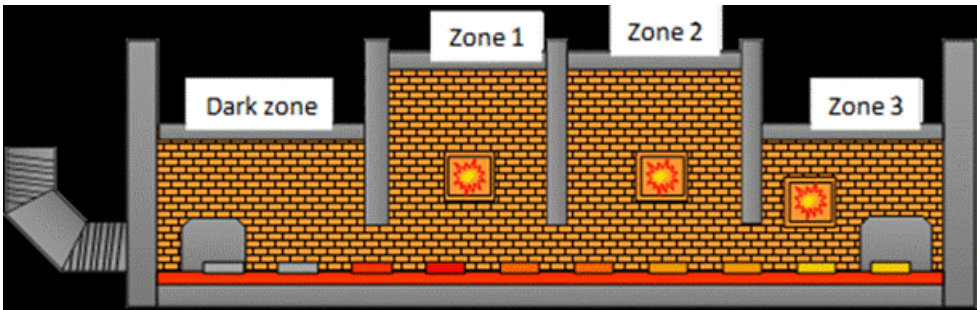


Figure 83. The WBF

The slabs are fed forward in the furnace by a walking beam that moves at a set distance, resulting in 18 slab positions. The slabs weigh approximately 400 kg each and remain at each position for 8 minutes, yielding a nominal load of 3 tons/h and a total residence time of approximately 2½ hours. To achieve the desired target top temperature and temperature homogeneity, the furnace was modelled using Steeltemp2D to determine the target-zone temperatures used in the experiments, as shown in Table 28. Input values for the Steeltemp simulations.. The final steel temperature is also verified using thermocouples inserted into selected slabs at positions through the mid-section (upper surface, mid, lower surface) and at the mid-end, see Figure 84. Thermocouples positions..

Table 27. Zone target temperatures.

	Zone 1	Zone 2	Zone 3	Slab
Target temperature, °C	1078	1141	1220	1200

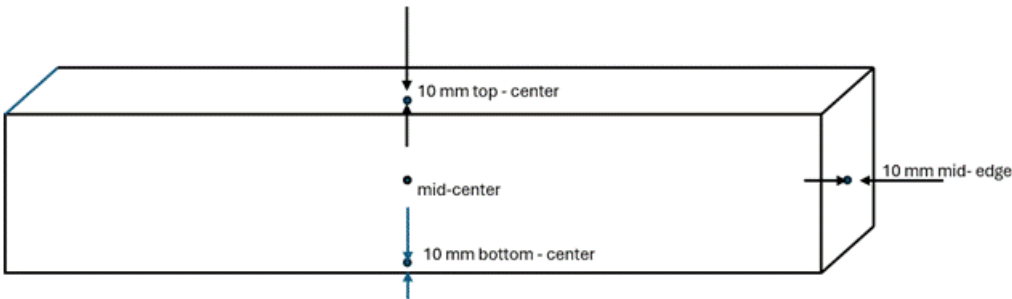


Figure 84. Thermocouples positions.

Before each experiment, the furnace was preheated with oil and air for 48 hours to reach a steady temperature. The test procedure begins by loading dummy slabs (220x150x1500 mm) into the dark zone

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until the furnace is filled. As all zones maintain a predefined set-temperature, and as slabs enter the zone, more fuel is required to maintain the target set-point until heat balance is achieved, i.e., the furnace is full, and there is continuous operation with cold slabs being charged and hot slabs discharged every eight minutes. So, once the last position is filled with dummy slabs, steady-state operation is achieved. The last dummy slab before the test slabs and the first dummy slab after the test slabs are equipped with thermocouples to measure actual steel temperature during heating. While the test slabs are in the furnace, dummy slabs are charged to maintain the heat balance. When the last dummy slab with thermocouples is discharged, the test is completed. Four test slabs (two steel grades) were used per experiment, and several dummy slabs (low-alloy steel) of similar dimensions and weight (~400 kg) were used. Temperatures, pressures, and mass flows (air, flue gas, fuel) are measured throughout the furnace to enable mass and energy balance calculations. Additional measurements included flue-gas concentrations of H₂O, CO, CO₂, and NO_x (wet basis), as well as flame diagnostics using Optical Emission Spectroscopy (OES). From the time the first dummy slab was loaded to the end of the experiments, approximately 8 hours elapsed. After each test slab finished heating, it was weighed, descaled, weighed again, and then allowed to cool. The descaling unit operates at 200 bar and consists of a hydraulic pump, accumulator tanks, piping, and water-jet nozzles.

5.2 Modelling

Steeltemp®, a finite-difference software program for temperature and heat-transfer analysis for various heating processes was used to prepare for the tests, by calculating temperatures and densities of the heat flow rate in the cross section of the steel. The target was to get zone temperatures in the three heating zones that will provide a steel target temperature, homogenously heated, at 1200°C. The results, the zone temperatures, will be maintained for all test cases and the feedback besides all the logged data is whether there will be differences in heating rate or final steel temperature based on heat transfer resulting from the generated flue gases composition.

What is needed as input in the simple model, based on wall temperatures and gas temperature offset from the wall temperature, is furnace geometry, the geometry of the steel slabs, emissivity, initial temperature and stock residence time. Some of the input variables for the simulations are shown in Figure 6.2.3.

Table 28. Input values for the Steeltemp simulations.

Variable		Variable	Oil + air	H ₂ +Air	H ₂ +O ₂
O ₂	2.5% (wet)	Fuel temp	100	20	20
T initial	20	Air/O ₂ temp	250	250	20
T end	1200				
Mass leakage	0				
Other losses	17 kW				

	CO ₂	H ₂ O	SO ₂	O ₂	N ₂
Oil+air	0.112	0.119	0.001	0.025	0.743
H ₂ +Air	0	0.306	0	0.025	0.669
H ₂ +O ₂	0	0.975	0	0.025	0

Stock data

☒ Rectangular

☐ Circular

Width

0.22 m

Thickness

0.15 m

Length

1.5 m

Initial temperature

20 °C

Emissivity

0.8

☐ Temp dependent

Material

☐ Microstructure model

5, SIS 1312

Furnace lining

Furnace wall

Inner

Mid

Outer

Thickness

0.21

0.1

0.135

 m

Lining

Castable

Firebrick

Fibre

Ambient temperature

20 °C

Emissivity of inner furnace wall

0.9

Emissivity of outer furnace wall

0.8

Variable	Oil E01	H ₂
LHV kJ/kg	43000	120000
Cp J/(kgK)	1840	14480

To get an overview of what is to be expected for the four test scenarios Steeltemp was initially run with high zone 2 and lower zone 3 power in the complex model where fuel flow is set to match the target temperature which initially was set higher for zone 2. The yellow line is the gas temperature, and dark blue is the wall temperature, whereas the three lower lines are the steel temperature throughout the midsection. The dark zone, (Z0), Steeltemp predicted the temperature to be lower for the OFC and OEC combustion of

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hydrogen, where the flue gas volume and energy content is lower. However, the target temperature is predicted to be achieved at the power levels applied, see Figure 85.

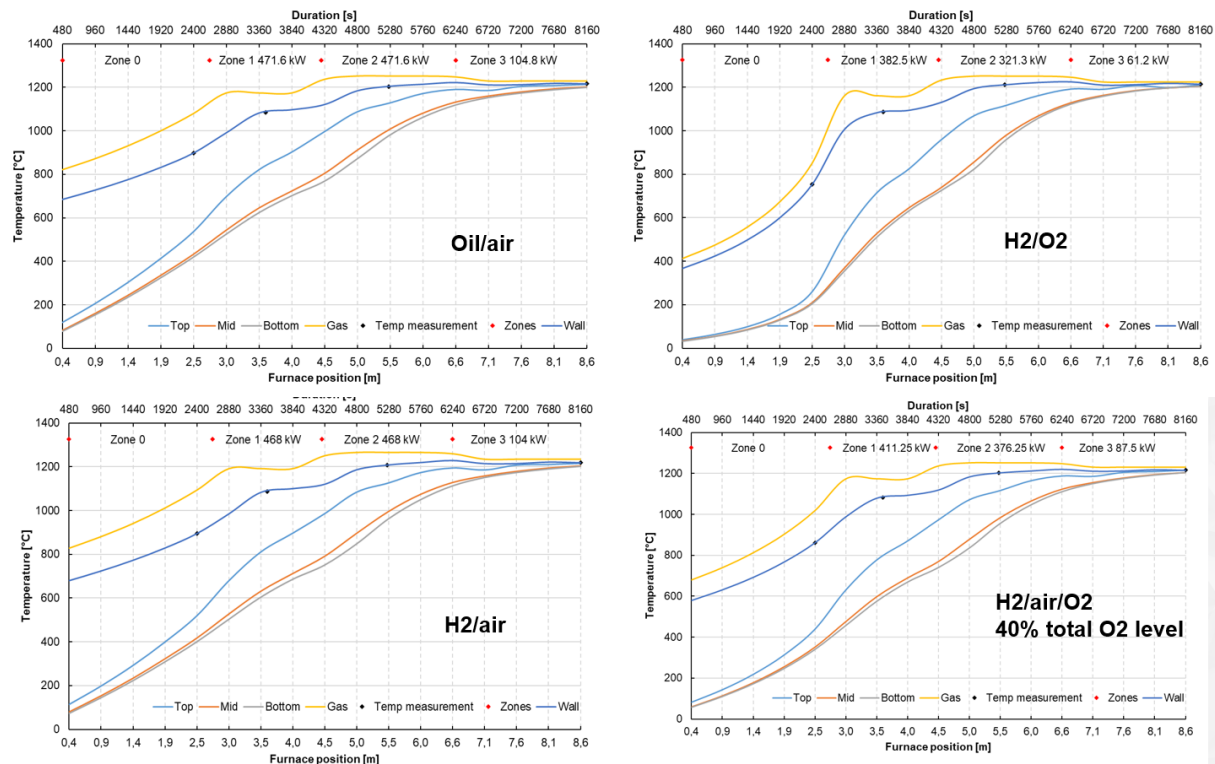


Figure 85. Steeltemp simulation in complex model.

During initial tests in the furnace during retrofit it was evident that it was more optimal to have a more even temperature between zone 2 and zone 3, and a new Steeltemp simulation was performed to verify that going a bit lower in zone 2 wall temperature will achieve the desired results, which it was and it was decided to run the tests according to the last simulation with wall temperatures according to Table 27. The simulation with the actual final temperatures can be seen in Figure 86.

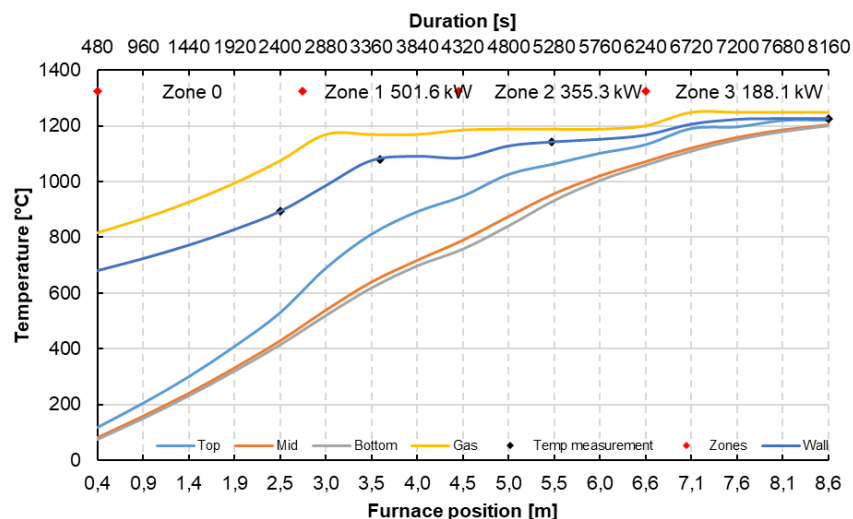


Figure 86. Steeltemp final simulation.

5.3 Experimental design

Several key factors influence furnace performance, such as energy efficiency and material quality. The process energy efficiency is investigated using three combustion modes: air-fuel combustion (AFC), oxygen-enhanced combustion (OEC), and oxy-fuel combustion (OFC). Replacing air with oxygen increases combustion efficiency primarily by reducing thermal ballast (N_2) in the system and allowing the fuel and oxidizer (reactants) to be the only inputs. The experiments also include two oxygen levels, 1% and 2.5%. Excess oxygen is used to ensure complete combustion; in particular, when using fossil fuels, this is necessary to avoid the risk of unburnt fuel, which can lead to the formation of CO, a lethal gas. A side effect of high oxygen levels is that higher oxygen concentration can lead to greater steel oxidation (i.e., scale losses). Therefore, the experiment investigates how oxygen concentration affects scaling when hydrogen is used as a fuel.

The combinations were tested twice, targeting 1% and 2.5% excess oxygen in the flue gas for a total of 8 experiments. To facilitate comparison, the same zone temperatures were used for all experiments. Table 29 provides an overview of the furnace parameters varied across the 8 experiments.

Four combinations of fuel and oxidizer were evaluated, including:

1. Light fuel oil (E01) with preheated air (reference case)
2. Hydrogen with preheated air
3. Hydrogen with preheated air enriched in oxygen (total O_2 -level 40%, flameless combustion)
4. Hydrogen and oxygen (flameless combustion)

Table 29. Experimental design

Experiment	ID	Fuel	Oxidant	Excess O_2
Ref _{AFC} 1	1	oil	Air (21% O_2)	1%
Ref _{AFC} 2.5	2	oil	Air	2.5%
H _{AFC} 1	3	H ₂	Air	1%
H _{AFC} 2.5	4	H ₂	Air	2.5%
H _{OEC} 1	5	H ₂	40% O_2 or POP 60	1%
H _{OEC} 2.5	6	H ₂	40% O_2	2.5%
H _{OFC} 1	7	H ₂	100% O_2	1%
H _{OFC} 2.5	8	H ₂	100% O_2	2.5%

During the tests, two steel grades were used in duplicates, see Table 30. Both grades are carbon steel, with alloy 362 being less prone to oxidation but more on the difficult side regarding descaling. These two grades were of special interest to the industrial supplier as they are commonly produced alloys.

Table 30. Reference composition of tested steel grades

Steel grade	C	Si	Mn	Cr	Ni	M	Ti	Al	Nb	B
313	0.2	0.2	0.7	0.8	-	-	0.012	0.06	0.01	0.001
362	0.375	0.25	0.7	0.5	2	0.35	-	0.055	-	0.01

5.3.1 KPI description and calculated values

For the calculations, average values (logged at 1 Hz) were used during steady-state furnace operation, which, in most cases, spanned most of the experiment. Furnace efficiency for the experiment was evaluated by calculating the average energy increase of the steel at the mass flow used and dividing it by the average fuel input, equation 1.

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$$\text{furnace efficiency} = \frac{\dot{m}_{\text{steel}} * C_{p\text{steel}} * (T_{\text{out}} - T_{\text{in}})}{\dot{m}_{\text{fuel}} * H_{\text{fuel}}} \quad [1]$$

Similarly, specific energy consumption was calculated by dividing the average fuel input by the average steel mass flow, equation 2.

$$\text{specific energy consumption} = \frac{\dot{m}_{\text{fuel}} * H_{\text{fuel}}}{\dot{m}_{\text{steel}}} \quad [2]$$

Flue gas losses were estimated by calculating the sensible heat content of the flue gas based on the temperature measurement after the recuperator, equation 3. The flow of flue gases was estimated based on measured fuel and air/O₂ flows, assuming complete combustion with the excess oxygen taken into account (slightly over-stoichiometric by 1% and 2.5%), and no leakage of air into the furnace. The resulting flue gas composition was then used to estimate the specific heat capacity of the gas mixture.

$$\text{flue gas losses} = \dot{m}_{\text{flue gases}} * C_{p\text{flue gases}} * (T_{\text{out}} - T_{\text{ref},0^\circ\text{C}}) \quad [3]$$

5.4 Results and discussion

The next two figures show typical furnace, gas, and slab temperatures measured during the reference experiment with oil (Ref_{AFC} 2.5 and Ref_{AFC} 1). The data show that thermocouples positioned in the midsection (mid-mid and end-mid) exhibit lower heating rates than those closer to the surface; however, all sensors ultimately reach the same discharge temperature. Because there is no heating zone below the slab, the top of the slab heats significantly faster than the bottom.

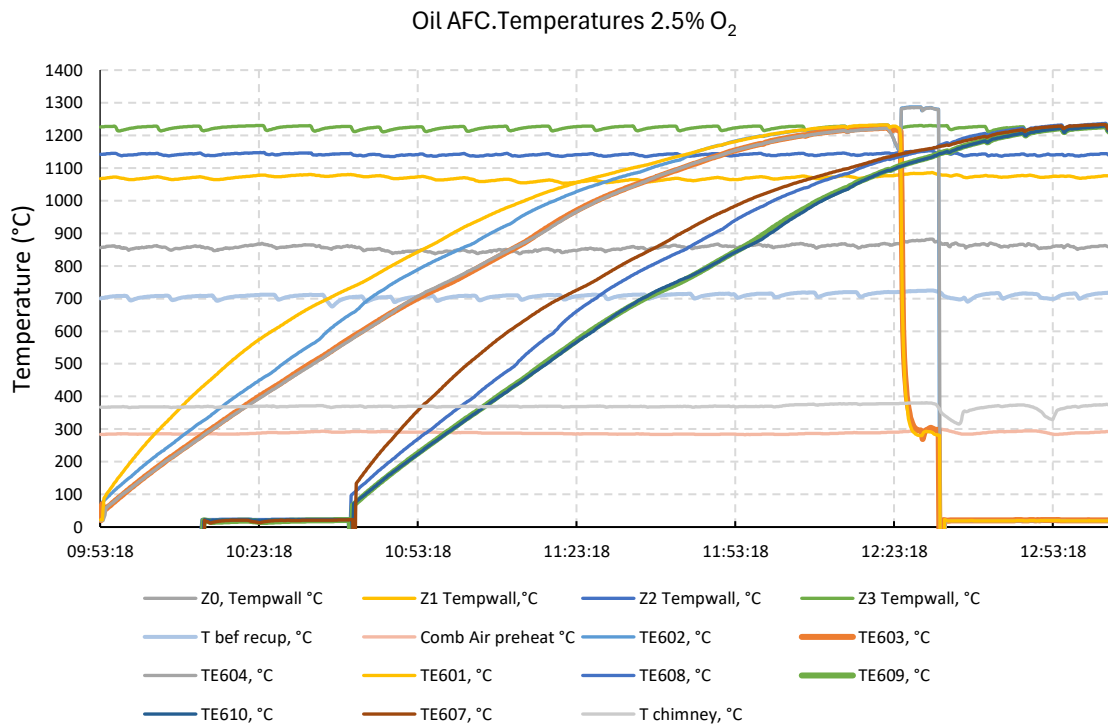


Figure 87. Sample logged test data over temperatures, RefAFC 2.5. ID2.

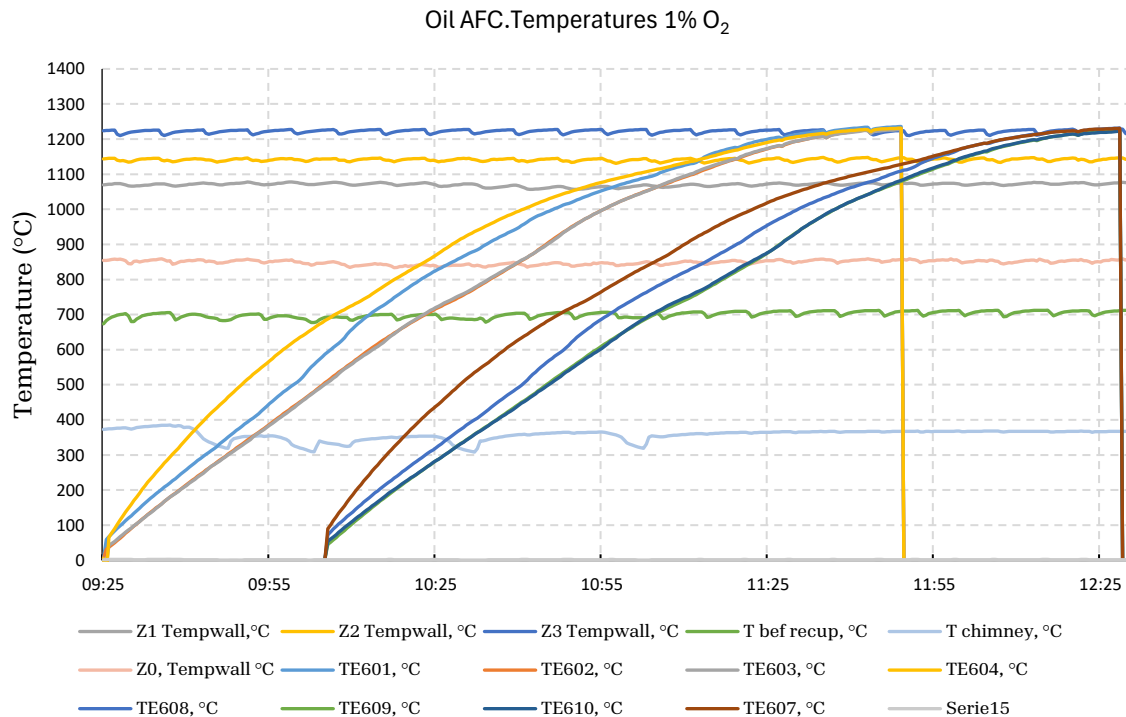


Figure 88. Sample logged test data over temperatures, RefAFC 1. ID1.

The next two figures show typical furnace, gas, and slab temperatures measured during the reference experiment with hydrogen (H_{AFC} 2.5 and H_{AFC} 1). Comparing H_{AFC} with oil, the two experiments show very similar overall behaviour with respect to the measured slab heating rate and process temperatures. However, there are some differences. The temperature of preheated combustion air is higher for oil, as are the temperatures before the recuperator and in the stack. No significant difference in slab heating was observed between experiments performed at 1% O₂ and 2.5% O₂.

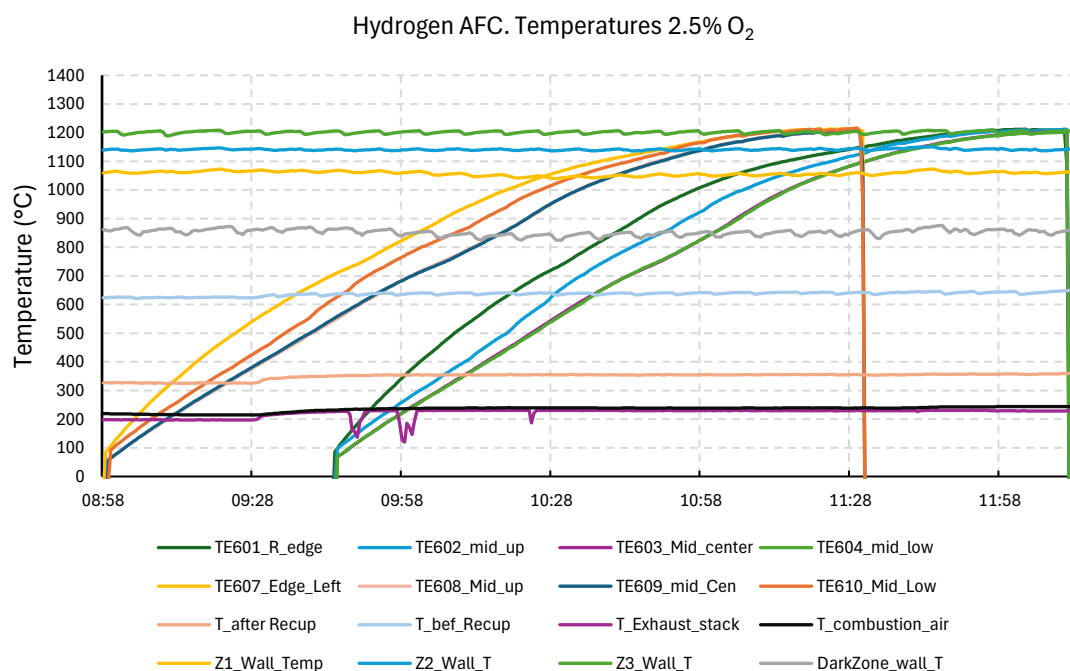


Figure 89. Sample logged test data over temperatures, H_{AFC} 2.5. ID4.

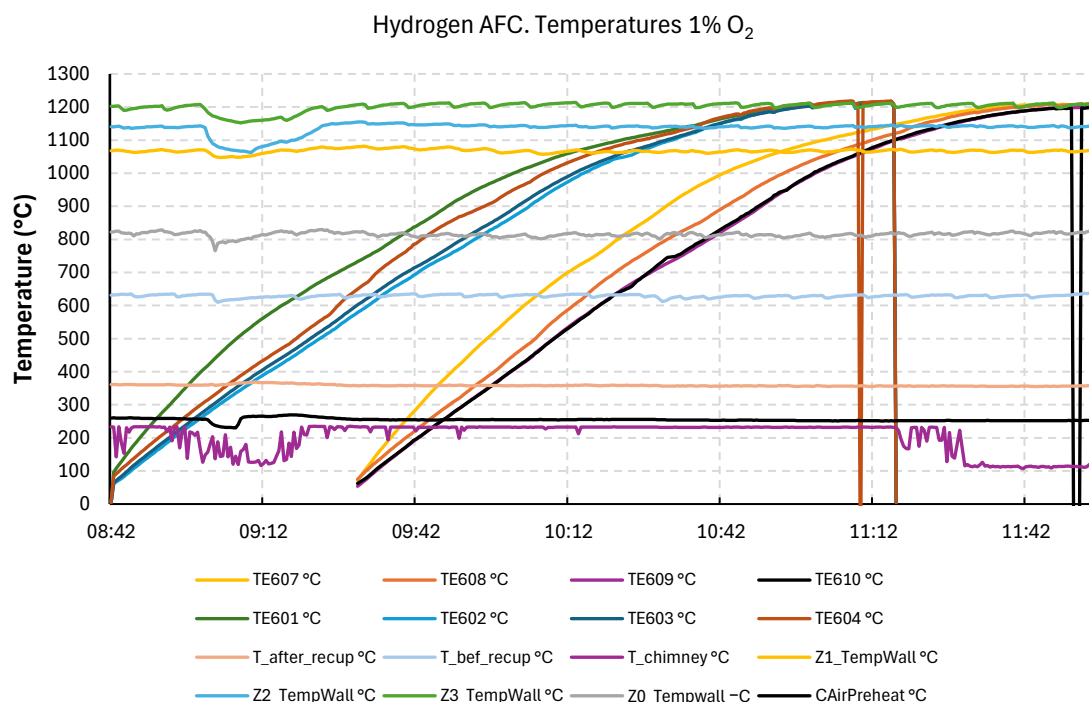


Figure 90. Sample logged test data over temperatures, HAFC 1. ID3.

The next two figures show logged temperature data for hydrogen oxide-enhanced combustion (HOEC) operated with 2.5% and 1% excess oxygen. The temperature profiles demonstrate relatively high and stable temperature levels, indicating enhanced reaction intensity compared to air-fired hydrogen operation. The smooth temporal evolution and limited short-term fluctuations suggest well-controlled heat release and stable thermal conditions during the test. The elevated temperatures are consistent with oxygen enrichment promoting faster kinetics and increased local energy release while maintaining stable operation.

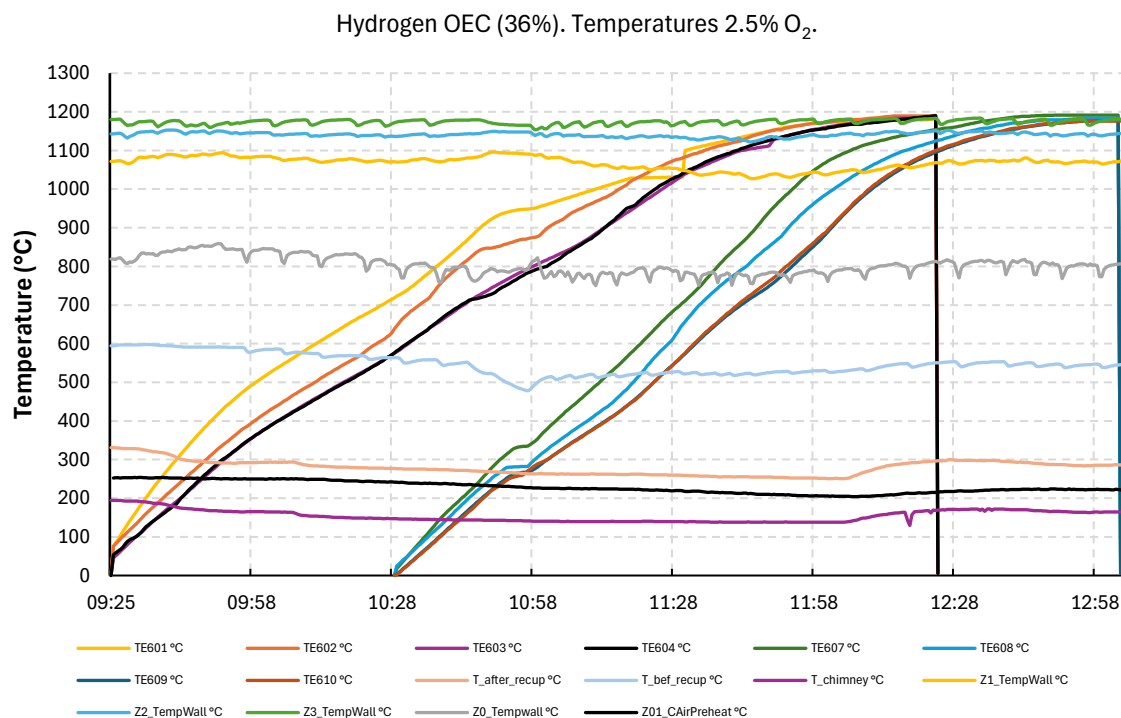
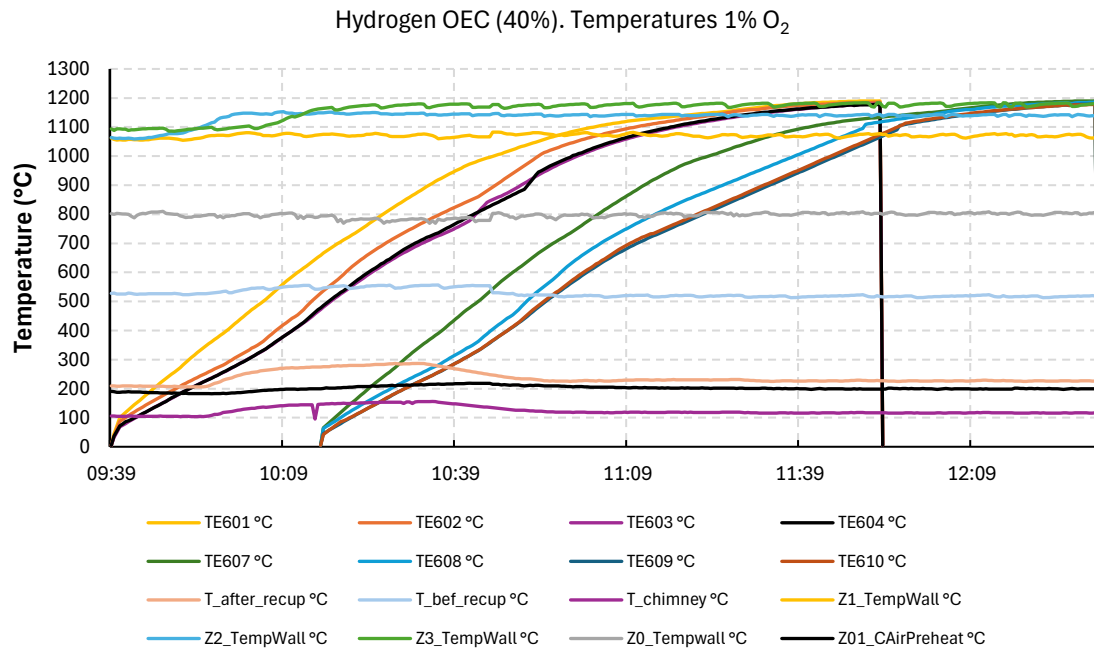
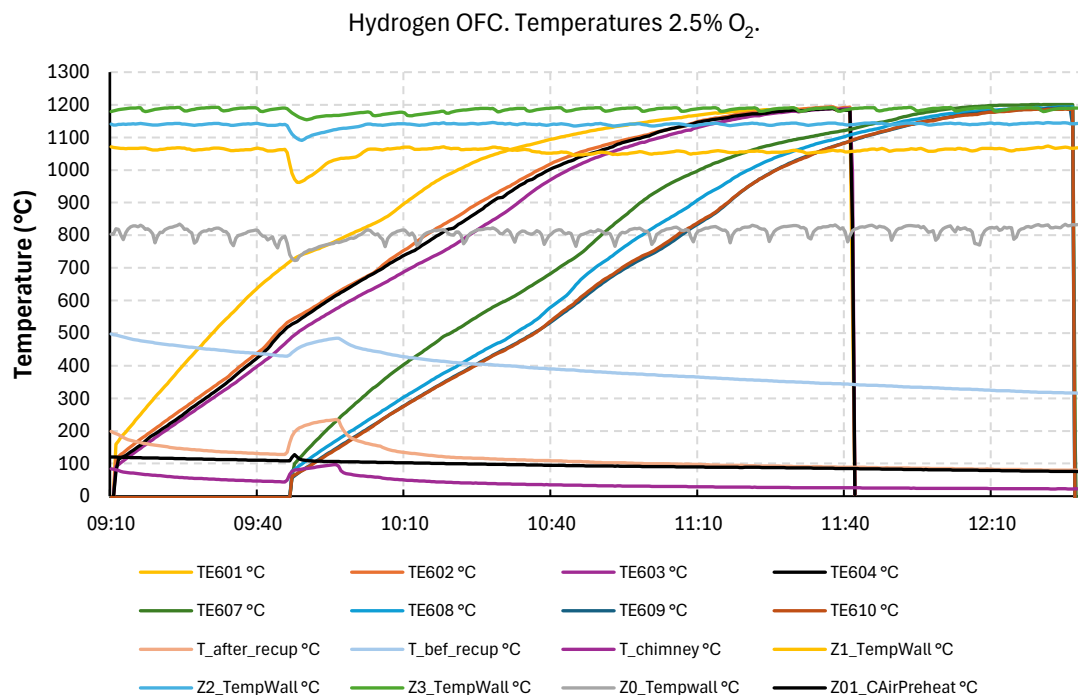


Figure 91. Sample logged test data over temperatures, HOEC 2.5%. ID 6.



The next two figures show temperature evolution for hydrogen oxy-fuel combustion (HOFC) with 2.5% and 1% excess oxygen. The temperatures are comparable to, or slightly higher than, those observed for HOEC at the same oxygen level, reflecting the higher effective oxygen concentration and reduced nitrogen dilution. The profiles remain smooth and continuous, indicating stable combustion despite increased thermal intensity. This suggests that HOFC enables higher energy density operation without introducing unstable heating behaviour.



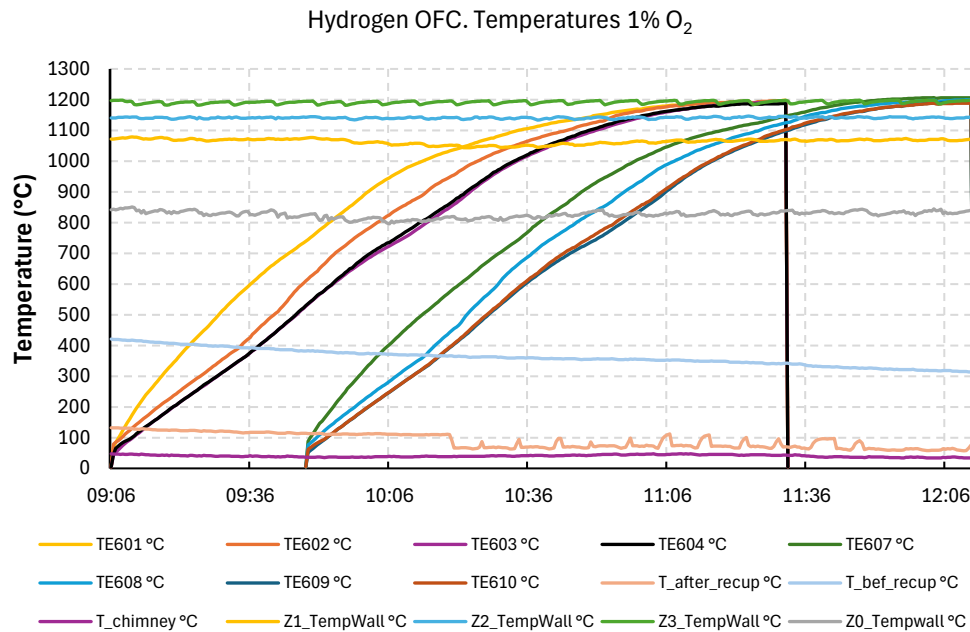


Figure 94. Sample logged test data over temperatures, HOFC 1%. ID 7.

A consistent temperature asymmetry between the top and bottom of the slab was observed, with faster heating on the top surface. This is attributed to the lack of active heating below the slab, which remains a fundamental limitation of the walking beam furnace design.

Importantly, the transition to hydrogen combustion does not inherently resolve this limitation, indicating that furnace design has a stronger influence than fuel type on temperature uniformity.

The next two figures compare thermocouple-measured temperatures for all air-fuel cases at 2.5% and 1% excess oxygen respectively, with TE607 located near the upper slab surface and TE610 positioned at mid-length and mid-thickness. The data reveal systematic temperature differences between near surface and internal slab locations, indicating controlled thermal gradients through the slab thickness. In figure 97, HOEC and HOFC cases exhibit higher temperatures at TE607 compared to air-fuel reference conditions, reflecting increased surface heat flux and faster energy transfer. At TE610, temperature differences are reduced, suggesting effective internal heat redistribution. In figure 96, HOEC-test, experienced a short stop, making the HOEC more difficult to interpret.

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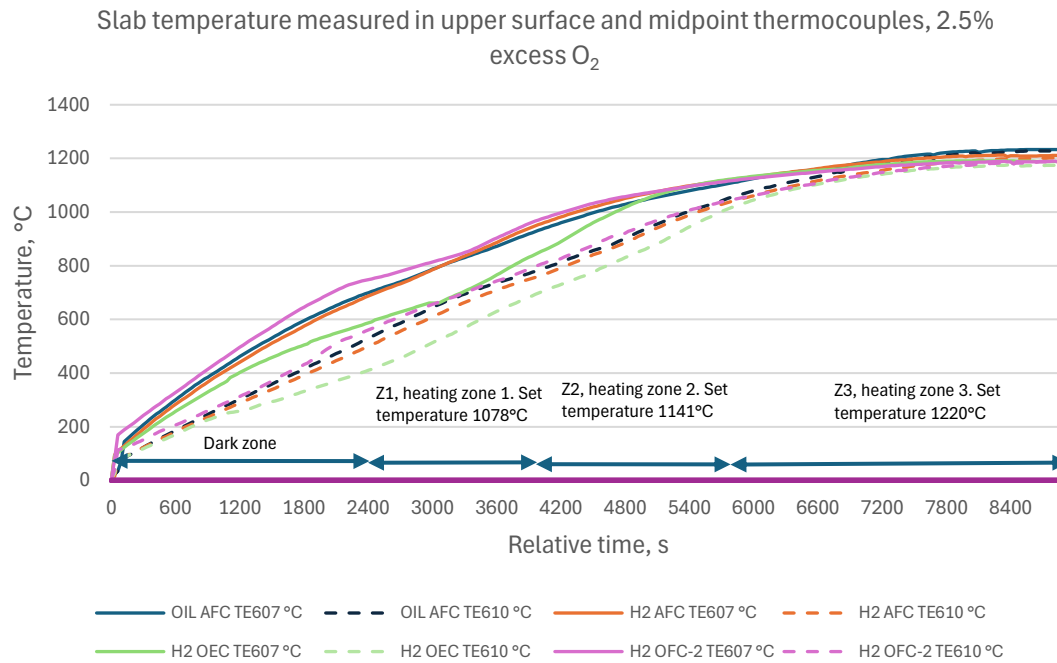


Figure 95. Sample logged test data over thermocouples logged temperatures, relating to zone.

In Figure 95, TE607 is mid slab and 10 mm under upper surface whereas TE610 is mid-length slab mid thickness. All measurements are for 2.5% excess oxygen, and all air-fuel cases are included for comparison.

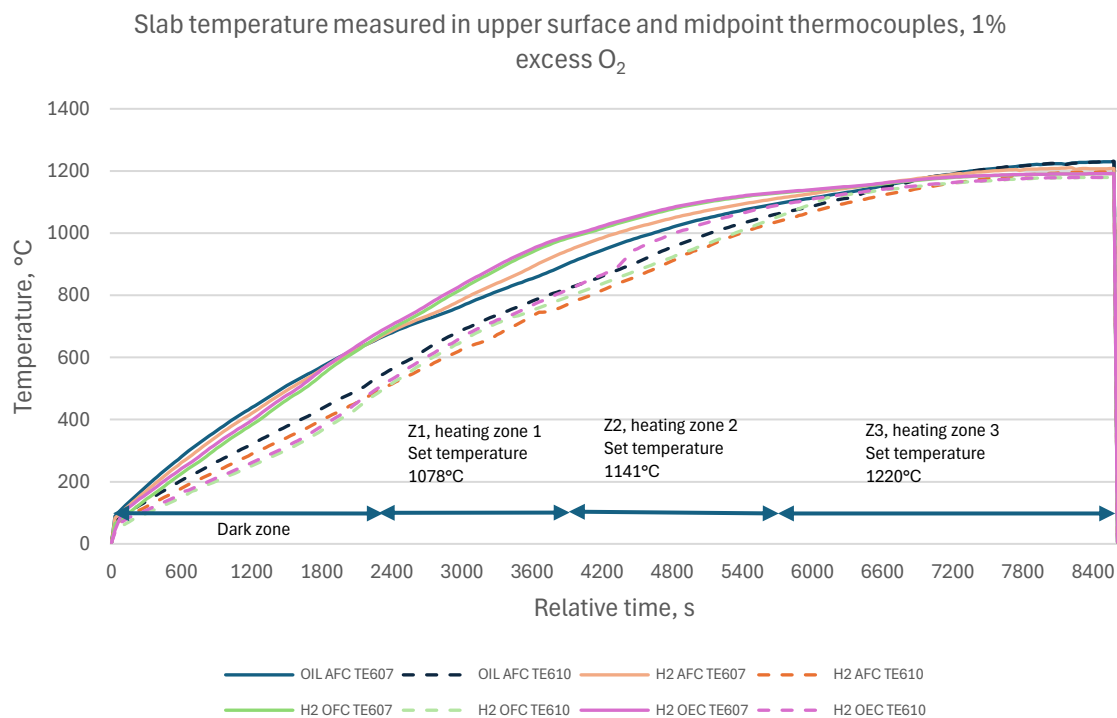


Figure 96. Sample logged test data over thermocouples logged temperatures, relating to zone.

In Figure 96, TE607 is mid slab and 10 mm under upper surface whereas TE610 is mid-length slab mid thickness. All measurements are for 1% excess oxygen, and all air-fuel cases are included for comparison.

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Figure 97 shows the measured excess oxygen distribution across zones for all test cases operated at a nominal 2.5% excess oxygen. The data indicates good agreement between target and measured oxygen levels, confirming stable and well-controlled oxidizer delivery. Minor zonal variations are observed but do not translate into unstable temperature behaviour, Figure 98 presents the measured excess oxygen distribution for all test cases at a 1% setpoint.

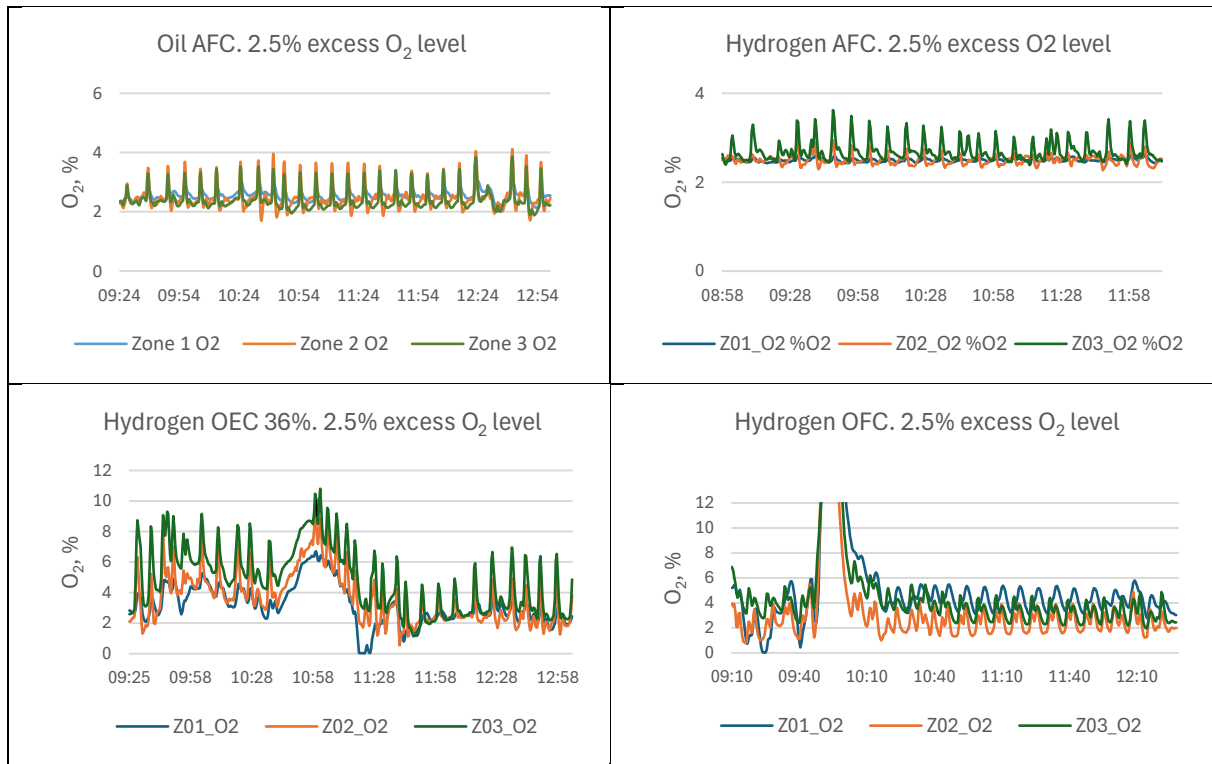


Figure 97. Measured excess oxygen per zone, all test cases, and 2.5% excess oxygen.

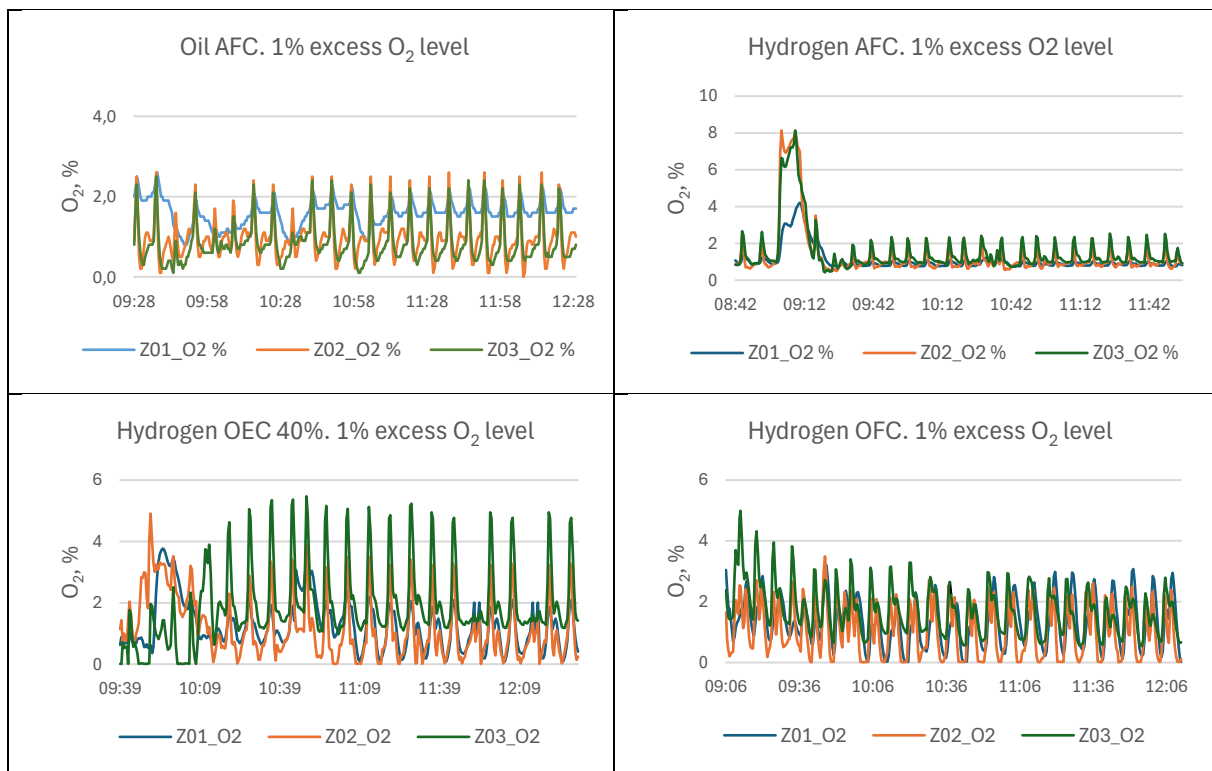


Figure 98. Measured excess oxygen per zone, all test cases, and 1% excess oxygen.

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5.4.1 Energy balance

Figure 99 shows the energy balances for the four test cases. The flue gas loss decreases progressively from oil to hydrogen, from air combustion to H_{OEC} , and from H_{OEC} to H_{OFC} . For the H_{OFC} experiments, there are almost no losses to the flue gas. This also reduced the amount of preheating that occurred in the dark zone. As a result, heating occurred more rapidly throughout zones 1, 2, and 3.

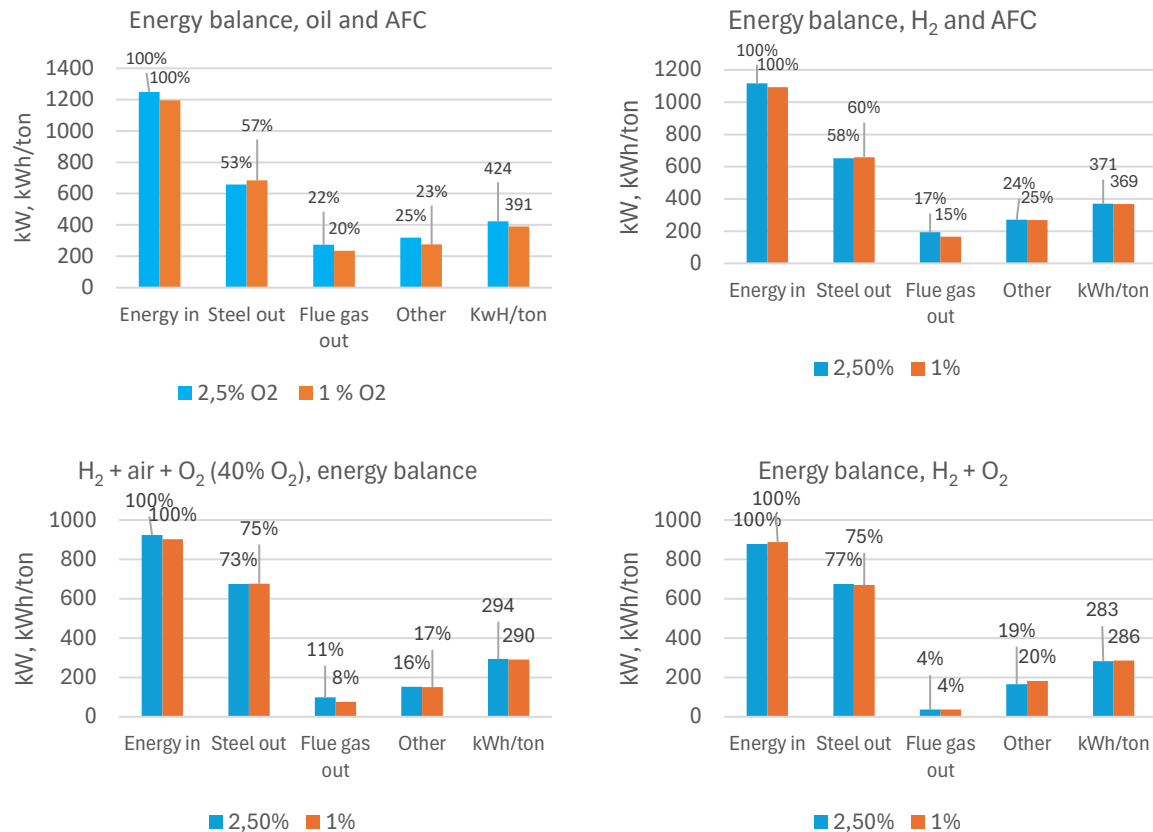


Figure 99. Energy balances for the tests.

5.4.2 Oxide scale growth

The difference between weight increase and the net change for the experiments is shown in figure 101. The net change is the weight gain change minus the descale change. A positive residual value indicates that the slab's weight increased from its starting weight, even after descaling. The weight added is only the oxygen added for the formation of the oxide(s), which means that the Fe part of the oxide weight is not included. A net but still positive change, compared to before heating weight, indicates that a considerable amount of oxides remains, suggesting poor descaling performance. The same can be said if there is a net zero net change comparing weight before heating and after descaling. However, to elaborate further, the remaining metallurgical investigation will provide more insights. The metallurgical investigation is performed by Oulu University and cut out sections of the heated test slabs were sent to them for analysis.

The material forms slightly more scale when hydrogen is used as fuel; however, the difference is not statistically significant. The amount of scale removed during descaling was also comparable and did not indicate a change in descaling difficulty. For both steel grades, scale growth increased for the H_{OEC} and H_{OFC} experiments. For steel grade 362, the weight after descaling indicates greater difficulty in scale removal, particularly in the H_{OEC} and H_{OFC} cases. No statistically significant difference was observed with experiments carried out at 2.5% or 1% excess oxygen. During the tests, the targeted excess O₂ level was maintained, whereas lower flue gas volumes with H_{OEC} and H_{OFC} led to larger fluctuations as material was charged and discharged and the furnace pressure dropped, with temporary spikes that increased the average excess O₂ measured.

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Throughout all tests, the 1% excess oxygen level resulted in less or equal amounts of oxide formation compared to 2.5% excess oxygen.

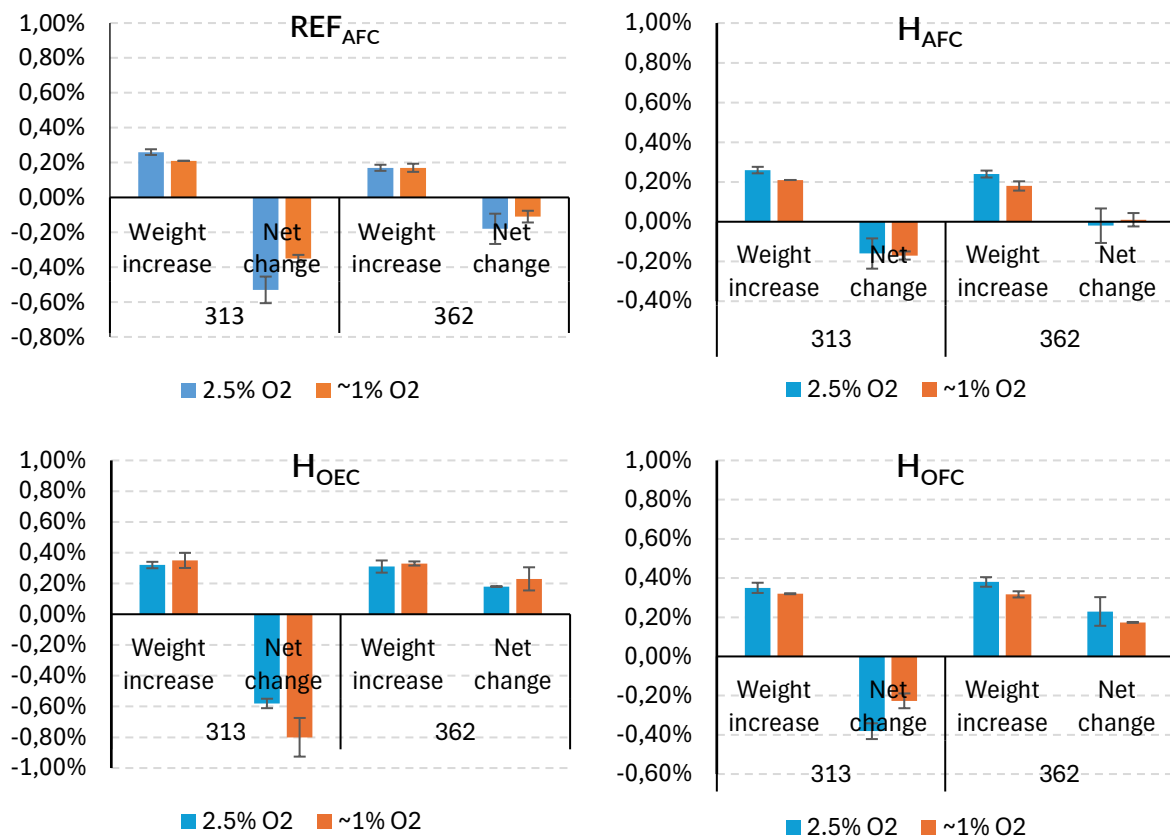


Figure 100. Measurements of oxide growth and removal.

5.4.3 Descaling quality

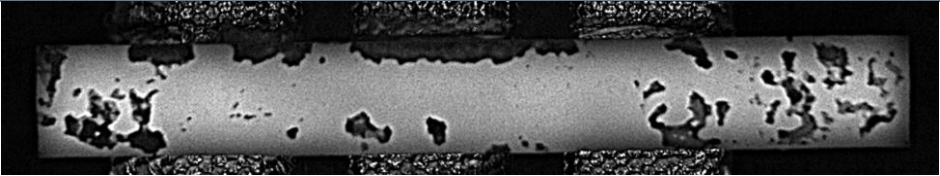
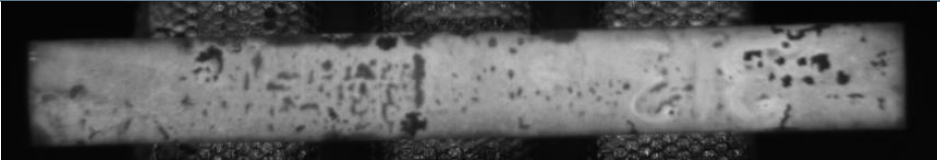
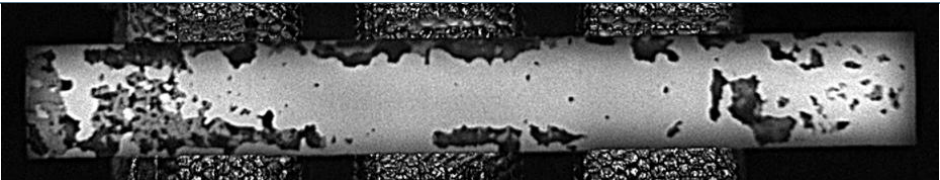
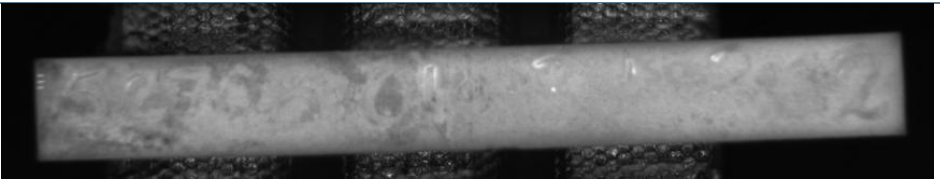
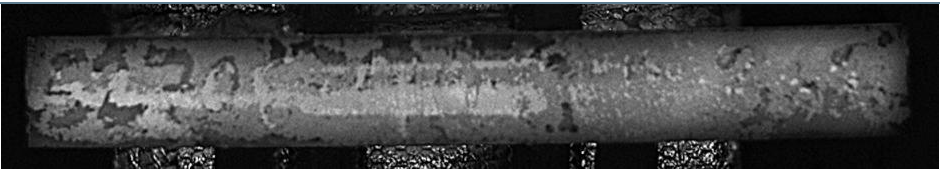
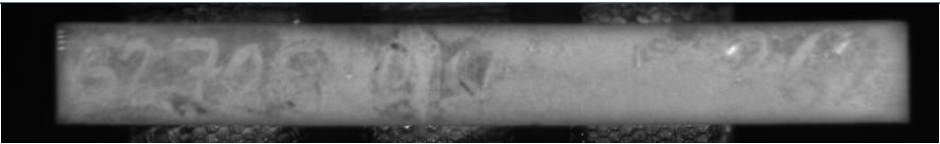
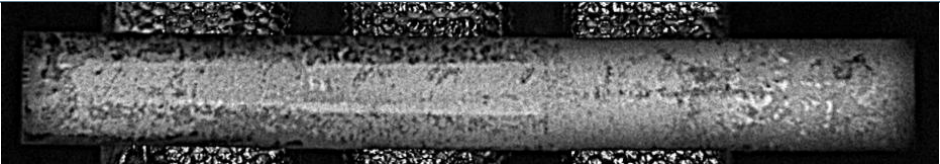
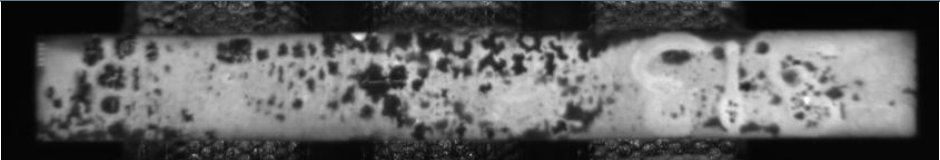
To get a better insight on descaling quality, images were taken after heating, and then again after descaling. Equipment suggestions were provided by SSAB, who provided information about the equipment they use on-site, and the same equipment was purchased and set up. The equipment was:

- Basler asA1300-75gm camera
- FujiFilm DF6HA-1B objective
- Midwest Optical systems BN532-27 green bandpass filter

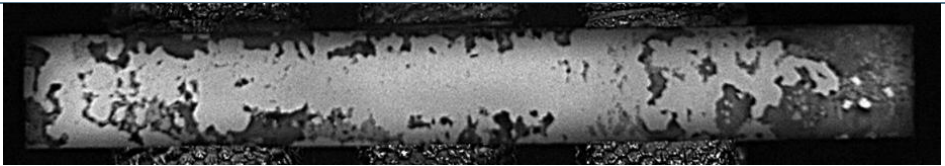
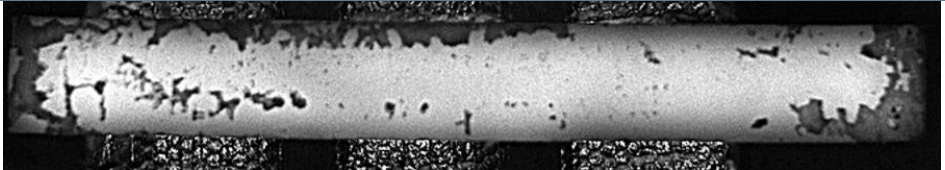
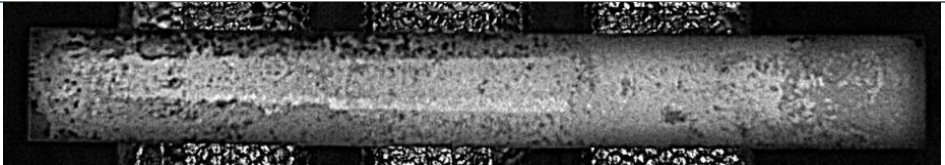
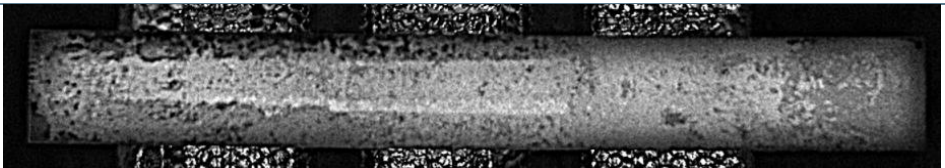
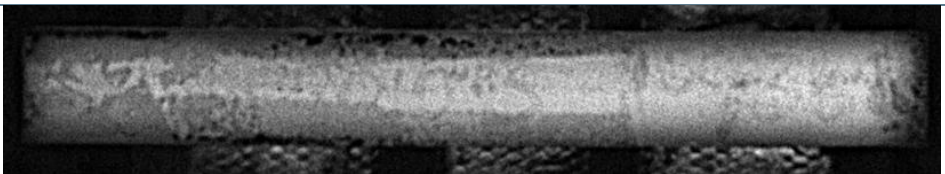
Images were taken using this equipment at Swerim, and the images were also delivered to SSAB for further analysis in their Labview environment which is adjusted for analysis of descale quality. The images from test ID 1 through to ID 8 can be seen in Table 31, one before descaling and one after descaling on each of the four slabs passing. The first two slabs were double samples, slabs, of alloy 313, and the following two slabs were on the other alloy tested, alloy 362. For more insights into the analysis, SSAB will perform their analysis in Labview, and all images were delivered to them.

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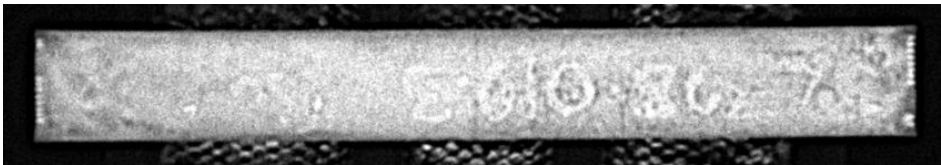
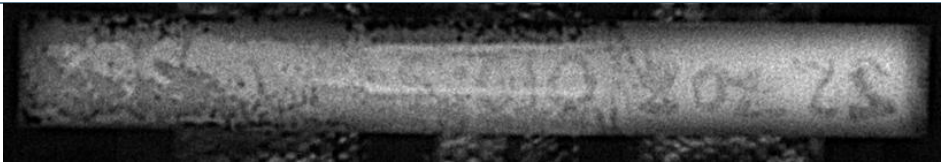
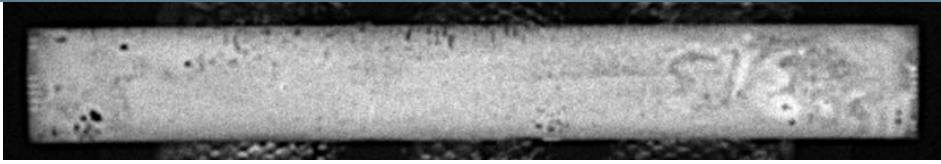
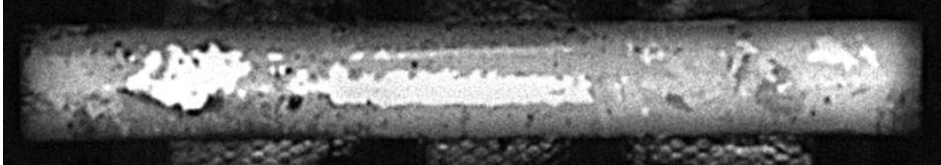
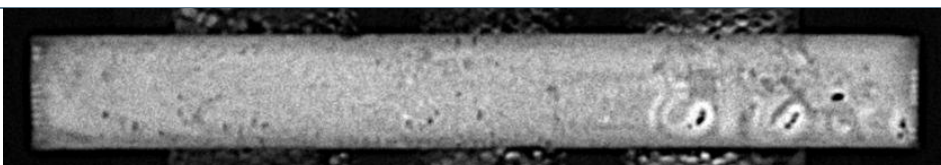
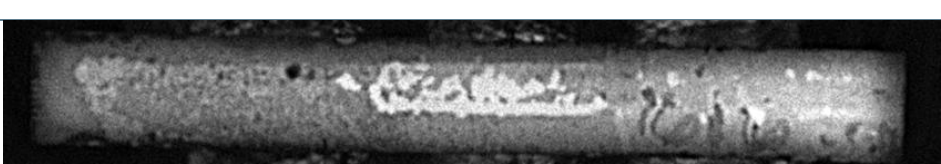
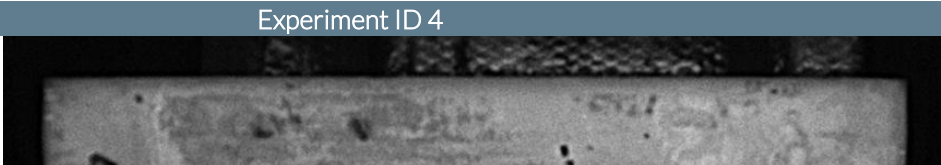
Table 31. Descaling images, after heating and before and after descaling, for test ID1 to ID8

Experiment ID 1	
313-1 before descaling	
313-1 after	
313-2 before descaling	
313-2 after descaling	
362-3 before descaling	
362-3 after descaling	
362-4 before descaling	
362-4 after descaling	
Experiment ID 2	
313-1 before descaling	

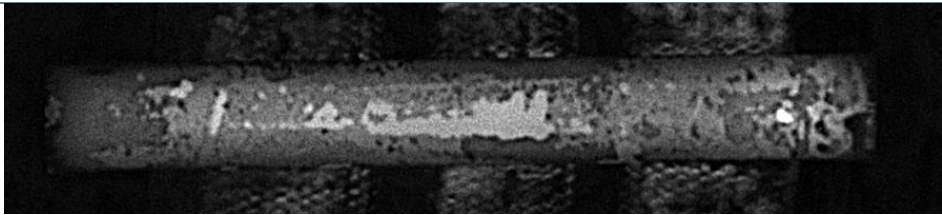
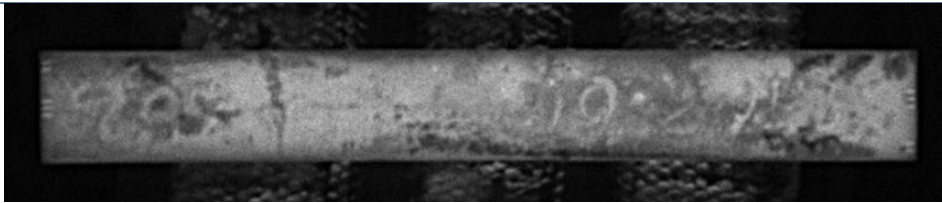
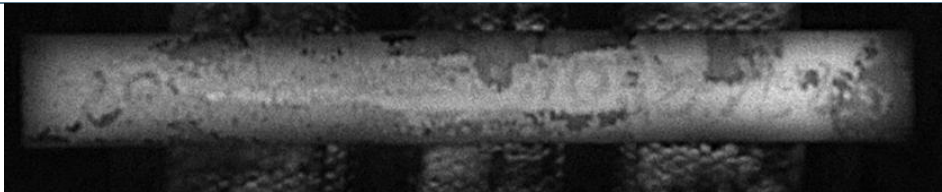
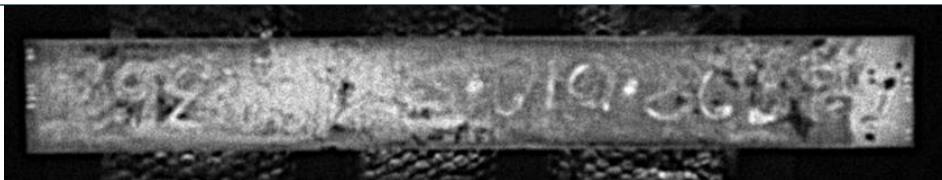
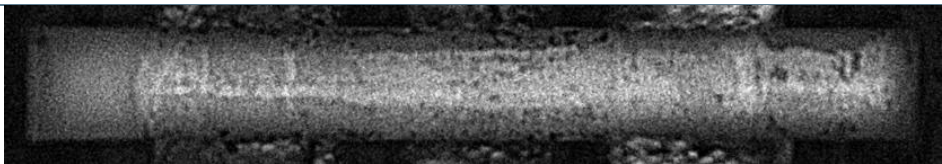
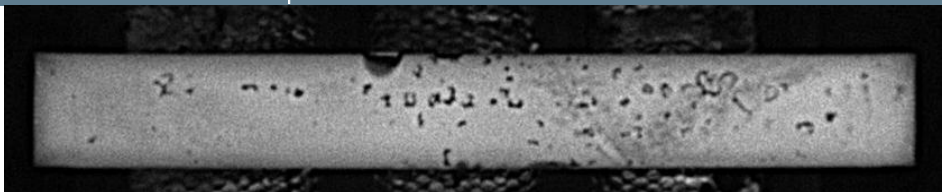
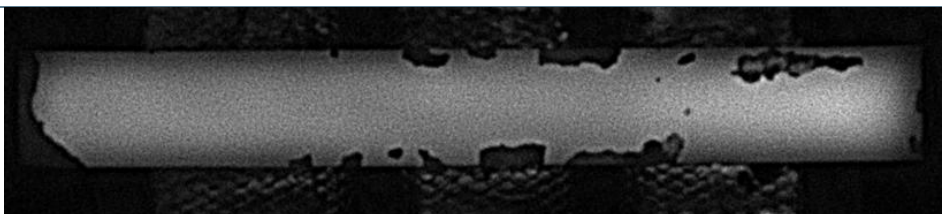
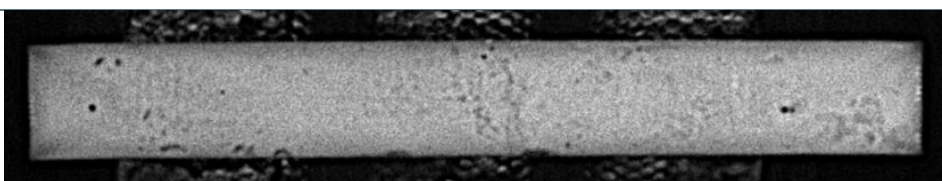
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313-1 after	
313-2 before descaling	
313-2 after descaling	
362-3 before descaling	
362-3 after descaling	
362-4 before descaling	
362-4 after descaling	
Experiment ID 3	
313-1 before descaling	
313-1 after	

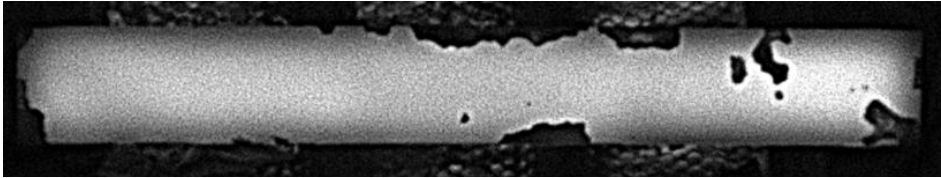
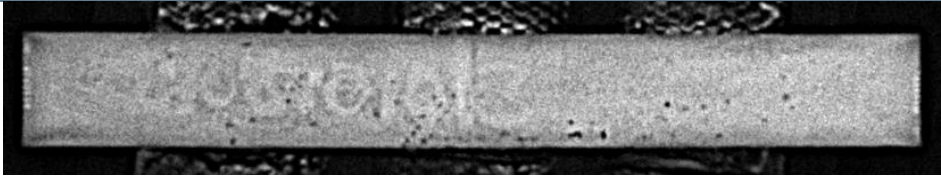
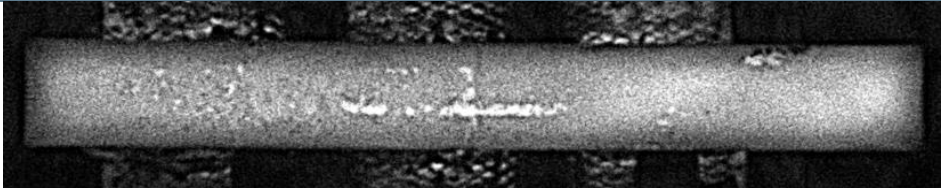
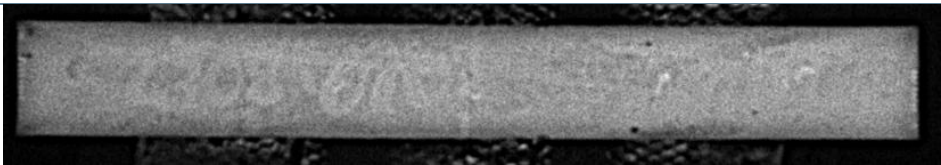
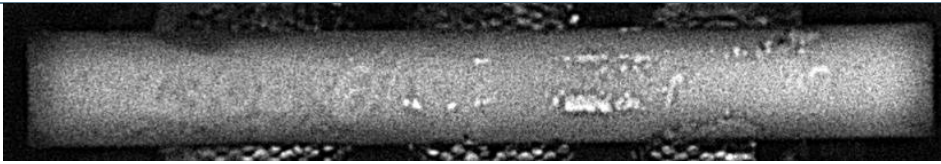
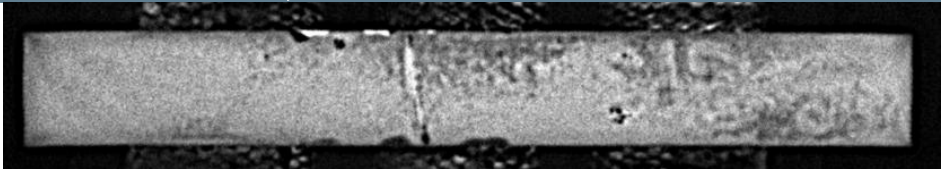
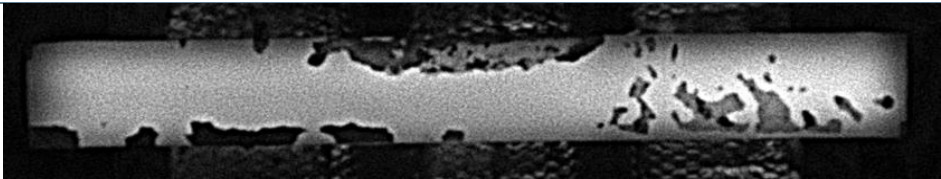
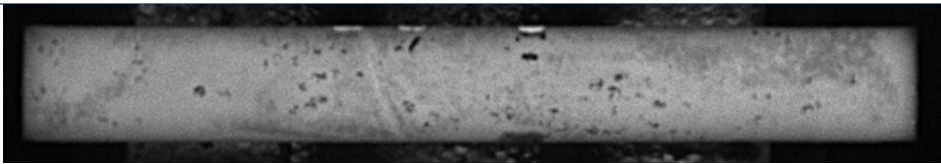
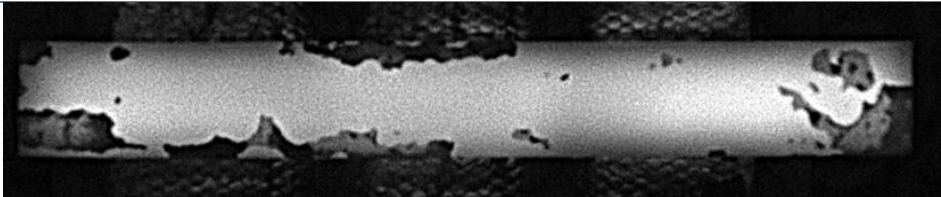
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313-2 after descaling	
362-3 before descaling	
362-3 after descaling	
362-4 before descaling	
362-4 after descaling	
Experiment ID 4	
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313-1 after	
313-2 before descaling	

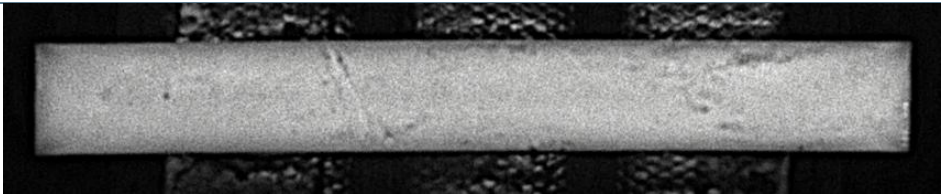
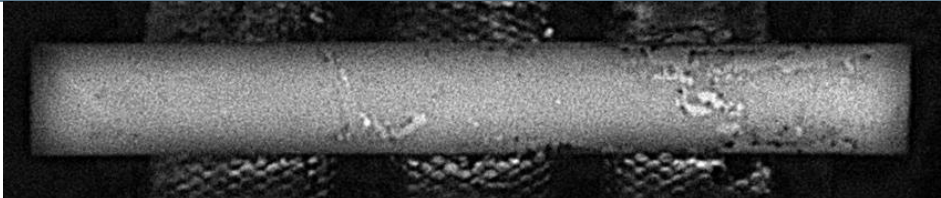
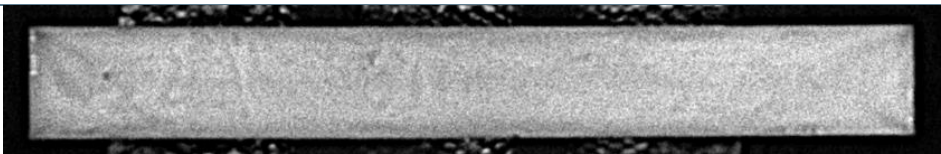
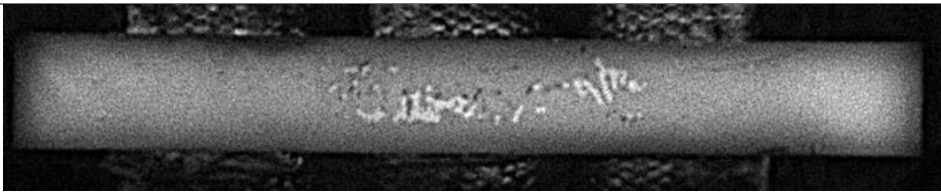
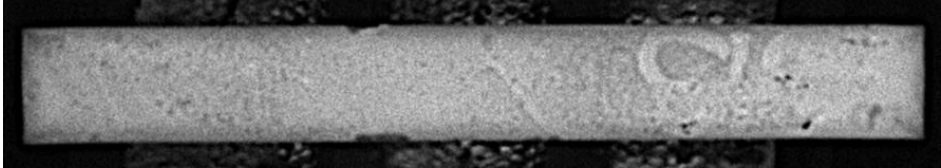
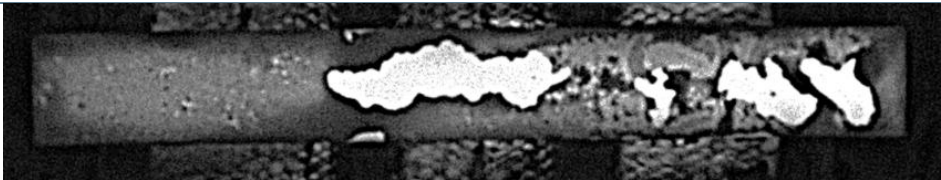
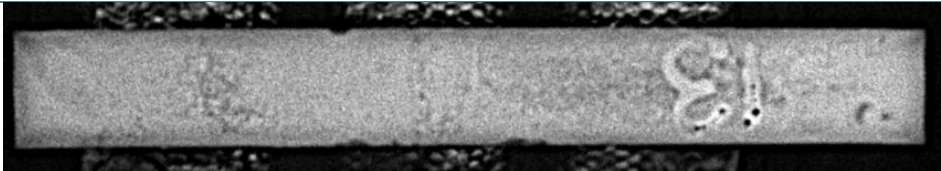
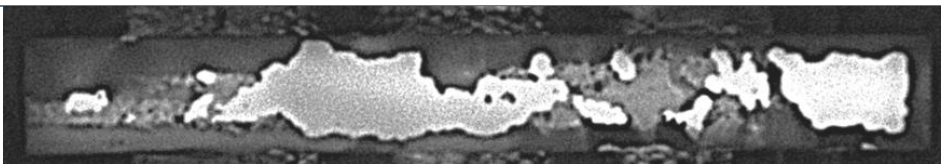
D6.3 Report on results of the steel use case implementations

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362-3 after descaling	
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Experiment ID 5	
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313-2 before descaling	

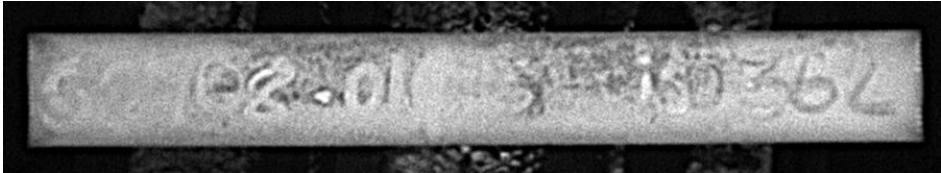
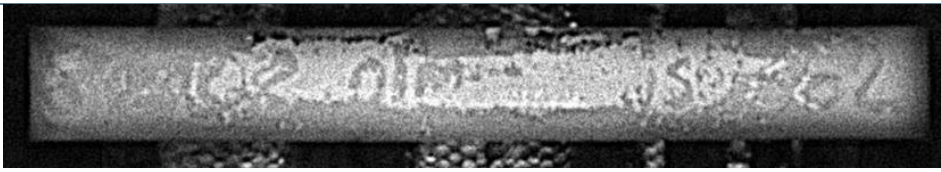
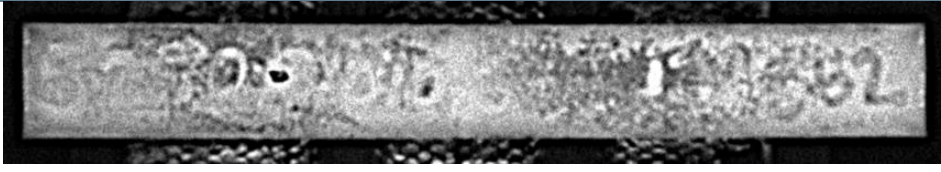
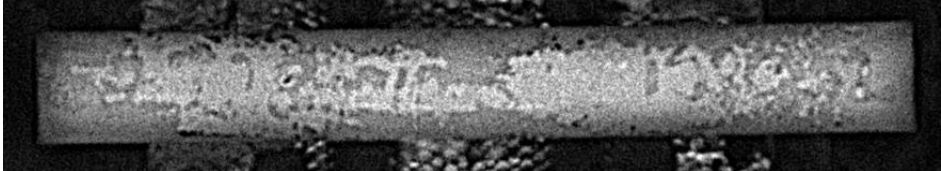
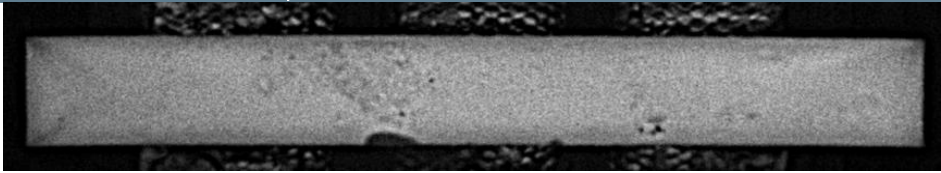
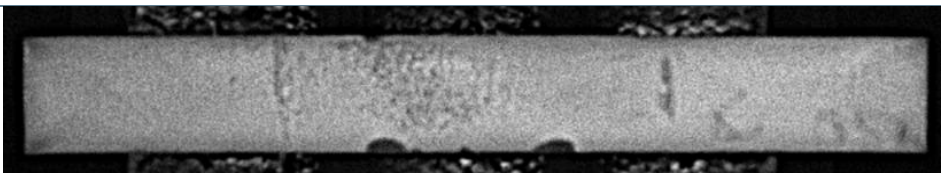
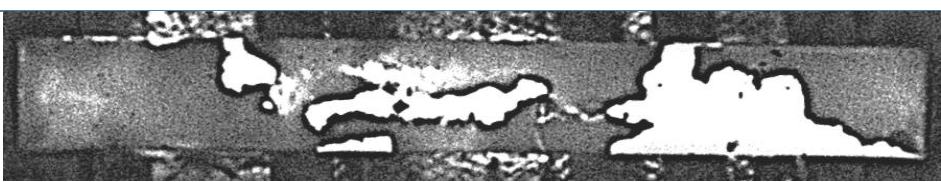
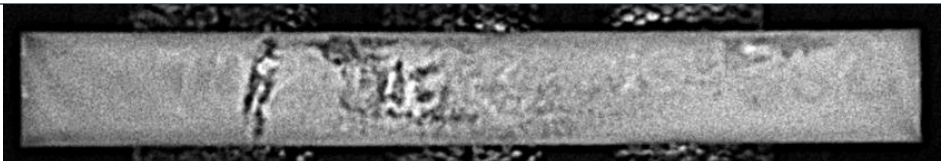
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362-3 after descaling	
362-4 before descaling	
362-4 after descaling	
Experiment ID 6	
313-1 before descaling	
313-1 after	
313-2 before descaling	
313-2 after descaling	

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362-3 before descaling	
362-3 after descaling	
362-4 before descaling	
362-4 after descaling	
Experiment ID 7	
313-1 before descaling	
313-1 after	
313-2 before descaling	
313-2 after descaling	

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362-3 before descaling	
362-3 after descaling	
362-4 before descaling	
362-4 after descaling	
Experiment ID 8	
313-1 before descaling	
313-1 after	
313-2 before descaling	
313-2 after descaling	
362-3 before descaling	

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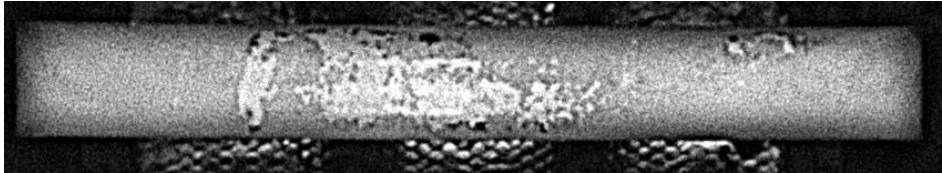
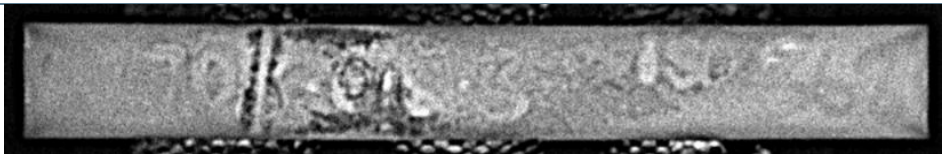
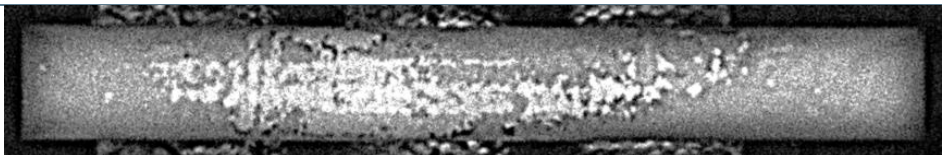
362-3 after descaling	
362-4 before descaling	
362-4 after descaling	

Table 32 provides an overview of the experimental results, which indicate a similar energy performance between hydrogen and light fuel oil when used with preheated air.

Energy consumption was significantly reduced by using oxygen-enhanced combustion (H_{OEC}) and oxygen fuel combustion (H_{OFC}). The final steel temperature was lower in all hydrogen tests because, during the overhaul, the set-point temperature in zone 3 was accidentally switched from the wall thermocouple to the roof thermocouple. This had a small but clear impact on the final material temperature, as the actual wall temperatures were below their intended setpoint.

The dark-zone temperature varied little across the experiments, which was unexpected given the lower flue gas flows in H_{OFC} and H_{OEC} . A potential reason is the thermocouple's location in the dark zone near the end of the first heating zone (zone 1). Even though the flue gas flow was lower, it likely did not have time to cool significantly in such a short period. However, upstream of the recuperator in the exhaust channel, there is a marked drop in temperature across all hydrogen tests, with H_{OFC} achieving the lowest value, as was expected. The stack temperature shows the same trend. For the H_{OEC} , the temperature remains above the condensation point, whereas for the H_{OFC} it is clearly below the condensation point, and condensation has occurred in the stack. This did not cause issues during the experiment but could cause problems if the experiment lasted longer.

The average fuel load decreased by about 25-30% in the H_{OEC} and H_{OFC} cases compared to the reference case, whereas the H_{AFC} case with hydrogen showed a 10% decrease. Efficiency increased by approximately 20% for H_{OEC} and H_{OFC} , with the difference between the two cases being marginal. The same applies to specific energy consumption: there is a 5-10% improvement with hydrogen H_{AFC} , whereas the main impact is with H_{OEC} and H_{OFC} (28-33%), with the last two showing marginal differences.

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Table 32. Overview of average data from experiments

Experiment ID	1	2	3	4	5	6	7	8
Final steel T, °C	1228	1225	1216	1210	1185	1191	1200	1195
ΔT steel, °C	8	8	15	12	9	12	5	8
Zone T, °C								
Dark zone	857	867	855	851	816	824	813	821
Zone 1	1074	1072	1067	1060	1071	1072	1064	1074
Zone 2	1142	1143	1142	1143	1140	1143	1142	1141
Zone 3	1222	1223	1207	1215	1202	1205	1188	1192
Exhaust T, °C								
Before recuperator	699	708	629	637	518	546	361	360
After recuperator	357	368	357	350	228	290	89	96
Stack T, °C	256	268	208	221	117	167	40	29
Weight gain after heating, %								
313	0,2	0,26	0,26	0,21	0,32	0,35	0,32	0,35
362	0,17	0,17	0,24	0,18	0,31	0,33	0,32	0,37
Weight loss descaling, %								
313	0,55	0,78	0,41	0,37	1,16	0,89	0,54	0,73
362	0,27	0,34	0,26	0,18	0,1	0,13	0,14	0,14
Total weight loss, %								
313	0,35	0,53	0,16	0,17	0,58	0,8	0,23	0,38
362	0,11	0,18	0,02	-0,01	-0,18	-0,46	-0,35	-0,46
Excess O ₂ , % (wet basis)								
Zone 1	1,59	2,5	0,9	2,5	1,3	2,5	1,35	4,2
Zone 2	0,97	2,5	1	2,5	1	2,4	1,07	2,4
Zone 3	0,83	2,4	1,2	2,7	2,2	3,5	1,72	3,4
Fuel load, kW								
Zone 1	579	576	546	543	531	537	543	544
Zone 2	287	295	267	296	210	222	192	175
Zone 3	330	379	279	277	162	165	152	159
Total	1195	1250	1092	1116	903	924	887	878
Change compared to reference case, %	0	0	-9	-11	-24	-26	-26	-30
Efficiency, %	57	53	60	58	75	73	75	77
Relative improvement compared to reference case, %	0	0	-3	-5	-18	-20	-18	-24
Steel out, kW	684	657	658	652	676	674	669	675
Flue gas loss, kW	240	302	194	165	76	98	37	37
Other losses, kW	271	290	240	299	151	152	181	166
Specific energy kWh/ton	392	423	369	371	290	294	286	283
Change compared to reference case, %	0	0	-6	-12	-25	-31	-29	-33
Throughput, ton/h	3,05	2,95	2,96	3,01	3,11	3,14	3,1	3,11

5.4.4 Dark-zone (Z0) temperature behaviour and combustion mode effects

Measured dark-zone (Z0) temperatures for the investigated combustion cases at constant wet-basis excess oxygen (2.5 % and 1 %) are summarized in Table 33.

Table 33. Dark zone temperature T_{Z0} for the different tests

Case	T_{Z0} (°C) 2.5%	T_{Z0} (°C) 1%
Oil AFC	867	857
H ₂ AFC	851	855
HOEC	824	816
HOFC	821	813

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The observed ranking of Z0 temperatures follows the expected physical behaviour under wet-basis oxygen control. Oil–air combustion exhibits the highest Z0 temperatures, primarily due to luminous radiation from soot-forming hydrocarbon flames, despite the strong dilution effect of nitrogen in air combustion.

For hydrogen-based combustion, flames are essentially soot-free and radiative heat transfer is dominated by molecular gas radiation, mainly from H₂O. As the combustion mode transitions from hydrogen–air to oxygen-enhanced and finally to oxyfuel operation, the concentration of radiatively active species increases while the total flue-gas mass flow decreases.

The modest reduction in Z0 temperature observed between hydrogen–air, oxygen-enhanced, and oxyfuel cases can be attributed to a combination of:

- reduced convective heat transfer due to lower flue-gas flow rates,
- altered flame geometry and flame–wall interaction,
- elimination of nitrogen dilution, which reduces sensible exhaust enthalpy.

Importantly, these results demonstrate that higher furnace heating effectiveness does not require higher dark-zone gas temperatures.

5.4.5 Heating effectiveness versus radiation source characteristics

When the Z0 temperature data are evaluated together with the aggregate energy balance results shown in Table 5, a clear distinction between gas temperature and heating effectiveness emerges.

Oil–air combustion achieves high radiative intensity in the Z0 region due to luminous flames but suffers from significant energy losses through large flue-gas volumes and high exhaust enthalpy. In contrast, hydrogen oxyfuel combustion fundamentally shifts the furnace energy balance:

- Lower gas temperatures in the dark zone,
- Substantially higher radiative effectiveness per unit fuel input,
- Drastically reduced flue-gas and exhaust losses.

As a result, final slab discharge temperatures are slightly lower for hydrogen oxyfuel cases, while the specific energy consumption is markedly improved. This indicates a higher fraction of the supplied energy is effectively transferred to the steel rather than lost with the exhaust gases.

The results indicate that the furnace operates in a radiation-dominated regime, where:

- Furnace walls act as the primary radiative sources
- Slabs absorb heat predominantly from wall radiation
- Gas radiation plays a secondary role

This is consistent with high-temperature furnace physics, where radiative heat flux is proportional to: $q \propto \epsilon \sigma T^4$. Thus, small differences in wall temperature dominate over variations in gas composition. Hydrogen combustion produces flue gases with high H₂O concentration and low CO₂ content.

Both H₂O and CO₂ are strong infrared emitters, but H₂O has broader absorption/emission bands and potentially higher contribution to gas radiation

However, under the present conditions, especially considering that MILD combustion is used for H_{OE}C and H_{OF}C giving lower and more evenly distributed flame temperatures, it is probable that gas radiation remains secondary to wall radiation and increased H₂O content does not significantly impact slab heating

5.4.6 Specific energy consumption and throughput

Despite the reduction in Z0 gas temperatures for oxygen-enhanced and oxyfuel hydrogen combustion, and the fact that the steel throughput is maintained the specific energy consumption is markedly decreased. Thermocouple data from the slabs confirm that heating curves remain sufficient to meet the targeted discharge conditions.

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This result is experimentally robust and demonstrates that radiative heat-transfer efficiency and reduced exhaust losses compensate for lower furnace gas temperature in the dark zone.

5.4.7 Summary results

The following two figures summarise some important KPI's for 2.5% and 1% excess O₂-levels. A clear trend can be seen of decreasing fuel consumption, increased efficiency and decreased specific energy consumption for both cases, progressing from Oil_{AFC} to H_{AFC} and then further to H_{OEC} and H_{OFC}. The temperature homogeneity is comparable across all cases, comparing slab exit temperature across all measured locations, see figure 84 for thermocouple placements.

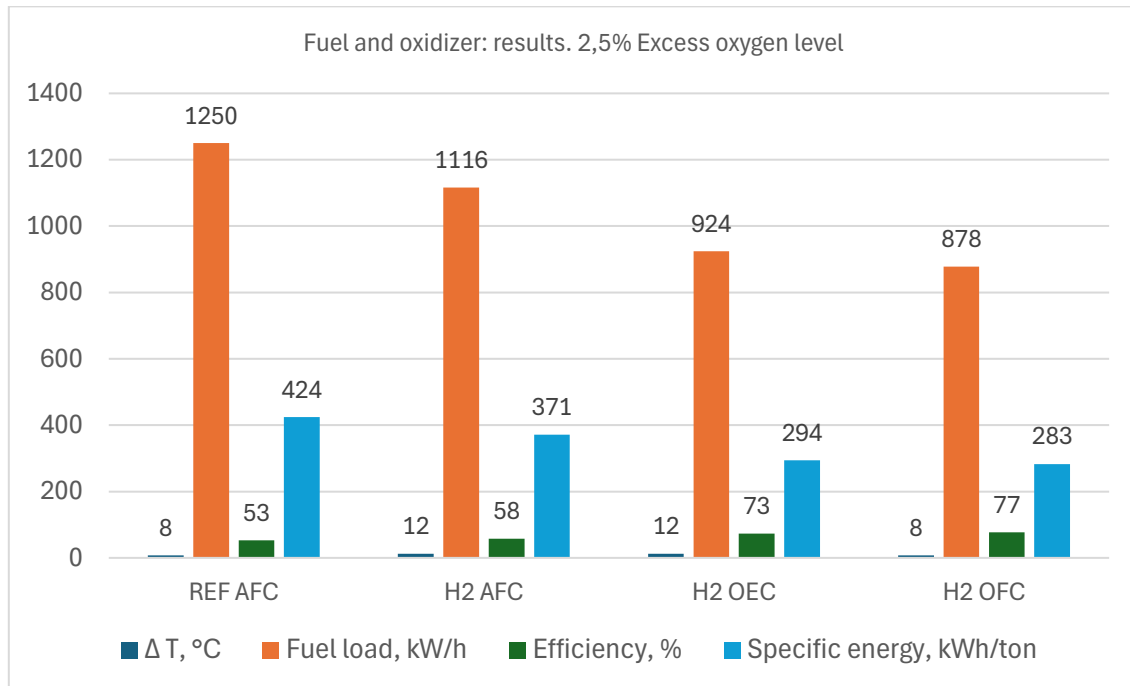


Figure 101. Some important KPI's summarized over all cases, 2.5% excess O₂-level.

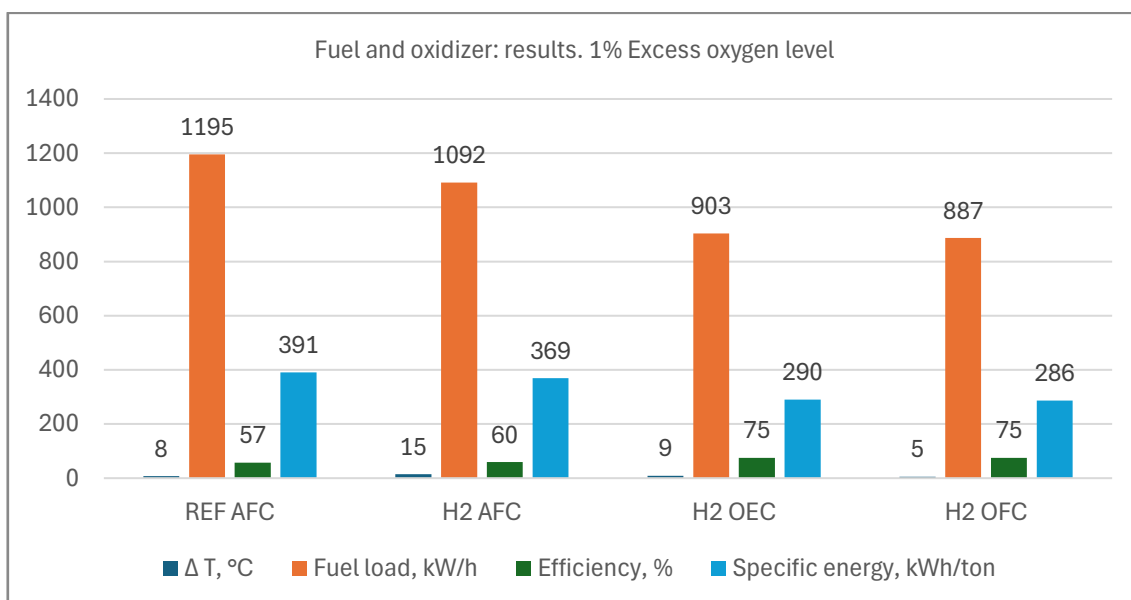


Figure 102. Some important KPI's summarized over all cases, 1% excess O₂-level.

The next two figures illustrate the qualitative energy distribution across combustion modes. Oxygen-enriched and oxy-fuel combustion reduces flue gas losses by decreasing nitrogen dilution, thereby increasing

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the fraction of useful heat transferred to the steel. This supports the observed improvements in energy utilization

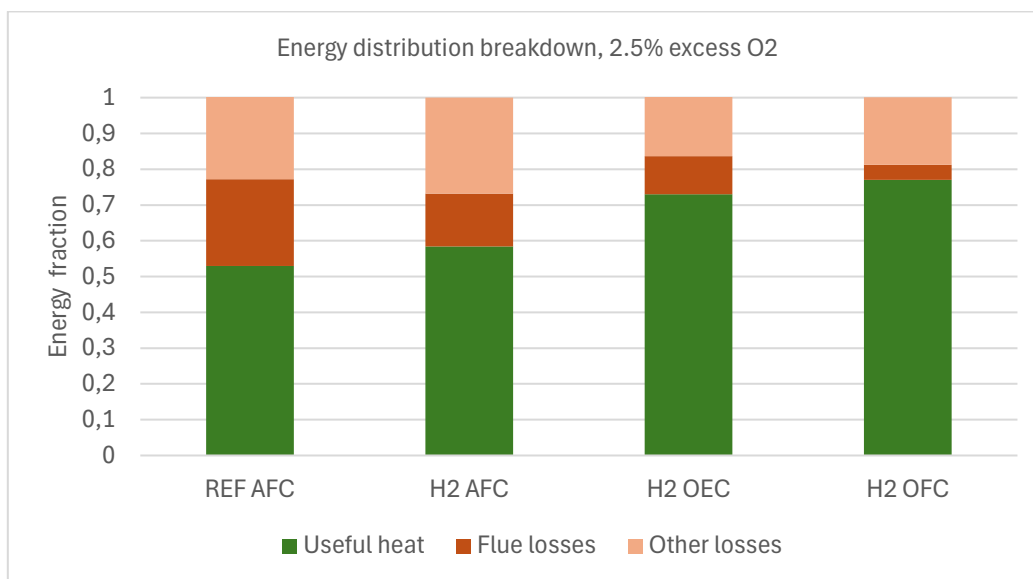


Figure 103. Qualitative energy distribution across combustion modes, 2.5% excess O₂-level.

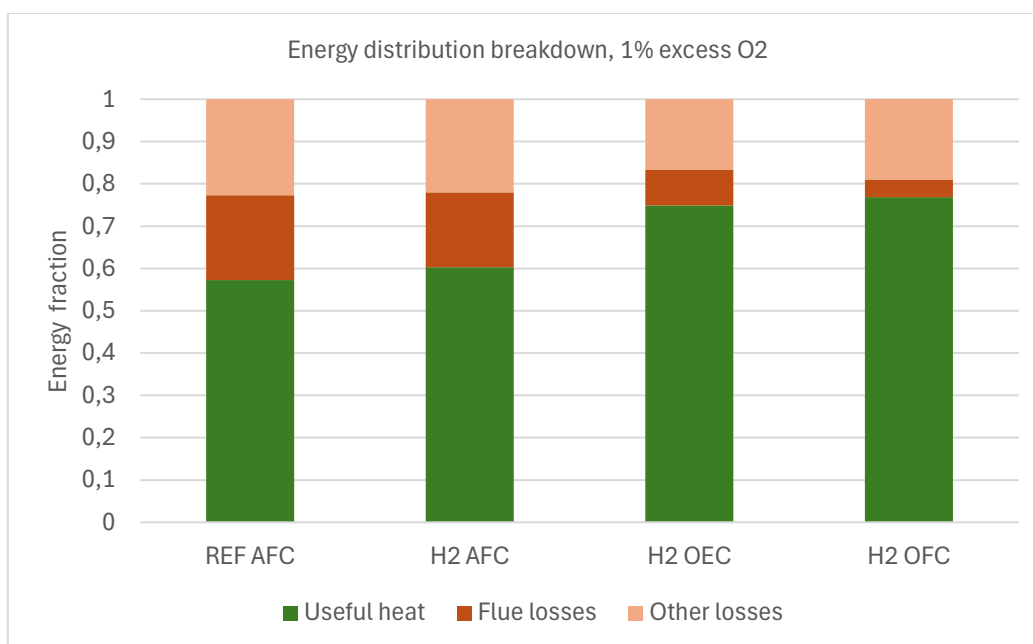


Figure 104. Qualitative energy distribution across combustion modes, 1% excess O₂-level.

5.5 Conclusions

This study demonstrates the technical feasibility and performance implications of using hydrogen as a fuel in a pilot-scale walking beam furnace under various combustion strategies. Key findings include:

For the experiments with preheated air, switching from light fuel oil to hydrogen resulted in a fuel energy savings of ~10%, an increased furnace efficiency of 3-5%, and a decrease in specific energy consumption by 6-12%. The switch to hydrogen also resulted in a slightly higher, but not statistically significant, increase in scale growth for both steel grades.

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These gains are achieved without higher dark-zone gas temperatures. The governing mechanisms are instead:

- reduced flue-gas dilution,
- reduced exhaust enthalpy losses,
- improved gas–solid radiative heat transfer per unit fuel input.

Oxygen-enhanced (H_{OEC}) and oxyfuel combustion (H_{OFC}) required lower fuel energy inputs than air combustion due to lower sensible heat loss from the flue gas. Considering fuel consumption, efficiency, and specific energy consumption, there are only small differences between H_{OEC} and H_{OFC} at a total O_2 level of 40% (POP60). Compared to the reference case, H_{OEC} and H_{OFC} with hydrogen improve efficiency by ~20%, reduce fuel consumption by ~25-30%, and decrease specific energy consumption by ~30%. It also resulted in a significant increase in scale growth and more difficult decaling. For all fuel and oxidation mixtures, lowering the excess oxygen content in the flue gas from 2.5% to ~1% slightly improved energy performance but had minimal effect on scale formation or descaling.

The tests and subsequent analysis of the results of the tests demonstrates that hydrogen combustion can be successfully integrated into walking beam furnaces, with performance primarily governed by radiative heat transfer conditions, while oxygen enrichment and oxyfuel combustion markedly increase process efficiency and reduces energy losses.

6 Task 6.3: Ladle Preheating with H_2/O_2 Combustion – Simulation by BSC

6.1 Description of the problem

Computational fluid dynamics (CFD) simulations were carried out to analyse the heating of a ladle from ambient temperature to operational conditions. The process is driven by heat exchange between the ladle walls and the hot products generated from CO/O_2 and H_2/O_2 combustion. In the configuration considered, the reactants are injected separately through an annular injector, with the hydrogen supplied through the centre, the carbon monoxide through one annulus and pure oxygen through a second concentric annulus. The high injection velocities promote intense mixing and, after initial ignition, lead to stable combustion. The large momentum of the reacting jet transports the combustion products through the interior of the ladle, closed by a lid, progressively heating the system until the required operating conditions are reached. The nozzles are mounted at the top of the ladle with the jet directed downwards. Exhaust gases leave the chamber through an opening in the lid. The global equivalence ratio in both CO/O_2 and H_2/O_2 mixtures is the same and close to 0.8. The thermal power of both mixtures is similar and around 1 MW even the CO/O_2 mixture operates at around 20% higher power than that of the H_2/O_2 mixture. A representation of one half of the ladle, corresponding to the side where the outlet is located, is shown below:

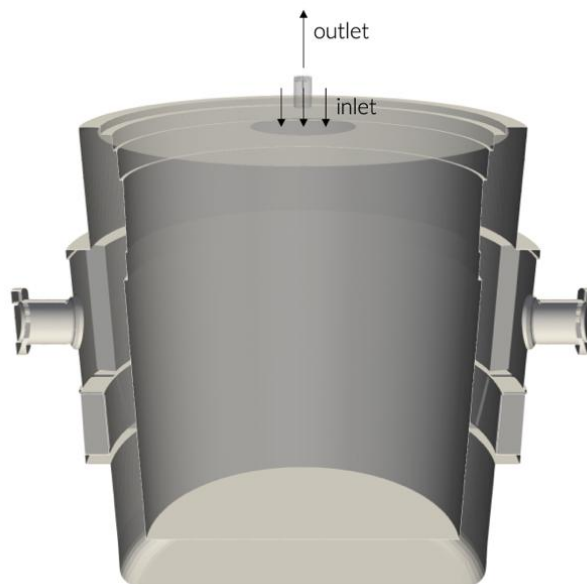


Figure 105. Representation of one half of the ladle.

6.2 Modelling strategy and numerical description

The problem is solved in the frame of Large Eddy Simulations (LES). While this approach offers some advantages in terms of the accuracy of the simulations, it also presents some limitations when used for the current problem. Due to the high temporal scales associated to the heating of the ladle (in the order of hours) and the significant separation between the fluid-dynamic and solid-heating time scales, simultaneously resolving both the combustion process and the thermal evolution of the ladle is impractical. Consequently, a modelling strategy based on such scale separation is developed:

- a. First the combustion problem is solved in adiabatic conditions without simulating the heating of the ladle (no fluid-solid thermal coupling). A fictional surface, surrounding the jet, where the thermal state of the products and velocities are locally registered and saved in a database is created. Such database gathers these quantities as a function of time and space (over the surface). This auxiliary surface is chosen big enough to assure that combustion is fully encapsulated in its interior volume but small enough to not artificially distort the flow in the subsequent thermally coupled simulation.

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As simulations are solved in the frame of LES, instantaneous quantities were saved in the database instead of averaged ones to not artificially laminarize the flow in the thermally coupled simulations (the fictional surface acts as an inlet in such simulations). The database was created for a period of 250 and 112 ms for CO/O₂ and H₂/O₂ cases, respectively.

- b. A second simulation is then performed in which the fluid and the ladle are thermally coupled through a conjugate heat transfer formulation (Houzeaux et al., 2024). In this case, the volume enclosed by the auxiliary surface defined in step a) is removed from the computational domain. The flow quantities previously recorded on this surface during the combustion simulation are imposed cyclically as boundary conditions, thereby reproducing the effect of the reacting jet without explicitly resolving the combustion process. As a result, the flow solved in this simulation is chemically inert, although density variations arising from temperature changes are still accounted for. Several important aspects of this approach should be highlighted:
 - Although thermal radiation is expected to be a major contributor to the overall heat transfer, it is not included in the present simulations due to the complexity associated with its accurate modelling and the lack of a suitable radiation solver. Consequently, convective heat transfer is the sole mechanism considered, which may lead to deviations from the actual ladle preheating process.
 - Due to entrainment of the surrounding gases into the burner jet, part of the flow crosses the auxiliary surface in the direction opposite to the main injection (i.e., with velocities directed towards the jet). These inward velocities are retained in the imposed boundary conditions to ensure global mass conservation.
 - The velocity field interpolation from the combustion simulation to the inlet boundary mesh from the thermally coupled simulation may introduce some deviations in the mass flow rate. The velocities are corrected with the factor needed to preserve the original mass flow rate.
 - The simulation time step is selected such that its value and the duration of the database generated in step a) after scaling by the minimum power of ten required to express both as integers are relatively prime. This ensures a uniform sampling of the stored flow realizations throughout the calculation.

An illustration of the scale separation strategy is shown in the next figure.

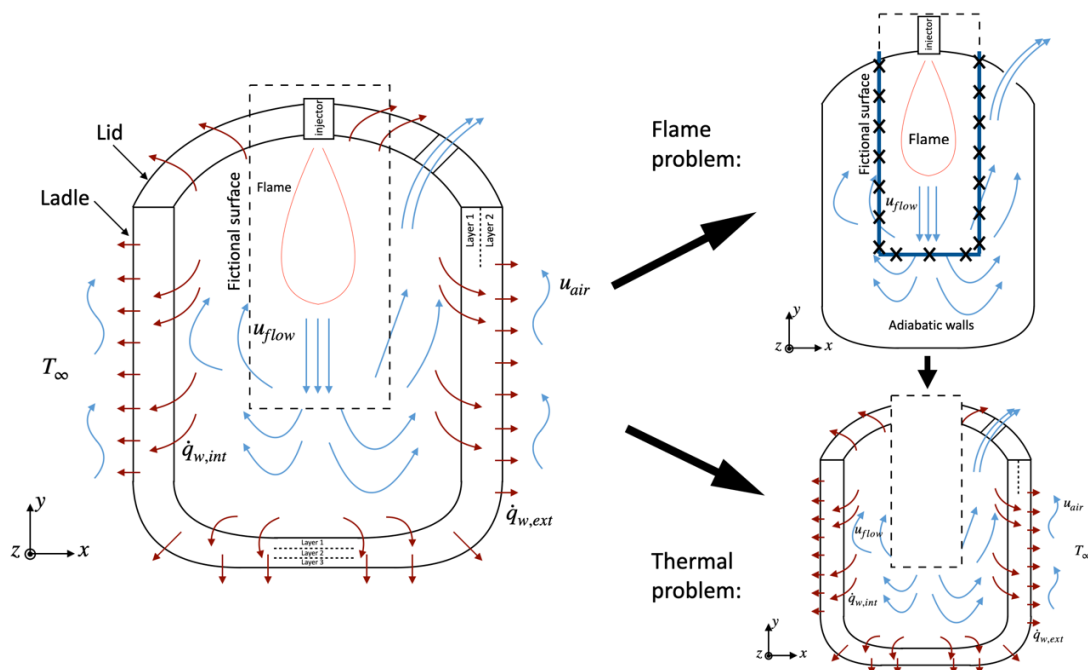


Figure 106. Scale separation-based strategy to simulate the heating up of the ladle.

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A representation of the temperature fields for both simulations is included in the next figures. At the left, an instantaneous temperature field for the CO/O₂ adiabatic combustion simulation is shown. The auxiliary cylindrical surface where data is registered for the later thermally coupled simulation is also marked with black lines. At the right, a visualization of a slice of the solid and fluid domains for the thermally coupled simulation during the first instants of the ladle heating is shown. It can be observed that the region occupied by the jet has been removed from the computational domain and replaced by velocity and temperature boundary conditions derived from the combustion simulation, thereby emulating the effect of the jet without explicitly resolving it.

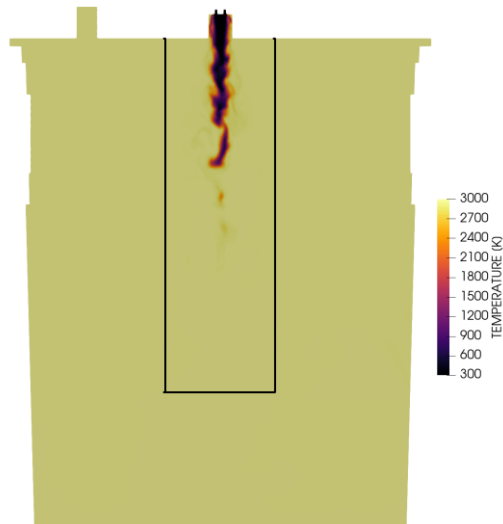


Figure 107. Slice for the temperature field for the combustion of CO/O₂. The fictional surface is indicated with a black line.

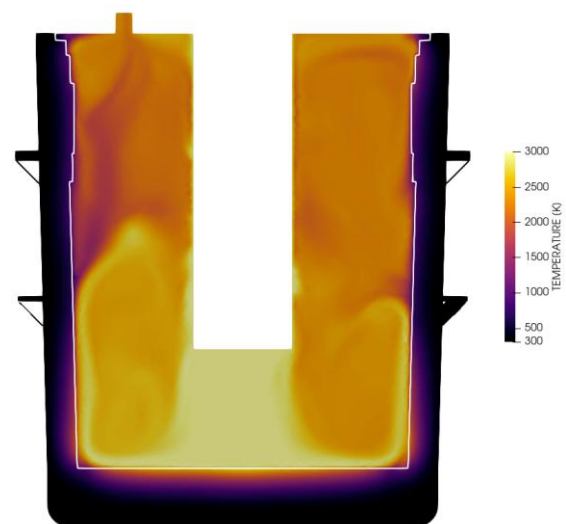


Figure 108. Slice for the temperature field of the fluid and ladle in the thermally coupled simulation (CO/O₂ case). The region where the jet is encapsulated is removed and its effect substituted by boundary conditions from the combustion simulation. The white line denotes the interior surface of the ladle and interface between the gas and the ladle wall.

Simulations were carried out with Alya, the Finite Element Method code developed at CASE Department from BSC to solve multiphysics problems (Vázquez et al, 2016). Calculations have been solved in the frame of Large Eddy Simulations (LES) under the low Mach hypothesis.

The combustion model is based on the flamelet hypothesis, according to which the turbulent flame can be described as a set of embedded laminar flames, that is, the flame front is a thin layer that separates the fresh and burnt gases whose internal structure remains laminar. Such hypothesis is valid when the flame thickness is small compared to the smallest turbulent length scales (Kolmogorov scales) (van Oijen et al., 2016). In this way, a database that gathers all the possible thermochemical states of the turbulent flame can be constructed from laminar flames which are solved before the simulation (tabulated chemistry approach) (van Oijen et al., 2016). For the current configuration the manifold is created from one-dimensional adiabatic premixed laminar flames. Moreover, the laminar flames are parametrized as a function of some few variables which characterize the local state of the flow. Such variables act as independent variables to access the database, typically named as manifold. In the present case they consist of the progress variable and the mixture fraction. While the mixture fraction measures the amount of mass coming from the fuel stream, the progress variable is an indicator of the degree of completion of the reaction. The mass fraction for CO₂ has been used as progress variable for the CO/O₂ case while H₂O for the H₂/O₂ flame.

Turbulence Chemistry Interaction (TCI) is accounted for using presumed Filtered Probability Density Functions (FPDFs). Such FPDFs are parametrized through the filtered progress variable, filtered mixture fraction and their variances, which correspond to the controlling variables of the turbulent combustion manifold. Statistical independence is assumed for the progress variable and the mixture fraction. The FPDF

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for each of these variables is modelled with a beta function. Finally, a four-dimensional manifold for the turbulent combustion is created based on the filtered progress variable, filtered mixture fraction and their variances after integration of the laminar manifold (defined by the set of thermochemical states from the one-dimensional adiabatic laminar flames parametrized as a function of the progress variable and the mixture fraction). The control variables are transported in the turbulent flow and used to access the manifold during the CFD to retrieve the thermochemical and transport properties together with the chemical source terms.

Regarding the transport equations solved in the CFD, as they are filtered (Large Eddy Simulations), Reynolds stresses are modelled assuming the Boussinesq hypothesis while scalars turbulent transport are described with a gradient hypothesis. The turbulent eddy viscosity (ν_t) is modelled with the Vreman model (Vreman et al., 2004) for momentum. For the scalars a turbulent diffusivity is obtained dividing the eddy viscosity by a turbulent Schmidt number (set to 0.7). Therefore, the equations to be solved are (after substituting the turbulent transport with previous models):

- Continuity

$$\frac{\partial \bar{\rho}}{\partial t} + \nabla \cdot (\bar{\rho} \tilde{\mathbf{u}}) = 0$$

- Navier-Stokes

$$\frac{\partial (\bar{\rho} \tilde{\mathbf{u}})}{\partial t} + \nabla (\bar{\rho} \tilde{\mathbf{u}} \otimes \tilde{\mathbf{u}}) = -\nabla \bar{p} + \nabla \cdot \boldsymbol{\tau} + \nabla \cdot \boldsymbol{\tau}_{sgs}$$

- Progress variable

$$\frac{\partial (\bar{\rho} \tilde{Y}_c)}{\partial t} + \nabla \cdot (\bar{\rho} \tilde{\mathbf{u}} \tilde{Y}_c) = \nabla \cdot (\bar{\rho} (\tilde{D} + \tilde{D}_{sgs}) \nabla \tilde{Y}_c) + \bar{\omega}_{Y_c}$$

- Progress variable variance

$$\begin{aligned} \frac{\partial (\bar{\rho} Y_{c,v})}{\partial t} + \nabla \cdot (\bar{\rho} \tilde{\mathbf{u}} Y_{c,v}) = & \nabla \cdot (\bar{\rho} (\tilde{D} + \tilde{D}_{sgs}) \nabla Y_{c,v}) + 2\bar{\rho} \frac{\nu_t}{Sc_t} \nabla \tilde{Y}_c \cdot \nabla \tilde{Y}_c \\ & - \bar{\rho} C_{Y_c} \frac{\nu_t}{Sc_t} \frac{Y_{c,v}}{\Delta^2} + 2(\bar{Y}_c \bar{\omega}_{Y_c} - \tilde{Y}_c \bar{\omega}_{Y_c}) \end{aligned}$$

- Mixture fraction

$$\frac{\partial (\bar{\rho} \tilde{Z})}{\partial t} + \nabla \cdot (\bar{\rho} \tilde{\mathbf{u}} \tilde{Z}) = \nabla \cdot (\bar{\rho} (\tilde{D} + \tilde{D}_{sgs}) \nabla \tilde{Z})$$

- Mixture fraction variance

$$\begin{aligned} \frac{\partial (\bar{\rho} Z_v)}{\partial t} + \nabla \cdot (\bar{\rho} \tilde{\mathbf{u}} Z_v) = & \nabla \cdot (\bar{\rho} (\tilde{D} + \tilde{D}_{sgs}) \nabla Z_v) + 2\bar{\rho} \frac{\nu_t}{Sc_t} \nabla \tilde{Z} \cdot \nabla \tilde{Z} \\ & - \bar{\rho} C_Z \frac{\nu_t}{Sc_t} \frac{Z_v}{\Delta^2} \end{aligned}$$

where a bar denotes Reynolds filtering while a tilde Favre filtering, ρ density, $\mathbf{u}$ velocity, p the pressure, $\boldsymbol{\tau}$ the deviatoric stress tensor, $\boldsymbol{\tau}_{sgs}$ the sub-grid stress tensor, D the molecular diffusivity while D_{sgs} the sub-grid diffusivity, Y_c the progress variable and $Y_{c,v}$ its variance, Z the mixture fraction and Z_v its variance, $\bar{\omega}_{Y_c}$ the progress variable source term and Δ the cell size. Constants C_{Y_c} and C_Z are taken equal to 2.

In this simulation the chemical mechanism GRI3.0 is used [Smith et al., 1999].

The thermal coupling is achieved through the Conjugate Heat Transfer (CHT) algorithm as described in [Houzeaux et al., 2024] which is used to solve the heat transfer at the interface of two sub-domains, in this

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case, one representing the fluid (hot products) and other the solid (the ladle). For this simulation the transport equations to be solved are:

- Fluid sub-domain:
 - Continuity

$$\frac{\partial \bar{\rho}}{\partial t} + \nabla \cdot (\bar{\rho} \tilde{\mathbf{u}}) = 0$$

- Navier-Stokes

$$\frac{\partial(\bar{\rho} \tilde{\mathbf{u}})}{\partial t} + \nabla(\bar{\rho} \tilde{\mathbf{u}} \otimes \tilde{\mathbf{u}}) = -\nabla \bar{p} + \nabla \cdot \boldsymbol{\tau} + \nabla \cdot \boldsymbol{\tau}_{\text{sgs}}$$

- Energy (written in terms of temperature)

$$\frac{\partial(\bar{\rho} \tilde{T})}{\partial t} + \nabla \cdot (\bar{\rho} \tilde{\mathbf{u}} \tilde{T}) = \frac{1}{\tilde{c}_p} \nabla \cdot (k \nabla \tilde{T}) + \nabla \cdot (\bar{\rho} \tilde{\alpha}_{\text{sgs}} \nabla \tilde{T})$$

with c_p the specific heat at constant pressure, k the thermal conductivity and α_{sgs} is the sub-grid thermal diffusivity.

- Solid sub-domain:
 - Energy (written in terms of temperature)

$$\frac{\partial T}{\partial t} = \frac{k}{\rho C} \Delta T$$

with C the solid heat capacity. Properties are considered constant for the ladle. The ratio $\frac{k}{\rho c_p}$ corresponds to the thermal diffusivity.

The CHT algorithm guarantees continuity for temperature and heat flux at the interface between sub-domains through a series of iterations in which one of these magnitudes is imposed as a boundary condition for one of the sub-domains and the other magnitude is obtained as part of the solution to be later transferred to the other sub-domain which will use it in turn as boundary condition. The process is repeated until the difference of the temperature and the heat flux between iterations is smaller than a tolerance set beforehand. More precisely:

1. The fluid sub-domain solution is advanced in time taking as boundary condition the heat flux at the fluid-solid interface from previous time step. The solution will give new fields in the interior of the fluid sub-domain together with a new temperature at the interface.
2. Such temperature at the interface is sent to the solid sub-domain where the energy equation is solved using such new temperature at the interface as boundary condition. The solution will give a new temperature field in the interior of the solid sub-domain together with a new heat flux at the interface.
3. Such heat flux is sent back to the fluid sub-domain which is used to recompute the solution of the fields and so steps 1)-2) are repeated till the error between iterations is smaller than a tolerance given beforehand.

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In previous description the roles of the fluid and solid can be exchanged in such a way that fluid sub-domain was solved imposing the temperature and solid the heat flux. A scheme of such algorithm is illustrated in the following for clarity:

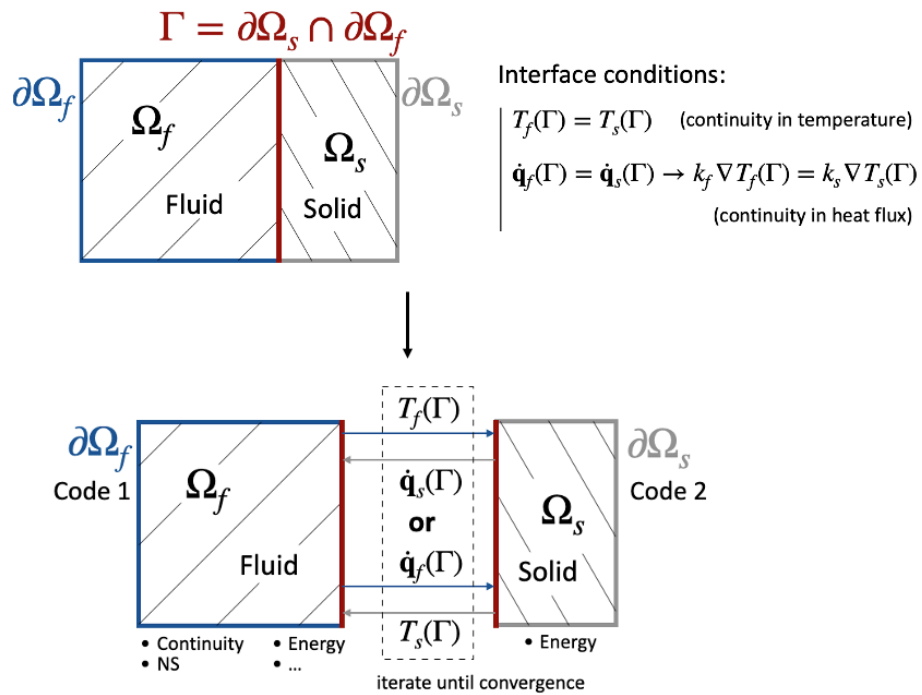


Figure 109. Illustration of the Conjugate Heat Transfer (surface coupling).

Each of the sub-domains is solved in a different Alya execution. Both Alya execution (fluid and solid) communicate each other to exchange the values of temperature and heat flux at the interface. Such exchange can be done in two ways:

- Jacobi algorithm: more parallelizable.
- Gauss-Seidel algorithm: more robust and convergence achieved in less number of iterations.

An illustration of both schemes is shown in the following.

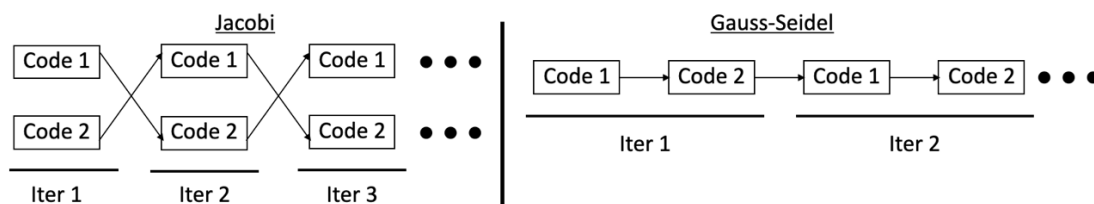


Figure 110 Illustration of the Jacobi (left) and Gauss-Seidel (right) schemes for the exchange of information in the thermal coupling.

In this case stability was prioritized and then the Gauss-Seidel algorithm was chosen. It is important to note, however, that the suitability of each scheme is case dependent.

By virtue of the different temporal scales of the fluid and the solid and to reduce the computational cost (usually applying the CHT is expensive as both sub-domains have to be solved several times), the thermal coupling is not solved at each time step but each certain number of time steps. The time interval between information exchange varies during the simulation. Considering that the solid is an insulator (thermal diffusivity in the order of $10^{-7} - 10^{-6} \text{ m}^2/\text{s}$) the characteristic time scale for it is orders of magnitude higher than that of the fluid and then the time between information exchange varies from 0.2 s to 2.6 s.

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For the wall properties have been set independent of temperature with a thermal conductivity of 1.8 W/m/K, a density of 800 kg/m³ and a specific heat of 1000 J/K/kg for the refractory material. This gives a thermal conductivity of $2.25 \cdot 10^{-6}$ m²/s.

Regarding the solver discretization, second order spatial discretization is used. For combustion simulations (simulations from a)) an explicit 3rd order Runge-Kutta scheme in time with time steps in the order of 10^{-6} s is used. On the contrary, simulations from b) are solved with a trapezoidal temporal scheme (implicit scheme) allowing time steps in the order of tenths of seconds. More specifically, at the start of the pre-heating, as the gradients between the combustion products and the walls are extremely high, it is convenient to take relatively low time steps to better capture the heating curve of the walls. As time passes and walls reach higher temperature such time step is increased accordingly to reduce the computational cost.

Meshes for a) and b) are unstructured with 12.7M elements for a) and 2.3M for the fluid and 1.8M for the solid in b).

6.3 Preliminary analysis

Previously to analysing the results of the ladle preheating it is interesting to study the laminar one-dimensional flames characteristics. Despite the simplicity of these flames, they still provide insightful information and are the basis of the model. Next figures show the adiabatic temperature at the left and flame speed and thickness at the right, first for CO/O₂ and later for H₂/O₂ mixtures, at atmospheric pressure and ambient temperature for fresh gases, as a function of the equivalence ratio. As can be observed, adiabatic temperatures are not very sensitive to equivalence ratio considering the wide range represented and for an equivalence ratio of 0.8 such temperature is close to 3000 K due to the absence of nitrogen or any other diluent. Slightly higher values are appreciated for H₂/O₂ mixtures.

Regarding the flame speed, CO/O₂ and H₂/O₂ mixtures feature a difference of two orders of magnitude being the highest values for H₂/O₂. As will be observed later, this will drastically impact on the flame morphology and, in particular, on the flame length. In addition, for CO/O₂ mixtures the maximum flame speed is found at lean mixtures around an equivalence ratio of 0.75 while in the case of H₂/O₂ it appears at stoichiometry. To end this preliminary analysis it is noted that the flame thickness is sensitively lower for H₂/O₂ which, in addition, shows increasing values with the equivalence ratio. While at an equivalence ratio of 0.8 the CO/O₂ mixtures feature a flame thickness of 3 mm approx. such value reduces to around 0.9 mm for H₂/O₂. The flame thickness is computed as the distance between points at 0.05 and 0.95 of the normalized temperature.

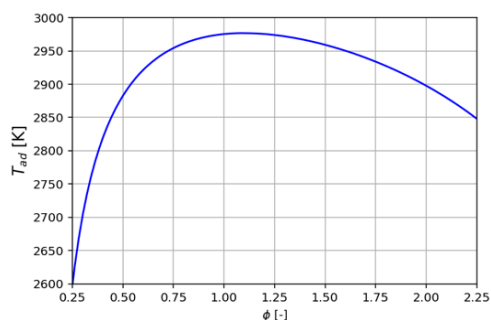


Figure 111. Adiabatic temperature for 1D flames for CO/O₂ combustion.

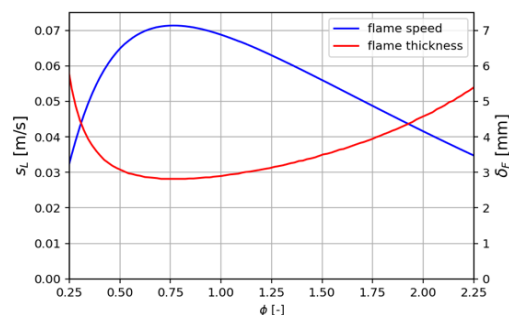


Figure 112. Flame speed and flame thickness for 1D flames for CO/O₂ combustion.

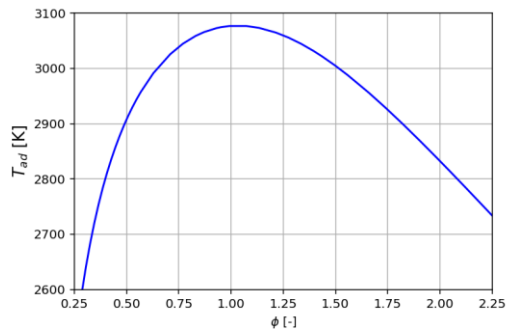


Figure 113. Adiabatic temperature for 1D flames for H_2/O_2 combustion.

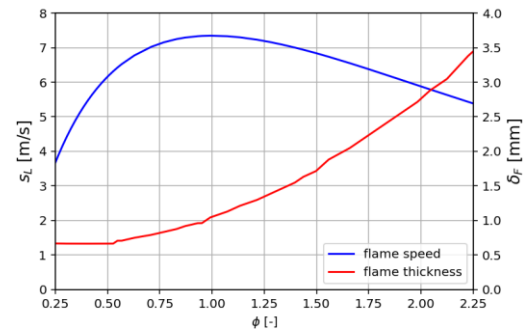


Figure 114. Flame speed and flame thickness for 1D flames for H_2/O_2 combustion.

6.4 Combustion simulation

The combustion simulations are first analysed to understand the preheating of the ladle. Since the characteristic time for the ladle preheating is much greater than those of the flow, it is argued that representative states for the register of data on the fictional surface correspond to adiabatic simulations after long time, that is, after homogenization of the mixture far away from the flame has taken place. This is deemed to be more representative of the flame states for long times than taking thermal states from a flame where there exists heterogeneity in the mixture fraction field during the filling of the ladle with the hot products. Heat release, temperature and axial velocity are represented for different snapshots in a slice that contains the outlet pipe axis for both CO/O_2 and H_2/O_2 combustion.

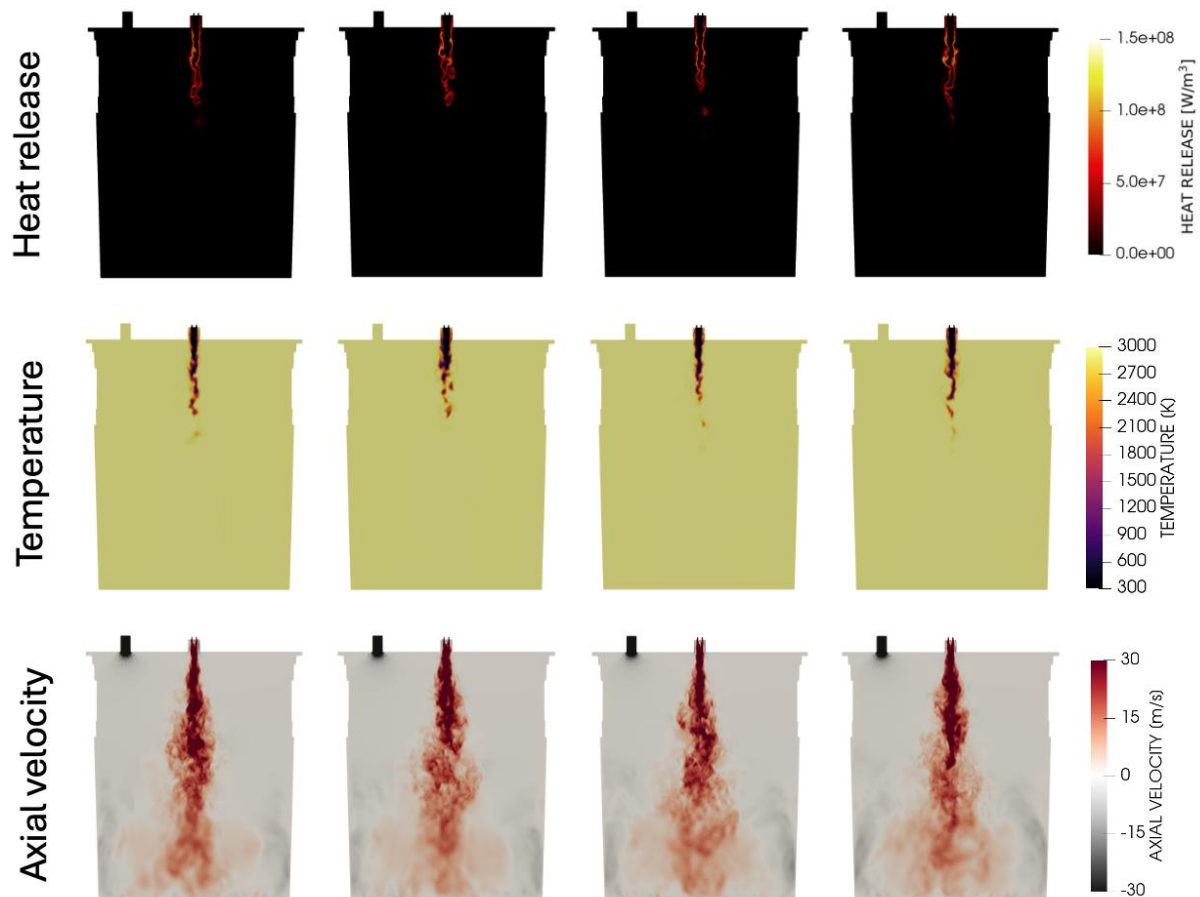


Figure 115. Representation of slices of instantaneous heat release (first row), temperature (second row) and axial velocity (third row). Each column corresponds to a different instant. Scales are common for each row. Results for CO/O_2 combustion.

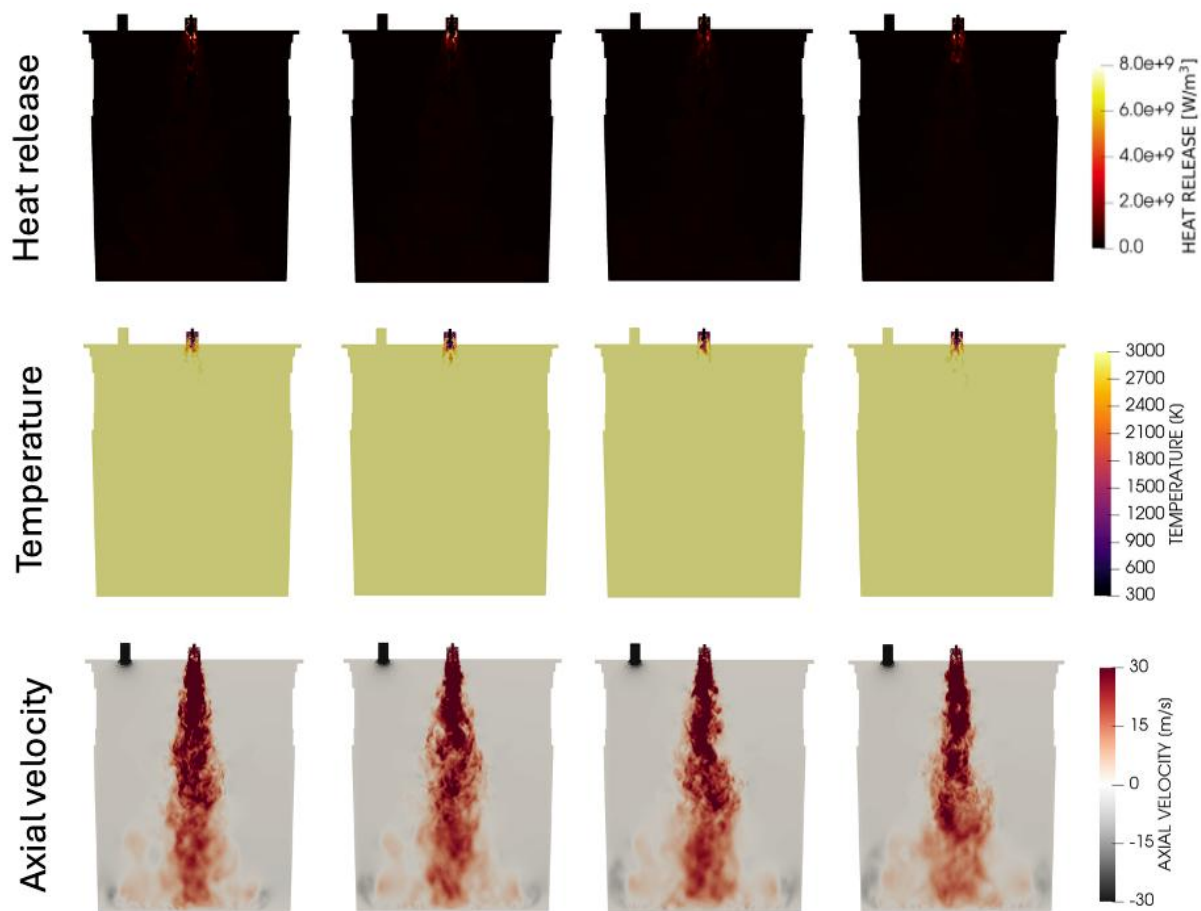


Figure 116. Representation of slices of instantaneous heat release (first row), temperature (second row) and axial velocity (third row). Each column corresponds to a different instant. Scales are common for each row. Results for H_2/O_2 combustion.

Due to the high Reynolds numbers at which the fuel and oxygen are injected (Re in the order of 10^4), a highly turbulent jet is generated, characterized by intense vorticity and a broad spectrum of vortical structures arising from the interaction between the jet and the surrounding combustion products. Such structures have a minimum size close to the nozzles while they increase in size in the downstream direction. Due to the products entrainment, the jet velocity decreases significantly as momentum is transferred through shear stresses with the surrounding fluid. This deceleration continues until the jet impinges on the opposite wall, where the flow is reversed.

The flame morphology clearly contrasts between both cases: while long flames are observed for CO/O_2 , with a flame length around one fourth of the ladle height, as appreciated from the heat release field, H_2/O_2 combustion features a much more compact flame and, hence, much shorter flame lengths. This is attributed to the noticeably higher flame speeds of the H_2/O_2 combustion already pointed out in the 1D flame analysis.

On top of this, the axial velocity is slightly higher for H_2/O_2 combustion since, even the oxygen is injected in both cases at the same speed, hydrogen is introduced at a noticeable higher velocity than that of carbon monoxide due to the low density of the former. Such higher axial velocities of the jet are expected to impact on the ladle preheating.

6.5 Thermally coupled simulation

As explained previously, the combustion simulation is run separately of the thermally coupled simulation to avoid incurring in an excessive computational cost. In this manner the values for velocity and temperature

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that were tracked on a fictional surface and saved in a database from the combustion simulation are here imposed on such surface.

Due to the convective heat transfer (as mentioned thermal radiation was not considered in these simulations) the ladle is preheated due to the hot products of the combustion.

The figure represents the temperature field for the fluid and the solid in a slice that contains the ladle and outlet pipe axes.

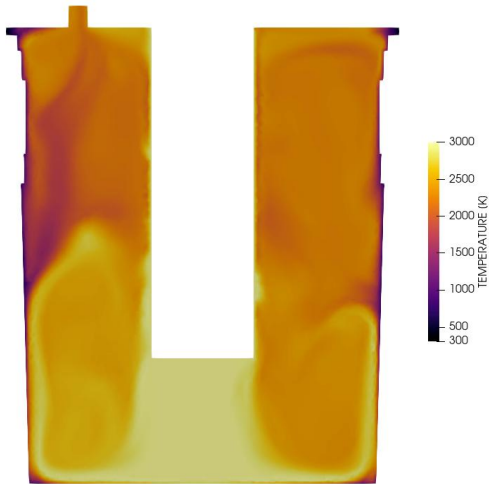


Figure 117. Instantaneous temperature fields for the fluid for a slice.

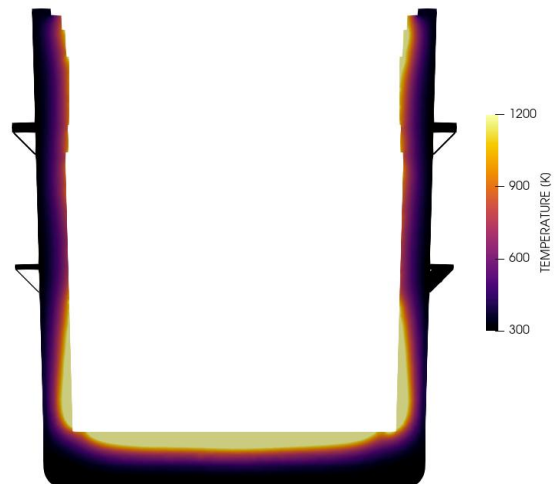


Figure 118. Instantaneous temperature fields for the solid for a slice. Temperature is saturated to improve visualization.

It is evident that, as advanced previously, there exists a non-uniform heating of the ladle. Highest temperatures are observed at the bottom wall and lower part of the lateral wall (first one third in height). These zones correspond to the regions where the jet impinges, deflects and, finally, reverses, and, hence, there exists an intense contact between the fluid and the solid that enhances the convective heat transfer.

Before proceeding with a more detailed analysis it is interesting to compare some thermodynamic and transport properties of the burnt mixtures injected in these simulations to understand the heat transfer pattern. As observed from the CFD adiabatic simulations a uniform mixture is achieved far from the nozzles. In particular, the mixture that passes through the fictional surface (inlet boundary in the thermally coupled simulations) is uniform what greatly simplifies subsequent analysis. In the following table, values for different properties of interest are given for such mixtures:

Table 34. Thermodynamic and transport properties for the mixtures injected to the thermally coupled simulations (tracked on fictional surface) for CO/O₂ and H₂/O₂ mixtures.

	CO/O ₂	H ₂ /O ₂
Adiabatic flame temperature (K)	2961	3049
Mixture molecular weight (g/mol)	35.7	16.85
Density (kg/m ³)	0.147	0.067
Mass specific heat at constant pressure (J/kg/K)	1355	2804
Thermal conductivity (W/m/K)	0.165	0.437
Kinematic viscosity (m ² /s)	$5.98 \cdot 10^{-4}$	$1.47 \cdot 10^{-3}$
Dynamic viscosity (Pa s)	$8.78 \cdot 10^{-5}$	$9.89 \cdot 10^{-5}$
Thermal diffusivity (m ² /s)	$8.29 \cdot 10^{-4}$	$2.31 \cdot 10^{-3}$

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As observed, even the adiabatic flame temperatures are similar, there exists a very large difference in the molecular weight due to the presence of CO_2 (molecular weight of 44 g/mol) as main product in the CO/O_2 combustion and H_2O (molecular weight of 18 g/mol) in the H_2/O_2 flame. Such difference in the mixture molecular weight causes the large discrepancy in density (since flame temperatures are similar). The very different molecular weight also impacts on the mass specific heat at constant pressure (but similar values for molar specific heat at constant pressure). Finally, the global effect of density, mass specific heat at constant pressure and thermal conductivity are summarized in the thermal diffusivity coefficient, which features a ratio between mixtures similar to that of the thermal conductivities (since the product of density and mass specific heat at constant pressure is approximately the same for both mixtures). Such higher thermal diffusivity for the H_2/O_2 case should promote the heat transfer compared to the CO/O_2 one. Moreover, as shown before, the velocities are higher for the H_2/O_2 mixture what in turn promotes higher heat transfer coefficients and, hence, the heat transfer with the ladle walls.

Insights about the temperature distribution are revealed when representing this field over the inner surface of the ladle. Both halves of the ladle have been represented.

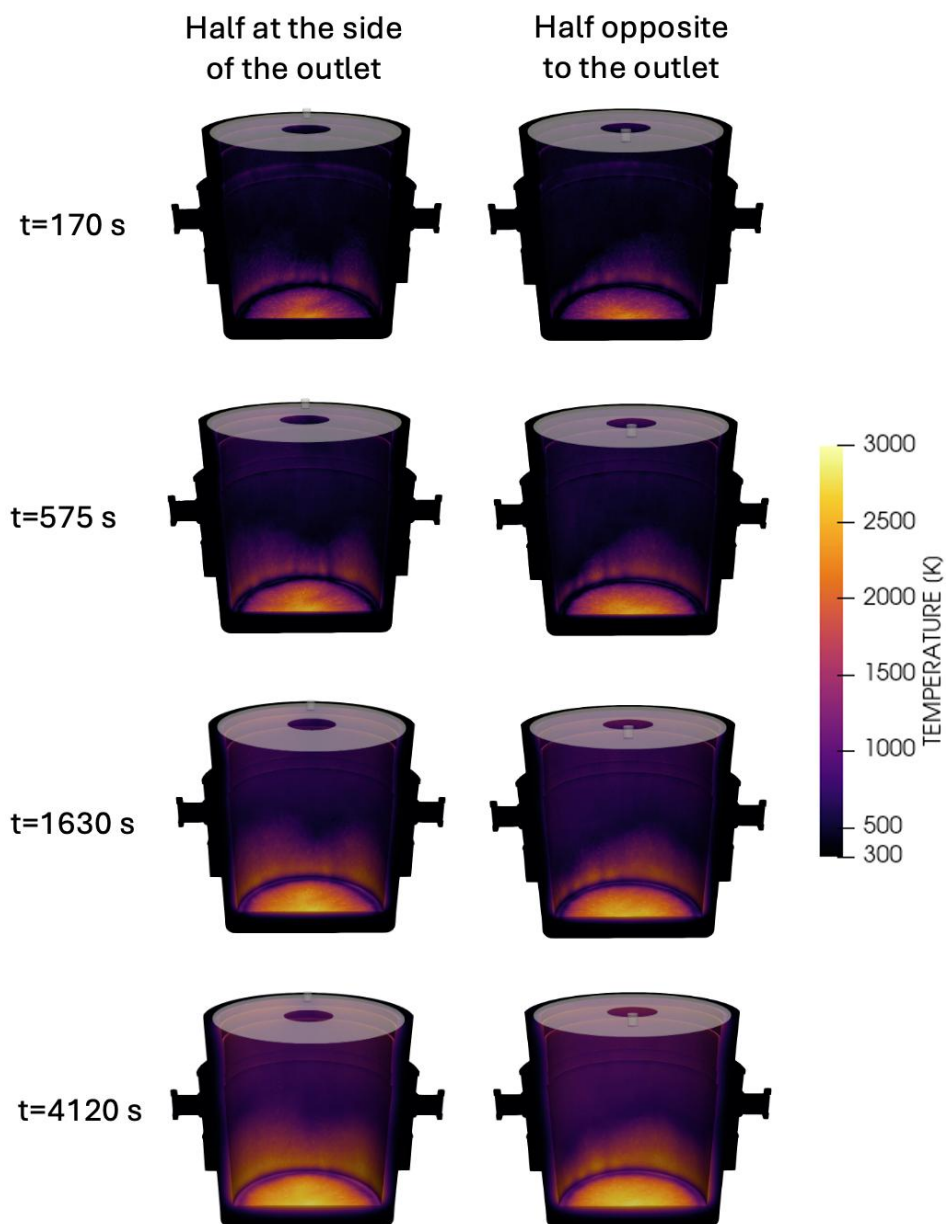


Figure 119. Temperature field over the inner surface of the ladle for the CO/O_2 combustion. Scale is common for all the figures.

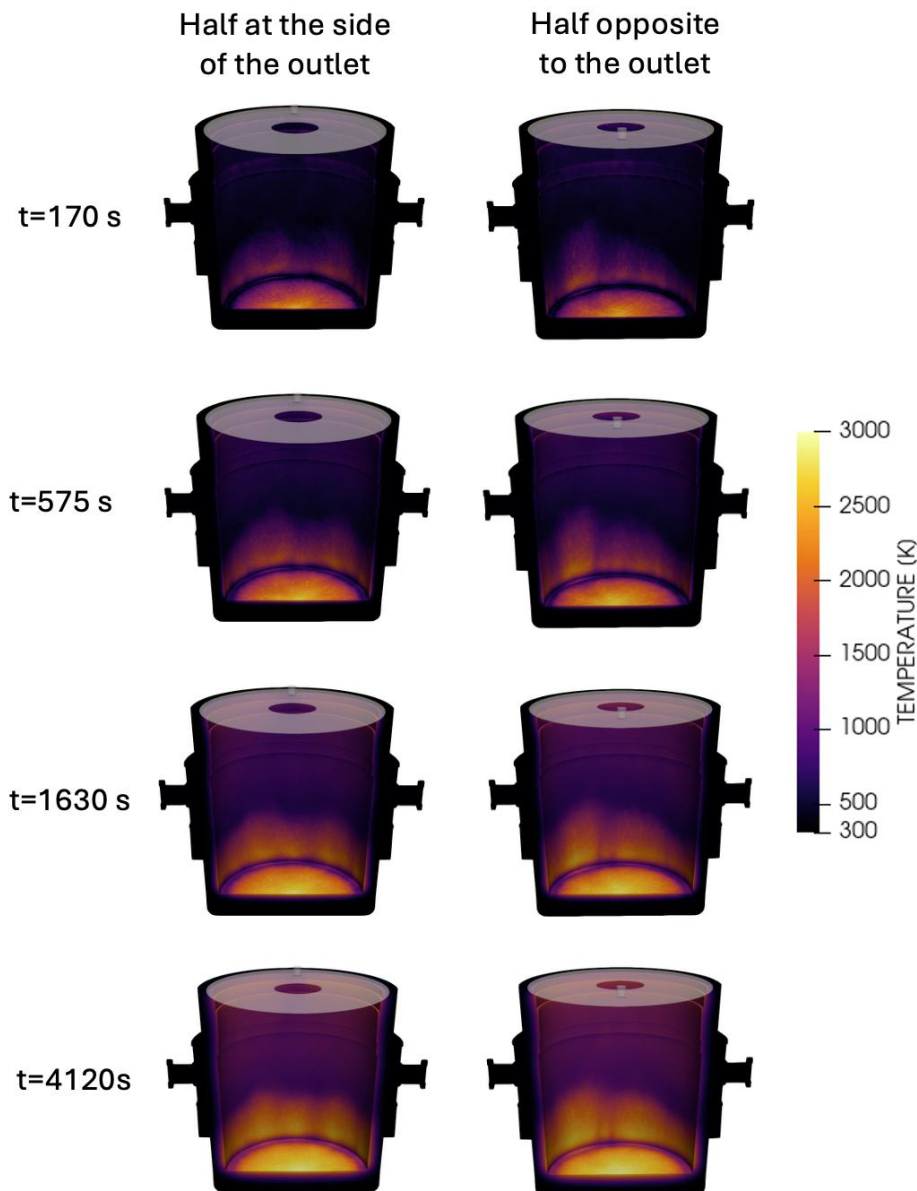


Figure 120. Temperature field over the inner surface of the ladle for the H₂/O₂ combustion. Scale is common for all the figures.

The results show that the central region of the ladle bottom heats up rapidly, reaching very elevated temperatures within a few hundred seconds. As the heating process progresses, the thermal front expands towards the periphery of the bottom surface and subsequently along the lower portion of the lateral walls, which become the regions most directly affected by the hot flow. The heating of the lower part of the lateral wall is not uniform, showing some non-uniformities caused by the flow pattern. Such heterogeneity will be further analysed.

It should also be noted that thermal radiation is not included in the present simulations and since radiative heat transfer generally promotes a more uniform distribution of thermal energy, its omission likely accentuates the temperature gradients observed within the ladle.

It is also worth mentioning that despite the high thermal resistance of the refractories used for the ladle, the temperatures may seem high (close to 2700 °C). A possible cause for this could be the absence of the thermal radiation mechanism which would, on the one hand, as already said, lead to an enhanced temperature homogeneity and, on the other hand, decrease the combustion products temperature. In other words, radiation would produce a redistribution of the heat flux over the walls and, even the zones where the jet

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impinges would receive part of the heating flux due to thermal radiation, it cannot be discarded that as the gases would be at lower temperature such zones would receive a lower total heat flux (convection and radiation) what would in turn produce lower wall temperatures.

Comparing both figures, it is evidenced that the ladle is preheated faster for the H₂/O₂ mixture with regards to the CO/O₂ flame. Such behaviour is observed in both the basis and the lateral wall even it is more evident for the lateral wall where higher temperatures are registered at higher heights for H₂/O₂ (at same instant).

A more detailed understanding of the heating process can be obtained by analysing the heat flux in addition to the temperature field. To facilitate this analysis, the local convective heat transfer coefficient h , is evaluated according to Newton's law of cooling:

$$\dot{q} = h(T_w - T_\infty)$$

where $\dot{q}$ is the heat flux (W/m²) and T_w and T_∞ are the wall temperature and the temperature far away from the wall, respectively, which in this case is taken as the adiabatic flame temperature (see previous table for the exact value). To improve the visualization the lateral wall is developed onto the plane and the basis is added at the bottom of the figure. The central line of the developed lateral wall (0°) corresponds to the azimuth where the outlet pipe axis is found.

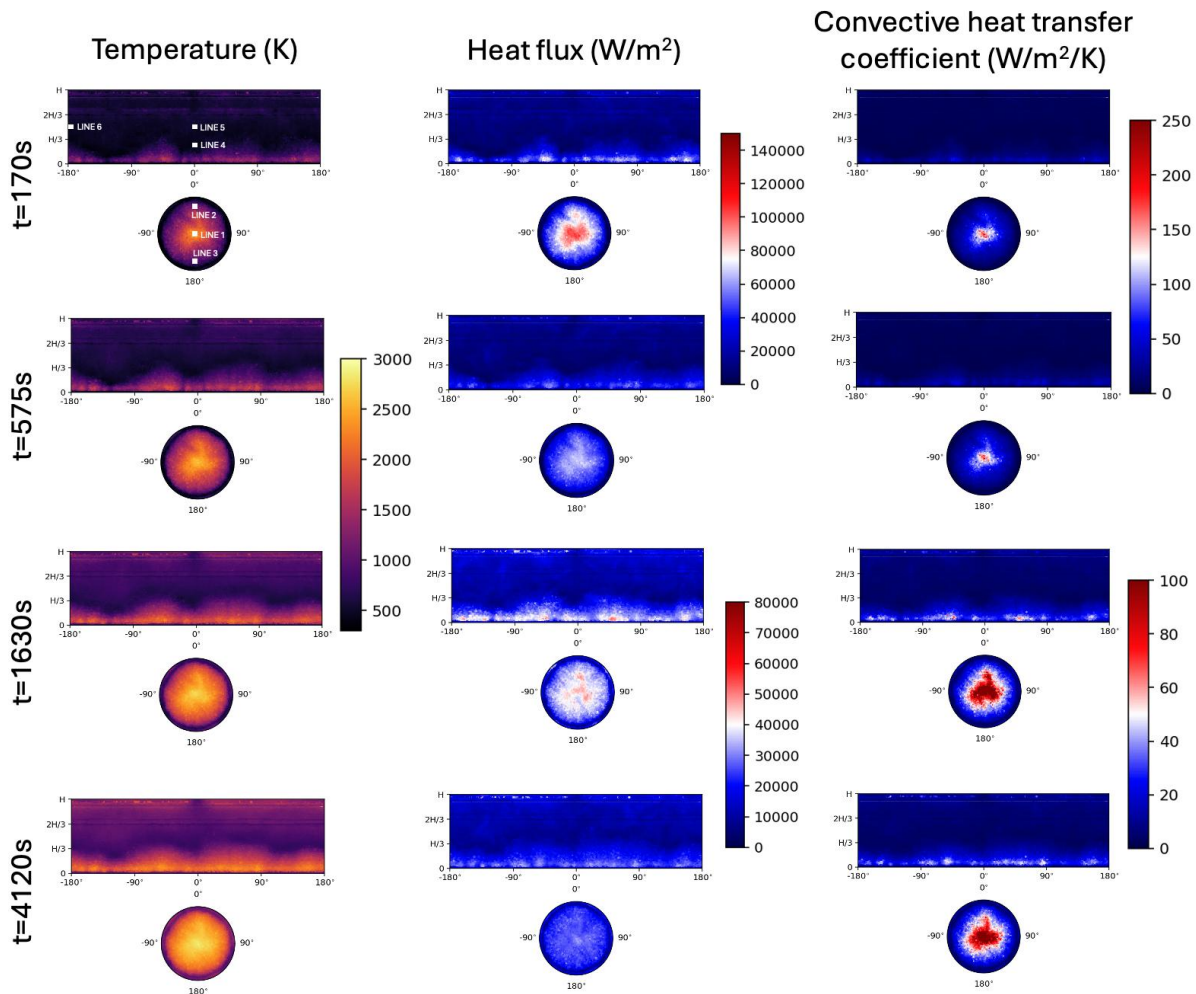


Figure 121. Temperature (left), heat flux (centre) and convective heat transfer coefficient (right) fields for different instants over the ladle inner surface for the CO/O₂ case.

In Figure 121, each row corresponds to a different instant. The lateral wall surface has been developed for visualization. Different points are marked in the first row for the temperature field for subsequent analysis.

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Scales are common for temperature while for heat flux and convective heat transfer coefficient scales are common for the figures of each pair of snapshots.

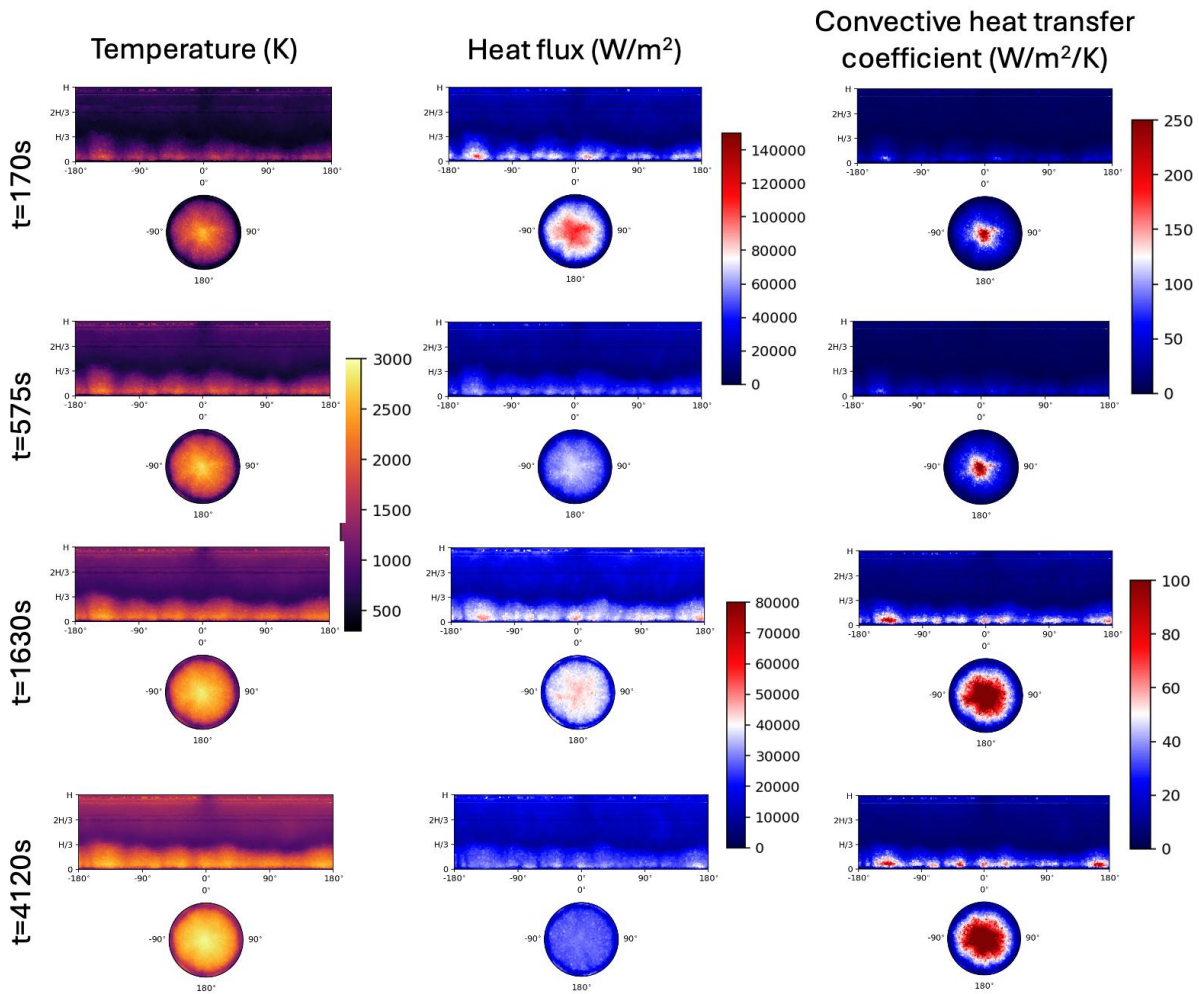


Figure 122. Temperature (left), heat flux (centre) and convective heat transfer coefficient (right) fields for different instants over the ladle inner surface for the H₂/O₂ case.

In Figure 122 Each row corresponds to a different instant. The lateral wall surface has been developed for visualization. Scales are common for temperature while for heat flux and convective heat transfer coefficient scales are common for the figures of each pair of snapshots.

This representation reveals a strong correlation between regions of elevated temperature, high heat flux, and large convective heat transfer coefficients. The most severe thermal loads are clearly concentrated at the bottom wall, especially close to the centre. Also, high thermal loads are revealed along the lower part of the lateral wall. As previously discussed, there is a non-uniformity in azimuth, being the part of the ladle closer to the outlet preheated faster (region around the central line of the developed lateral wall representation) for the CO/O₂ case. On the contrary, for H₂/O₂ the process is more uniform. This is primarily a consequence of the flow dynamics which enhance the contact between the interior surface ladle and the fluid in such regions, and the resulting convective heat transfer. As mentioned, radiative heat transfer would tend to redistribute thermal energy more uniformly across the ladle surfaces. In contrast, to these regions, others exhibit only marginal increases in temperature, remaining almost cold (walls of the ladle close to the lid).

The maximum heat flux occurs during the initial stages of the heating process and progressively decreases over time. This behaviour is expected, as the temperature difference between the hot combustion products and the initially cold ladle wall is largest at the beginning of the simulation and gradually diminishes as the wall

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temperature rises. As the wall temperature increases, the thermal gradients weaken, and the heat flux correspondingly decreases. The highest convective heat transfer coefficients are also observed on the bottom wall but shows slow variations over time.

Regarding the comparison between cases previous observations are corroborated. The heat flux is higher for the H_2/O_2 what entails higher temperatures. Such higher heat fluxes for the H_2/O_2 case are appreciated over the whole ladle surface and, specially, at the basis and the lowest part of the lateral wall. This is accompanied by higher heat transfer coefficients in turn.

Finally, it is of interest to examine the temperature temporal evolution along the ladle thickness. To this end, temperature profiles are presented along lines normal to the wall at the locations indicated in the previous figure (see first instant for temperature for the CO/O_2 case).

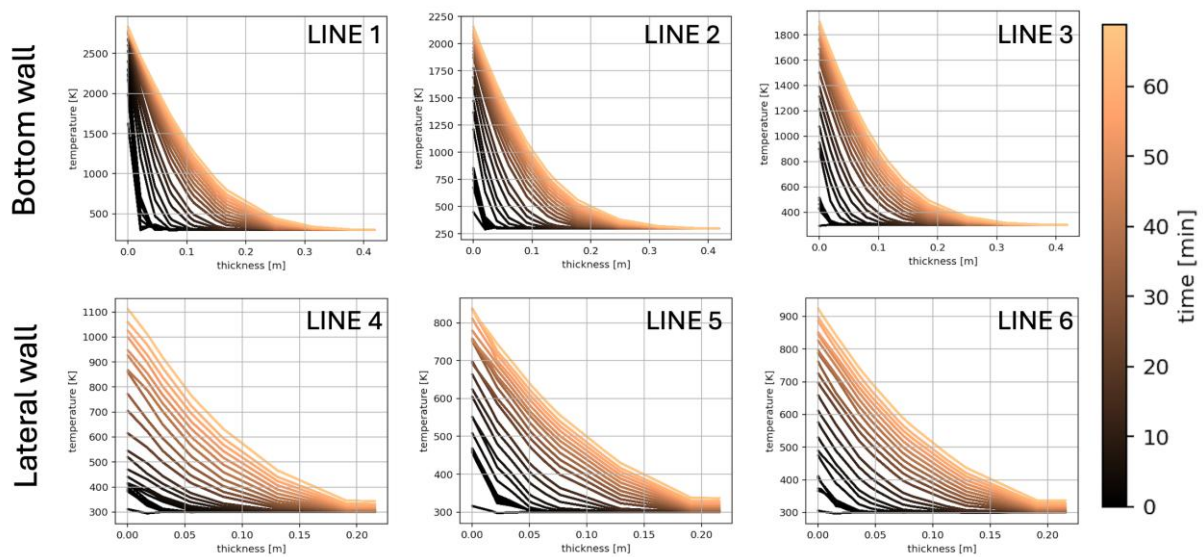


Figure 123. Temperature temporal evolution along specific lines transversal to the ladle wall for CO/O_2 combustion (for line numbering see temperature projection map for $t=170$ s for CO/O_2 case). Scales adapted for visibility.

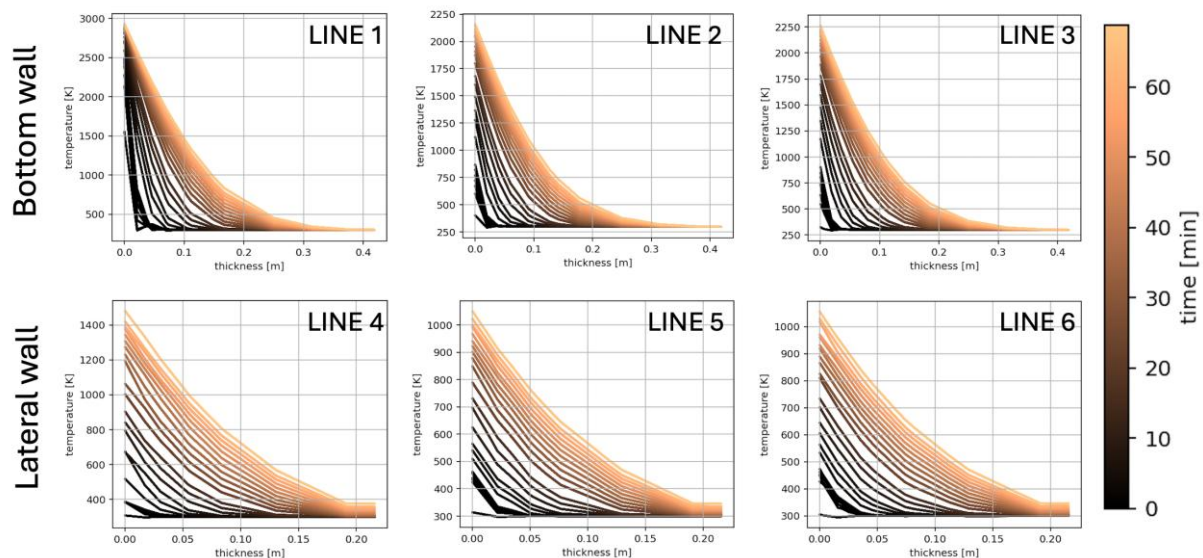


Figure 124. Temperature temporal evolution along specific lines transversal to the ladle wall for H_2/O_2 combustion (for line numbering see temperature projection map for $t=170$ s for CO/O_2 case). Scales adapted for visibility.

As observed, the heating occurs very fast at the first stages close to the inner surface, reducing its velocity as time passes until reaching an equilibrium. Heat is gradually conducted to more external layers after the heating of the most inner ladle surface. The velocity at which this happens depends clearly on the line

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considered. While the lines at the bottom wall experience fast heating, this may not be the case for points far from the bottom wall.

As most of the heat flux increases the wall temperature, due to the isolating material of the ladle, even after more than one hour of simulated heating, only a limited amount of thermal energy has penetrated through the full thickness of the ladle wall. Consequently, the outer surface remains close to its initial temperature and exhibits only a marginal temperature increase.

These profiles confirm once more that the ladle preheating occurs faster with H_2/O_2 combustion as clearly observed from lines 3-6 where higher maximum temperatures are achieved for H_2/O_2 compared to CO/O_2 . On the contrary, for lines 1 and 2 such a fast heating occurs that it is difficult to appreciate this behaviour.

To summarize, the simulations indicate strong thermal loads at the lower part of the lateral wall and, more intensely, at the ladle basis where the jet directly impinges. However, there exist very important temperature differences between the inner surface, in contact with the burnt gas, and the outer one caused by the strong insulation of the refractory. Moreover, higher thermal loads are systematically observed for H_2/O_2 combustion.

6.6 References

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7 Task 6.3: Ladle Preheating with H₂/O₂ Combustion – Demonstration at 7 Steel Nordic

7.1 Introduction

7.1.1 Background

This chapter describes the demonstration of hydrogen and oxygen combustion for the heating of a steel ladle, carried out at 7-Steel Nordic Manufacturing in Mo i Rana, Norway. Ladle heating is an essential process in steel production, serving both to cure newly installed refractory linings and to preheat ladles prior to receiving molten steel. Proper preheating is critical to ensure thermal stability, minimise heat losses, and prevent damage to the refractory lining during operation.

Steel ladles are typically heated using dedicated ladle heater systems equipped with burners operating in either air-fuel or oxy-fuel mode. These systems conventionally rely on fossil fuels such as diesel, natural gas, or CO-mix gas, resulting in direct CO₂ emissions.

The substitution of these fossil-based fuels with hydrogen represents a promising pathway for decarbonising ladle heating. When hydrogen is used in combination with oxygen and sourced from renewable energy, the combustion process can be effectively carbon-free, thereby significantly reducing the environmental footprint of steelmaking operations.

7.1.2 Objectives of the Demonstration

The overarching objective of the demonstration is to validate the technical feasibility and industrial applicability of hydrogen as a fuel for ladle heating. The work focuses on achieving stable and reliable operation of a hydrogen-fuelled ladle heater, capable of heating a ladle from ambient temperature to a target temperature of 1200 °C and maintaining this level throughout a full heating cycle.

In addition, the demonstration aims to compare hydrogen operation with the conventional CO-mix gas operation currently used at the site under comparable conditions. This comparison allows for evaluation of differences in thermal performance, operational stability, and combustion characteristics.

A further objective is to assess the interaction between hydrogen combustion and the refractory materials used to line the inner surface of the ladle, with particular attention to potential effects of the water-rich combustion environment. Finally, the demonstration seeks to provide results that are robust, transferable, and relevant for wider implementation across the European steel industry.

7.2 Demonstrator Description

7.2.1 Industrial Site

A detailed description of the demonstrator can be found in Deliverable 6.1: Demonstrators of the steel use cases. The tests were conducted during winter, with outside temperatures down to -20 °C and an ambient temperature on the plant floor of around -10 °C. These low temperatures introduced additional challenges such as heat losses and condensation phenomena. However, such temperatures are common in the area and were therefore representative of typical winter operating conditions.

7.2.2 Ladle Description

The ladle used in the demonstration has a nominal capacity of 85 tonnes of molten steel and is used to transport molten steel from the electric arc furnace (EAF) to a ladle furnace and then on to a continuous casting machine. The ladle shell is made of steel, includes tapping and flushing systems, and is lined with a refractory lining. The refractory lining consists of a working lining supported by a safety lining, both built up with different types of refractory bricks. The purpose of ladle preheating is to raise the temperature of the refractory in a controlled manner to avoid thermal shock and ensure readiness for molten steel handling.

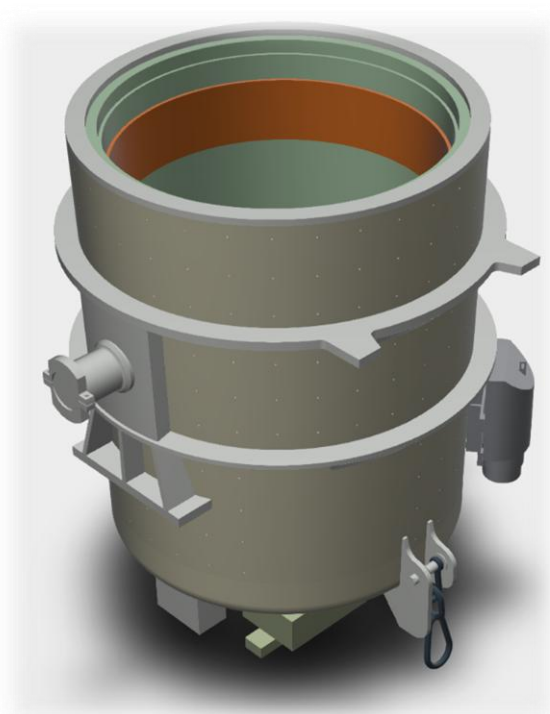


Figure 125. 3D-model of the ladle.



Figure 126. Actual ladle use during the demonstration.



Figure 127. View inside the ladle used in the demonstration. The worn refractory visible.

The ladle that has been subject of the demonstration had been used in production for approximately 70 production cycles, meaning that the refractory lining was aged and representative of a normal production cycle.

7.2.3 Ladle heater

The Grant Agreement originally foresaw retrofitting an existing ladle heater at the plant. However, detailed technical reviews revealed significant challenges. The existing CO-mix gas equipment of the existing ladle heater was not compatible with hydrogen and would have required extensive replacement of piping, safety components, and control hardware. This would mean an almost complete rebuild of the ladle heater. In addition, the burner management and process control system would have needed substantial redesign and

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recertification to meet hydrogen safety standards. The combined cost, complexity, and impact on production made this approach impractical.

Therefore, it was decided to install a new hydrogen-capable ladle heater which turned out to cost half that was budgeted for the retrofit. The chosen 1 MW vertical heater supports operation with either CO-mix gas or hydrogen and can be configured for both air-fuel and oxy-fuel combustion. The dual-fuel design enables direct comparison between conventional and hydrogen-based heating within the same physical setup, thereby providing an optimal platform for research measurements.

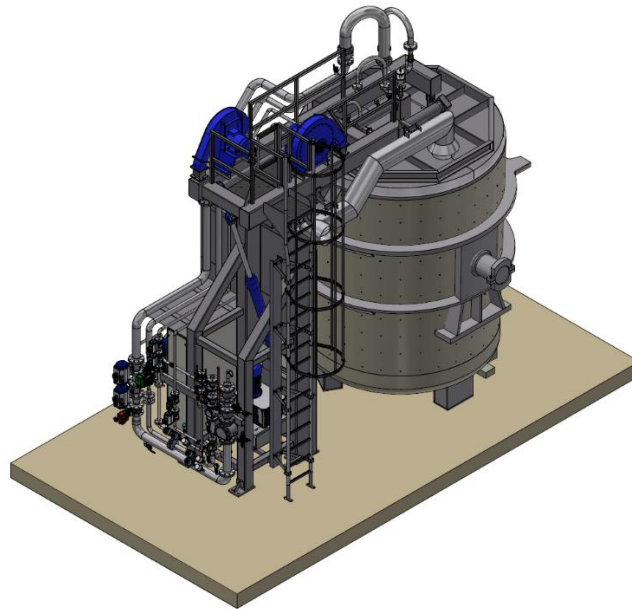


Figure 128. 3D-model of the ladle heater used in the demonstration.



Figure 129. Photo of piping and instrumentation on the rear side of the ladle heater.



Figure 130. Photo of the ladle heater shortly after installation on site.

7.2.4 CO-mix gas

The main fuel for ladle heating at 7 Steel Nordic manufacturing is CO-mix gas. This is a byproduct of a neighbouring plant producing ferro manganese (FeMn). The composition of the CO-mix gas already contains a fair amount of hydrogen gas, especially from a volume perspective.

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Table 35. Composition of the CO-mix gas:

Component	Weight percent	Volume percent
Carbon monoxide (CO)	79,6wt%	38,2%
Hydrogen (H ₂)	8,6wt%	57,5%
Carbon dioxide (CO ₂)	7,7wt%	2,36%
Nitrogen (N ₂)	3,8wt%	1,83%
Oxygen (O ₂)	0,3wt%	0,13%

The CO gas mix has a lower heating value: $LHV_{\text{mix}} = 2.88 \text{ kWh/Nm}^3$

The CO-mixgas is fed to the steel plant through fixed piping at a pressure of approximately 50 mbar.

7.2.5 Hydrogen Supply

For the demonstration, a temporary set-up for the hydrogen was implemented. While the ladle heater itself was designed for hydrogen operation, no permanent hydrogen infrastructure was available at the site. Therefore, a temporary supply station and flexible hose connection were established.

Hydrogen was supplied by Linde using a high-pressure tube trailer with a capacity of approximately 4500 Nm³. The hydrogen gas was delivered at around 200 bar and reduced to approximately 3,5 bar using a dedicated pressure regulation system. From the regulator, hydrogen was transported to the ladle heater through metal-reinforced flexible hoses routed along elevated structures to avoid interference with plant traffic and operations.

The routing and installation were defined following a comprehensive risk assessment involving representatives from project management team, health and safety, production, maintenance, logistics, the fire department of the industrial park, the industrial park support department, and the hydrogen supplier Linde.

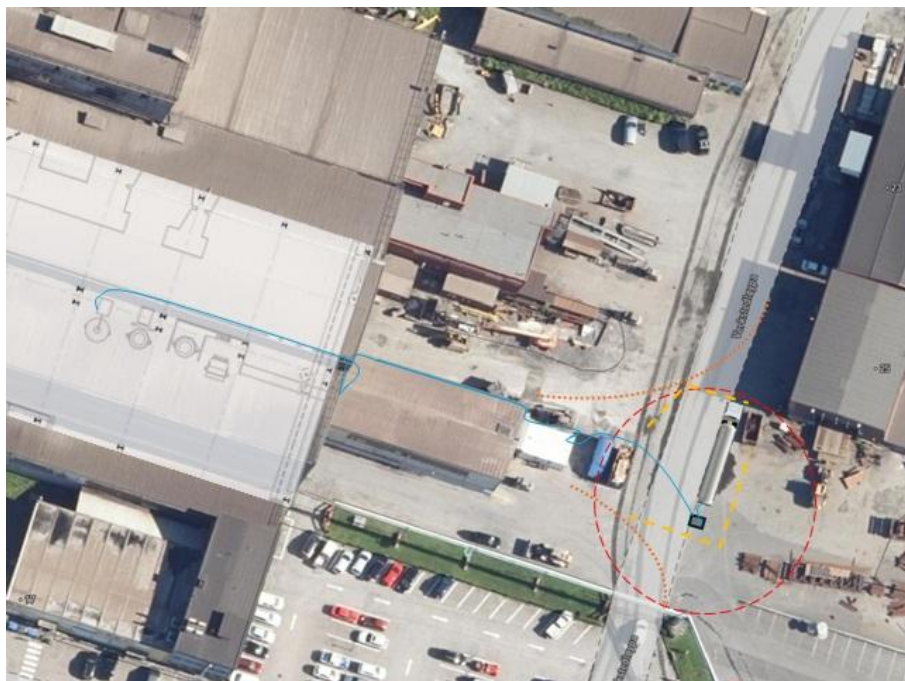


Figure 131. Sketch of the temporary hydrogen infrastructure with flexible hose in blue, the safety zone around the tube-trailer in red and the placement of barriers in yellow and orange.



Figure 132. Photo of the tube-trailer with hydrogen

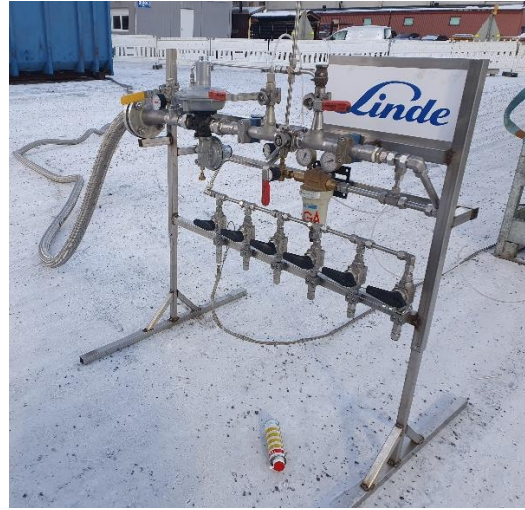


Figure 133. Photo of the pressure regulating system.

7.2.6 Oxygen Supply

Oxygen was supplied through the plant's existing infrastructure at a pressure of approximately 10 bars and at flow rates suitable for oxy-fuel combustion. The same oxygen system was used for both hydrogen and CO-mix gas operation to ensure consistency between tests.

7.2.7 Safety of the demonstrator

During the planning of the demonstration, strong emphasis was placed on safety, particularly regarding the hydrogen supply system. An extensive risk assessment was conducted, supported by expert input including from Linde. Additionally Deliverable D3.2 "Modular training courses and material for safe operations of H₂ heating" was used as a reference.

The tube trailer was equipped with dedicated safety systems, and the pressure regulation system included both a pressure relief valve and a slam-shut valve to prevent over-pressurization of the line to the ladle heater. A vent stack was installed with a designated safety zone free of ignition sources and flammable materials. In the event of venting, hydrogen would be released vertically. While ignition scenarios were considered, the risk was deemed low due to the outdoor location. During hydrogen operation, the installation was continuously monitored.

To feed the hydrogen gas from the pressure regulation rig to the ladle heater inside the plant building three lengths of metal reinforced flexible hose were used, two of 40 meters and one of 25 meters. The hoses had an outside diameter of approximately 65 mm and were connected with DN50 PN16 flanges. The first 20-meter metal reinforced flexible hose was routed over ground from the pressure regulation rig to the eastern edge of the plant building. There it was connected to the first 40 m hose which was led up and attached along the outside wall of the smaller building. In this way the hose was clear of the ground which is used as an entrance way into the building, while the flange connection was still accessible for inspection. Next, the hose was led up the eastern side of the main building up to the 12-meter level. Here the hose went through a hole in the wall and was routed over the main horizontal truss supporting the overhead cranes. Here the next flange connection was situated, accessible from the walkway. Close to the location of the ladle heater, the flexible hose was led down along the vertical truss to the fixed flange on the rear side of the ladle heater. By routing the flexible hoses in this way, the hoses and flange connections were clear of all traffic areas with vehicles and personnel. A possible leakage along the hoses would be high up, allowing the hydrogen to disperse quickly upwards and out of the building, helped by the open roof structure.

Prior to operation, the hoses were purged with nitrogen and pressure-tested to verify tightness and ensure safe operation.

7.3 Experimental Methodology

7.3.1 General plan

The process investigated in this demonstration is the preheating of a used ladle prior to its return to production. This would be the case after a medium length production stop, for example a weekend stop. As the ladle had already been in operation, the refractory lining did not require curing, and the focus of the demonstration was solely on the preheating phase.

The initial experimental plan consisted of one heating cycle using hydrogen and one using CO-mix gas, which serves as the conventional baseline at the plant. The number of hydrogen tests was limited by the available hydrogen supply. In contrast, the CO-mix gas tests had to be repeated three times in order to obtain a reliable dataset, as the first two attempts were affected by technical issues.

The main objectives of the demonstration were:

- To demonstrate the feasibility of hydrogen/oxygen combustion for industrial ladle preheating
- To achieve stable heating of a ladle from ambient temperature to 1200 °C under realistic conditions
- To compare hydrogen performance with conventional CO-mix gas operation
- To assess emissions behaviour, with particular focus on NO_x formation
- To evaluate the impact of hydrogen combustion on refractory materials and overall system operation

7.3.2 Heating Procedure

All tests followed a common heating procedure representative of normal plant operation. The objective was to heat the ladle to the required temperature for receiving molten steel, defined as 1200 °C and measured under the lid of the ladle heater.

At the start of each test, the ladle temperature corresponded to ambient conditions, which during the campaign ranged between -10 °C and 0 °C. The total heating duration was set to ten hours, which reflects typical industrial practice for ladle preheating.

The ladle heater was controlled using a temperature setpoint of 1200 °C. During the initial heating phase, the burner operated at its maximum capacity of approximately 1 MW in order to reach the target temperature as efficiently as possible. Once the setpoint was reached, the control system automatically reduced the burner power to maintain a stable temperature. This phase, commonly referred to as the soaking phase, allows heat to distribute throughout the ladle structure, ensuring uniform temperature conditions in both the refractory lining and the steel shell.

7.3.3 Operation modes of the ladle heater

The ladle heater used as a demonstrator can operate with either CO-mix gas or hydrogen as fuel, combined with either air or oxygen as the oxidizer. The burner control system has two main operating modes: air-fuel mode and oxy-fuel mode. In addition, the system allows for the injection of supplementary air or oxygen through lances located in the lid. This functionality is primarily intended to support refractory curing in newly lined ladles, although this feature was not the focus of the demonstration.

To supply combustion air in air-fuel operation, as well as additional air through the lid lances, the ladle heater is equipped with two independently frequency-controlled fans located on the cantilever arm supporting the lid and burner assembly. These fans are an integral part of the system design and must remain in operation during all modes of operation.

To prevent backflow of hot combustion gases into the air ducts and to avoid overheating of the fans, the system requires that the fans operate at a minimum load even during oxy-fuel mode. As a result, a continuous parasitic air flow is introduced into the combustion chamber. Consequently, fully pure oxy-fuel combustion conditions cannot be achieved, as air is always entrained into the system.

This parasitic air flow is of particular importance for the interpretation of the experimental results, as it introduces nitrogen into the combustion environment. Under high-temperature conditions, this nitrogen can contribute to the formation of NO_x, even when operating nominally in oxy-fuel mode.

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7.3.4 Temperature Measurement Setup

To measure the temperature of the gases inside the ladle during heating, three thermocouples were installed at different predefined locations within the ladle. These came in addition to the thermocouple installed in the lid of the ladle heater, which is used for the burner control system.

The thermocouples in the ladle were installed in the upper section of the ladle wall, the lower section of the ladle wall, and in the bottom of the ladle, through the tapping hole. This configuration was selected to provide a representative measurement of the temperature distribution along the vertical axis of the ladle.

All three thermocouples were installed such that they protruded fully through the steel shell and refractory lining and were therefore directly exposed to the hot combustion gases inside the ladle. This ensured that the measurements reflected the gas-phase temperature conditions rather than the refractory temperature, allowing for better assessment of the heating performance.

All thermocouples were connected to the ladle heater control panel, ensuring that their data was available in the plant's main data logging system, where they were recorded together with the process and combustion data.



Figure 134. Photo of the two thermocouples in the ladle wall

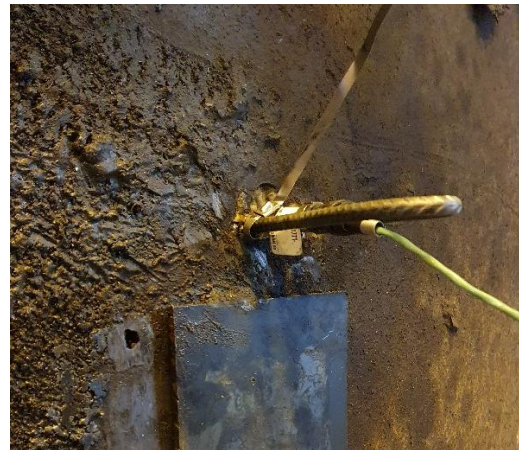


Figure 135. Close-up of the thermocouple in the lower section of the ladle wall



Figure 136. Photo of the thermocouple inserted through the tapping hole.

In addition to the fixed thermocouple measurements, surface temperature measurements of the ladle shell were performed manually using a handheld infrared thermometer. These measurements were taken near the thermocouple locations at approximately hourly intervals throughout the ten-hour heating cycles. The intention was to provide an indicative assessment of the external temperature distribution of the ladle.

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However, the measurements showed significant variability depending on the exact measurement position and surface conditions, resulting in limited accuracy. Consequently, these infrared measurements are considered qualitative and were used primarily for observational purposes rather than quantitative analysis.

7.3.5 Flue Gas and Stack Measurements

During the heating with hydrogen and the first heating with CO-mix gas, flue gas measurements were conducted using extractive gas analysis equipment installed and operated by Linde. The sampling point was in the flue gas channel directly above the ladle lid, positioned close to the burner head to capture representative exhaust gas conditions leaving the ladle, although later analysis indicated that dilution or sampling-location effects may have influenced the measured composition. Gas was extracted without heated sampling lines, resulting in measurements on a dry basis at approximately 2–3 °C after conditioning.

The flue gas composition was analysed using an ABB URAS NDIR (non-dispersive infrared) analyser, measuring concentrations of O₂, CO, and CO₂ over wide ranges (0 to 100% for O₂ and CO₂, and 0 to 3% for CO) with an uncertainty of approximately ±2%. NO measurements were also performed using the same analyser, although no separate NO₂ measurement was available.

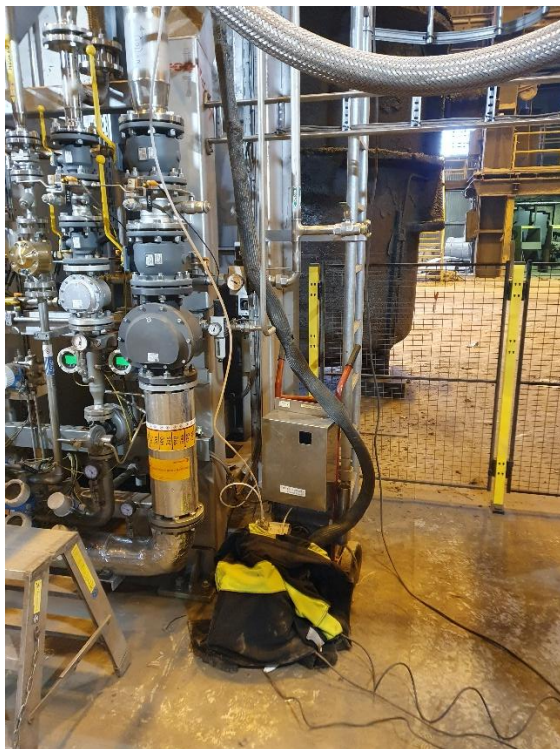


Figure 137. Photo of the gas sampling equipment.



Figure 138. Photo of the gas analyser.

The temperature, flow rate and pressure at the measurement location were not directly measured, and therefore quantitative mass flow-based emission calculations were not performed. Raw analyser values were retained, but selected periods affected by analyser instability or test disturbances were identified during interpretation. Where filtered or stable-period values are discussed, this is explicitly stated.

Overall, the measurement setup provided useful information of combustion behaviour and emission trends during hydrogen operation. However, limitations related to dry sampling, absence of heated lines, and incomplete measurement of nitrogen oxide species should be considered when interpreting the results, particularly with respect to absolute emission levels.

7.4 Refractory investigation

The transition from conventional fuel to hydrogen for ladle preheating introduces changes in the combustion atmosphere, most notably an increased concentration of water vapour and the absence of carbon-containing

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species. These changes may influence the degradation behaviour of refractory materials, particularly with respect to hydration reactions, decarburisation, and microstructural stability.

To investigate the effects of hydrogen combustion on refractory materials, two complementary investigations were performed within work package 6.3: one by RWTH-GHI and one by RHI-Magnesita. The RWTH-GHI investigation focused primarily on the chemical, mineralogical, and microstructural behaviour of refractory materials, whereas the RHI-Magnesita investigation focused on the macroscopic behaviour and mechanical performance of a broader range of refractory types.

The detailed results of the RWTH-GHI and RHI-Magnesita investigations are presented in the following chapters of this report.

7.4.1 Industrial Exposure Trials (RHI-Magnesita)

RHI-Magnesita conducted an industrially oriented investigation aimed at assessing the behaviour of a wide range of refractory materials under ladle preheating conditions.

The original plan was to expose dedicated sample sets to both CO-mix gas–oxygen combustion and hydrogen–oxygen combustion under identical industrial conditions. For this purpose, RHI-Magnesita designed and manufactured a metal test box containing multiple refractory samples, intended to be placed directly inside the test ladle during preheating.



Figure 139. Photo of the refractory sample box provided by RHI-Magnesita.



Figure 140. The refractory sample box inside the test ladle prior to the heating with CO-mix gas.

However, due to logistical constraints, the first test box arrived too late to be included in the hydrogen ladle heating test. Instead, this test box was placed in the ladle during a subsequent CO-mix gas heating cycle carried out later the same week. As a result, the industrial exposure under plant conditions was performed using CO-mix gas combustion.

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To complement this, RHI-Magnesita plans to expose a second set of samples to hydrogen–oxygen combustion in a controlled furnace environment at a later stage. This approach allowed comparison between conventional and hydrogen-based atmospheres, although under different experimental setups.

The investigation included a broad selection of refractory materials, representing typical industrial grades and bonding systems. The evaluation focused on:

- Visual inspection of samples before and after exposure
- Identification of degradation phenomena such as:
 - Decarburisation of carbon-containing materials
 - Surface reactions and discolouration
 - Cracking and structural deterioration

This approach provides a comparative and application-oriented assessment of refractory performance, with emphasis on macroscopic behaviour and material integrity under realistic or near-realistic conditions.

7.4.2 Laboratory Investigation of Doloma Refractories (RWTH-GHI)

In parallel, RWTH-GHI conducted a laboratory-based investigation with a focus on doloma-based refractory materials, which are commonly used in ladle linings and are known to be sensitive to hydration and water vapour exposure.

All samples used in this investigation were supplied by 7 Steel Nordic Manufacturing, ensuring direct relevance to the industrial demonstrator. The sample set included:

- New (unused) refractory materials:
 - Pentabrick C01
 - Sindoform K40
- Industrial reference samples after conventional operation:
 - CO-mix gas fired Pentabrick C01 and Sindoform K40
 - Extracted from a ladle lining after more than 70 production heats
- Samples exposed to hydrogen combustion:
 - New bricks placed inside the ladle prior to the hydrogen-oxygen heating test
 - Retrieved after completion of the test once the ladle had cooled sufficiently
 - Subsequently sent to RWTH-GHI for analysis



Figure 141. A Pentabrick C01 and Sindoform K40 sample in the prior to the hydrogen test.



Figure 142. One of the two refractory samples visible after the hydrogen test.

This unique combination of new, industrially used, and hydrogen-exposed materials enabled a direct comparison of refractory behaviour under different operating conditions.

The RWTH-GHI investigation focused on understanding fundamental degradation mechanisms associated with hydrogen combustion, particularly the effects of water vapour. Controlled laboratory experiments were conducted to isolate these effects under well-defined conditions.

Samples were exposed to water vapour-containing atmospheres at temperatures representative of the ladle preheating process:

- 1175 °C (target preheating temperature)
- 500 °C (intermediate temperature range)
- 200 °C (critical low-temperature range for hydration)

A comprehensive analytical methodology was applied, including:

- Thermodynamic modelling (FactSage) to predict phase stability
- Mass balance measurements to quantify degradation
- Microstructural analysis (SEM/EDX)
- Mineralogical characterisation (XRD)
- Chemical analysis (XRF, carbon content, loss on ignition)

The investigation aimed to identify key degradation mechanisms such as:

- Hydration of CaO to Ca(OH)₂ and associated volume expansion
- Carbon burnout in carbon-bonded materials
- Microstructural weakening and loss of bonding phases

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The two investigations provide complementary insights into refractory performance under hydrogen combustion conditions:

- The RHI-Magnesita investigation delivers a broad, application-oriented assessment, focusing on macroscopic behaviour and performance across multiple refractory types.
- The RWTH-GHI investigation provides a detailed mechanistic understanding, focusing on chemical reactions, phase transformations, and microstructural evolution, particularly in dolomite-based refractories.

Together, these approaches provide a comprehensive basis for evaluating the suitability of refractory materials for ladle heating using hydrogen. The results of both investigations, including observed degradation mechanisms and comparison between conventional and hydrogen-related effects, are presented and discussed in Section 6.

7.5 Key Performance Indicators (KPIs)

The performance of the ladle-heating demonstrator is evaluated using a reduced set of eight principal key performance indicators (KPIs). These KPIs were selected to provide a focused assessment of the most relevant aspects of the process, namely heating rate, temperature distribution, and energy and resource demand. The KPI framework is intended to support the HyInHeat objective of assessing whether hydrogen can provide technically feasible ladle preheating under industrial conditions while maintaining process performance comparable to conventional CO-mix gas operation.

The principal KPI comparison in the present report is based on the hydrogen test and CO test 3, which constitute the two complete datasets suitable for direct quantitative comparison. Supplementary observations from CO test 2 and indicative flue-gas results from CO test 1 are discussed elsewhere in the report, but they are not included in the formal KPI framework.

7.5.1 Thermal-performance KPIs

Time to 1200 °C (t_{1200})

This KPI represents the time required to heat the ladle from the initial recorded condition to the target temperature of 1200 °C, as measured by the thermocouple located in the lid. It is the principal indicator of overall heating rate.

Time to 800 / 1000 / 1100 °C

These three temperature thresholds are used as supporting indicators of the heating trajectory. They provide a more detailed description of the heat-up profile before the final setpoint is reached and therefore support interpretation of differences between fuels and tests.

Temperature uniformity (ΔT)

Temperature uniformity is defined here as the mean spread between the highest and lowest internal thermocouple temperatures during the final 30 min of active heating. This indicator is used to describe how evenly the ladle is heated during the final holding period.

Holding performance at setpoint

Holding performance is defined as the time during which the lid temperature remains at or above 1200 °C during active heating. This KPI provides an indication of the system's ability to maintain the process target once reached.

7.5.2 Energy and resource-demand KPIs

Total energy input (E_{total})

The total energy input is defined as the integrated burner power over the full logged heating cycle. It provides the principal measure of the total thermal demand of the ladle-heating process.

Energy input to first reaching 1200 °C (E_{1200})

This KPI represents the integrated burner power from the beginning of the test until the first time the lid temperature reaches 1200 °C. It is used to compare the energy demand associated with the heat-up phase.

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Fuel consumption

Fuel consumption is defined as the total measured fuel consumption over the heating cycle, reported in Nm³. Because different fuels are used in the hydrogen and conventional cases, this KPI is interpreted together with the energy input.

Oxygen consumption

Oxygen consumption is defined as the total measured oxygen consumption over the heating cycle, reported in Nm³. This KPI complements the fuel-consumption and energy-input indicators and reflects the total oxidant demand of the process.

7.5.3 KPI summary table

The eight principal KPIs used in the present evaluation are summarised in Table 36. The corresponding KPI results are presented in Section 7.5.3.

Table 36. Overview of principal KPIs used.

KPI	Description	Unit	Main interpretation basis
Time to 1200 °C (t ₁₂₀₀)	Time to reach lid setpoint	h	Hydrogen test vs CO test 3
Time to 800 / 1000 / 1100 °C	Supporting heat-up profile indicators	h	Hydrogen test vs CO test 3; CO test 2 supplementary
Temperature uniformity (ΔT)	Mean spread between thermocouples in the final 30 min of active heating	°C	Hydrogen test vs CO test 3
Holding performance at setpoint	Time with lid temperature ≥ 1200 °C during active heating	h	Hydrogen test vs CO test 3
Total energy input (E _{total})	Integrated burner power over full logged cycle	MWh	Hydrogen test vs CO test 3
Energy input to first 1200 °C (E ₁₂₀₀)	Integrated burner power until first reaching target	MWh	Hydrogen test vs CO test 3
Fuel consumption	Total fuel used during heating cycle	Nm ³	Hydrogen test vs CO test 3
Oxygen consumption	Total oxygen used during heating cycle	Nm ³	Hydrogen test vs CO test 3

7.6 Results and observations

7.6.1 Overview of the test campaign

The ladle heating campaign comprised one hydrogen heating test and three CO-mix gas heating tests. The hydrogen test was completed successfully and provided a complete dataset covering process temperatures, burner power, fuel and oxidant consumption, and flue gas composition. Of the three CO-mix gas tests, only the third test produced a valid and sufficiently consistent dataset and is therefore used as the conventional reference case in the present evaluation.

The tests were carried out during winter and early spring at the 7 Steel Nordic site in Mo i Rana. The conditions during the February campaign were particularly cold, with very low outdoor temperatures typical for the region. In addition, the ladle heater is located in a very open and naturally ventilated production building, meaning that the ambient temperature in the work area was only marginally higher than the outside temperature. Consequently, the ladle, the shell, and the surrounding equipment were exposed to conditions that were close to outdoor winter conditions during the test campaign. This should be taken into account when interpreting the heating curves, the external shell temperature development, and the observed condensation phenomena during hydrogen operation.

The hydrogen heating test was completed successfully overall. Prior to the start of ladle heating, the burner was tested without a ladle in place to verify ignition and general flame behaviour. As part of the normal operating sequence, the ladle heater starts in fuel-air mode, during which the hydrogen flame showed a luminous yellow appearance. After switching to fuel-oxygen operation, the flame became significantly brighter and exhibited a blue-to-white appearance. These observations are qualitative, but they confirm stable ignition and transition between the burner operating modes during the hydrogen test.



Figure 143. Photo of the flame during testing in hydrogen-air combustion mode.



Figure 144. Photo of the flame during testing in hydrogen-oxygen combustion mode.

The condensation during the hydrogen test was observed as a pronounced condensation cloud above the ladle almost immediately after burner start-up. The cloud persisted throughout the initial phase of the heating cycle and gradually diminished, disappearing between approximately 2 and 3 h after start-up. In addition, visible condensation and icing developed on parts of the ladle heater, particularly on the cantilever arm. These

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observations are consistent with the high water-vapour content of the hydrogen combustion products in combination with the very low ambient temperatures during the February campaign, and they illustrate an important practical aspect of hydrogen operation under cold industrial conditions.



Figure 145. A condensation cloud above the ladle heater shortly after start-up of the hydrogen test.



Figure 146. The ladle heater during the hydrogen test at around 3 hours after start-up.

The first CO-mix gas heating test did not provide a valid basis for thermal performance comparison because of an error in the scaling of the CO-mix gas flow measurement. Approximately halfway through the 10-hour heating cycle, it was identified that the actual fuel flow was significantly lower than the value displayed in the measurement system. As a result, the burner operated at a substantially lower power than intended during most of the test. Towards the end of the heating cycle, the fuel flow was manually adjusted and the burner power increased. Although the test is not suitable for direct comparison of heating performance, the final part of the dataset is used for indicative emission analysis at burner loads closer to those applied during the hydrogen test.

The second CO-mix gas test was carried out approximately one week later using the same ladle and the same thermocouple arrangement. In this test, no emission analysis equipment was available. The test was terminated prematurely due to a clogged gas filter, which caused burner shutdown. On that basis, CO test 2 cannot be used as a formal reference case for KPI comparison. However, supplementary analysis of the data indicates that the test still provides useful supplementary information on the CO-mix gas heating trajectory prior to failure. At the beginning of CO test 2, the internal thermocouple values were affected by wrapped signals associated with sub-zero temperatures; these values were therefore not used directly for quantitative comparison and were corrected only for graphical presentation.

The third CO-mix gas test was conducted later under comparable operational conditions and produced a stable and consistent temperature and process dataset. During this test, however, the burner stopped briefly between 09:26:30 and 09:34:14 due to low gas pressure. Following this short interruption, heating resumed and the test was completed successfully. No emission analysis equipment was available during this test, and therefore no flue gas measurements were performed. Despite the short interruption, this test constitutes the only valid conventional reference case and is used throughout the present report for comparison with the hydrogen test.

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Both valid tests, the hydrogen test and CO test 3, were performed using the same ladle heater, the same nominal setpoint temperature of 1200 °C measured in the lid, and the same programmed heating duration of 10 h. The comparison is therefore based on tests performed with the same equipment and general control philosophy, although not under fully identical starting conditions.

In particular, the initial thermocouple readings in the hydrogen test were slightly elevated due to a short burner function test performed immediately before the start of data logging. This burner test lasted less than 20 s and is considered to have resulted in only negligible heat transfer to the ladle. The slightly higher initial thermocouple values in the hydrogen dataset should therefore be interpreted as a measurement artefact rather than as evidence of a materially warmer ladle at the start of the test. For the hydrogen test, the recorded lid temperature at the beginning of the dataset was approximately 81 °C and the average temperature from the three internal thermocouples was approximately 39 °C. In the valid CO-mix gas reference test, the corresponding initial values were approximately 9 °C for the lid temperature and 11 °C for the average internal thermocouple temperature. For CO test 2, the initial internal thermocouple values were affected by wrapped signals associated with sub-zero temperatures and are therefore not used directly for quantitative comparison.

Table 37. Overview of the ladle heating test campaign

Test ID	Test date	Fuel	Oxidizer mode	Heating duration [h]	Role in evaluation	Remarks	Average outside temperature [°C]
H ₂ test	03.02.26	Hydrogen	Oxy-fuel operation with parasitic air from system fans	10.0	Hydrogen demonstration case	Complete dataset available, including emission analysis	- 15.7
CO test 1	05.02.26	CO-mix gas		10.0	Indicative emission case only	Incorrect scaling of fuel flow measurement; unsuitable for thermal comparison	- 24.2
CO test 2	09.02.26	CO-mix gas		5.8	Supplementary evidence only	Test interrupted due to clogged gas filter and burner shutdown	- 13.2
CO test 3	20.03.26	CO-mix gas		10.0	Conventional reference case	Valid thermal dataset; no emission analysis available	4.4

7.6.2 Internal temperature development and time to target temperature

Both fuels were capable of heating the ladle to the target lid temperature of 1200 °C. In the hydrogen test, the lid temperature first reached 1200 °C after 5.76 h, whereas the corresponding time in the valid CO-mix gas reference test was 8.00 h. Intermediate lid temperature levels were also reached earlier in the hydrogen test, with 800 °C, 1000 °C and 1100 °C recorded after 1.74 h, 3.47 h and 4.56 h, respectively. For the CO-mix gas reference test, the corresponding times were 2.45 h, 4.70 h and 6.00 h.

The hydrogen test further demonstrated stable operation around the target temperature once the setpoint had been reached. During active heating with the lid in position, the lid temperature remained at or above 1200 °C for 2.13 h and at or above 1180 °C for 4.18 h. In the valid CO-mix gas reference test, the corresponding durations were 1.98 h above 1200 °C and 2.44 h above 1180 °C. The maximum recorded lid temperature was 1220.8 °C in the hydrogen test and 1210.4 °C in the CO-mix gas test.

At the end of the hydrogen test, the recorded lid temperature decreased sharply. This decrease coincided with burner shutdown and the lid being moved out of heating position during the final part of the logging period. The final data points of the hydrogen test therefore do not represent stable soaking conditions. When the hydrogen test is evaluated over the last 30 min of active heating, the mean lid temperature was approximately 1199 °C, confirming stable holding close to the setpoint prior to shutdown. In the corresponding final 30 min of active heating for the CO-mix gas reference test, the mean lid temperature was approximately 1206 °C.

The average temperature from the three internal thermocouples during the final 30 min of active heating was approximately 1213 °C for hydrogen and 1251 °C for the CO-mix gas reference case. The corresponding mean temperature spread between the hottest and coldest internal thermocouples over that period was

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approximately 130 °C for hydrogen and 86 °C for the CO-mix gas reference case. This indicates that both tests achieved high internal gas temperatures, while the CO-mix gas reference test produced a somewhat more even internal temperature distribution in the final holding period.

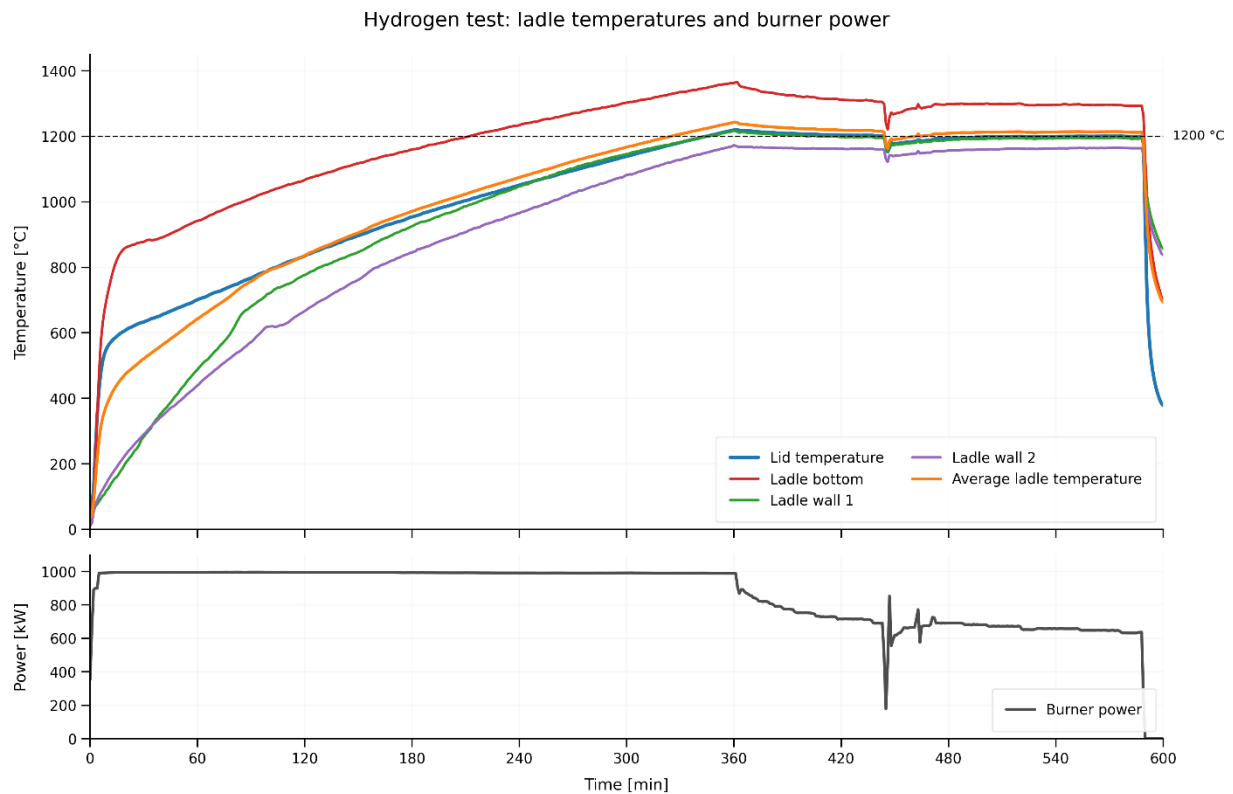


Figure 147. Temperature development and burner power during the hydrogen ladle heating test.



Figure 148. Photo of the lid of the ladle heater opening at the end of the hydrogen heating test.



Figure 149. Photo of the ladle heater roughly one minute after shutdown.

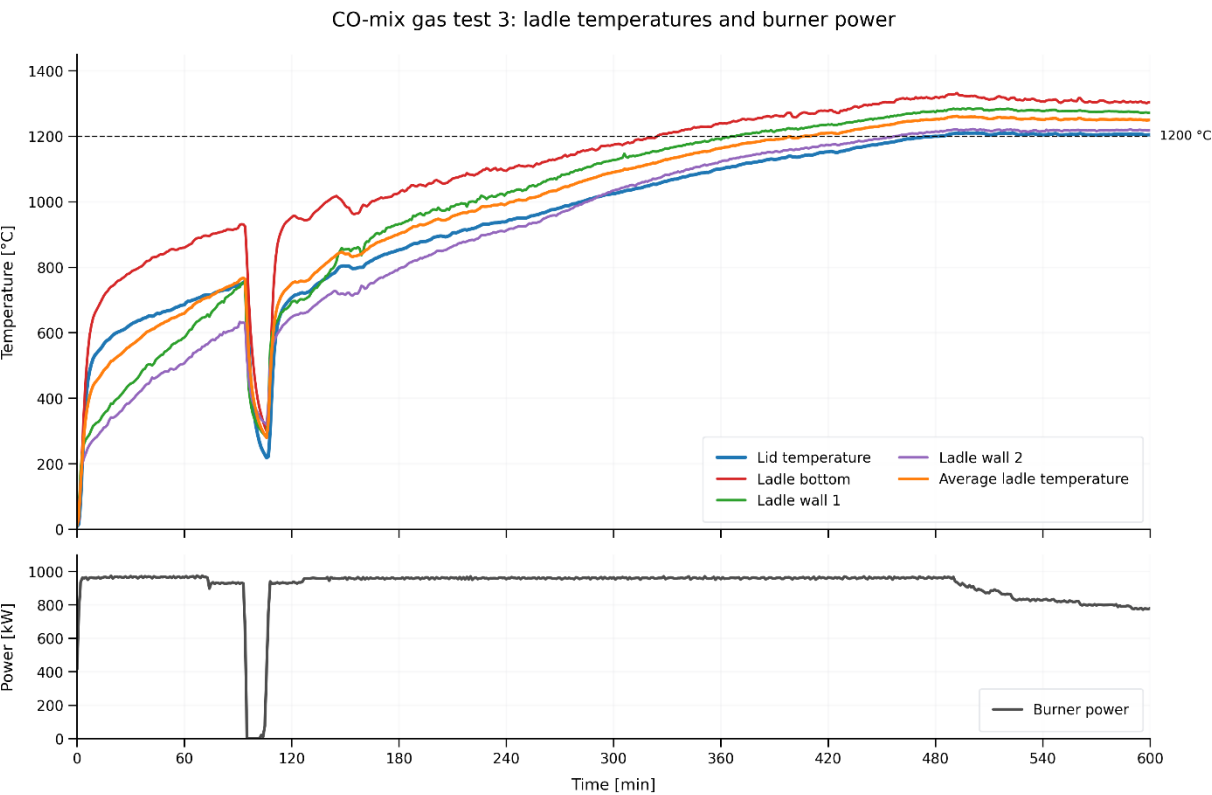


Figure 150. Temperature development and burner power during CO test 3.

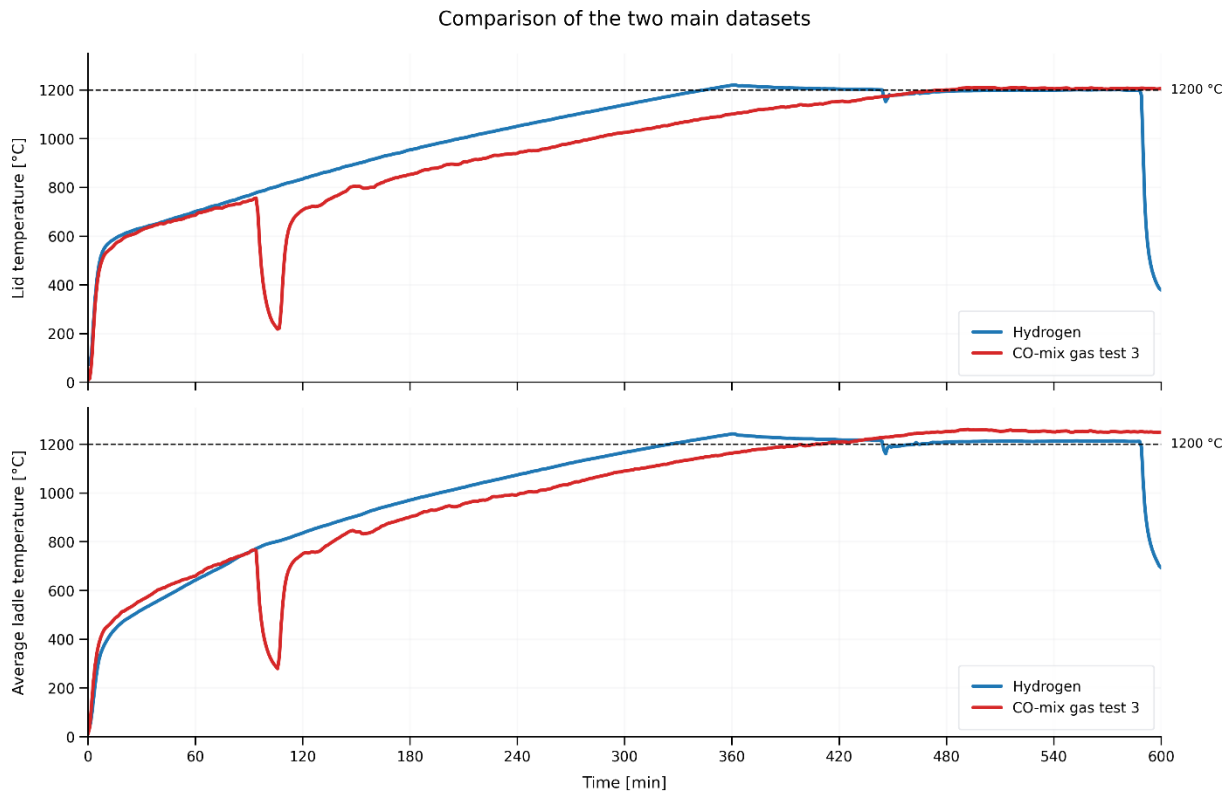


Figure 151. Comparison of lid temperature and average internal temperature for the hydrogen test and CO test 3.

7.6.3 Supplementary comparison of the hydrogen test, CO test 2, and CO test 3

In addition to the main comparison between the hydrogen test and CO test 3, the dataset from CO test 2 was analysed to assess whether it could provide further insight into the CO-mix gas heating behaviour. CO test 2 was not completed because the test failed due to a clogged gas filter. It is therefore not suitable as a formal reference case for the KPI comparison. However, the dataset remains useful as supplementary evidence when interpreting the relative heating rates and the sensitivity of the process to burner interruptions.

A data-quality limitation in CO test 2 must first be noted. At the beginning of the test, the internal thermocouple readings are clearly invalid, with recorded values in the order of several thousand degrees Celsius. These values are understood to be wrapped signals associated with sub-zero temperatures rather than physically real high temperatures. For graphical presentation, the initial wrapped thermocouple values were therefore transformed to approximate near-freezing temperatures based on the first physically reasonable readings, while the lid temperature and burner power were retained as measured. This correction was applied only to the initial presentation of the internal temperature curves and does not affect the interpretation of the later heating trajectory. The internal signals become physically reasonable from 09:44:36, approximately 109 s after the beginning of the recorded dataset.

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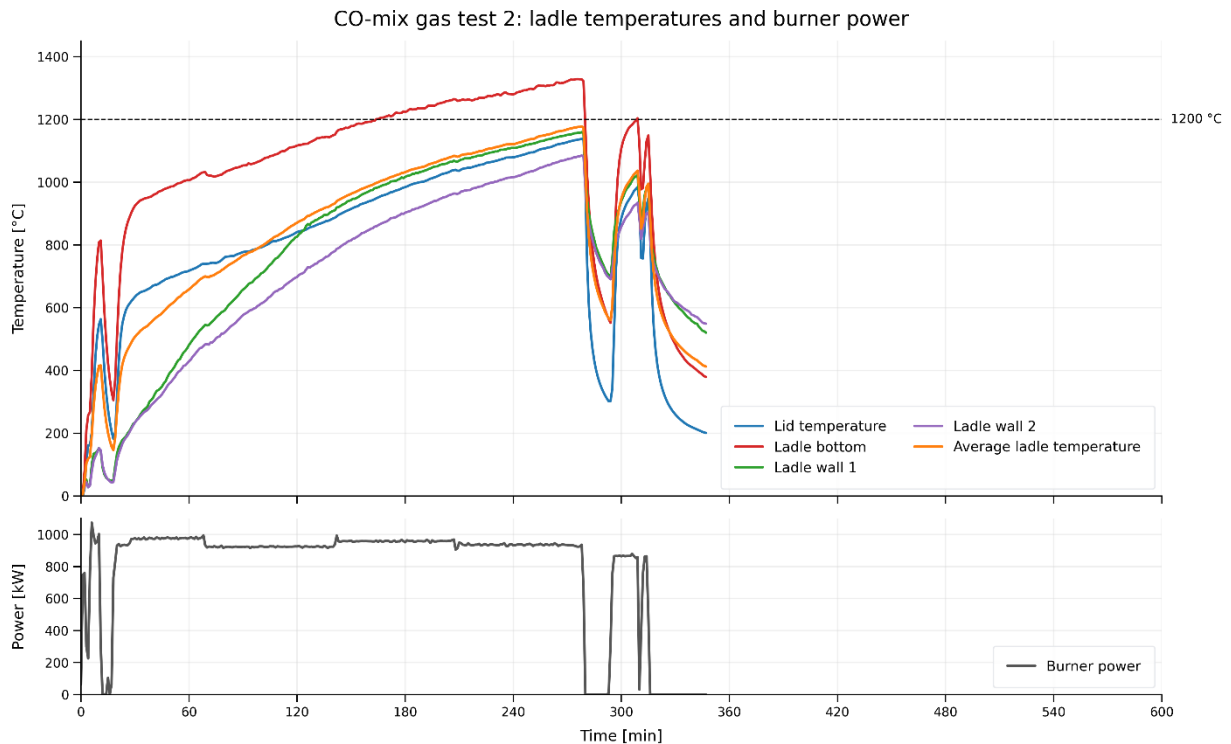


Figure 152. Temperature development and burner power during the second CO-mix gas test (CO test 2).

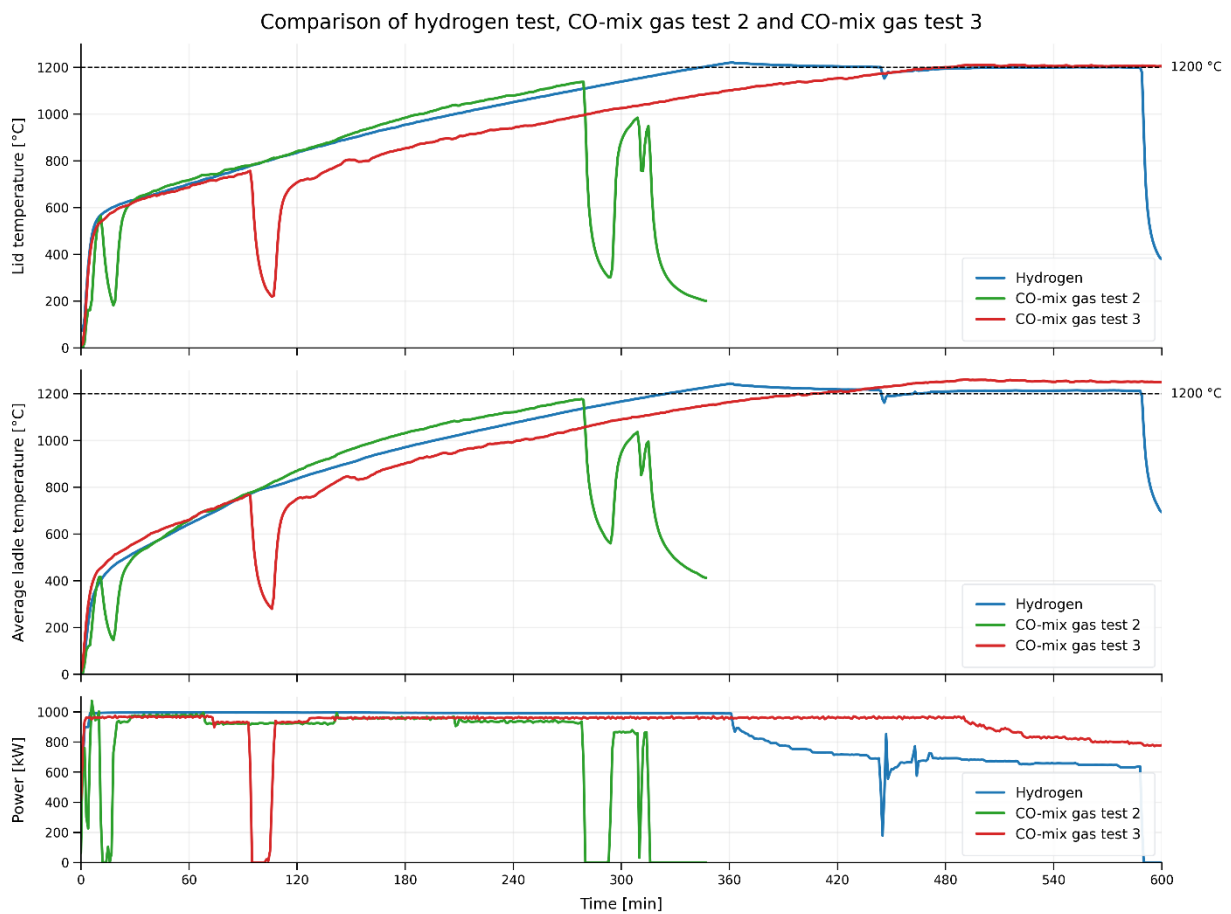


Figure 153. Comparison of lid temperature, average internal temperature, and burner power for the hydrogen test, CO-mix gas test 2, and CO-mix gas test 3.

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When the three tests are compared on the basis of lid temperature, CO test 2 follows a markedly steeper heating trajectory than CO test 3, and up to approximately 1100 °C it is close to, and in parts slightly faster than, the hydrogen test. The hydrogen test reached 800 °C, 1000 °C, and 1100 °C after 1.744 h, 3.468 h, and 4.561 h, respectively. The corresponding times for CO test 2 were 1.727 h, 3.147 h, and 4.228 h, whereas CO test 3 reached the same temperatures only after 2.450 h, 4.700 h, and 5.995 h. This indicates that the comparatively slow heat-up in CO test 3 is not representative of all CO-mix gas operation during the campaign.

The same conclusion becomes clearer when the tests are compared using active heating time, i.e. the cumulative time during which the burner was operating above low-load/shutdown conditions. On that basis, the hydrogen test reached 800 °C, 1000 °C, and 1100 °C after 1.740 h, 3.465 h, and 4.558 h of active heating, respectively. For CO test 2, the corresponding active-heating times were 1.587 h, 3.007 h, and 4.088 h, while CO test 3 required 2.258 h, 4.508 h, and 5.803 h. Thus, even after accounting for interruptions, CO test 2 remained substantially faster than CO test 3 and slightly faster than the hydrogen test up to the point where the test failed.

A comparison at fixed active-heating durations supports the same interpretation. After 1 h of active heating, the lid temperatures were approximately 700 °C for hydrogen, 739 °C for CO test 2, and 684 °C for CO test 3. After 2 h, the respective lid temperatures were approximately 833 °C, 857 °C, and 731 °C. After 4 h, the values were approximately 1050 °C for hydrogen, 1091 °C for CO test 2, and 951 °C for CO test 3. These values confirm that CO test 2 was consistently ahead of CO test 3 and remained at least comparable with the hydrogen test over most of the useful test period.

With respect to burner power, the adjusted CO test 2 data show that the active mean burner power was approximately 932.5 kW, which is very similar to the active mean burner power in CO test 3 (934.0 kW) and somewhat higher than in the hydrogen test (877.7 kW). The active mean burner power therefore does not support an interpretation that CO test 2 was faster simply because it was operated at an obviously higher burner load than CO test 3. Instead, the data suggests that process disturbances and the continuity of burner operation were major contributors to the slower overall performance seen in CO test 3.

CO test 3 included one clearly identifiable low-power interruption from 09:25:50 to 09:36:57, lasting approximately 11.1 min. Within this interval, the burner itself was stopped between 09:26:30 and 09:34:14. The effect of this interruption can be estimated by comparing the measured clock time and active-heating time to key temperature thresholds. For CO test 3, the difference between clock time and active-heating time was approximately 0.192 h both for reaching 800 °C, 1000 °C, 1100 °C, and 1200 °C, corresponding to roughly 11.5 min. This shows that the short stop had a real and measurable effect on the heating trajectory. However, the comparison with CO test 2 demonstrates that this penalty alone does not explain the much slower overall heat-up observed in CO test 3.

CO test 2 did not reach 1200 °C because the test ended prematurely. The maximum measured lid temperature in CO test 2 was approximately 1138.8 °C. Based on the stable pre-failure temperature trend in the latter part of the successfully heated portion of the test, a simple extrapolation indicates that CO test 2 would likely have reached 1200 °C after about 5.20 h of active heating if uninterrupted operation had been maintained. This extrapolated value should not be treated as a formal measured KPI, but it does strengthen the interpretation that the CO-mix gas system was capable of significantly faster heating than reflected by CO test 3 alone.

Taken together, the three-test comparison shows that the hydrogen test performed well and reached target temperature in a complete and stable manner. At the same time, the supplementary evidence from CO test 2 indicates that CO test 3 does not fully represent the intrinsic heating capability of the CO-mix gas system, and that the slower performance of CO test 3 was influenced by process-specific disturbances. CO test 2 should therefore not be used as a formal reference case in the KPI tables, but it does add valuable information for interpretation of the campaign results.

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7.6.4 External ladle shell temperatures measured by handheld infrared thermometer

In addition to the fixed thermocouples and process logging, external shell temperatures were measured manually using a handheld infrared thermometer at three positions: upper wall, lower wall and bottom. These measurements are considered qualitative only, since the temperature could vary significantly within a radius of approximately 50 cm around the intended measurement point, and the shell surface was uneven and partly covered by steel and slag deposits. This made it difficult to obtain fully consistent readings. Nevertheless, the data shows a clear and progressive increase in shell temperature throughout both tests. In both the hydrogen test and the valid CO-mix gas reference test, the highest shell temperatures were measured at the lower wall and the lowest at the bottom. After 10 h of heating, the measured shell temperatures in the hydrogen test were 371 °C at the upper wall, 406 °C at the lower wall and 114 °C at the bottom, while the corresponding values in CO test 3 were 357.8 °C, 413.1 °C and 171.1 °C. The measurements therefore confirm progressive heating of the shell in both tests, but they are not sufficiently precise for detailed quantitative comparison of heat penetration between the two fuels.

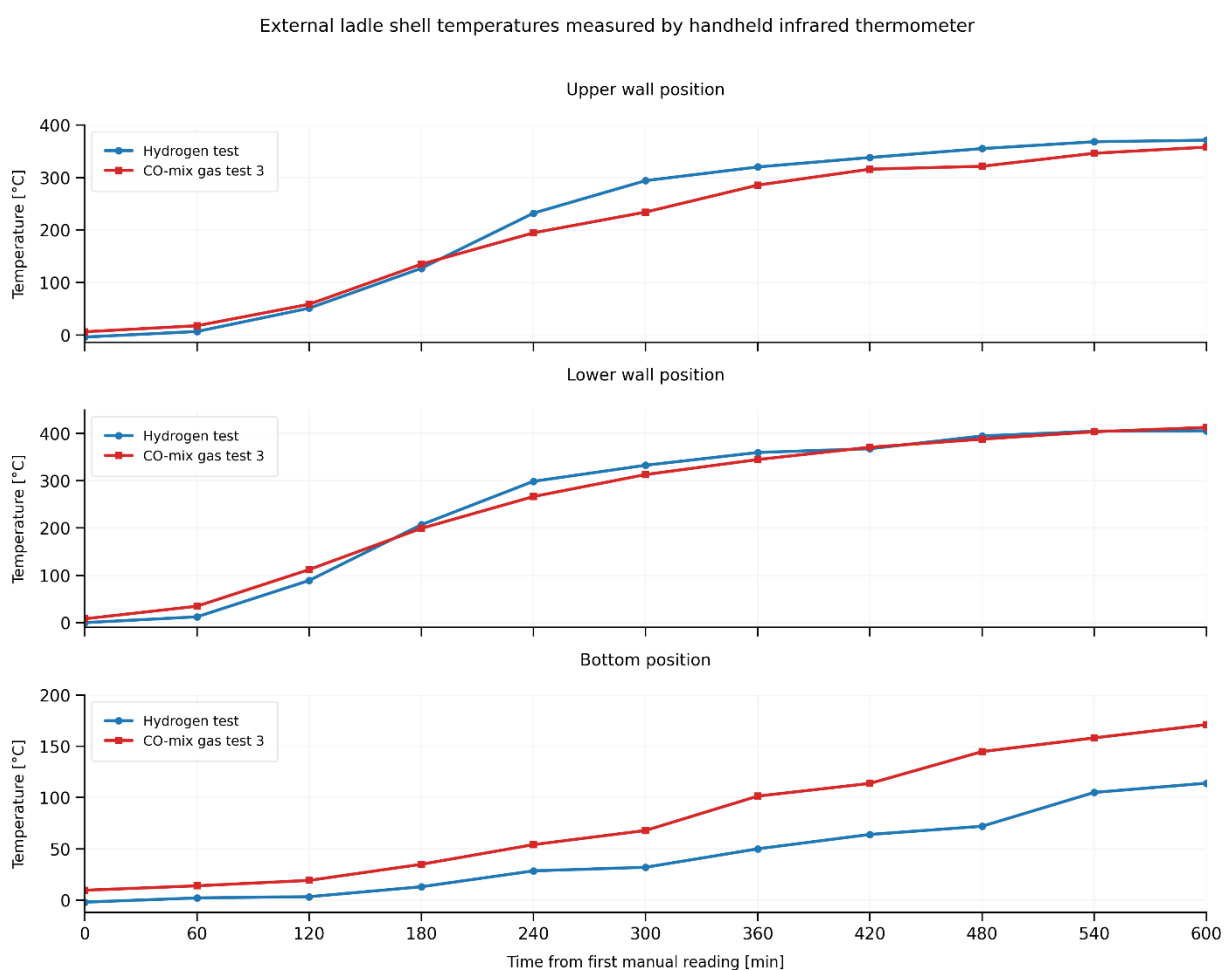


Figure 154. External ladle shell temperatures measured by handheld infrared thermometer at the upper wall, lower wall, and bottom positions during the hydrogen test and CO-mix gas test 3.

7.6.5 Energy input and fuel consumption

The integrated burner power over the full recorded heating cycle was 8.61 MWh for the hydrogen test and 9.16 MWh for the valid CO-mix gas reference test (CO test 3). On this basis, the total recorded thermal energy input during the hydrogen test was approximately 0.55 MWh lower than during the CO-mix gas reference test.

When the comparison is limited to the heating phase up to the point where the lid temperature first reached 1200 °C, the difference becomes more pronounced. In the hydrogen test, the recorded energy input up to

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first reaching the setpoint was 5.70 MWh, whereas the corresponding value for CO test 3 was 7.47 MWh. The recorded energy demand to reach target temperature was therefore approximately 1.77 MWh lower in the hydrogen test.

The total recorded fuel consumption over the 10-hour heating cycle was 2869.6 Nm³ hydrogen in the hydrogen test and 3181.0 Nm³ CO-mix gas in CO test 3. The corresponding oxygen consumption values were 1454.2 Nm³ for hydrogen and 1585.5 Nm³ for CO-mix gas, while the recorded air consumption was 1421.8 Nm³ and 1206.3 Nm³, respectively. These values reflect the actual operation of the burner and associated auxiliary air flow under the specific test conditions.

The average burner power over the full logged cycle was 860.8 kW in the hydrogen test and 916.1 kW in CO test 3. Both tests showed the expected general pattern of high burner load during the main heat-up phase followed by lower burner load during the soaking period once the setpoint had been reached.

The energy comparison should, however, be interpreted with some care. As discussed in Section 6.1, the tests did not start from fully identical initial conditions, and the initial thermocouple readings in the hydrogen test were slightly elevated due to a very short burner function test immediately before the start of data logging. This short test lasted less than 20 s and is considered to have caused negligible heat transfer to the ladle. The slightly higher initial temperature readings in the hydrogen dataset should therefore be regarded primarily as a measurement artefact rather than as evidence of a materially warmer ladle.

In addition, CO test 3 included a short burner interruption caused by low gas pressure. This interruption most likely had some influence on the recorded heating trajectory and therefore on the time-based comparison with the hydrogen test. In particular, the short shutdown will have slightly prolonged the recorded time to target temperature and may also have introduced a small penalty in the recorded total energy demand by allowing the ladle and combustion chamber to cool before heating resumed. The CO-mix gas dataset should therefore be interpreted as a valid industrial reference case, but one that includes a short operational disturbance not present in the hydrogen test.

Overall, the recorded data indicate that the hydrogen test achieved the required heating duty with a lower measured energy input and lower measured fuel and oxygen consumption than the valid CO-mix gas reference test. At the same time, the influence of differing initial conditions and the short interruption in CO test 3 should be taken into account when interpreting the quantitative differences.

7.6.6 Flue-gas and emission behaviour

Flue-gas data were available for the hydrogen test and for CO test 1. However, the CO test 1 dataset must be interpreted with caution because the test did not provide a valid thermal performance comparison: the CO-mix gas flow measurement was incorrectly scaled, which caused the burner to operate at substantially lower power than intended during most of the heating period. In addition, the flue-gas analyser returned usable values only during a limited part of the CO test 1 dataset, mainly towards the end of the test after the fuel flow had been manually increased. Accordingly, the CO test 1 flue-gas results are considered indicative only and are used here to support interpretation of combustion behaviour rather than for formal KPI comparison. Additionally, the emission results should be interpreted in the context of the parasitic air flow described in Section 3.3.

For the hydrogen test, the mean measured NO concentration during periods with burner power above 100 kW was approximately 2892 ppm on a dry basis, with a maximum recorded value of 3660 ppm. During periods where the lid temperature was at or above 1100 °C and the burner power exceeded 100 kW, the mean NO concentration was approximately 2988 ppm, while the corresponding dry O₂ concentration in the flue gas was approximately 22.1 vol%. Over all periods with burner power above 100 kW, the mean dry O₂ concentration was approximately 22.7 vol%, while the mean measured CO₂ concentration was only about 0.73 vol% and the CO concentration remained essentially 0 vol%.

The visible condensation cloud and the local icing observed on the ladle-heater cantilever arm during the hydrogen test provide qualitative confirmation of the high moisture content of the exhaust gas under the prevailing winter conditions. These observations are consistent with hydrogen combustion, which generates water vapour as the principal combustion product, and they further illustrate that the behaviour of the

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demonstrator must be interpreted in the context of both fuel chemistry and site-specific operating conditions. While the condensation and icing observations do not constitute quantitative flue-gas measurements, they support the conclusion that the exhaust from hydrogen operation was strongly influenced by moisture-related effects, which are of practical relevance for interpretation of the flue-gas behaviour and for the design of future industrial hydrogen-heating systems.

For CO test 1, the flue-gas data that could be interpreted as valid were obtained mainly in the later part of the test. Over all periods with burner power above 100 kW for which usable analyser data were available, the mean dry O₂ concentration was approximately 17.3 vol%, the mean NO concentration was about 168 ppm, the mean CO₂ concentration was about 1.5 vol%, and the mean CO concentration was close to 0 vol%. In the final higher-power part of the test, where burner power exceeded 900 kW, the mean burner power was approximately 954 kW, the mean dry O₂ concentration was approximately 10.4 vol%, and the mean NO concentration was approximately 578 ppm, although the valid analyser coverage in this high-power segment was limited to only about 0.39 h. The maximum recorded NO concentration in the usable CO test 1 data was 2499 ppm, but this value should be interpreted cautiously because of the generally limited and unstable measurement period.

A key observation in both datasets is the strong influence of the parasitic air flow from the two burner fans, which had to remain in operation at minimum power to cool themselves and the air ducts. The recorded total air flow during combustion was substantial in both tests. For the hydrogen test, the mean total air flow during periods with burner power above 100 kW was approximately 144 Nm³/h, while the corresponding mean hydrogen flow and oxygen flow were approximately 293 Nm³/h and 148 Nm³/h, respectively. For CO test 1, the corresponding mean total air flow, CO-mix gas flow, and oxygen flow were approximately 147 Nm³/h, 222 Nm³/h, and 143 Nm³/h, respectively. In the higher-power segment of CO test 1, the mean total air flow increased further to about 191 Nm³/h, together with a mean fuel flow of about 331 Nm³/h and a mean oxygen flow of about 198 Nm³/h. These values show that the parasitic air flow was not a minor leakage term, but a significant additional gas flow entering the combustion chamber.

This has two important implications for interpretation of the flue-gas data. First, the parasitic air markedly diluted the combustion products, which tends to increase the measured dry O₂ concentration and reduce the measured CO₂ concentration. This is especially evident in the hydrogen test, where the dry O₂ concentration remained around 22 vol% while the measured CO₂ concentration stayed below 1 vol%, even though hydrogen combustion itself produces no CO₂. The measured CO₂ in the hydrogen flue gas should therefore be interpreted mainly as a consequence of background contributions, analyser behaviour, and gas dilution rather than as a direct combustion product. In CO test 1, the combination of relatively high dry O₂ and still modest dry CO₂ values likewise indicates substantial dilution by unintended air ingress.

Second, the parasitic air flow introduced a significant amount of nitrogen into a system that would otherwise have operated closer to oxy-fuel conditions. This is highly relevant for NO formation. In the hydrogen test, the elevated combustion temperatures combined with the nitrogen introduced by the fan air provide a plausible explanation for the high measured NO concentrations. The results therefore should not be interpreted as intrinsic NO emissions from a fully sealed oxy-fuel hydrogen system, but rather as emissions from a burner arrangement in which a large parasitic air contribution was present throughout the test.

The CO test 1 results must also be interpreted in the light of this burner configuration. The lower mean NO concentrations observed in the usable CO test 1 data do not by themselves demonstrate that CO-mix gas would necessarily produce lower NO emissions than hydrogen under otherwise identical conditions. CO test 1 differed from the hydrogen test in several important respects: most of the heating cycle was run at too low burner power due to the flow-scaling error, the usable emission data were obtained only during a limited part of the test, and the thermal trajectory was not directly comparable with the hydrogen test. In addition, because the parasitic air flow represented a substantial fraction of the total gas entering the chamber, the flue-gas composition in both tests was strongly shaped by dilution and unintended nitrogen ingress. The emission data should therefore be interpreted primarily as demonstrator-specific burner-system results rather than as pure fuel-property comparisons.

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Taken together, the flue-gas results show that the current burner arrangement had a decisive influence on both composition and emissions. The two burner fans running at minimum power supplied enough parasitic air to materially alter the dry O_2 and CO_2 levels in the flue gas and to create conditions for significant NO formation, especially during hydrogen firing. Consequently, the measured flue-gas data are highly informative for understanding the behaviour of the demonstrator as installed, but they should not be used as a direct basis for comparing the intrinsic emission potential of hydrogen and CO-mix gas under ideal oxy-fuel conditions. For future development, reduction of parasitic air ingress is therefore likely to be one of the most important measures both for improving flame-control conditions and for obtaining more representative emission data.

7.6.7 Summary of KPI results

The principal KPI results confirm that hydrogen can be used successfully for ladle preheating under industrial conditions. In the formal comparison against the valid conventional reference case (CO test 3), the hydrogen test reached the target lid temperature of 1200 °C more rapidly and with a lower recorded energy input. Hydrogen reached 1200 °C after 5.756 h, while CO test 3 reached the same temperature after 7.997 h.

The supporting heating-profile KPIs show the same trend. The hydrogen test reached 800 °C, 1000 °C, and 1100 °C after 1.744 h, 3.468 h, and 4.561 h, respectively. The corresponding values for CO test 3 were 2.450 h, 4.700 h, and 5.995 h. These results indicate a consistently faster heat-up trajectory in the hydrogen test across the full heating phase.

With respect to holding performance, the hydrogen test maintained lid temperature at or above 1200 °C for 2.131 h of active heating, whereas CO test 3 maintained the same condition for 1.982 h. This indicates that both systems were capable of sustained operation around the target temperature, while the hydrogen test showed a somewhat longer effective holding duration at setpoint.

The temperature-uniformity KPI shows that both fuels achieved high internal ladle temperatures during the final stage of active heating, but that the conventional reference case produced a more even internal temperature distribution. The mean internal temperature spread during the final 30 min of active heating was 130.5 °C for the hydrogen test and 86.4 °C for CO test 3. This suggests that the CO-mix gas reference case provided a somewhat more homogeneous thermal field inside the ladle during the final holding period.

In terms of total energy demand, the integrated burner power over the full logged heating cycle was 8.61 MWh for the hydrogen test and 9.16 MWh for CO test 3. When the comparison is limited to the heating phase up to first reaching 1200 °C, the corresponding energy inputs were 5.697 MWh and 7.465 MWh, respectively. The KPI results therefore indicate that, in the present campaign, hydrogen achieved the target heating duty with a lower recorded energy input than the valid CO-mix gas reference case.

The resource-demand KPIs show the same general pattern. Total measured fuel consumption was 2869.6 Nm^3 hydrogen in the hydrogen test and 3181.0 Nm^3 CO-mix gas in CO test 3. Total oxygen consumption was 1454.2 Nm^3 for hydrogen and 1585.5 Nm^3 for CO test 3. These values confirm that the hydrogen test required lower recorded fuel and oxygen consumption over the full cycle than the valid conventional reference case.

The KPI results should nevertheless be interpreted in the context of the full campaign. The principal comparison is based on the two complete tests, but the supplementary analysis of CO test 2 indicates that CO-mix gas heating can proceed substantially faster than observed in CO test 3 if stable operation is maintained. Accordingly, the KPI comparison demonstrates the strong performance of hydrogen in the present campaign, while also indicating that the measured conventional reference result was influenced by operational disturbances.

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Table 38. Summary of principal KPI results for the hydrogen vs CO-mix gas comparison

KPI	Hydrogen test	CO-mix gas test 3	Comment
Time to 1200 °C [h]	5.756	7.997	Faster in hydrogen test
Time to 800 °C [h]	1.744	2.450	Faster in hydrogen test
Time to 1000 °C [h]	3.468	4.700	Faster in hydrogen test
Time to 1100 °C [h]	4.561	5.995	Faster in hydrogen test
Temperature uniformity, mean internal spread in final 30 min [°C]	130.5	86.4	Lower value = more even temperature distribution
Holding performance: time with lid ≥ 1200 °C [h]	2.131	1.982	Active heating only
Total energy input [MWh]	8.61	9.16	Full logged cycle
Energy input to first 1200 °C [MWh]	5.697	7.465	Heat-up demand to setpoint
Total fuel consumption [Nm ³]	2869.6 H ₂	3181.0 CO-mix gas	Different fuels and lower heating values
Total oxygen consumption [Nm ³]	1454.2	1585.5	

The formal KPI comparison is based on the hydrogen test and CO test 3, which are the only complete and valid datasets for direct thermal comparison. Supplementary evidence from CO test 2 indicates a substantially faster CO-mix gas heat-up trajectory than that observed in CO test 3, but CO test 2 is not included in the formal KPI table because the test was not completed.

7.7 Discussion

7.7.1 General performance of hydrogen as ladle-heating fuel

The demonstration confirms that hydrogen can be used successfully for industrial ladle preheating. In the formal comparison against the valid conventional reference case, CO test 3, the hydrogen test reached the target lid temperature of 1200 °C more rapidly and with a lower recorded energy input. Hydrogen reached 1200 °C after 5.756 h, compared with 7.997 h for CO test 3. The corresponding total recorded energy inputs were 8.61 MWh for hydrogen and 9.16 MWh for CO test 3. The hydrogen test also reached the intermediate temperature levels of 800 °C, 1000 °C, and 1100 °C earlier than CO test 3. These results show that hydrogen operation provided strong thermal performance in the present campaign.

The results should nevertheless not be interpreted as a universal efficiency ranking between hydrogen and CO-mix gas. The hydrogen test and CO test 3 were the only complete datasets suitable for formal comparison, but the tests were not conducted under fully identical starting and operating conditions. The hydrogen dataset started with slightly elevated measured temperatures due to a short burner function test performed immediately before logging. This is considered to have caused negligible heat transfer to the ladle, but it remains a minor difference between the tests. In addition, CO test 3 included a short burner interruption. The hydrogen results therefore demonstrate technical feasibility and strong performance under the conditions of this campaign, while the quantitative comparison should be interpreted with the uncertainty typical of industrial demonstration testing.

7.7.2 Interpretation of the supplementary CO test analysis

The supplementary analysis of CO test 2 provides important context for interpreting the formal comparison. If only the hydrogen test and CO test 3 are considered, hydrogen appears clearly faster. However, CO test 2 showed a significantly steeper heating trajectory than CO test 3 before the test was stopped. On a clock-time basis, CO test 2 reached 800 °C, 1000 °C, and 1100 °C after 1.727 h, 3.147 h, and 4.228 h, respectively, compared with 2.450 h, 4.700 h, and 5.995 h for CO test 3. On an active-heating-time basis, CO test 2 was also substantially faster than CO test 3 and was in parts comparable to, or slightly faster than, the hydrogen test up to the point of failure.

This indicates that the slower heat-up observed in CO test 3 cannot be attributed to fuel type alone. The short low-power interruption in CO test 3 had a measurable effect, but it explains only part of the difference. Other factors, such as gas-supply stability, burner response, process control behaviour, or general operating stability, must also have influenced the result. CO test 2 should not be used as a formal KPI reference because it was incomplete and affected by initial thermocouple signal issues. Nevertheless, it shows that CO-mix gas operation was capable of faster heating than indicated by CO test 3 alone.

The formal KPI comparison therefore remains valid for the two complete datasets, but it should be understood as a comparison of the actual campaign results rather than a definitive comparison of the maximum achievable performance of the two fuels. The hydrogen test demonstrated stable and rapid heating, while the CO-mix gas tests showed that conventional operation was more affected by practical disturbances during the campaign.

7.7.3 Temperature distribution and setpoint holding

Both the hydrogen test and CO test 3 achieved high internal temperatures and maintained operation close to the target lid setpoint during the final active-heating period. The hydrogen test held the lid temperature at or above 1200 °C for 2.131 h, while CO test 3 maintained the same condition for 1.982 h. The mean lid temperatures during the final 30 min of active heating were approximately 1200.0 °C for hydrogen and 1205.8 °C for CO test 3. This confirms that both fuels were capable of maintaining stable high-temperature operation once the setpoint had been reached.

The temperature-uniformity results indicate that CO test 3 produced a more even internal temperature distribution during the final holding period. The mean spread between the internal thermocouples during the final 30 min of active heating was 130.5 °C for hydrogen and 86.4 °C for CO test 3. The larger spread during hydrogen operation may be related to flame characteristics, gas-flow pattern, burner-ladle interaction, or local heat-transfer differences. The available process data do not allow the cause to be identified in detail, but

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the result is relevant for future burner optimisation and for ensuring uniform ladle heating during hydrogen operation.

7.7.4 Flue-gas behaviour, parasitic air, and emission interpretation

The flue-gas and emission results must be interpreted in relation to the actual burner configuration used in the demonstration. Although the burner was operated in oxy-fuel mode, the two burner fans had to remain in operation at minimum load to cool the fans and air ducts and to prevent backflow of hot gases. This introduced a continuous parasitic air flow into the combustion chamber. For the hydrogen test, the mean total air flow during periods with burner power above 100 kW was approximately 144 Nm³/h, compared with mean hydrogen and oxygen flows of approximately 293 Nm³/h and 148 Nm³/h. For CO test 1, the corresponding mean total air flow, corrected CO-mix gas flow, and oxygen flow were approximately 147 Nm³/h, 222 Nm³/h, and 143 Nm³/h. The parasitic air flow was therefore a significant part of the total gas flow and had an important influence on both flue-gas composition and NO formation.

During hydrogen operation, the measured dry O₂ concentration was high, with a mean value of approximately 22.7 vol% during periods with burner power above 100 kW. This high dry O₂ level cannot be explained by parasitic air alone, since air contains only about 20.9 vol% O₂. Instead, it results from the combined effect of excess oxygen supplied in oxy-fuel mode, oxygen introduced with the parasitic cooling air, and dry-basis measurement after removal of water vapour. This effect is particularly important for hydrogen combustion because the main combustion product is water vapour. Once the sample is dried, the remaining dry gas consists mainly of residual O₂ and nitrogen from the parasitic air.

The hydrogen test also showed high NO concentrations. During periods with burner power above 100 kW, the mean measured NO concentration was approximately 2890 ppm on a dry basis, with a maximum recorded value of approximately 3660 ppm. Since hydrogen contains no fuel-bound nitrogen, the NO formation is interpreted primarily as thermal NO formation from nitrogen introduced by the parasitic fan air. The combination of high flame temperature, excess oxygen, and continuous nitrogen ingress created favourable conditions for NO formation. The measured NO levels therefore represent the demonstrator configuration as installed, not the intrinsic emission behaviour of ideal sealed oxy-fuel hydrogen combustion.

The CO-mix gas flue-gas data are more uncertain. They originate from CO test 1, which was affected by incorrect fuel-flow scaling and limited analyser coverage. Over usable periods with burner power above 100 kW, CO test 1 showed a mean dry O₂ concentration of approximately 17.3 vol% and a mean NO concentration of approximately 168 ppm. In the limited higher-power segment above 900 kW, the corresponding mean values were approximately 10.4 vol% dry O₂ and 578 ppm NO, but this segment lasted only about 0.39 h and included unstable analyser behaviour. These values should therefore be treated as indicative observations only and not as a direct benchmark against the hydrogen test.

A further limitation is that the measured CO₂ concentration during CO-mix gas firing was much lower than expected from the carbon content of the fuel. This suggests that the sampled gas was affected by dilution, non-representative sampling, leakage in the sampling system, analyser instability, or a combination of these effects. When the disturbed period is excluded and only the stable late part of CO test 1 is considered, the measured NO concentration was close to zero to a few ppm and CO remained essentially zero. This indicates good CO burnout and low measured NO during stable late operation, but the absolute values require confirmation in a repeat test with improved flue-gas sampling.

The main conclusion from the emission results is therefore not that hydrogen intrinsically produces more NO than CO-mix gas. Rather, the current demonstrator configuration introduced enough parasitic air to materially affect the flue-gas composition and to provide nitrogen for thermal NO formation, especially during hydrogen operation. The available flue-gas data are valuable for understanding the behaviour of the installed burner system, but they should not be used as a direct comparison of intrinsic fuel-specific emissions under ideal oxy-fuel conditions.

7.7.5 Operational robustness and industrial interpretation

A key outcome of the campaign is the importance of operational robustness. The hydrogen test completed the full heating cycle successfully and delivered strong thermal performance. By contrast, the CO-mix gas

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tests were affected by practical and technical issues. CO test 1 was compromised by a fuel-flow scaling error, CO test 2 was stopped due to a clogged gas filter, and CO test 3 was completed successfully but included a short interruption caused by low gas pressure. These events show that fuel comparison in an industrial environment cannot be separated from gas-supply stability, instrumentation quality, burner control, and general system reliability.

From an industrial perspective, this is an important result. The demonstration shows that hydrogen can be integrated into ladle preheating and can achieve the required thermal duty under realistic plant conditions. At the same time, it shows that fair comparison between fuels requires stable operation and reliable measurement systems. The strong hydrogen result is therefore both a proof of technical feasibility and an example of the value of stable process execution. Conversely, the CO-mix gas results show how non-fuel-related disturbances can strongly influence the apparent performance of a conventional reference case.

For future implementation, the most important technical priorities are to reduce parasitic air ingress, improve burner and fan integration for oxy-fuel operation, and strengthen flue-gas measurement methodology. Reducing parasitic air would lower nitrogen ingress and may significantly reduce NO formation during hydrogen firing. Improved sampling and mass-flow-based emission quantification would also allow stronger conclusions regarding absolute emissions and fuel-to-fuel comparisons in future campaigns.

7.8 Relevance for HyInHeat and future work

7.8.1 Relevance for the HyInHeat project

The demonstration is directly relevant to the objectives of HyInHeat because it provides industrial-scale evidence that hydrogen can be used for ladle preheating under realistic steel-plant conditions. The hydrogen test showed that the required process target of 1200 °C could be reached and maintained in a full industrial heating cycle, while the total recorded energy input remained lower than in the valid conventional reference case. This provides a strong practical contribution to the project objective of demonstrating the technical feasibility of hydrogen-based industrial heating technologies.

The results are also relevant because they move beyond laboratory-scale or pilot-scale evaluation. The tests were conducted with an industrial ladle heater, an industrial ladle with a representative aged refractory lining, and under challenging winter conditions typical for the site. This gives the results a high degree of practical relevance for industrial replication and demonstrates that hydrogen conversion is not only a theoretical decarbonisation pathway but can also be realised in an operating steelworks environment.

A further contribution to HyInHeat is the insight into system integration. The campaign clearly showed that performance and emissions are strongly influenced not only by the fuel, but also by the burner configuration, fan operation, parasitic air ingress, instrumentation, and gas-supply stability. These findings are highly relevant for future demonstration activities in the project and for industrial implementation beyond the present site. They show that successful hydrogen deployment requires a systems approach that includes burner design, control strategy, gas infrastructure, and flue-gas management.

7.8.2 Main lessons for industrial transferability

Several lessons from the campaign are directly transferable to other industrial heating applications. First, hydrogen is technically capable of delivering the required heat-up performance in ladle preheating. Second, the comparison with the CO-mix gas tests shows that operational stability can dominate the apparent outcome of a fuel comparison. Third, the flue-gas results show that the burner system must be designed to minimise unintended air ingress if the intended benefits of oxy-fuel hydrogen combustion are to be fully realised.

The winter conditions under which the tests were performed are also relevant for transferability. Condensation and icing effects were observed during hydrogen operation, and these effects must be considered when designing future permanent systems for cold climates. This is particularly important for exhaust handling, drainage, inspection routines, and the layout of outdoor hydrogen-supply equipment. The present campaign therefore contributes practical insight not only into combustion behaviour but also into the surrounding balance-of-plant requirements for industrial hydrogen use.

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7.8.3 Future work

Based on the present results, several areas for future work can be identified.

Reduction of parasitic air ingress

The most important development priority is to reduce the parasitic air flow from the burner fans. The emission analysis showed that the minimum-fan air strongly diluted the flue gas and introduced nitrogen that contributed to NO formation, especially during hydrogen firing. Future burner-system development should therefore focus on improved fan and duct cooling concepts, better sealing, or alternative arrangements that allow closer approach to true oxy-fuel conditions.

Additional repeat tests under stable reference conditions

A second priority is to generate a more robust conventional reference dataset. The supplementary analysis of CO test 2 indicates that conventional CO-mix gas heating can proceed much faster than observed in CO test 3, and therefore the present campaign would benefit from additional repeat testing under more stable CO-mix gas conditions. Equivalent repeat hydrogen tests would also be valuable, although these were limited in the present campaign by hydrogen availability.

Improved flue-gas measurement methodology

Future campaigns should include improved flue-gas measurement methodology, including more stable analyser operation, improved sampling robustness, and, if possible, mass-flow-based emission quantification. The present data are useful for trend interpretation, but a more advanced measurement setup would allow stronger conclusions regarding absolute emission levels and better comparison between fuels.

Further optimisation of thermal uniformity

The hydrogen test showed strong heating performance but a larger internal temperature spread than the conventional reference case. Future work should therefore examine the interaction between flame characteristics, burner position, flow pattern, and ladle geometry in order to improve thermal homogeneity while maintaining the rapid heat-up observed in the present test.

Continued refractory assessment

Finally, the implications of hydrogen combustion for refractory durability remain a key area for future work. The present process-test campaign provides the operational basis for this assessment, but the detailed evaluation of refractory condition, degradation mechanisms, and long-term service relevance requires the dedicated post-test analyses described elsewhere in the report. Future validation should combine process testing and refractory assessment more closely in order to build a complete basis for industrial implementation.

7.9 Conclusions

The demonstration has shown that hydrogen can be used successfully for industrial ladle preheating at the 7 Steel Nordic site. The hydrogen-fuelled ladle heater reached the target temperature of 1200 °C, maintained stable high-temperature operation, and completed the full heating cycle under realistic industrial conditions. This confirms the technical feasibility of hydrogen-based ladle heating within the scope of the HyInHeat project.

In the formal KPI comparison against the valid conventional reference case, CO test 3, the hydrogen test showed faster heat-up and lower recorded energy input. Hydrogen reached 1200 °C after 5.756 h, compared with 7.997 h for CO test 3. The total recorded energy input was 8.61 MWh for hydrogen and 9.16 MWh for CO test 3. Fuel and oxygen consumption were also lower in the hydrogen test than in the valid CO-mix gas reference case.

The campaign also showed that fuel comparison in an industrial environment is strongly influenced by operational conditions. The supplementary analysis of CO test 2 indicated that CO-mix gas heating can proceed substantially faster than shown by CO test 3 if stable operation is maintained. The brief interruption in CO test 3 had a measurable effect, but it did not fully explain the difference between the CO-mix gas tests. The results therefore show both that hydrogen performed strongly in the campaign and that the conventional reference case was affected by practical operating disturbances.

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The flue-gas measurements showed that the burner configuration had a major influence on measured emissions. During hydrogen operation, high dry O₂ and high NO concentrations were measured, while CO remained essentially zero. The high dry O₂ concentration is attributed to the combined effect of excess oxygen in oxy-fuel operation, oxygen introduced with the parasitic fan air, and dry-basis gas analysis after removal of water vapour. The high NO concentration is interpreted primarily as thermal NO formation from nitrogen introduced by the parasitic fan air under high-temperature, oxygen-rich conditions.

The CO-mix gas emission data from CO test 1 are indicative only. The test was affected by incorrect fuel-flow scaling, limited analyser coverage, transient analyser behaviour, and evidence of dilution or sampling uncertainty. Stable late-period CO test 1 measurements suggest low NO and essentially complete CO burnout, but the absolute values should be confirmed in a repeat test with improved flue-gas sampling and quantitative emission measurement.

Overall, the demonstration supports the conclusion that hydrogen is a technically viable fuel for ladle preheating and represents a relevant decarbonisation pathway for the steel industry. At the same time, successful industrial implementation will require further optimisation of burner design, improved control of parasitic air ingress, robust fuel-supply conditions, and improved emissions measurement methodology. The demonstration has therefore not only proven feasibility, but also identified the most important technical priorities for further development and industrial scale-up.

8 Task 6.3: Ladle Preheating with H_2/O_2 Combustion – Investigation of refractory by RWTH-GHI

8.1 Motivation

In recent decades, modern societies have substantially increased their demand for steel. Along with increased steel consumption, the steel sector alone accounts for 7-9 % greenhouse gas emissions [1]. Therefore, transforming the fossil fuel-based steel manufacturing into a sustainable economy is of significant concern. Due to its high impact to greenhouse gas emissions, it has a high leverage to reduce the consequences of human caused climate change.

To mitigate the adverse impacts of climate change, the steel industry must improve its processes to increase efficiency and lower CO_2 emissions. In line with the commitment to the European Union's Green Deal, green hydrogen has become a highly discussed energy carrier capable of replacing traditional fossil fuels. In steelmaking, green hydrogen can be used as suitable option for direct reduction processes (DRI) or as combustion gas for flame heating. Flame heating is an essential step in preheating of steel ladles. Preheating steel ladles is an essential process, as it lowers the risk of thermal shock, reduces heat loss during transportation, and removes moisture trapped within the lining. The moisture within the lining depends on the choice of refractory lining. When mortar and liquid fillings are used, the water needs to be carefully removed to keep the materials integrity by drying the steel ladle[2].

Currently, preheating of steel ladles is mostly carried out by burning natural gas (NG), which causes significant CO_2 emissions. Some companies such as 7 Steel rely on the reuse of CO firing which increases efficiencies and sustainability but still causes the emissions of CO_2 . Using green H_2 as a sustainable carrier, oxy-fuel combustion ($H_2 + O_2$) effectively reduces CO_2 and NO_x emissions.

Steel ladles lining is commonly made of highly resistant basic refractory materials and generally withstand molten steel as well as basic slag. Doloma bricks are a well-established group of materials which is used in steel ladles and withstands aggressive conditions such as molten steel and slag at high temperatures. They are mainly made of periclase (MgO) and calcium oxide (CaO) along with sinter additives such as Fe_2O_3 , SiO_2 or Al_2O_3 . Doloma refractories are particularly attractive because of their high refractoriness, good compatibility with basic slags [3], and low cost [4]. There are two ways to tailor the mechanical stability. On the one hand, mechanical strength is realized by carbon-bonding using graphite and organic binders such as pitch, tar or resin. Due to carbon-bonding, the material shows very good thermal shock resistance and does not require a sintering treatment therefore it saves energy. The trade-off is lower mechanical stability and a limited oxidation resistance due to carbon burnout. On the other hand, there is ceramic-bonded doloma, which is sintered before use. The microstructure is defined by sintered doloma grains and is usually used in highly corrosive slag zones since it is more abrasion-resistant compared to carbon-bonded doloma.

However, the presence of free CaO makes doloma susceptible to hydration [5] at lower temperatures and humid conditions. Lower temperatures are of great concern, mainly during storage but also during preheating when the ladle is cold. Due to condensation at lower temperatures and potential reaction of CaO and water, hydration could lead to catastrophic lining collapse. Hydration reactions cause volumetric expansion inducing cracking, and loss of mechanical stability. In industrial practice, hydration is mitigated by optimized firing schedules, the use of carbon-bonded bricks (pitch or resin bonded), and appropriate preheating procedures.

There are different studies which focused on the hydration behaviour and how to increase hydration resistance of doloma. One way to increase the hydration resistance is the formation of brownmillerite ($Ca_2Fe_2O_5$). The formation of brownmillerite occurs due to the reaction of hercynite ($FeAl_2O_4$) with lime (CaO) or portlandite ($Ca(OH)_2$). When Al_2O_3 is also present, the stoichiometry can vary depending on the local chemical composition and form $Ca_2(Al_xFe_{1-x})_2O_5$. Brownmillerite encapsulates and passivates CaO grains and shields them from water vapour [6]. In context of doloma manufacturing, Brownmillerite is associated with liquid phase sintering and tends to form at grain boundaries. Generally, by adding Fe_2O_3 , the melting point can be lowered which enhances the grain size [4], density and therefore hydration resistance [7]. Another approach of increasing hydration resistance is by adding nanoparticles which can react with CaO and form a hydration resistant protective phase.

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Using nanoparticles as additives follow similar approaches by forming low melting phases which then causes densification and higher hydration resistance. Out of a selected group of nanoparticles, ZrO_2 showed the highest hydration resistance by forming CaZrO_3 [8], whereas SiO_2 was shown to form low melting phases which less effectively protected CaO hydration [4]. Moreover, additives such as NiO can cause lattice distortions which reduces activation energy and increases the sinter activity [9].

Since most experimental studies have evaluated hydration resistance only at relatively low temperatures, they typically expose materials to a moist atmosphere and monitor mass change before and after moisture exposure, usually near room temperature. In reality, during steel ladle preheating the lining experiences a heating profile with pronounced temperature gradients as the ladle heats up. In this regard, there is insufficient literature addressing the influence of higher temperatures on the hydration behaviour of doloma refractory bricks and the resulting microstructural evolution.

Therefore, the corrosion resistance of carbon-bonded and ceramic-bonded doloma refractories to water vapour was studied at 200 °C, 500 °C, and 1175 °C. The present project contribution serves as a feasibility study, in cooperation with the steel producer 7 steel (formerly Celsa).

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8.3 Material identification and test setup

The following Table 39 lists the different doloma materials without testing in their pioneer state. The table contains internal sample ID, the condition of each brick and its individual report ID which relates to the condition of each material. Photographs of initial conditions are shown in Figure 156 to Figure 161. In the following report, the report ID will be consequently used for simplification reasons. Figure 161 illustrates a 3-dimensional and simplified graphical rendering of a steel ladle lined by two different refractory materials namely Sindoform K40 a ceramic bonded material (RHI Magnesita, Austria)) and Pentabrick a carbon bonded material (Dolomite Franchi, Italy).

As part of project contribution of WP 6.3, RWTH-GHI was provided by factory new Sindoform K40 and Pentabrick C01 materials. In addition, doloma samples were provided to RWTH-GHI after conventional CO preheating as well as after industrial H₂ firing campaign (as indicated in Table 39).

Table 39. Sample identification table with corresponding report ID

Product Name	Sample ID (internal)	Reference	Report ID	Condition
Sindoform K40	3621	Figure 156	SF	Factory new
Pentabrick C01	3622	Figure 157	PB	Factory new
Sindoform K40	3750	Figure 158	SF-H ₂	after H ₂ firing
Pentabrick C01	3749	Figure 159	PB-H ₂	after H ₂ firing
Pentabrick C01	3738	Figure 160	PB-CO	after CO firing
Sindoform K40	3739	Figure 161	SF-CO	after CO firing

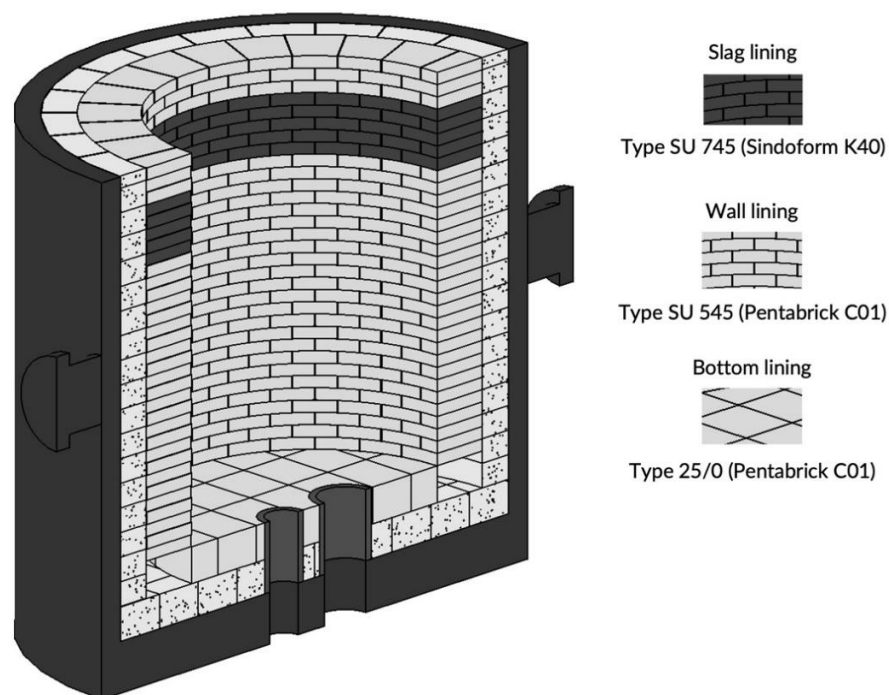


Figure 155. Schematic 3D rendering of a steel ladle (RWTH-GHI) with 7 Steel lining setup using SF and PB



Figure 156. Sindoform K40 (as received)



Figure 157. Pentabrick C01 (as received)



Figure 158. H₂ fired Sindoform (as received)

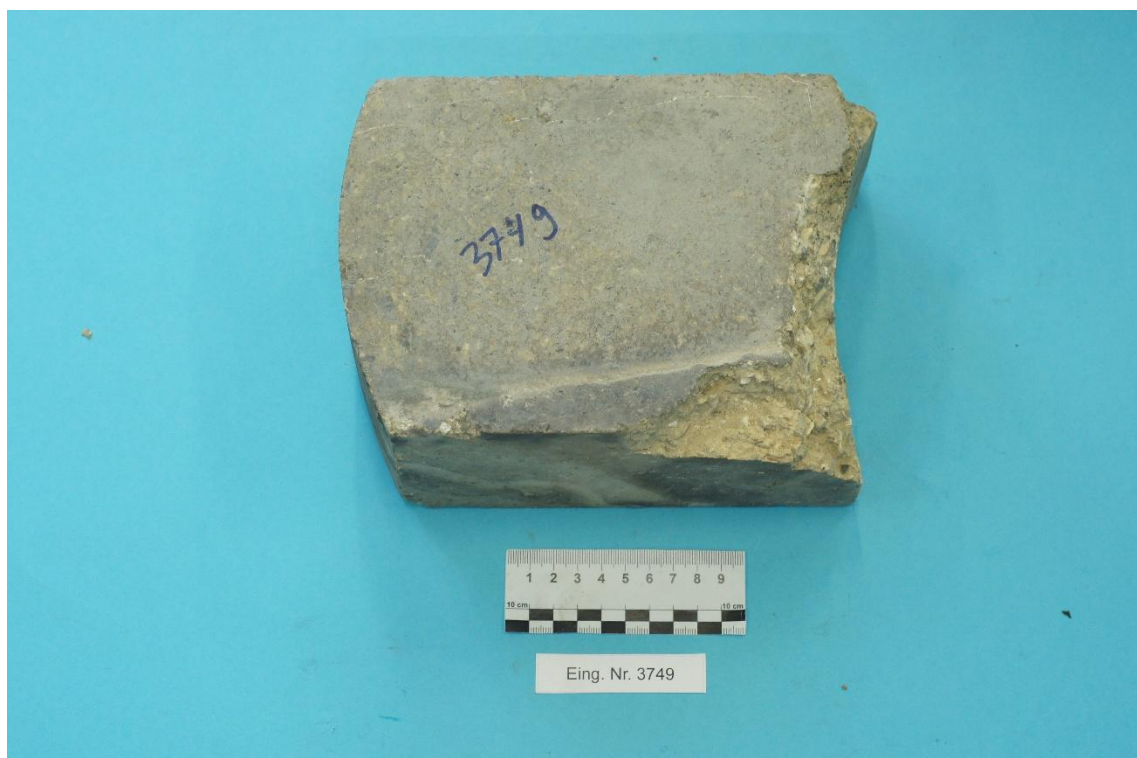


Figure 159. H₂ fired Pentabrick 3749 (as received)



Figure 160. CO fired Pentabrick 3738 (as received)



Figure 161. CO fired Sindoforn (as received)

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8.3.1 Experimental Procedure

Table 40. Test matrix for PB and SF shows the experimental test matrix for PB and SF, respectively. For simplification the sample ID will be further used which to represent specific test conditions. The testing temperatures were used as they represent potential critical operating temperatures within the steel ladle. Whereas 1175 °C was selected based on preheating target temperature for commercial preheating of 7-Steel, 500 °C and 200 °C were chosen for hydration resistance at lower temperatures.

Within this work, the samples were analysed by tracking mass balance, carbon content, mineralogy, chemical composition as well as their microstructure. The initial weight was determined by using a precision balance and rounded to the second decimal place. The gas corrosion tests were conducted on fractions of each material due to geometrical limitations of the testing setup (Figure 162/Figure 163) which will be introduced in the next section about the experimental setup. The samples were placed in the centre of the alumina tube using high alumina crucibles. Consequently, throughout all experimental conditions, Argon was used as purging gas for heating up and cooling down using 30 l/h. Water vapour was inserted once the test temperature was reached to isolate the corrosion effect. For corrosion tests, Argon served as carrier gas for water vapour by using a flow rate of 65 l/h. After cooling, the samples were weight and embedded in epoxy and polished for microstructural analysis via scanning electron microscopy (SEM). Moreover, powder was milled <63 µm for X-ray diffraction (XRD) and X-ray fluorescence (XRF) characterization. The samples were consequently stored in a desiccator to avoid hydration.

Table 40. Test matrix for PB and SF

Sample ID	Material	Experimental conditions			Flowrate	
		Temperature [°C]	Dwell time [h]	Atmosphere	Ar (l/h)	H ₂ O (g/h)
Pentabrick C01						
PB-T01	PB	1175 °C	24	H ₂ O/Ar	65	100
PB-T02	PB	1175 °C	48	H ₂ O/Ar	65	100
PB-T03	PB	500 °C	24	H ₂ O/Ar	65	100
PB-T04	PB	200 °C	3	H ₂ O/Ar	65	100
PB-COw-T01	PB-CO (white)	1175 °C	24	H ₂ O/Ar	65	100
PB-COb-T01	PB-CO (black)	1175 °C	24	H ₂ O/Ar	65	100
PB-H2-T01	PB-H2	1175 °C	24	H ₂ O/Ar	65	100
Sindoform K40						
SF-T01	SF	1175 °C	24	H ₂ O/Ar	65	100
SF-T02	SF	1175 °C	48	H ₂ O/Ar	65	100
SF-T03	SF	500 °C	24	H ₂ O/Ar	65	100
SF-T04	SF	200 °C	3	H ₂ O/Ar	65	100
SF-T05	SF	200 °C	24	H ₂ O/Ar	65	100
SF-CO-T01	SF-CO	1175 °C	24	H ₂ O/Ar	65	100
SF-H2-T01	SF-H2	1175 °C	24	H ₂ O/Ar	65	100

8.3.2 Experimental Setup

The experimental configuration is illustrated in Figure 162. For the water vapour corrosion tests, a tube furnace from Therm-Aix was used. The tube is made of high α -Al₂O₃ (99.5 % purity) and has an inner diameter 55 mm. The samples were paced within the centre of the tube furnace as illustrated in Figure 163. For testing, both ends of the tube were sealed to ensure controlled conditions. Based on experience, the flanges were heated by using a temperature of 130 °C heated silicon oil to prevent water condensation at the in or outlet of the tube. By using a gas flow control unit (Figure 162a), a defined gas atmosphere was set up for argon as carrier gas as well as for water vapour. Considering the present setup, a partial pressure for water was calculated to be 0.68 considering 100 g/h H₂O and 65 l/h Argon at 25 °C.

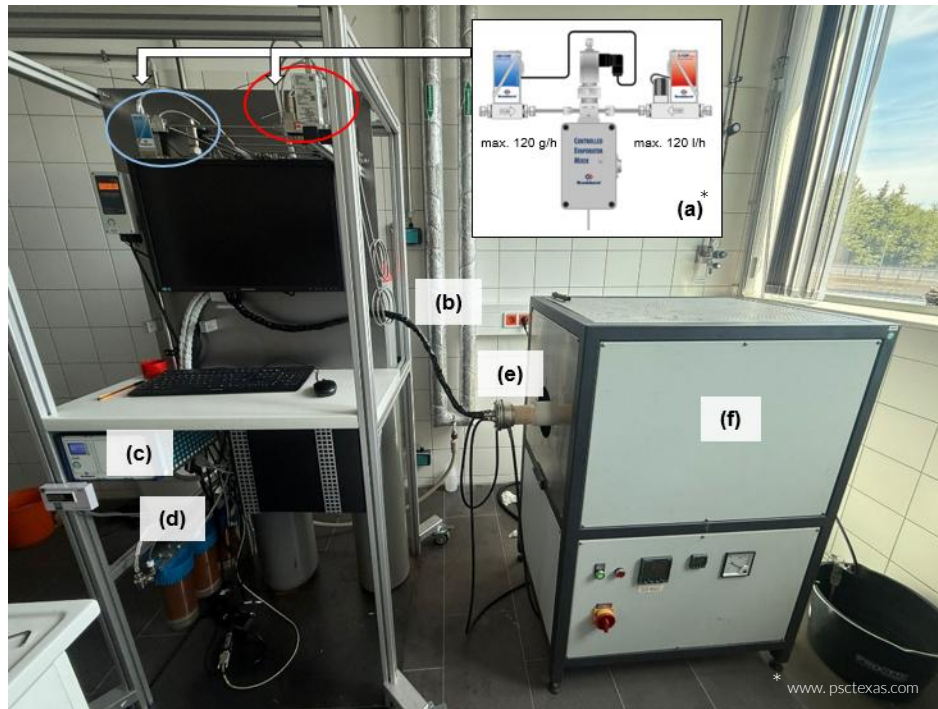


Figure 162. Experimental setup: (a) water flow controller (blue), water evaporator (centre) and gas flow controller (red) (b) heated hose, (c) CEM control unit, (d) water demonization unit, (e) oil heated flange, (f) furnace unit

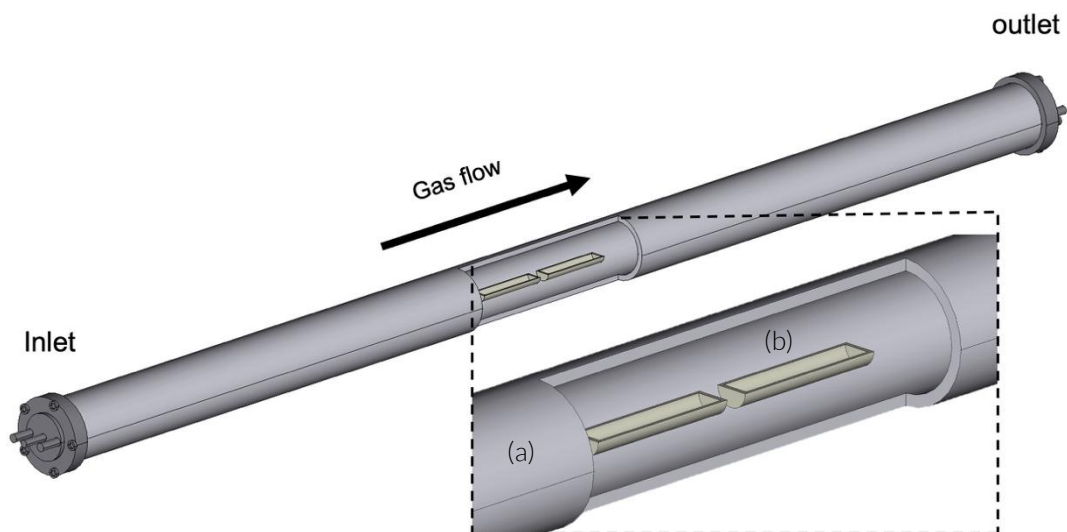


Figure 163. 3D rendering of alumina tube (a) and alumina sample holders (b), (RWTH-GHI Aachen)

8.4 Results and Discussion

Initially, after the corrosion tests, optical assessments of the test specimens were carried out along with tracking the mass balance. After visual inspection, the carbon content (C_{tot}) and XRF were carried out followed by XRD and SEM. For simplicity, the results will be separated for PB and SF.

8.4.1 Thermodynamic simulation

Thermodynamic FactSage simulations were carried out prior to experiments. The software provides a graphical user interface and relies on thermodynamic databases which are based on CalPhaD. Generally, FactSage follows Gibbs energy minimization to simulate and predict chemical reactions and stable phases. For our investigation, the simulation module „*Equilib*“ was used which calculates multiphase equilibrium for various chemical compositions. Simulations were carried out based on the chemical composition of each technical datasheet for H_2O atmosphere. As thermodynamic databases, „*FToxid*“ and *FactPS* were selected to ensure a suitable choice of database especially for hydroxides. To reduce complexity only mayor phases (more than 5 wt.%) will be presented.

FactSage simulation of PB + H_2O (100 - 1175 °C, 1 atm.)

Figure 164 visualizes the thermodynamic calculations of PB exposed to water vapor at atmospheric pressure. As initial conditions, 100 g of total mass of doloma sample along with 100 g of water was used. According to the datasheet the chemical composition was chosen as follows: MgO (39.4 %), CaO (58,3 %), SiO_2 (1.0 %), Fe_2O_3 (0.8 %) and Al_2O_3 (0.5 %). The x-axis represents the simulated temperature range from 100 °C to 1175 °C. The y-axis shows the stable mass fraction for each temperature.

A constant mass fraction across a temperature range indicates a stable phase with no significant change in equilibrium amount. Variations in the mass fraction with temperature indicate phase transformations or chemical reactions that change the equilibrium phase composition.

At the maximum temperature of 1175 °C the main components CaO (light green) and MgO (orange) remain thermodynamically stable in solid state as the mass fractions are same as the initial chemical composition. Water vapor (light blue) and CO_2 (red) sum up to form the dark green Gas line and are present as gaseous species. This thermodynamic stability is shown to be stable until about 710 °C. Below this temperature, the CaO content significantly decreases along with CO_2 due to the simulated formation of $CaCO_3$ (grey). Although FactSage simulation showed the formation of $CaCO_3$, this phase formation needs to be critically considered since graphite within the sample is expected to be stable and in practice usually non-reactive. It is important to highlight that kinetic effects in general cannot be simulated which in this case could explain potential mismatch between thermodynamics and later experimental observations. At a temperature of about 510 °C, the formation of portlandite ($Ca(OH)_2$) gets initiated and mark a potential critical operating temperature within industrial application when water vapor is present. It is considered as potential critical temperature since $Ca(OH)_2$ could lead to volume expansion and loss of mechanical integrity. Whereas at about 510 °C CaO transformed into $Ca(OH)_2$, MgO is still stable and first starts to transform into its hydroxide at about 271 °C under the present conditions. Similarly, to the formation of portlandite, this reaction consumes the water which is present within the setup and could lead to volume expansion and loss of the materials stability.

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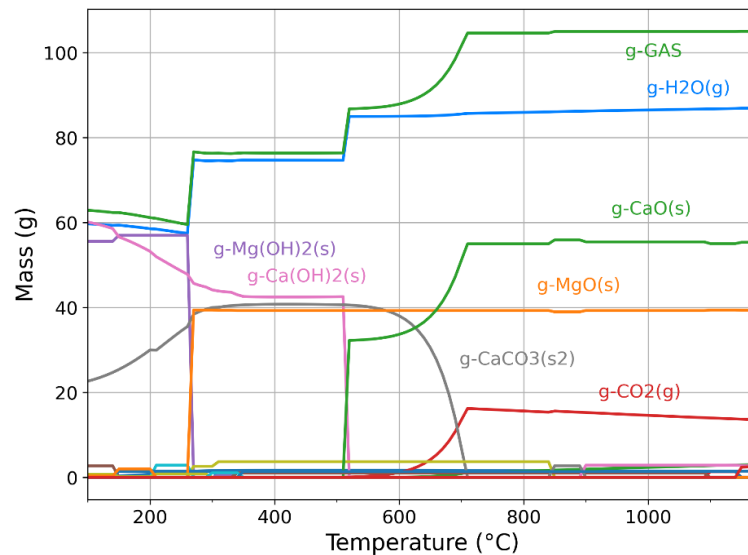


Figure 164. Thermodynamic simulation of PB composition and water vapour ranging from 100 °C to 1175 °C

FactSage simulation of SF + H₂O (100 – 1175 °C, 1 atm.)

Figure 165 represents the thermodynamically stable phases calculated for SF composition. The general setup follows the same approach as for PB.

Since it is a ceramic bonded doloma refractory, the thermodynamic simulation of the initial chemical composition (also taken from the product datasheet) does not show any carbonate formation. The formation of carbonates is expected to be insignificant due to carbon burnout at higher temperatures and high chemical stability of graphite at lower temperatures. Therefore, for doloma refractories the significant chemical interactions are expected to be between MgO and CaO when exposed to water vapor.

Within this graph, each colour represents a different species which is calculated to be stable. Each equilibrium mass is calculated for various temperatures (ranging from 100 °C to 1175 °C and 1 atm.). At the maximum temperature of 1175 °C, the main oxides CaO (brown) and MgO (violet) remained unaffected and do not show a significant change in weight. Along with the stable oxides water CaO and MgO, water vapor is present as gas and coloured in (Fuchsia). At lower temperatures, the main oxides still were calculated to be stable until reaching about 530 °C. Considering constant thermodynamic stability from 1175 °C to about 530 °C, the material presents a broad range of thermodynamic stability under water vapor exposure. With respect to the steel ladle preheating process and expected operating temperatures more than 1000 °C, water vapour is therefore expected to be less critical for SF as there is no carbon burnout which might affect the overall performance of the refractory material.

Although simulation predicted good corrosion resistance at temperatures higher than about 530 °C, the reaction significantly changes for lower temperatures. Below about 530 °C, a rapid increase in the formation of Ca(OH)₂ can be seen which is shown as green line. Simultaneously, the water vapor decreases its mass fraction since it gets incorporated within calcium hydroxide. Due to the rapid change in the stability of CaO at this temperature, a temperature of 500 °C was selected as a critical threshold for further corrosion lab trials. Although CaO reacts with water vapor, MgO still tends to be stable at this condition. Below about 270 °C, both hydroxides portlandite and brucite are thermodynamically stable along with unreacted water vapor steam. Therefore, this temperature marks a critical temperature for brucite formation. However, as also mentioned in literature [10], the reaction kinetics of MgO to form brucite is slow and for our tests not expected.

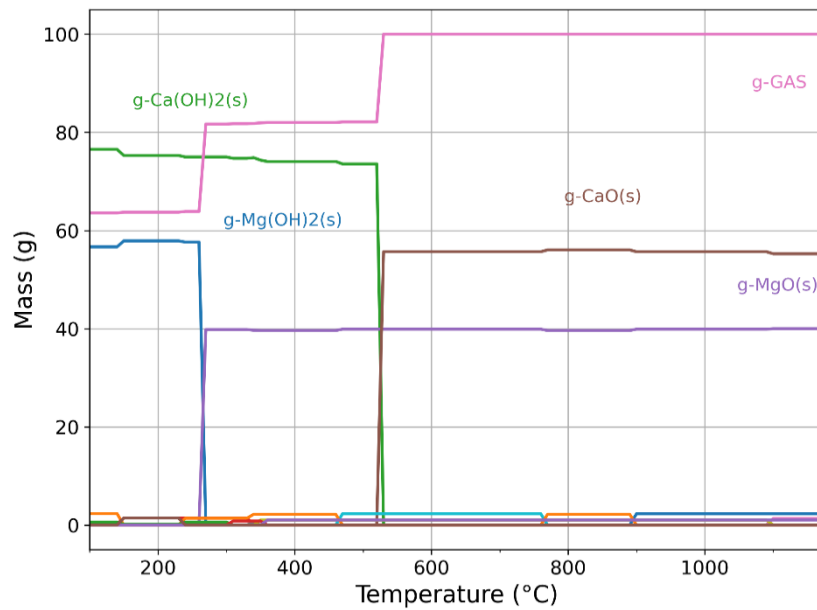


Figure 165. Thermodynamic simulation of SF composition and water vapour ranging from 100 °C to 1175 °C and 1 atm.

Critical temperature of MgO and CaO volatilization under H₂O conditions

Since the main oxides MgO and CaO were found to be stable at 1175 °C and H₂O atmosphere, the temperature range was extended. To determine a potential critical temperature, the volatilization of both oxides under water vapor conditions was simulated. As initial conditions, 40 g MgO, 58 g CaO and 100 g H₂O were included for calculation. Calculations were performed on a temperature range from 100 °C to 2000 °C and 1 bar.

Figure 166 shows the graphical visualization of the calculated volatilization of the four main volatile species MgOH, Mg(OH)₂, CaOH and Ca(OH)₂. Until a temperature of about 1250 °C, there is no significant evaporation of volatile hydroxides. Exceeding 1250 °C at present conditions, first volatilization processes can be observed, although they remain less significant until a temperature of about 1300 °C. Exceeding 1500 °C at present conditions, the calculation showed higher tendency for evaporation of Ca(OH)₂ compared to Mg(OH)₂ and consequently lower evaporation tendency of CaOH and MgOH respectively. Although evaporation of hydroxides was verified by thermodynamic simulation, the general amount of volatilization related to their mass fraction remains comparably small and thermodynamically rather less significant. Therefore, volatilization of hydroxides during preheating under water vapour atmosphere is expected to be neglectable.

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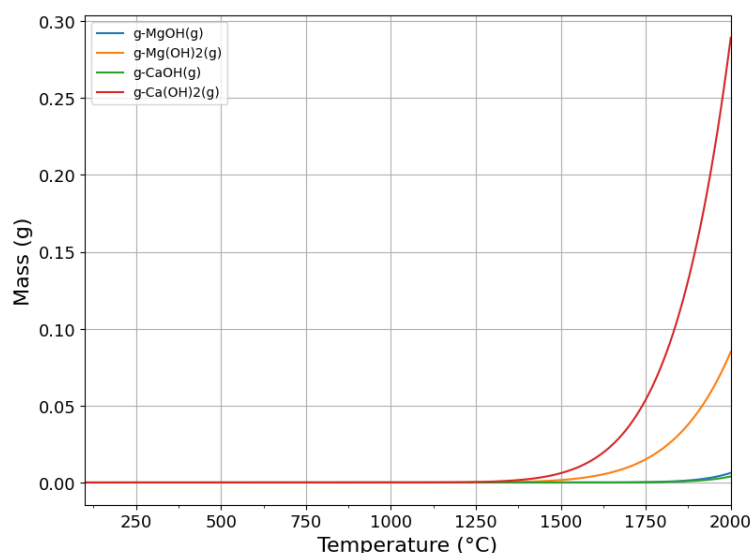


Figure 166. Thermodynamic simulation of 40 g MgO and 58 CaO exposed to 100 g water vapour up to 2000 °C

8.4.2 Visual inspection and Mass balance

Optical assessment and mass balance of PB test series

Table 41 shows the visual appearance of PB samples before (left) and after (right) water vapour tests. For PB-T01 (1175 °C, 24 h) and PB-T02 (1175 °C, 48 h) conditions, the samples showed a pronounced colour change from black to beige, indicating burnout of carbon. Despite this pronounced colour change, the integrity of the material was not severely degraded, and by eye, the ceramic framework remained intact. As the bonding phase (graphite) was removed from the material matrix, the mechanical properties are expected to decrease; however, these were not investigated in the present study. After exposure to water vapour at 500 °C for 24 h (PB-T03), the overall colour of the bricks remained black. Only partially, on top of the surface there was a slight colour change noticeable fading from black to white-grey. Similar to PB-T01 and PB-T02, the ceramic framework appeared mostly unaffected. In contrast to the tests at 1175 °C and 500 °C, the visual assessment after exposure to water vapour at 200 °C (PB-T04) indicated a complete loss of the material's mechanical stability. The brick disintegrated into a fine powder because of the hydration of CaO to Ca(OH)₂ which caused a significant volumetric expansion within the microstructure, causing internal stresses that fractured the refractory matrix. Accompanied within fine powder particles, larger grains with a diameter of several mm were present, which represent the coarse grain aggregates of the bricks. Within a doloma refractory, those larger aggregates are characteristically for this type of refractory material and originate from the manufacturing process. Moreover, the larger grains remain less affected by the hydration reaction likely due to their lower specific surface area. For PB-CO-T01 (1175 °C, 24 h) and PB-H2-T01 (1175 °C, 24 h), the samples showed no significant change in their optical appearance, suggesting no substantial change in their material properties. This can be explained by the initial absence of carbon, which was already burned out by prior CO and H₂ firing. Noticeable optical differences were only observed for the PB-Cob-T01 from the colder zone of PB-CO, which initially contained residual graphite. After water vapour experiments at 1175 °C, the residual carbon was burned out, leaving again a ceramic framework with a beige colour.

The optical assessment gets supported by the gravimetric mass balance. Table 42 shows the mass balance for the doloma refractory testing series of PB. Here, m₁ indicates the initial mass before the experiment and m₂ the weight after the corrosion test. For comparison, the actual change in mass is given by its delta as well as in relative weight percentage referring to the initial weight before each test.

For tested samples PB-T01 and PB-02, a significant change in mass was observed by about 6.3 to 6.7 weight percent respectively. This mass loss can be explained by carbon burnout and matches with optical assessment. Compared to PB-T03, the weight loss was less with about 3.6 wt. %. The weight loss corresponds to minor optical changes and “lower temperature” of 500 °C. After testing for 3 h at 200 °C the weight balance was impossible to track due to the loss of material. Due to significant volume increase and hydration reaction, the material spilled out of the sample holder and no reliable balancing was possible.

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For PB-CO-T01 and PB-H2-T01, the weight changes were found to be insignificant, again, due to absence of carbon. The only minor change in balance was observed for PB-COb-T01 which showed a change in weight of about 2.6 wt.%. This weight loss correlates with optical changes and the content of unburned carbon after CO firing.

Table 41. Visual inspection for Pentabrick CO1 test series before (left) and after corrosion test (right)

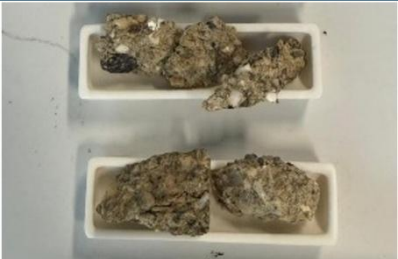
Sample ID	Conditions	Before test	After test
PB-T01/PB-T02	1175 °C (24/48h)		
PB-T03	500 °C (24 h)		
PB-T04	200 °C (3h)		
PB-H2-T01	1175 °C (24 h)		
PB-COb-T01	1175 °C (24 h)		
PB-COW-T01	1175 °C (24 h)		

Table 42. Collection of weight balance for PB after different test conditions: T01 – T04.

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Sample ID	m1 (g)	m2 (g)	Δm (g)	Δm in %
PB-T01 (1)	14,86	13,93	0,93	6,26
PB-T01 (2)	19,33	18,03	1,30	6,73
PB-T02	18,12	16,92	1,20	6,62
PB-T03	19,09	18,40	0,69	3,62
PB-T04	Failed			
PB-H2-T01(1)	31,92	31,89	0,03	0,09
PB-H2-T01 (2)	21,29	21,26	0,03	0,15
PB-COW-T01	24,40	24,27	0,13	0,54
PB-COb-T01	23,96	23,35	0,62	2,58

Optical assessment and mass balance of SF test series

Table 43 shows the collection of images which were taken after optical assessments of SF before (left) and after testing (right). Contrary to PB, the overall materials integrity remained unaffected and no significant colour changes were observed for most tested conditions. Substantial differences were observed after exposure to 200 °C. For SF-T04 (200 °C for 3 h), the colour changed from black-brownish to a slight greyish appearance, but its essential mechanical integrity remained. However, for SF-T05 (200 °C for 24 h), the material similarly to PB-T04 lost its integrity and turned into fine grained powder along with a larger intact grain fraction.

Table 44 shows the overview of collected mass balances of tested SF samples. Throughout all conducted corrosion tests, there was no significant change in weight except for SF-T04 –T05. At 200 °C and water vapour conditions, a mass gain was observed which correlates with hydration, leading to increase in mass although the materials general integrity remained intact. Extending the experimental time from 3 to 24 h, the materials integrity got lost and therefore no weight tracking was possible.

Comparing PB to SF, the hydration kinetics at temperatures below 500 °C was shown to significantly impact the hydration resistance to water vapour. Considering weight loss and optical assessments, SF was less affected compared to PB after 3 h tests. Since both materials after 24 h lost their mechanical stability, optical assessment and mass balance already indicate the susceptibility of both materials to temperatures below 500 °C in water vapour atmosphere.

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Table 43. Visual inspection of SF test series before (left) and after water vapour corrosion tests (right)

Sample ID	Conditions	Before test	After test
SF-T01 / SF-T02	1175 °C (24 h)		
SF-T03	500 °C (24 h)		
SF-T04	200 °C (3 h)		
SF-T05	200 °C (24 h)		
SF-H2-T01	1175 °C (24 h)		
SF-CO-T01	1175 °C (24 h)		

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Table 44. Collection of mass balance for SF before (m1) and after (m2) different test conditions

Sample ID	m1 (g)	m2 (g)	Δm (g)	Δm in %
SF-T01 (1)	20,91	20,89	0,02	0,10
SF-T01 (2)	36,53	36,47	0,06	0,16
SF-T02	16,38	16,31	0,07	0,43
SF-T03 (1)	27,44	27,43	0,01	0,03
SF-T03 (2)	47,85	47,81	0,04	0,08
SF-T04 (1)	13,13	13,21	-0,08	-0,63
SF-T04 (2)	16,52	16,64	-0,12	-0,70
SF-T05	Failed			
SF-T02 (1)	31,13	31,09	0,04	0,13
SF-T02 (2)	22,74	22,72	0,02	0,08
SF-CO-T02	26,24	26,22	0,02	0,06

8.4.3 Microstructural analysis (SEM) and local chemical analysis (EDX)

SEM combined with energy-dispersive X-ray spectroscopy (EDX) was used to study the microstructure and elemental composition of tested samples. SEM and EDX analysis were carried out on a Zeiss Gemini 500 field-emission SEM equipped with an Oxford X-Max 80 silicon-drift detector (SDD). A back-scattered electron detector (BSE) was used for imaging. Tested samples were imbedded in epoxy resin (Epoxy 2000 system, Cloeren Technology GmbH) and infiltrated under vacuum. The samples were ground by using SiC sandpaper and diamond grinding disks using a Saphir 550 manufactured by QATM. Final polishing was performed with a water-free diamond suspension following the following treating steps (15 μm → 6 μm → 1 μm). The presented treatment was done by using isopropanol/ethanol as to avoid hydration of the sample surface. For SEM analysis the samples were sputter-coated with a homogeneous 10 nm carbon layer by using a Leica EM ACE 600 sputter coater. Sample storage was consequently done in a vacuumed environment.

PB

Figure 167 shows the micrograph of untested PB. The overall microstructure is made of heterogeneously distributed grains which strongly vary in diameter which is typical for a refractory material. Whereas large grains are found to be in mm scale, fine fractions have a diameter of only a couple of μm. As indicated in Figure 167, bright spots were measured to consist of CaO and darker areas of MgO, reflecting a typical dolomite refractory microstructure. This microstructure forms due to firing of the raw material dolomite ($\text{CaMg}(\text{CO}_3)_2$) which at high temperatures transforms into MgO and CaO along with formation of CO_2 . Due to disintegration of carbonates and free CO_2 , micro porosity can generally be observed within grains. Depending on the chemical composition, larger grains have varying contents of CaO/MgO due to either solid solutions or irregularly distributed CaO and MgO segregations. Smaller grains (about 50 to 200 μm in diameter) are more frequently elongated and show mostly rounded habitus. The finest grain fraction (about 5 to 40 μm) can be found in between the small grain fractions. This fraction fills out the volume between the small grains along with flake graphite as indicated in Figure 168. Graphite acts as a densification aid for shaping and promotes mechanical stability. Furthermore, due to its poor wettability, it counteracts the infiltration tendency of molten steel within the ladle application. Lower contrasted areas represent infiltrated resin which was used to fix the materials microstructure for SEM sample preparation and do not participate in the actual microstructure of the material.

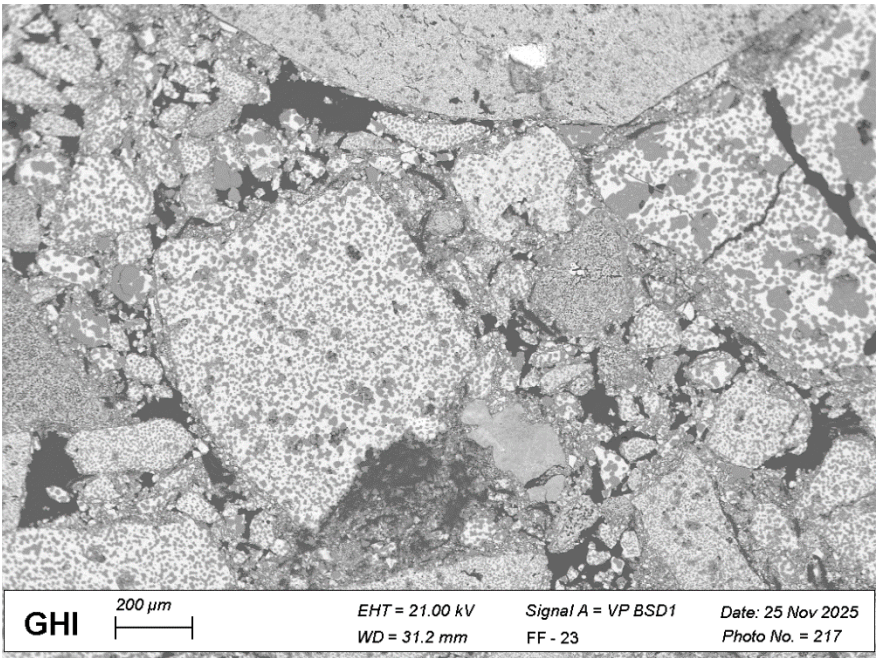


Figure 167. SEM Micrograph of PB (overview), as received

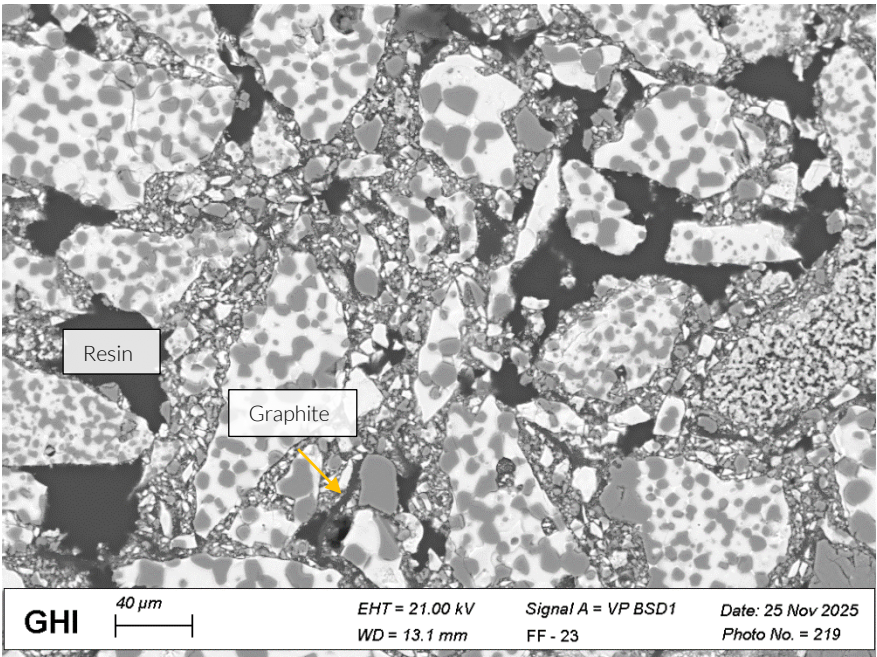


Figure 168. SEM micrograph of PB Detail of Figure 167 showing matrix, as received

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Figure 169 shows the micrograph of PB-T03. A heterogeneous microstructure can be found comparable to the initial microstructure of PB. Again, the grain size distribution ranges from few micrometres to mm scale. As indicated in Figure 170, flaky graphite is still present within the microstructure indicating general resistance of graphite burn out.

Figure 171 represents the microstructure of PB-T02. Similarly, to New Pentabrick C01, the grain morphology, and chemical composition did not show significant changes after the test. The only significant difference compared to the reference material was the absence of flaky graphite indicating carbon burnout due to temperature and oxidizing H₂O conditions. This observation corresponds to significant weight loss (Table 42) as well as a colour change from black to beige (Table 41).

The general microstructure was significantly affected at high temperatures in water vapour-rich conditions which led to carbon burnout, increase in porosity and expected loss of mechanical strength. Although for PB-T01 and -T02 the ceramic doloma microstructure remained intact, the loss of carbon could significantly impact the resistance against molten steel. Due to increased formation of channel pores, molten steel might penetrate deeper into the microstructure potentially causing enhanced corrosion. Another critical aspect could be lower hydration resistance compared to ceramic-bonded SF.

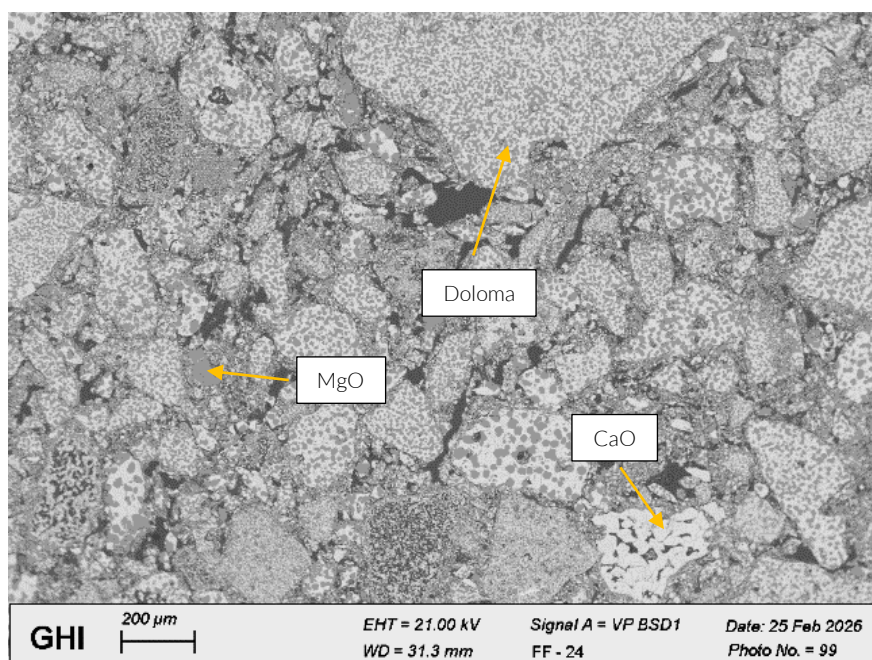


Figure 169. SEM Micrograph of PB-T03 (500 °C, 24 h), overview

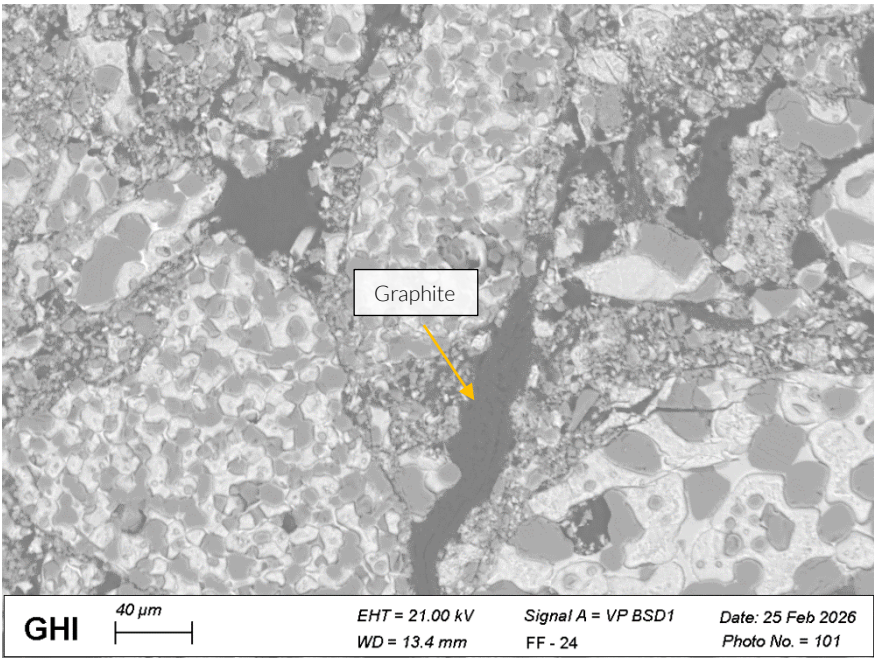


Figure 170. SEM microstructure of PB-T03 (detailed zone from Figure 169)

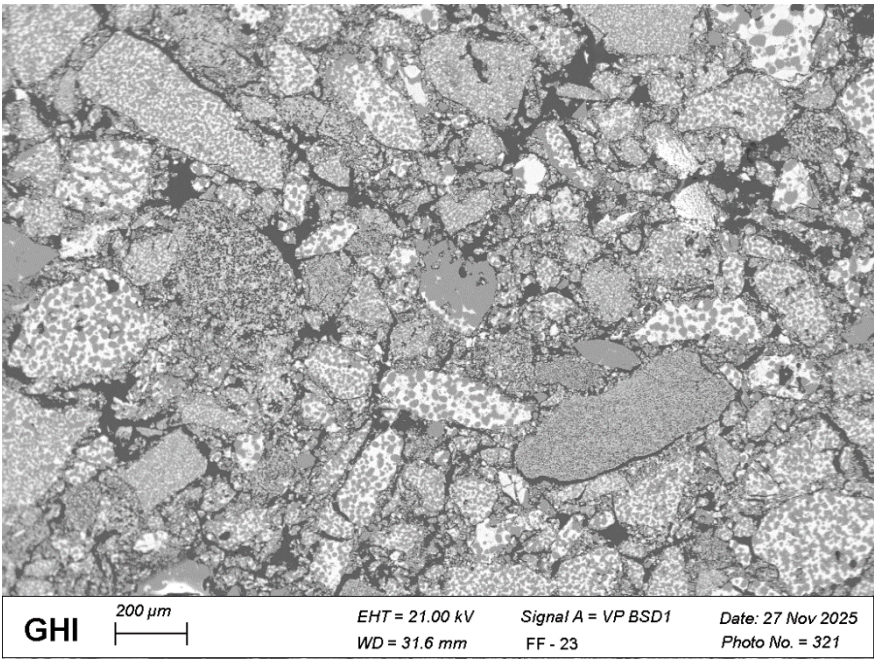


Figure 171. SEM micrograph of PB-T02 (1175 °C, 48 h), overview

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SF

Figure 172 shows the microstructure of an untested SF doloma brick. The microstructure is made of heterogeneously distributed grains which range from mm to micrometre scale. The individual grains consist of smaller MgO grains (darker grey) imbedded in a matrix of CaO, which again is a typical doloma microstructure. The morphology of the grains can be described as mostly rounded and low sphericity. The open porosity is filled by epoxy resin (displayed in darker grey/black). Fine grains of doloma as well as MgO grains from a porous matrix. Due to the absence of a pore filling bonding phase, the mechanical stability gets substantially realized by ceramic bonds. EDS verified the presence of SiO_2 , Al_2O_3 and Fe_2O_3 in traces which matches with conducted XRF measurements. Those impurities contribute to higher densification, lowering of sintering temperature and are typically found in doloma refractories. Comparing SF, SF-T01, SF-T02 and SF-T03, no essential differences in microstructure were observed.

However, significant differences in microstructure occurred for SF-T04. Figure 174 and Figure 175 indicate the microstructure investigation for SF-T04. Comparing the microstructure to new SF, to SF-T04, the porous matrix was significantly attacked causing a disintegration of the ceramic-bonded framework. This attack causes an increase in porosity but also loses the ceramic bonding. Based on this observation, a general loss of mechanical performance is expected (which was not tested within this report). Figure 175 shows the explanation for the loss of ceramic bonding indicating the formation of clusters in contact to CaO grains. Interestingly, this phenomenon gets consequently observed at the outer interface of CaO grains indicating the local formation of portlandite ($\text{Ca}(\text{OH})_2$). This theory is supported by thermodynamic simulation as well as XRD. Due to the formation of portlandite, the microstructure is weakened which for longer tests (SF-T05) showed catastrophic collapse. Comparing the hydration performance of PB and SF at 200 °C, SF shows a substantially enhanced hydration resistance compared to PB which already lost its integrity after 3 h.

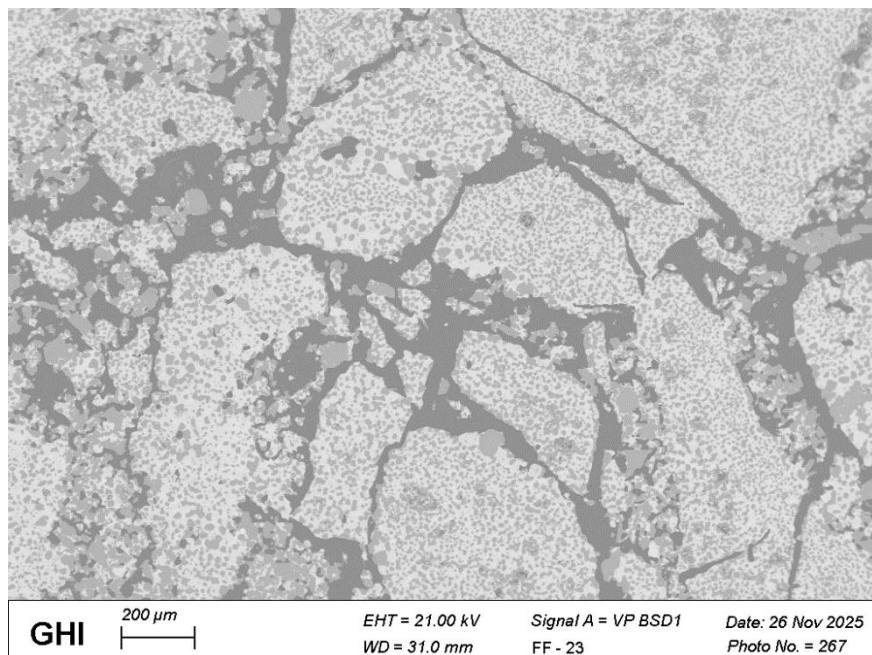


Figure 172. SEM micrograph of SF (overview), as received

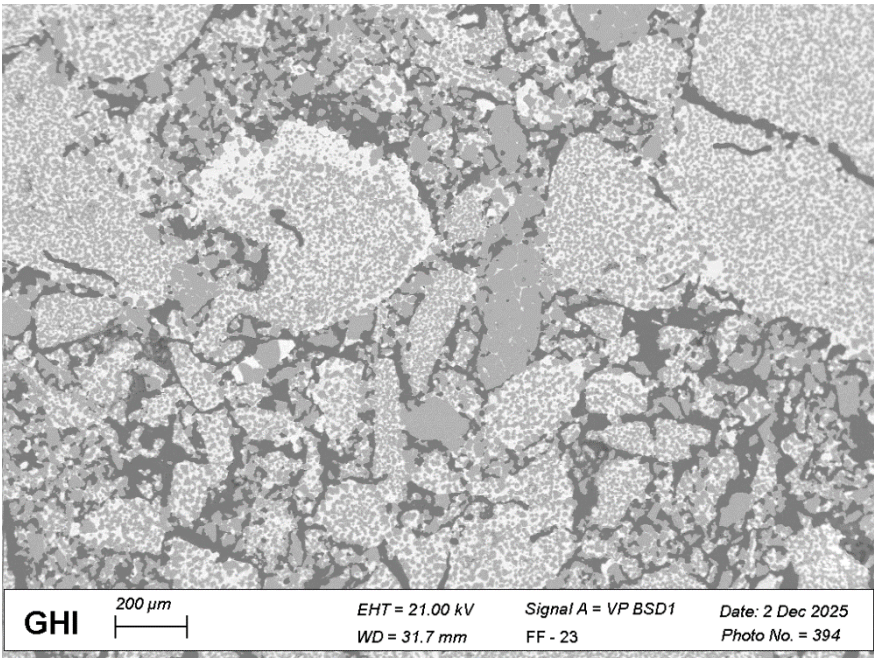


Figure 173. SEM micrograph of representative microstructure of SF-T01 and -T02 (1175 °C, 24/48 h)

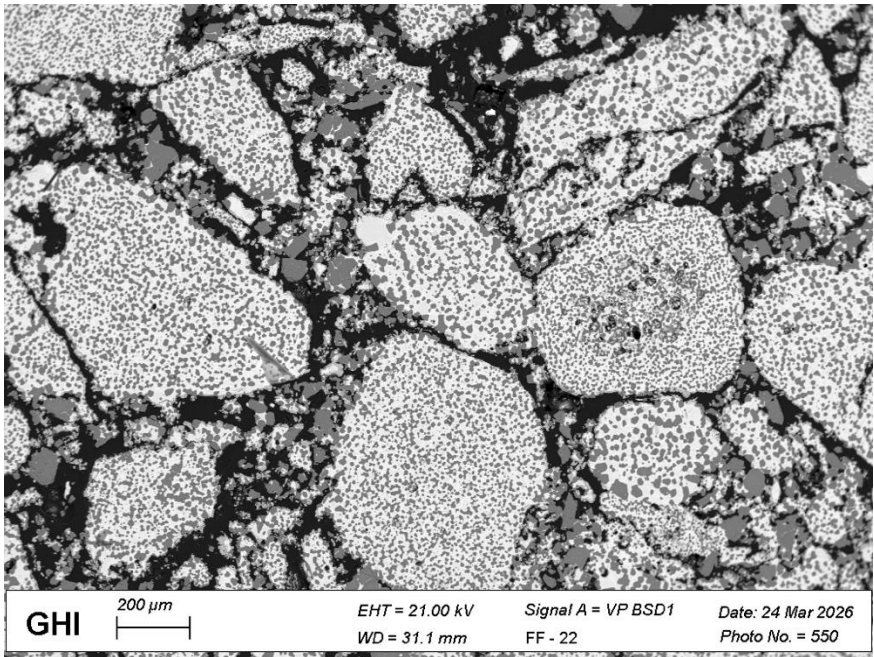


Figure 174. SEM micrograph of SF-T04 (200 °C 3 h), overview

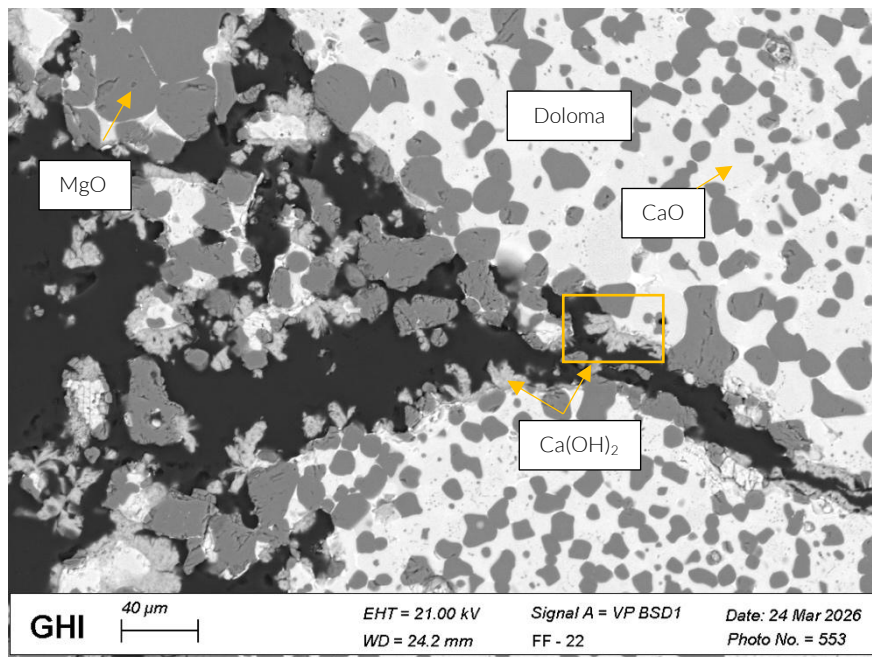


Figure 175. SEM micrograph of SF-T04 (detailed zone out of Figure 174)

8.4.4 Chemical analysis: XRF, LOI and Carbon content

Chemical analysis was carried out by using an energy dispersive X-ray fluorescence spectrometer (SPECTRO XEPOS). The samples were measured as powder and prior dried within a desiccator. Measurements were carried out under Helium atmosphere. For better comparability, the XRF results were rounded to the second digit and only components >0,05 wt.% are presented.

Since carbon was not possible to quantify by EDS, the carbon content was separately measured (based on DIN EN 21068-2). Briefly, to determine the total carbon content, about 0.5 g were put into a tube furnace and pure oxygen was used to cause an ideal combustion atmosphere causing the formation of CO₂. Evaporated CO₂ was captured by using CO₂ absorbing NaOH. The change in weight of the NaOH containing vessel was used to determine the carbon content within the sample. Contrary to DIN EN 21068-2, no Palladium 2-chlorid was used.

Furthermore, the loss of ignition (LOI) was added which was carried out by tracking the mass loss after heating to 1050 °C for 4 h in normal atmosphere. To avoid rehydration, the samples were cooled down in desiccator.

PB

Table 45 shows the chemical composition of tested PB materials along with their LOI and total carbon content. Considering carbon content and LOI to the measured chemical compositions, there were no significant changes in chemical compositions. Smaller variations in CaO and MgO content could be caused by sample preparation, inhomogeneity's of samples or limitations of present EDX method. Interestingly, the chemical trace components such as SiO₂, Al₂O₃ and Fe₂O₃ showed only minor deviations which indicates no significant chemical attack or evaporation of volatile species. Consequently, for all tests, there is a strong correlation between high carbon content along with high LOI and the presence of SO₃. Interestingly, SO₃ was enriched in untested PB as well as in PB-CO samples and is a common impurity in graphite. As indicated in Table 45 the content of sulphur was measured to strongly depend on temperature. Whereas for PB-T01 and -T02 practically no sulphur was found, the content increased by lowering the temperature which again strongly correlates to the presence of graphite.

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Table 45. Chemical composition, total carbon content and LOI of tested PB samples

Component	PB	PB-T01	PB-T02	PB-T03	PB-T04		
MgO	45,24	48,41	46,92	45,15	36,42		
Al ₂ O ₃	0,71	0,77	0,69	0,70	0,60		
SiO ₂	1,18	1,09	1,01	1,15	0,93		
SO ₃	0,94	0,01	0,02	0,37	0,77		
K ₂ O	0,06	0,06	0,05	0,06	0,06		
CaO	52,18	50,20	51,59	52,98	54,26		
Fe ₂ O ₃	0,36	0,32	0,57	0,37	0,38		
Sum	100,77	100,96	100,95	100,88	93,50		
LOI (wt. %)	6,35	0,65	0,91	4,45	9,53		
C _{tot} (wt. %)	4,6	-	<1	3,2	3,4		
Component	PB-CO (black)	PB-COb-T01	PB-CO (white)	PB-COw-T01	PB-H2	PB-H2-T01	
MgO	46,22	48,27	46,86	47,70	48,13	48,29	
Al ₂ O ₃	0,94	1,14	0,81	0,74	0,73	0,65	
SiO ₂	1,35	1,72	1,26	1,28	1,12	1,34	
SO ₃	1,21	0,02	0,97	0,02	0,34	0,00	
K ₂ O	0,06	0,06	0,05	0,06	0,06	0,05	
CaO	50,47	49,33	50,32	50,72	50,07	50,08	
Fe ₂ O ₃	0,37	0,32	0,40	0,34	0,34	0,39	
Sum	100,72	100,94	100,77	100,95	100,89	100,92	
LOI (wt. %)	4,43	0,84	0,76	1,25	1,04	0,88	
C _{tot} (wt. %)	3,7	<1	<1	<1	<1	<1	

SF

Table 46 shows the chemical compositions of tested SF samples together with their LOI. Due to absence of carbon bonding, the loss of ignition was insignificant. The chemical composition after conducted tests did also not show significant differences compared to the reference sample SF. Even elemental fractions such as MnO, SiO₂ and Fe₂O₃ were not strongly affected by conducted tests. Therefore, there was no substantial change in chemical composition of SF for any condition including H₂ and CO firing which shows the chemical inertness of ceramic- bonded SF.

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Table 46. Chemical composition and LOI of tested SF samples.

Component	SF	SF-T01	SF-T02	SF-T03	SF-T04
MgO	53,13	52,94	52,17	55,37	51,74
Al ₂ O ₃	0,66	0,61	0,90	0,81	0,65
SiO ₂	0,45	0,40	0,88	0,66	0,44
P ₂ O ₅	0,05	0,06	0,05	0,06	0,05
SO ₃	0,01	0,01	0,01	0,01	0,00
K ₂ O	0,05	0,05	0,05	0,05	0,05
CaO	45,41	45,79	45,83	42,87	46,86
MnO	0,13	0,13	0,11	0,12	0,13
Fe ₂ O ₃	0,98	0,89	0,83	0,92	0,96
Sum*	100,95	100,95	100,92	100,94	100,95
LOI (wt. %)	1,17	0,62	0,95	0,78	2,04

Component	SF-CO	SF-CO-T01	SF-H2	SF-H2-T01
MgO	48,72	50,61	48,41	47,17
Al ₂ O ₃	0,79	0,47	0,46	0,50
SiO ₂	0,71	0,63	0,72	0,71
P ₂ O ₅	0,06	0,05	0,05	0,05
SO ₃	0,06	0,04	0,12	0,00
K ₂ O	0,06	0,05	0,06	0,05
CaO	49,34	47,97	50,01	51,26
MnO	0,14	0,14	0,11	0,13
Fe ₂ O ₃	0,95	0,90	0,91	0,98
Sum*	100,92	100,93	100,91	100,94
LOI (wt. %)	1,24	0,92	0,55	0,70

8.4.5 Mineralogical analysis (XRD)

Powder X-ray diffraction (XRD) was performed on a Bruker D8 Advance diffractometer in Bragg-Brentano geometry and using Cu radiation. The investigated materials were powdered by using a tungsten-carbide mill until a particle size of $\leq 63 \mu\text{m}$ was obtained. Because the samples contain free CaO, which reacts readily with atmospheric moisture, the milled powder was stored. To avoid moisture uptake during the measurement, each powder was loaded into an airtight sample holder made of a silicon single-crystal. The Si substrate does not produce Bragg reflections within the measured 2θ range and therefore does not interfere with the diffraction pattern. The holder is sealed with a dome-shaped cap that includes a metallic X-ray beam knife, reducing background noises. Diffraction patterns were recorded from 5° to 90° 2θ without sample rotation, using the instrument's standard operating conditions (40 kV, 40 mA, step size 0.02° 2θ , 1 s counting time per step). The obtained diffractograms were processed with DIFFRAC.EVA (Bruker) and compared with reference patterns from the PDF-2 and ICSD databases for qualitative phase identification.

Mineralogical analysis of PB

Figure 176 shows the X-ray diffractograms for all tested experimental conditions stacked above each other to track changes in mineralogical composition throughout conducted experiments. Untested PB contains graphite, Periclase and lime as major phases and some minor traces of portlandite.

For all XRD measurements, traces of Portlandite were found in insignificant amounts, which can even not be resolved in the XRD plot. The presence of portlandite could be explained due to storage, transportation or

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any reaction to ambient moisture after the tests. Moreover, low-intensity peaks, could be reasonably attributed to traces of Brownmillerite and partially to the presence of Alite (Ca_3SiO_5). For doloma refractories Brownmillerite could form due to the synthesis procedure by reaction of CaO with Al_2O_3 and Fe_2O_3 . Interestingly, for PB-Cob and PB-Cow, traces of Oldhamite (CaS) might be present. Since for the other samples no indications of CaS were found, the CO-firing could have caused the reaction of CaO and S under reducing conditions to form.

For PB-T04 (200°C and 3h), a significant increase in portlandite content was observed as it can be seen in Figure 176(b). After PB-T04 test, only traces of lime were possible to detect indicating a nearly fully conversion from CaO to Ca(OH)_2 . Moreover, whereas initial lime peaks were found to be sharp (low full width at half maximum), the hydroxides were found to cause wider peaks in the diffractograms. Comparing the FWHM, gives rise to averaged particle size in nm scale. After PB-T03, no significant hydroxides were found even after 24 h at constant steam flow conditions (Figure 176(c)). Therefore, as soon as the operating temperatures are higher than 500 °C, hydroxides are less likely which is in general good agreement with thermodynamic simulations.

Conducted PB-T01/T02 tests lead to graphite burnout which can be seen by comparing Figure 176(a) and Figure 176(d). Phase analysis of tested PB-CO samples revealed the burnout of graphite by comparing PB-Cob and PB-Cob-T01. For both tested materials, T01 conditions caused the oxidation of the trace component CaS . For PB-Cow, the major phases did not show significant change in mineralogy by XRD.

Testing PB-H2 ladle samples, the graphite was found to be burned out after PH-H2-T01 and only lime and Periclase remained as significant phases present.

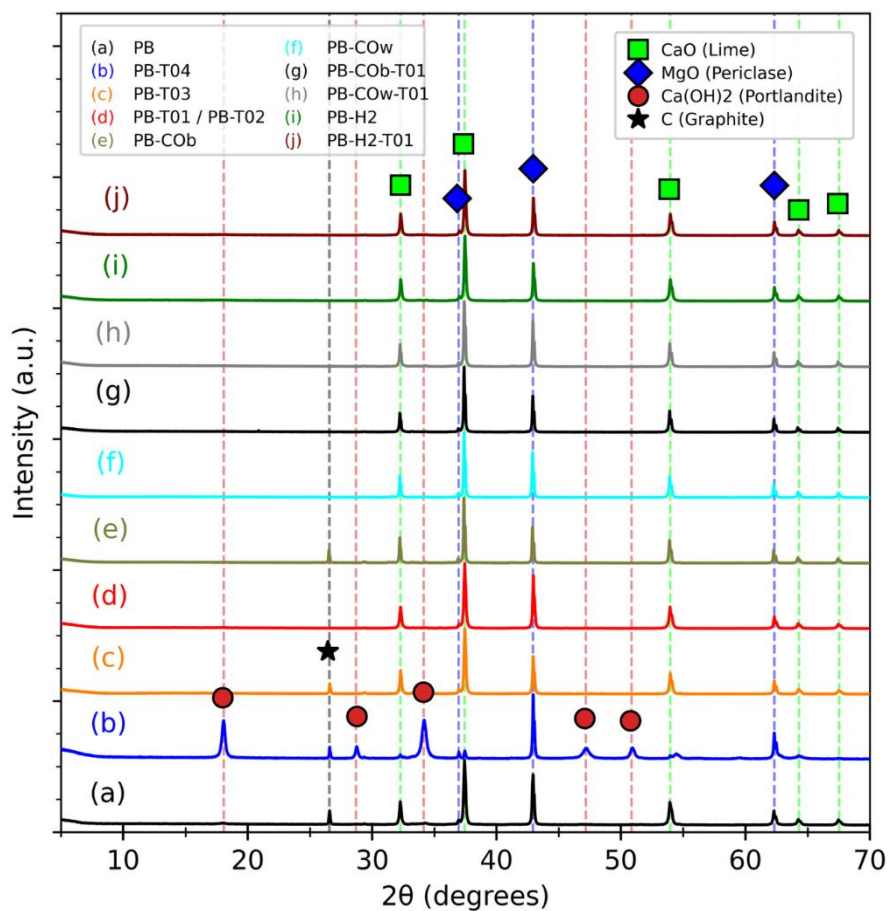


Figure 176. Qualitative phase analysis of Pentabrick after different experimental conditions

Mineralogical analysis of tested SF

Figure 177 shows the collection of diffractograms for each testing conditions along with their reference materials (as received). Initially, the mineralogy of factory new SF was characterized and showed the presence of MgO (Periclase) and CaO (Lime) as major phases along with traces of portlandite and Brownmillerite. Brownmillerite was consequently present at all tested conditions and is an initial material caused by the manufacturing of doloma.

Testing SF after being exposed to water vapour at 200 °C for 3 h, a small increase in portlandite was observed and its significant positions are marked by a spherical red marker. This observation correlates with a small increase in weight after the test (Table 44). As trace minerals, Brownmillerite ($\text{AlCa}_2\text{FeO}_5$) was found which gets attributed to the synthesis of SF.

For SF-CO and SF-CO-T01, the mineralogy revealed no significant differences. Therefore, the usage of CO burning followed by water vapour corrosion at 1175 °C showed no significant change of the mineralogical composition.

For SF-H2 and SF-H2-T01 the mineralogy remained unaffected similarly as for SF-CO and SF-CO-T01. Again, there is no significant phase change comparing H₂ fired samples to water vapour corroded samples at 1175 °C indicating a similar corrosion effect to SF.

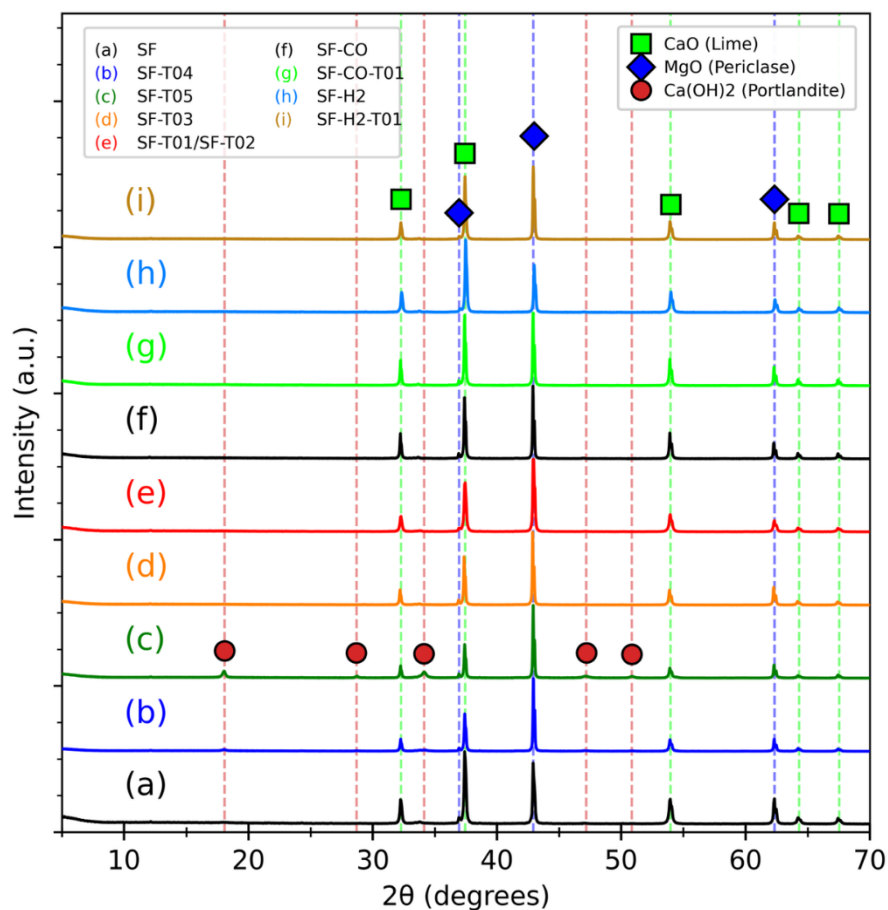


Figure 177. Quantitative phase analysis of SF after different experimental conditions

8.5 Conclusion

In this study, the corrosion resistance of carbon- and ceramic-bonded doloma refractories was investigated at 1175 °C, 500 °C, and 200 °C. Under all test conditions, Sindoform K40 (SF) showed the highest corrosion resistance. In contrast, Pentabrick C01 (PB) was significantly affected at higher temperatures due to graphite loss and at lower temperatures due to its lower hydration resistance compared with SF. In practice, however, the formation of hydroxides can be minimized by rapid heating to temperatures above 500 °C. Although hydroxide formation may be reduced by fast heating rates, carbon burnout of PB during preheating in an H₂O-rich atmosphere can still lead to significant damage once the molten steel comes into contact with the doloma refractory. In the absence of graphite and its repulsive mechanism, the molten steel is likely to infiltrate deeper into open pore channels, causing increased wear and potentially catastrophic corrosion.

9 Task 6.3: Ladle Preheating with H₂/O₂ Combustion – Investigation of refractory by RHI Magnesita

It was observed that the combustion of different gas mixtures could influence not only aluminium or steel quality but also the refractory lining. For this trial, fifteen different refractory products were sent to 7 Steel Nordic Manufacturing AS, Mo i Rana, Norway. The samples were embedded in a sintered magnesia ramming mix (ANKERHARTH NN25) inside a metal box that was open at the top. This box was placed in an 85-ton ladle and heated from 0 °C to 1200 °C over a period of 10 hours.

A CO-rich gas mixture, which is a by-product of silicomanganese and/or ferromanganese alloy production, was used. The gas composition was: CO 79.6 wt%, H₂ 8.6 wt%, CO₂ 7.7 wt%, N₂ 3.8 wt%, O₂ 0.3 wt%.

This preliminary report summarizes the macroscopic observations of the trial specimens and outlines the next steps for further analysis.

9.1 Problems/Objectives

This study analyses the influence of different combustion gas mixtures on the refractory lining. 15 different brands were selected because they are typical for the respective grade:

Table 47. List of refractory samples

Grade	Main Components	Bonding Type
High Alumina	- Bauxite	Ceramic bonded
	- Fused alumina, MA-spinel	Ceramic bonded
Alumina-Magnesia-Carbon Grades	- Antioxidants, carbon black, fused alumina, Graphite, Magnesia	Resin bonded
	- Bauxite, carbon black, fused alumina, graphite, magnesia	Resin bonded
Magnesia-Carbon Grades	- Circular material, graphite, sintered magnesia	Carbon bonded
	- Fused magnesia, graphite	Carbon bonded
	- Circular material, graphite, sintered magnesia	Resin bonded
	- Fused magnesia, graphite	Resin bonded
Doloma Grade	- Sintered doloma	Resin bonded
	- Sintered doloma	Carbon bonded
	- Sintered doloma	Ceramic bonded
Magnesia Grade	- Sintered magnesia	Ceramic bonded
Magnesia Chromite Grade	- Chrome ore, fused magnesia chromite, high purity sintered magnesia	Ceramic bonded
Refractory Concrete	- Tabular alumina	Hydraulic bonded
	- MA-spinel, tabular alumina	Hydraulic bonded



Figure 178. Sample box and application for the trial.

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The samples were embedded in a sintered magnesia ramming mix (ANKERHARTH NN25) inside a metal box that was open at the top (Figure 160 left). This box was placed in an 85-ton ladle and heated from 0 °C to 1200 °C over a period of 10 hours at 7 Steel Nordic Manufacturing AS, Norway (Figure 160 middle and right).

A CO-rich gas mixture was used, composed of: CO 79.6 wt%, H₂ 8.6 wt%, CO₂ 7.7 wt%, N₂ 3.8 wt%, O₂ 0.3 wt%.

The ladle atmosphere was oxidizing throughout the whole trial time.

This preliminary report summarizes the macroscopic observations of the trial specimens and outlines the next steps for further analysis.

9.2 Preliminary results: Macroscopic overview and sample description

The following figures show all tested bands with two pictures: left is the uncut specimen, and right is the cut section of the sample. The upper end represents the hot face whereas the lower part of the images signifies the embedded part.

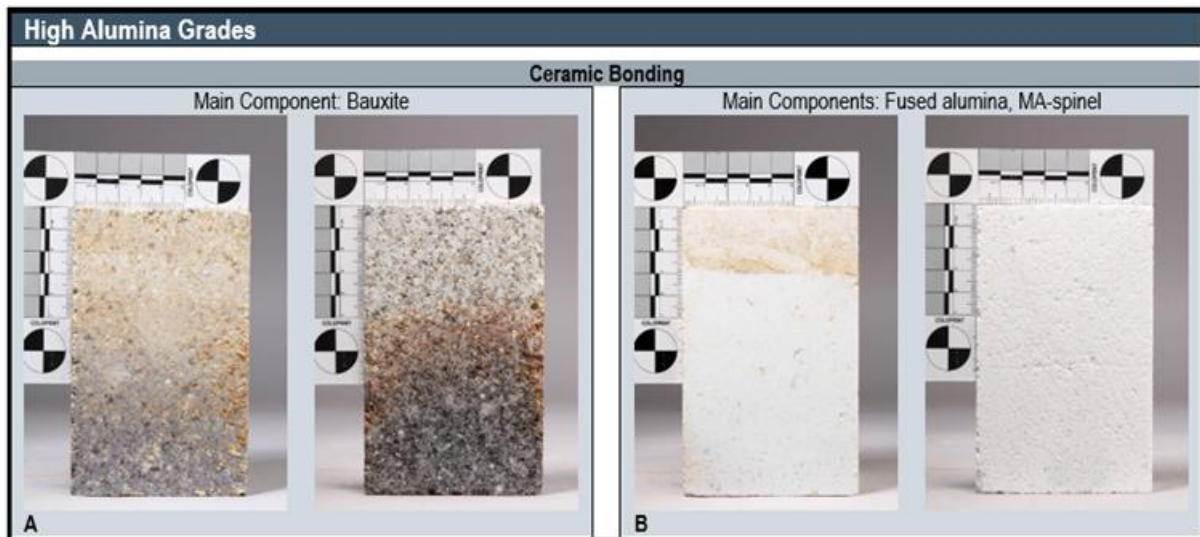


Figure 179. A: Bauxite sample; B: Fused alumina/MA-spinel sample

The bauxite sample (Figure 179 A) reacted with the ladle atmosphere at the hot face. The fused alumina/MA-spinel sample (Figure 179 B) seems completely unaffected. The discoloration on the outside stems from adhering dust.

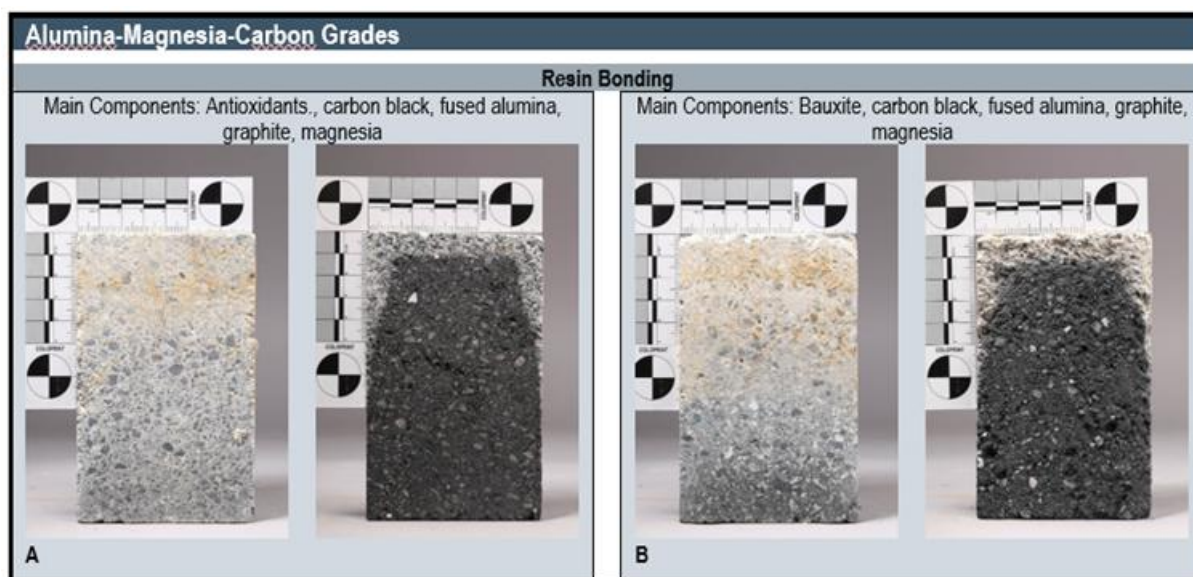


Figure 180. Al-Mg-carbon samples

The two Al-Mg-carbon samples (Figure 180) show decarbonization on the outer rims, which is normal under oxidizing conditions.

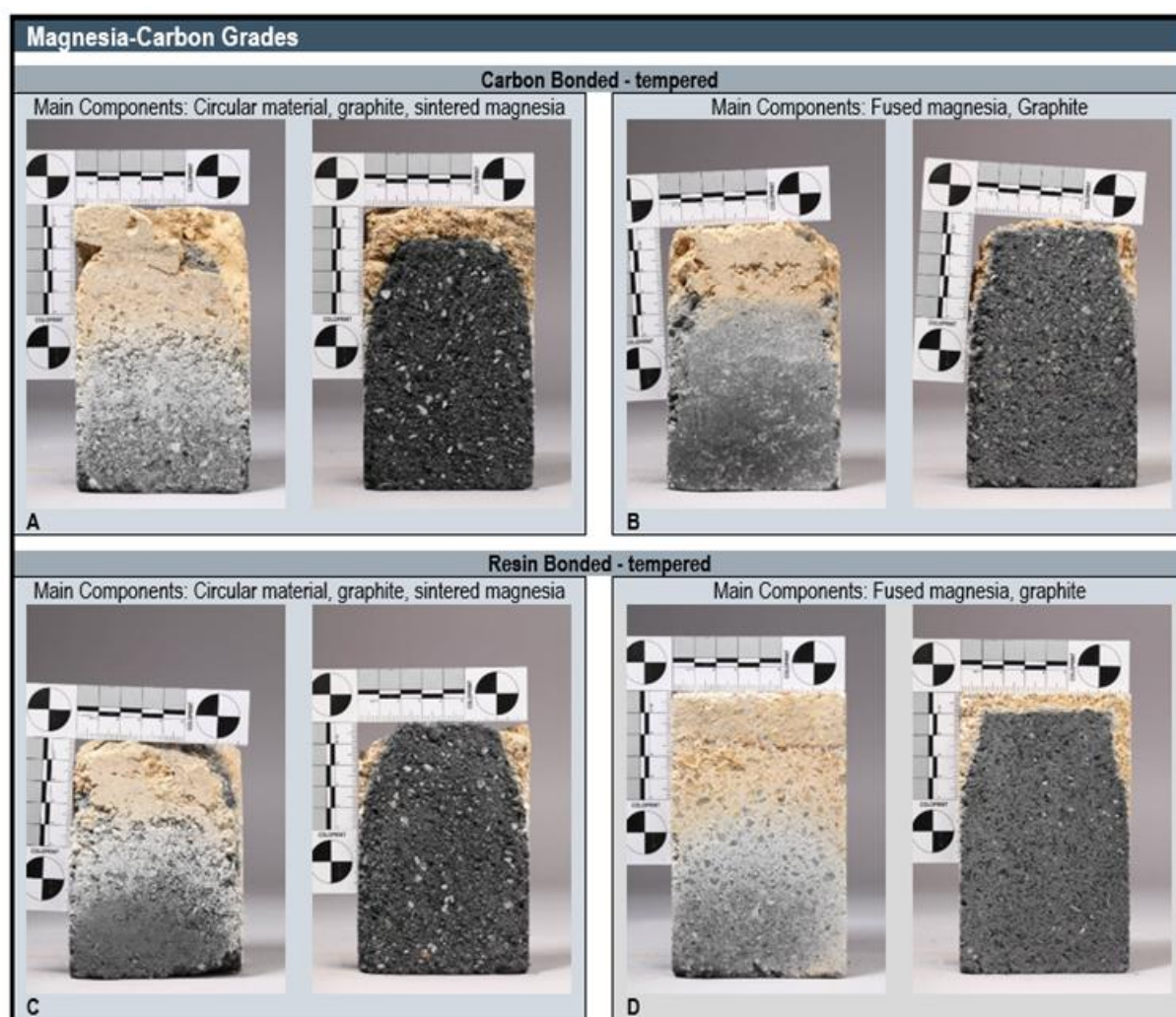


Figure 181. Magnesia-carbon samples

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All magnesia-carbon samples (Figure 181) show decarbonization on the outer rims, which is normal under oxidizing conditions.

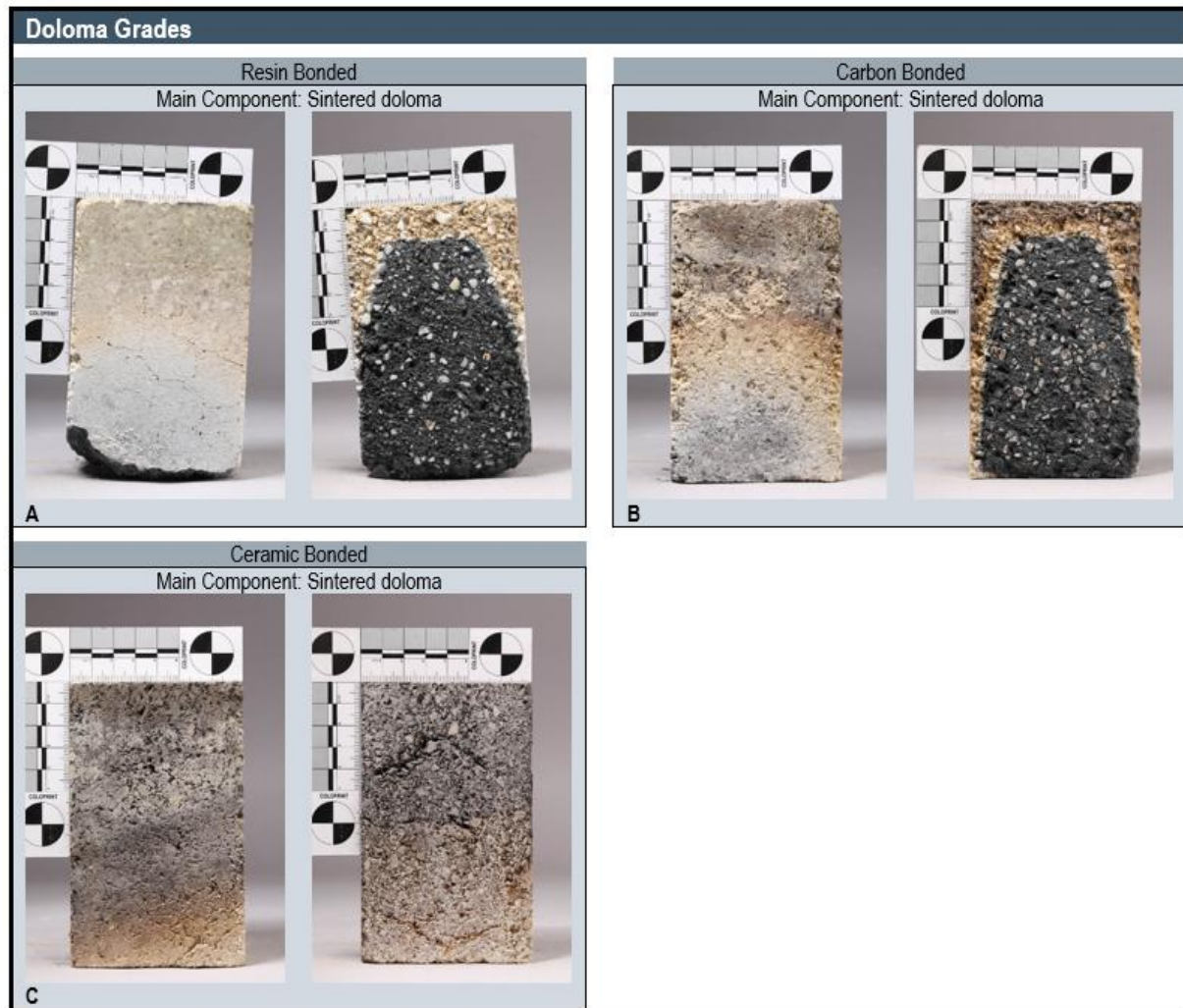


Figure 182. Doloma samples

Resin and carbon bonded doloma samples (Figure 182 A & B) show decarbonization on the outer rims, which is normal under oxidizing conditions. Ceramic bonded doloma sample (Figure 182 C) shows a discoloration on the cut section.

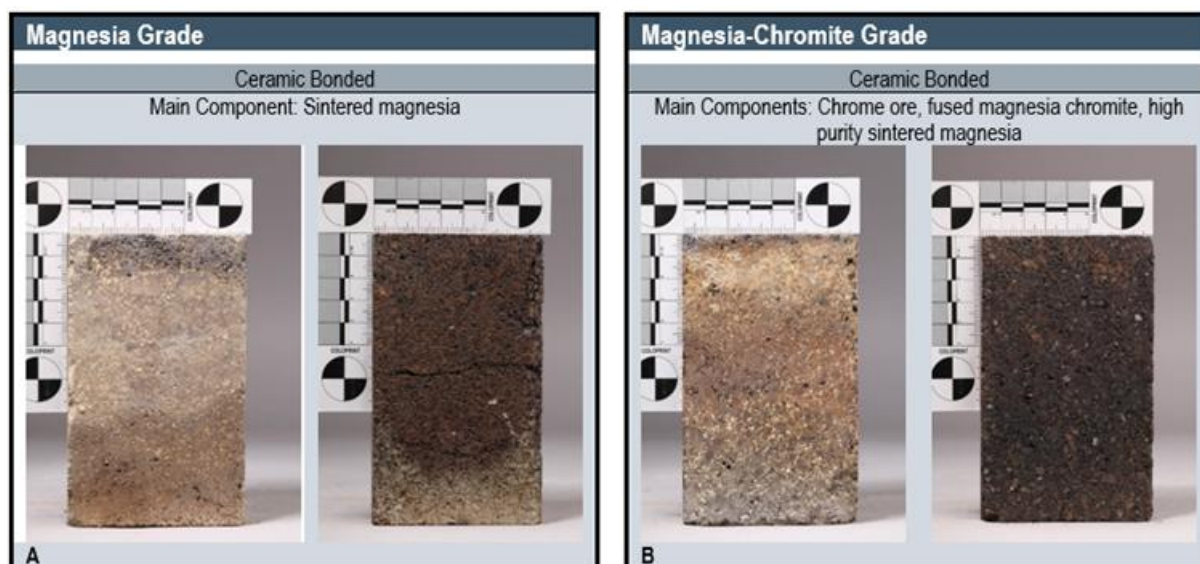


Figure 183. A: Magnesia sample, B: Magnesia-chromite sample

The Magnesia sample (Figure 183 A) shows minor discoloration on the lower rims of the cut surface. Horizontal cracking might arise from production-related layering or thermomechanical stresses related to temperature differences within the brick during heat-up; B: The Magnesia-chromite sample (Figure 183 B) shows no discoloration on the cut surface.

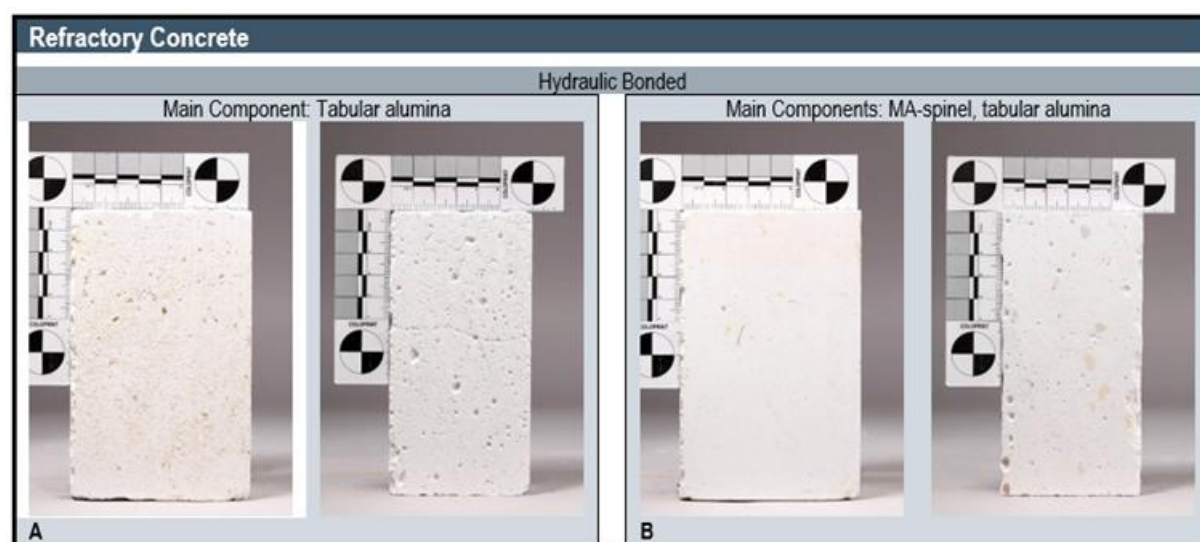


Figure 184. Refractory concrete samples

The refractory concrete samples (Figure 184) seem completely unaffected.

9.3 Conclusions and planned investigations

- High alumina grades consisting of fused alumina, tabular alumina and MA-spinel do not show any change after preheating of the ladle.
 - The Bauxite based sample shows discoloration after preheating the ladle
 - Magnesia and magnesia-chromite grades show discoloration on the sample surface after the test. The cut section reveals a slight discoloration of the Mg-Cr sample and no discoloration of the magnesia-based grade.
 - Alumina-Magnesia-Carbon and Magnesia-Carbon Grades all suffer from decarbonization of the outer rims due to oxidation.
 - The resin and carbon bonded doloma grades also show decarbonization of the outer rims due to oxidation.
 - The ceramic bonded doloma brick, however, shows signs of reduction at the hot face.
- Decarbonization of the carbon based and carbon/resin bonded bricks occurred due to the oxidizing conditions of the trial.
- Some samples show decolorization on the cut sections.
- Detailed Mineralogical/Chemical investigations are planned on the doloma grades as they are regularly lined in the ladle.
- Samples exhibiting no or only minimal alterations typical for standard preheating processes will not undergo further investigation.
- Alumina-Magnesia-Carbon and Magnesia-Carbon Grades will not be considered for detailed investigation as the main effect is decarbonization because of the oxidizing conditions during the trial.

10 Task 6.4: Greenfield Study on Walking Beam Furnace

Decarbonization of energy intensive industries is a key priority for Europe.

In the steel sector, reheating furnaces are among the main contributors to fuel consumption and CO₂ emissions. This makes them a strategic target for innovation.

Hydrogen is widely recognized as a promising carbon free fuel. Its industrial adoption, however, requires solutions that are efficient, flexible, and safe.

Within the HyInHeat project, Danieli developed a greenfield study to design a hydrogen ready slab reheating furnace grounded in the real layout constraints and operational needs of the SSAB Oxelösund site. The solution combines hydrogen operation with oxygen enriched combustion to enhance efficiency.

10.1 Danieli's Hybrid Furnace Concept

The study is based on a hydrogen ready reheating furnace designed for maximum operational flexibility.

The furnace can reheat 100tph of slabs operating with:

- natural gas, hydrogen and hydrogen natural gas blends
- air or oxygen enriched combustion

Danieli's solution is based on a hybrid combustion concept. It combines conventional air firing with oxygen enriched operation. This approach avoids the rigidity of full oxyfuel systems while still enabling significant efficiency gains.

The concept relies on:

- advanced hydrogen ready burner technologies,
- a design optimized for stable operation over a wide range of fuels,
- technologies and materials validated through real applications

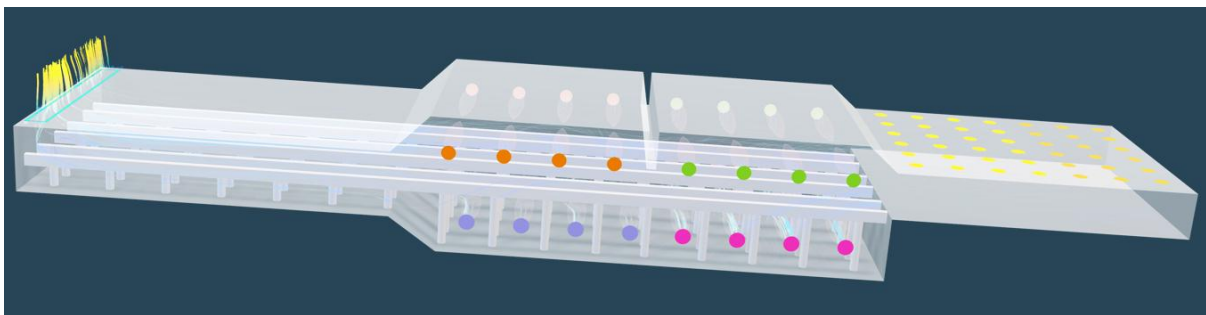


Figure 185. DAN Hybrid furnace withing the EFESTO Simulation Framework

10.2 Key Results of the Study

10.2.1 Energy Efficiency

Based on the results obtained with EFESTO, DAN proprietary furnace simulation tool, the study shows that hydrogen achieves energy performance comparable to natural gas in slab reheating applications.

Oxygen-enriched combustion leads to a clear reduction in fuel consumption for both fuels, confirming that oxidizer management is a key lever for efficiency improvement.

These results indicate that hydrogen adoption does not penalize furnace efficiency and can be effectively combined with oxygen enrichment in a hydrogen-ready furnace concept.

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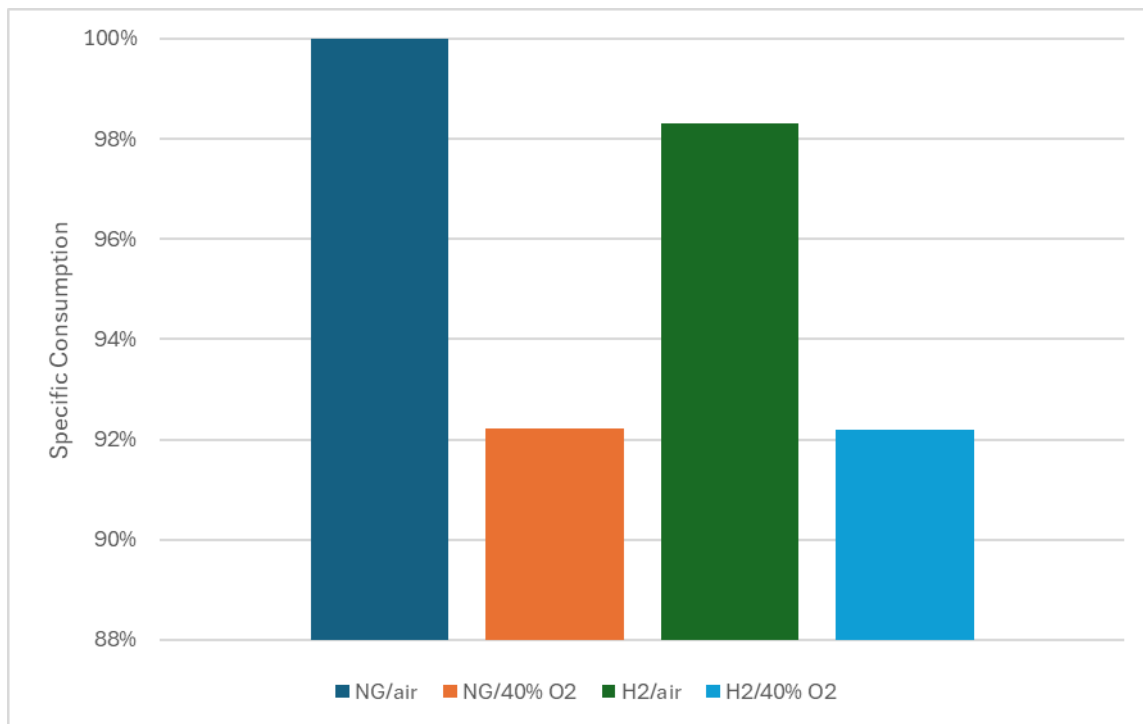


Figure 186. Comparison of specific energy consumption for different fuel (NG, H₂) and oxidizer (air, O₂ enriched)

10.2.2 Heating Quality

EFESTO simulations confirm that the furnace delivers heating quality fully comparable across all investigated operating modes, including natural gas, hydrogen, and oxygen-enriched combustion.

The use of hydrogen does not introduce any degradation in slab reheating performance.

The Danieli furnace concept ensures stable, reliable, and homogeneous heating, consistent with project-specific industrial requirements. These results demonstrate that hydrogen-ready operation can be implemented without compromising product quality.

10.2.3 Combustion System Design, Validation and Safety Assessment

As part of the greenfield study, complete Process Flow Diagrams (PFDs) were developed for the entire combustion system of the furnace.

The scope of the PFD development includes:

- fuel supply and distribution,
- hydrogen and natural gas blending and its distribution,
- oxygen enriched operation,
- combustion air management,

Safety was addressed from the early design phase.

The furnace concept and the related combustion system were assessed using HAZOP, ATEX, and PED evaluations.

The safety assessment confirms that hydrogen and oxygen enriched combustion can be safely managed through:

- safety by design principles,
- compliance with relevant European regulations and standards,
- appropriate monitoring, control, and purging strategies.

No intrinsic safety barriers were identified for industrial scale implementation.

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This confirms that the Danieli hydrogen ready furnace concept is technically coherent, robust, and suitable for real industrial environments.

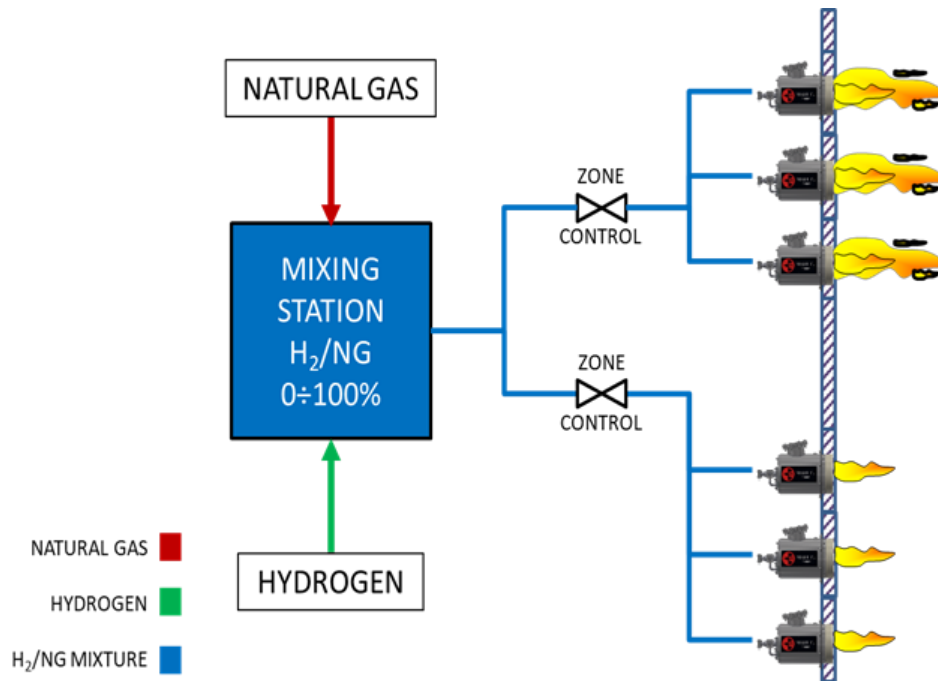


Figure 187. Basic concept of fuel blending and distribution

10.3 Hybrid Furnace vs Full Oxyfuel – Industrial Perspective

A comparison was performed between Danieli's hybrid hydrogen ready furnace and a full oxyfuel furnace concept, based on the same reference design data (productivity and slab dimensions).

Full oxyfuel combustion can theoretically achieve very high efficiency by using pure oxygen in combination with hydrogen or natural gas.

Compared with hybrid furnace, a full oxyfuel furnace:

- does not require combustion air systems (fans and air piping),
- does not include air preheating equipment such as recuperators,
- features a significantly smaller waste gas duct due to reduced flue gas flow.

As a result, a lower installed equipment scope and potentially lower CAPEX can be expected.

Comparative analyses and simulations show that the Danieli hybrid furnace:

- achieves efficiency levels comparable to full oxyfuel operation through oxygen enrichment combined with combustion air preheating;
- requires significantly lower oxygen demand than a full oxyfuel furnace, resulting in reduced OPEX;
- maintains the possibility of air-based operation, increasing robustness and operational flexibility;
- ensures BAT level NO_x emissions, while in full oxyfuel furnaces NO_x formation is strongly influenced by unavoidable air ingress, leading to emission levels that cannot be reliably controlled under real industrial conditions.

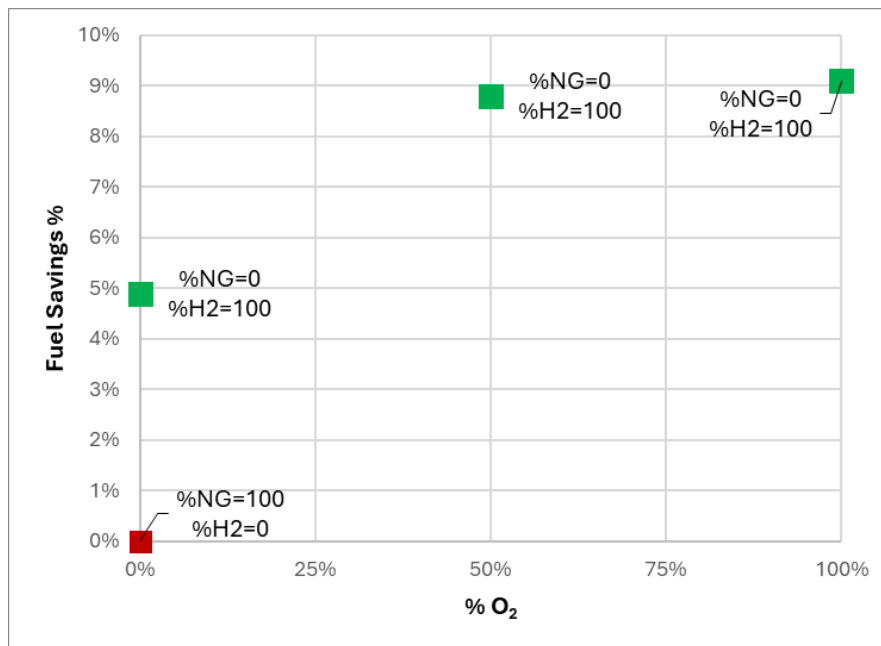


Figure 188. Simulated fuel Savings with different operating condition

10.4 Industrial Impact and Exploitation Potential

The greenfield study highlights strong industrial exploitation potential.

The proposed hydrogen ready furnace:

- supports a step-by-step decarbonization strategy,
- reduces dependence on full oxyfuel infrastructure,
- allows gradual integration of hydrogen as availability increases.

For industrial partners, this means:

- reduced transition risk,
- compatibility with existing plants and energy systems,
- readiness for future low carbon regulations.

The methodologies, design principles, and results developed within HyInHeat can be directly transferred to:

- new reheating furnace projects,
- revamping and modernization programs,
- future hydrogen based industrial heating solutions.

The study therefore provides a concrete foundation for future industrial application.

10.5 Conclusions

This public summary demonstrates that hydrogen is a viable and effective fuel for steel reheating furnaces.

Danieli's hybrid hydrogen ready furnace concept combines:

- high energy efficiency,
- operational flexibility,
- controlled emissions,
- safe industrial operation,

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By achieving near oxyfuel performance without the constraints of full oxyfuel systems, the solution offers a pathway for industrial decarbonization. The results confirm Danieli's technological leadership in sustainable reheating solutions and strongly support further industrial implementation and commercialization.

11 Task 6.5: Retrofit Study on CAL/CGL Furnace

Decarbonization of industrial heating is key for a sustainable future of the steel industry in Europe. This document is focused on indirect heating technology for annealing and galvanizing furnaces. The main target of HyInHeat project is the integration of hydrogen as fuel for high temperature heating processes in the energy intensive industries. While some of the equipment is already presented as hydrogen-ready, the integration of hydrogen combustion in heating processes still needs adoption and redesigning of infrastructure, equipment and the process itself.

The HyInHeat project studies the implementation of efficient hydrogen combustion systems to decarbonize heating and melting processes of the aluminium and steel sectors, covering almost their complete process chains. To reach this overarching objective within the project, furnace and equipment like burners or measurement and control technology but also infrastructure is redesigned, modified and implemented in eight demonstrators at technical centres and industrial plants.

11.1 Gaslab upgrading for HyInHeat project.

This deliverable for HyInHeat project is focused on testing indirect heating technology working with hydrogen and the comparison versus natural gas performance without any specific retrofitting in the lab, apart from new combustion fan, able to deliver higher air pressure burner manufacturer requirements.

In total, two different technologies have been tested at ArcelorMittal Gaslab radiant tube pilot plant (recuperative and self-recuperative burner technology). This laboratory is capable of testing two radiant tubes at the time. The maximum allowable power rate is 400 kW (200 kW by each radiant tube). This pilot plant allows high level of flexibility in terms of testing purpose (furnace temperature target, oxygen excess in flue gas, fix burner power rate...).

The hydrogen gas feeding system is composed by a decompression station, capable of providing hydrogen continuously by the connection of two semi-trailer (one semi-trailer supplying fuel and the other in back-up). The total capacity of each semi-trailer allows continuous operation for several days.



Figure 189. Hydrogen supply and decompression station at AMI.

Automatic pressure regulating valves are installed downstream to reduce the supply hydrogen pressure to working pressure defined in a range of current fuel needs.

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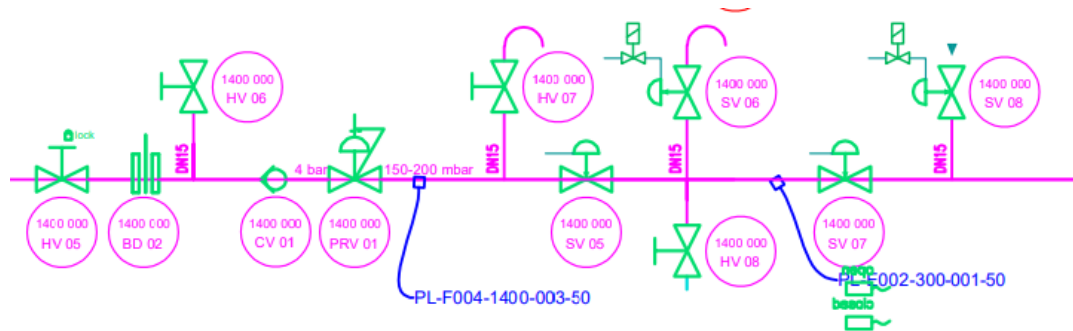


Figure 190. Hydrogen double shutoff valve and intermediate purge

On one hand, high pressure piping of the supply system was manufactured in AISI 316S stainless steel to avoid diffusion and embrittlement of the system at high pressure (American Petroleum Institute, 2016). On the other hand, for low pressure application (lower than 8 bar), standard carbon steel was installed since the previous issues described disappear when working under low pressure.

To avoid explosive atmospheres during long stoppages when the hydrogen train is not blinded, double valve with an intermediate vent was installed outside of the building. Therefore, in case of leakage of any of both valves, the gas is relief outside of the building.

The hydrogen is regulated and measured in the laboratory mixing station. Any blend between natural gas and hydrogen can be set up easily by measuring both fuel gas accurately. After the mixing station a tightness control is installed.

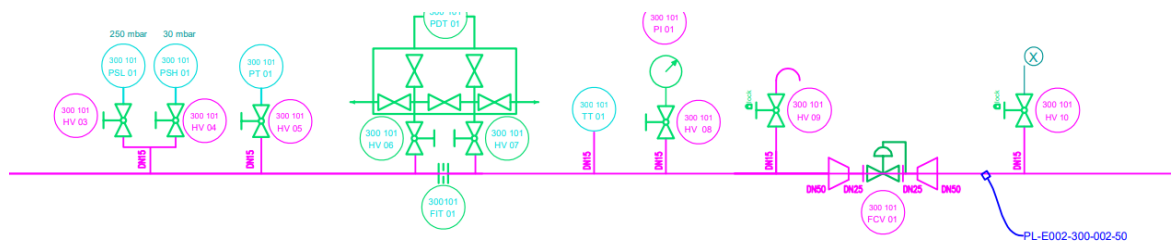


Figure 191. H₂ gas train mixing station P&ID

Downstream, a flow control valve is installed to each burner to control and adjust the desired fuel gas to be supplied to the burner.

Air train is composed by a combustion fan controlled by a variable speed driver. This allows to have more stable and accurate control of the flow for the different agreed experiments. The air flow rate is measured by a designed orifice plate and a differential pressure transmitter.

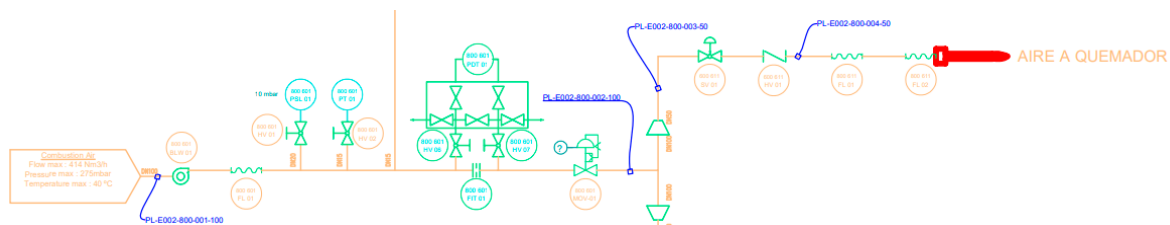


Figure 192. Air train to burner

Hence, both the proposed design for the main H₂ train and the selected new air fan were implemented with hot any unexpected issue. Here below a photograph of the commissioning at the lab is shown.

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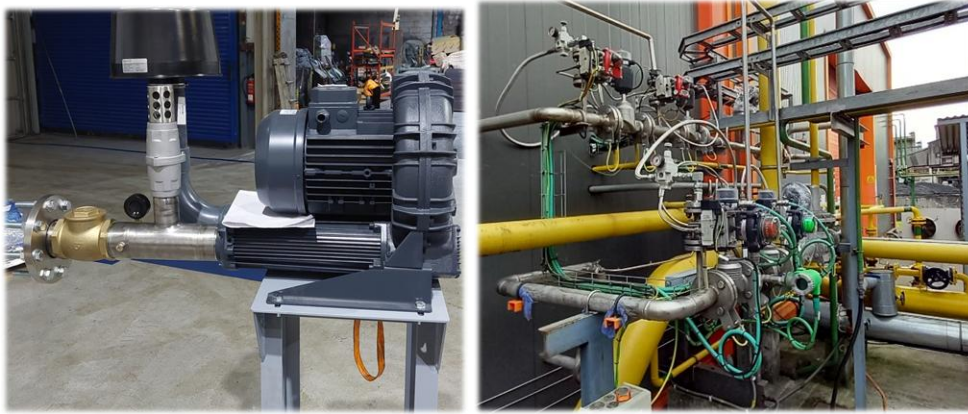


Figure 193. New air fan (left) and main H₂ feeding line (right)

Connections to the burner were made with metallic flexible hoses without any detectable leakage of fuel.

The flue gas pressure is also regulable by an exhauster equipped with a variable speed driver. The pressure transmitter is installed just at the burner flue gas outlet. Furthermore, emissions are measured continuously by a commercial analyser (O₂, NO_x, SO₂ & CO).

Upgrading the burner control unit interlock for safe hydrogen operation was required in terms of:

- Low air pressure.
- Discrepancy in target air/gas ratio.
- Low/high gas pressure.
- Cooling system ok.
- Nitrogen supply pressure ok.
- LEL detectors ok.
- Low oxygen value measured at exhaust.
- Low pressure at exhaust.

The protective atmosphere of the chamber is nitrogen that is renewed continuously for safety reason.

The heating charge along the furnace is generated by tube-plate heat exchanger, placed next to the radiant tube, simulating a strip product running in an industrial furnace. Delta temperature is being measured by thermocouples installed at inlet/outlet of the heat exchanger. Besides, the flow rate of cooling water is being measured and cool down by a dry-cooler outside of the laboratory building.

All the data is recorded in a database for a post-treatment analysis and conclusions of hydrogen firing under different scenarios.

Finally, low explosion level detectors were installed and tuned around the laboratory building for hydrogen gas leak detection.

During the project, two different technologies have been analysed under natural gas and hydrogen firing, proportional control working under W-shape radiant tube and On/Off control working operation in combination with a double P shape radiant tube. The technologies have been chosen to cover most of the heating cases used at the industrial furnaces with larger heat surface capacity. These two technologies are going to be describes in detail in the next section.

11.2 Technologies tested

11.2.1 Recuperative burner installed in W-shape radiant tube.

This state-of-the-art burner technology for U and W shape tubes is based on push-pull system for indirect heating and it is labelled as ready for natural gas and hydrogen firing- Figure 194. The tube selected is an industrial W-shape radiant tube that was selected from one industrial plant. Besides, several thermocouples have been installed to evaluate the thermal profile along the tube- Figure 195.



Figure 194. W-Shape radiant tube

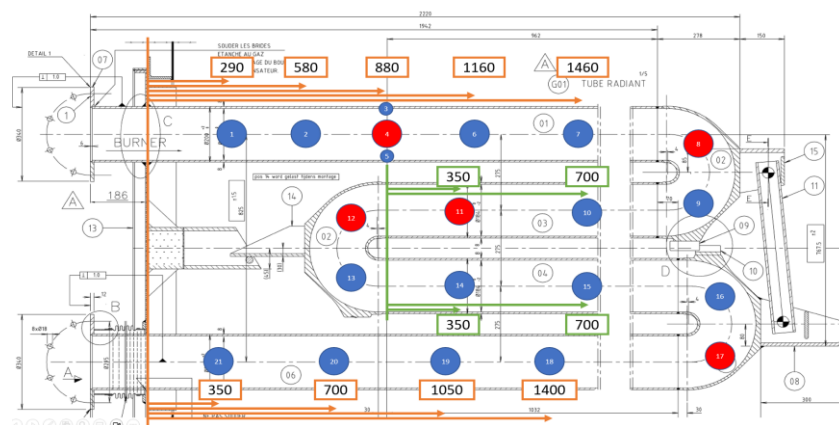


Figure 195. Proportional push-pull burner under operation and thermocouples position on the tube (right).

The burner is operated under Burner Control Unit (BCU) unit deployed in combination with pilot burner for a secure ignition and operation along the full turn down of the units. The pilot burner will always be fed with natural gas as fuel to avoid signal losses on H₂ firing. This technology has the capability of working in power modulation, adjusting the power demand with adjusting the fuel flow as the regulator acts.

11.2.2 Self-recuperative burner installed in double P shape radiant tube.

For simple P and double P shape tubes (Figure 196), the selected burner is described as state-of the-art technology, also labelled as hydrogen ready. The selected burner nominal power is 160 kW, typical burner/power installed in galvanizing furnaces. The tube selected is based on the configuration of an industrial site from the company.

This burner deployed three different working modes in combination with a burner control unit:

- Start mode: Soft start of burner/tube avoiding high thermal stress of the furnace. Ignition of the burner by means of spark.

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- Flame mode: Power up to 150 kW approx. to reach minimum autoignition working temperature, measured by tube surface thermocouple.
- Low NOx mode: Deploy full power within Low NOx operation. No supervision of ultraviolet detector. Working minimum radiant tube temperature of 850 °C (autoignition ensured with safety margin). Increase of power to 160 kW approx.

The burner is designed to work in on single operational point, maximizing the efficiency. Therefore, operation is defined in digital mode (On/Off). In the trials, the characterization of the unit was carried out at continuous operation to determine the final performance of the system. In case the temperature of the chamber needs to be adjusted, some timing on the ON/Off regulation should be programmed. Apart from that, for safety reasons, a pressure switch is equipped at burner head to detect low pressure, opening the interlock of burner control unit.



Figure 196. Double P shape radiant tube

As in the other technology tested, the thermal profile along the full shape of the radiant tube has been obtained by means of several thermocouples installed in the external surface of the tube- Figure 197.

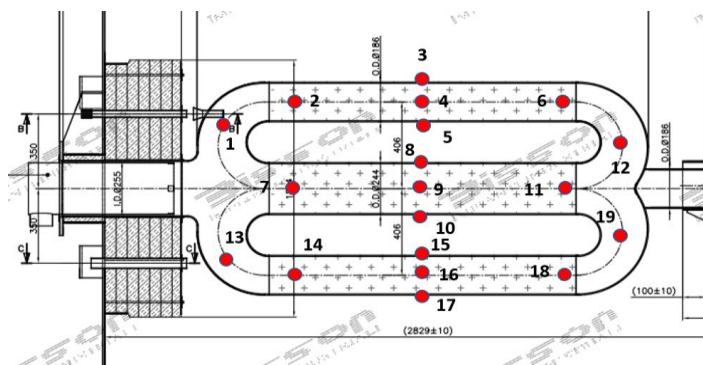


Figure 197. Self-recuperative burner and position of the thermocouples along the radiant tube (right).

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Besides, the flame control of this technology is based on ultraviolet cell that monitors the quality of the flame no matter the fuel. As commented above, there is a difference with the W-shape technology that requires continuous operation of pilot burner fired by natural gas for a safe operation and be compliant with the standards.

11.3 Conclusions

Different trials have been carried out for two different technologies for indirect heating in the steel industry. Hence, W-shape and PP radiant tubes have been covered with state-of-the-art new low emissions technologies.

All technologies tested could use pure hydrogen as fuel for a normal operation, with robust and replicable results.

As a summary, main conclusions for the units are listed here below.

11.3.1 Radiant tube recuperative burner

For recuperative burners with partial suction of product of combustion to reduce the emissions and enhance the efficiency.

- Better performance has been detected when using hydrogen as fuel.
- No major differences on the thermal profile.
- Enhanced efficiency when using hydrogen as fuel, with lower fumes temperature.
- Higher NO_x emissions with higher trend to increase when raising the O₂ content in the fumes.

11.3.2 Self-recuperative burner

For self-recuperative burner, main conclusions are:

- Thermal profiles are not different from fuel and operational mode. Lower Delta T on the surface of the radiant tube in comparison with W-Shape.
- Slightly better performance for hydrogen than natural gas in terms of efficiency.
- Higher demand needs for maintaining the temperature when using hydrogen
- Huge impact of the fuel for NO_x emission. Low emissions hydrogen mode is getting higher figures than flame mode in natural gas firing.
- UV cell works properly with this unit. Not possible to use direct spark detection.

11.4 Steel use case implementations. Report on retrofitting design study for CAL/GAL.

Based on the results listed here above, this chapter is focused on requirements for a full retrofitting of a continuous Annealing furnace or Galvanizing furnace. The required analysis is separate in sub chapters for better comprehension in several subchapters. The study is assuming an average vertical furnace of galvanizing of 150 radiant tubes as a proposed approach.

11.4.1 Flue gas exhaust.

Switching from natural gas to hydrogen would affect at flue gas composition, and its volumetric rate referred to normal conditions. Figure 198 describes the flues chemistry with same air excess.

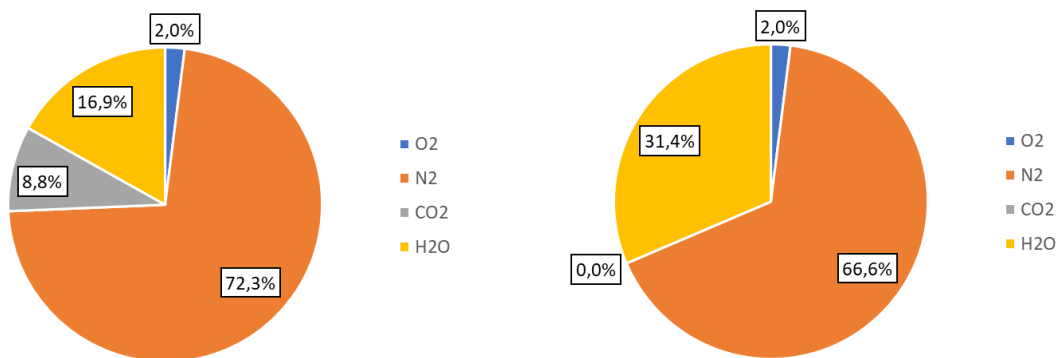


Figure 198. Flue gas volumetric share of products of combustion of NG (left) versus hydrogen (right).

The major change is referred to water vapor volume, which is increased from 6,9% to 31,4%. However, there is no works required in the flue gas ducts for a retrofitting of firing natural gas to hydrogen mainly due to two reasons:

- Flue gas temperature: It was demonstrated that temperature of flue gas is in the range of 10-20 °C lower. As a consequence, increased dilution capacity air is not required for exhaust protection.
- Flues volume: Due to chemistry of oxidation of hydrogen versus natural gas, the flow of products of combustion in Nm³/h for same power rate deployed is lower. Therefore, to keep the same pressure at main collector, (usually between -10 mbar to -40 mbar depending on the process) the capacity required by existing exhauster should be lower and can be managed with existing variable speed driver or damper position.

11.4.2 Emissions.

Best available techniques conclusions for the ferrous metal processing industry (Aries, 2022), establish the range of annealing furnaces in the range of 100-300 mg/Nm³h for NOx in continuous annealing and galvanizing furnaces. Furthermore, when using other fuels like coke oven gas or rich CO fuels, the limit is increased to 550 mg/Nm³h. There is no reference or limit to hydrogen fuel in the document to NOx emission, but it is expected to be presented in the future in case of massive implementation in industry domain.

If the natural gas limit is adopted as a reference, the technology tested is valid when firing at normal air excess. However, the self-recuperative burner tested presented higher emissions and close to the limit adopted (250 mg/Nm³).

Other emission evaluated from combustion system by the European stands are mainly Carbon Monoxide, Carbon dioxide, dust and Sulphur Dioxide. All these components are not emitted in full hydrogen firing operation.

11.4.3 Burner.

For the revamping of the existing furnaces, since burners are not compatible with hydrogen fuel or compliant with NOx emissions, all must be replaced. Average cost after consulting diverse burner manufacturers is in the range of 10.000-20.000 €.

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11.4.4 Air train.

Existing furnaces equipped with old firing systems, may require revamping of existing combustion fans. The deployed instrumentation remains valid. In any case, the chapter must be split based on the technology implemented:

For recuperative burners, traditional combustion technology was based on “flame operation”. This combustion mode is producing high amount of NO_x emissions mainly due to more stable flame formation inside of inner tube section, and because the low room available in a radiant tube (average diameter of a tube is around 200 mm or lower). To solve NO_x emissions in this technology, the main principle taken by burner manufacturers is the recirculation of part of products of combustion. Doing this technique, more diluted flame is achieved, generating lower NO_x emissions. However, to generate sucking effect to recirculate part of the products of combustion, high air pressure is required to generate a venturi effect in an internal ejector at recuperator side. The estimated investment required for substitution of existing fans in an average furnace for achieving about 150 mbar combustion air pressure is about 200-225K€ including fan, wiring, cabinet and driver.

For self-recuperative burner systems, the method of recirculating part of products of combustion in the flame, and how the flame formation is different. The recirculation of the flues is mainly done by flame formation, taking in consideration that flues are exiting the tube close to burner tip. Also, gas or air staging is implemented for more diluted flame. Therefore, the revamping of combustion air fans may not be required higher air pressure than today's technology.

Piping diameter is valid, due to the required volumetric air is lower for same power deployed comparing natural gas and hydrogen firing. In case of pull burner technology, the piping installation and the related instrumentation (valves, orifice plates, pressure switches) could be around 2 M€.

11.4.5 Tube heating.

Based on the results achieved in radiant tube pilot plant, the heating homogeneity is quite similar for the units tested. Therefore, there is not expected higher deformation grade or premature creeping with H₂ firing.

11.4.6 Flame instrumentation.

For flame ignition and detection in radiant tube burner, the most common instrument implement at industrial scale is direct spark. The burner control system allows the ignition of the air and gas mixture via spark that is also used for feedback of flame quality level. In case of flame is not detected, the gas valves shut-off for safety reasons.

The physical principle is that small voltage is applied to the spark. The resulted current (1-10 µA) is mainly caused by the flow of charged particles (ions and electrons) generated during the combustion/flame formation process. However, the ionization current level for hydrogen flames is not reliable with the current technology developed by burner and instrumentation manufactures. Therefore, the implementation of ultraviolet lamp sensor is required, and this could be traduced in the change of current supervision burner control systems from direct spark to an adapted ultraviolet lamp model. The estimated cost of this instrumentation is about 1500 € by burner in case of new burner control system is required in combination with UV lamp detection. Another possibility is the installation of pilot burner for flame ignition and supervision, only common in recuperative technology. In case of the existing facility disposes pilot burner, for recuperative technology, depending on burner manufacturer, can be installed for flame ignition and supervision powered with natural gas.

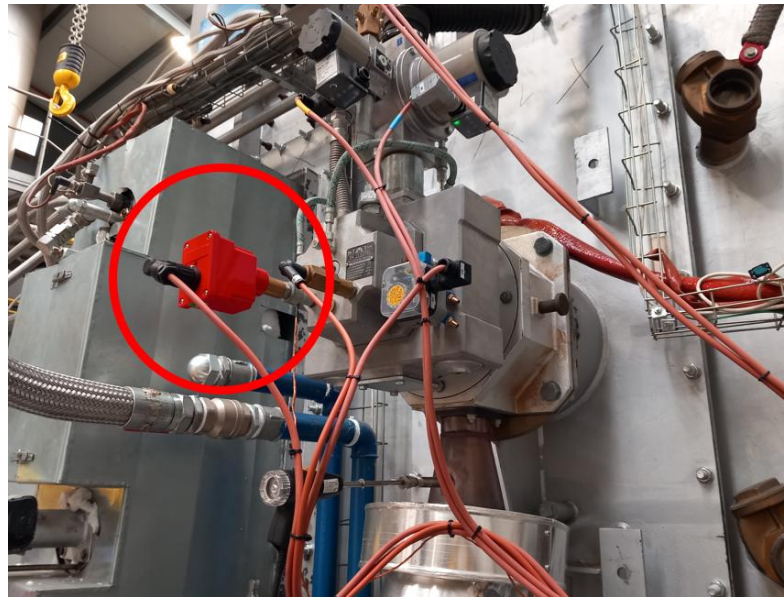


Figure 199. UV detection for self recuperative burner

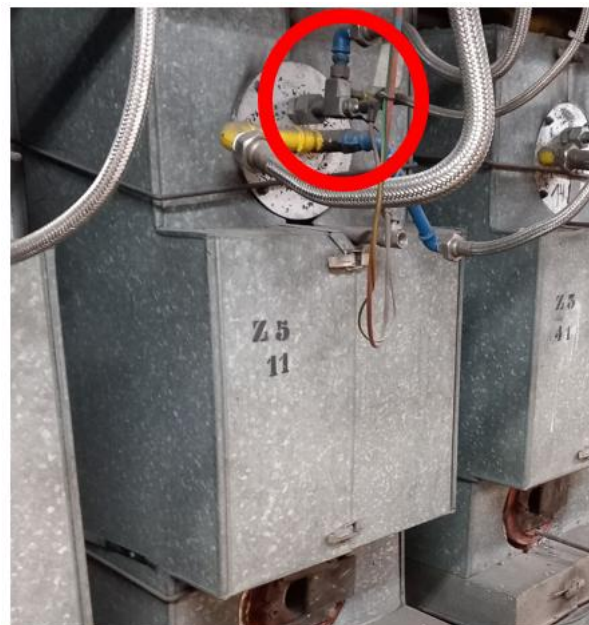


Figure 200. Industrial burner equipped with pilot burner

11.4.7 Gas Piping and fuel management.

CEN/TR 17924:2025 technical report (not a standard) proposes recommendations about “safety and control devices for burners and appliances burning gaseous and/or liquid fuels - Guidance on hydrogen specific aspects“. The standard for industrial firing operations is under development. Therefore, same piping and instrumentation can be implemented for hydrogen firing at date of writing this report. However, changes in the standard are expected in coming years. Apart from that, changes in fuel flow measuring devices are required due to low density of hydrogen. Specific orifice plates must be installed at each regulation zone in combination with pressure transmitter in case pressure regulation would be different value from nowadays natural gas pressure values. Each orifice plate could cost about 500-1000 €.

During the trials, standard carbon steel piping was implemented successfully with low gas pressure (about 100 mbar). Hydrogen embrittlement was not detected taking also into account the short duration (weeks) of

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the trials. There is a working draft “ISO/WD 25369.2 Evaluation method on piping materials compatibility in high-pressure hydrogen atmosphere applications” that is in preparation.

The diameter of actual piping is valid due to no regulation of maximum speed for hydrogen, taking into account the pressure could be increased (checking of maximum pressure working service of all fuel elements must be checked in advance). to overcome different pressure drops along piping. For example, the speed of natural gas, for low pressure applications, is limited to 20 m/s. Due to higher flow required for hydrogen, the velocity will be around 3-4 times higher in comparison with natural gas. Anyway, is recommended to renew fuel piping to ensure complete tightness of the system and to decrease hydrogen velocity in the piping. This work be estimated in 2 M€.

Pressure regulators for constant pressure feeding and shut off valve for general zones fuel supply must be added. The pressure required at burner head is similar to natural gas (about 50-100 mbar).

Since the standards are in preparation, existing valves and other instrumentation (thermocouples, manual valves, etc...) are valid for according to actual standards.

11.5 Bibliography

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Aries, E. G. (2022). *Best Available Techniques (BAT) Reference Document for the Ferrous Metals Processing Industry*. Luxembourg: : Publications Office of the European Union.

12 Task 6.5: Report on H₂ combustion system optimization for direct and indirect heating applications

12.1 Numerical study of indirect heating systems

The experimental trials are complemented by computational fluid dynamics (CFD) simulations conducted by RWTH-IOB. Numerical models of the W-type and PP-type radiant tubes are set up including the pilot scale furnace at ArcelorMittal. Simulations are run with hydrogen fuel at different oxygen excess in the flue gases, reassembling the experimental trials. Results are evaluated in terms of radiant tube surface temperatures, burner head temperatures, efficiency and NO_x emission predictions. Experimental data on surface temperatures and NO_x emissions is compared to the predictions from CFD to assess the quality of the simulation strategy.

12.1.1 Methodology

A numerical model is established for each of both radiant tubes, starting from the physical system, to the computational domain and mesh, boundary conditions, models for turbulence, chemical kinetics, turbulence chemistry interaction, and gas radiation, and the simulated operating conditions.

Radiant tube pilot furnace

The pilot scale furnace for radiant tube testing has dimensions of 2510 x 2510 x 1800 (WxHxD) mm³ with 250 mm of insulation towards the outside. Radiant tubes are inserted in two slots, the W-type on the left and the PP-type on the right, as shown in Figure 201. A water-cooled plate is inserted in between the strips, removing the heat that would be up taken by the steel strip in the industrial application. The furnace atmosphere is purged with N₂ and regulated to an O₂ content in the range of 40-50 ppm.

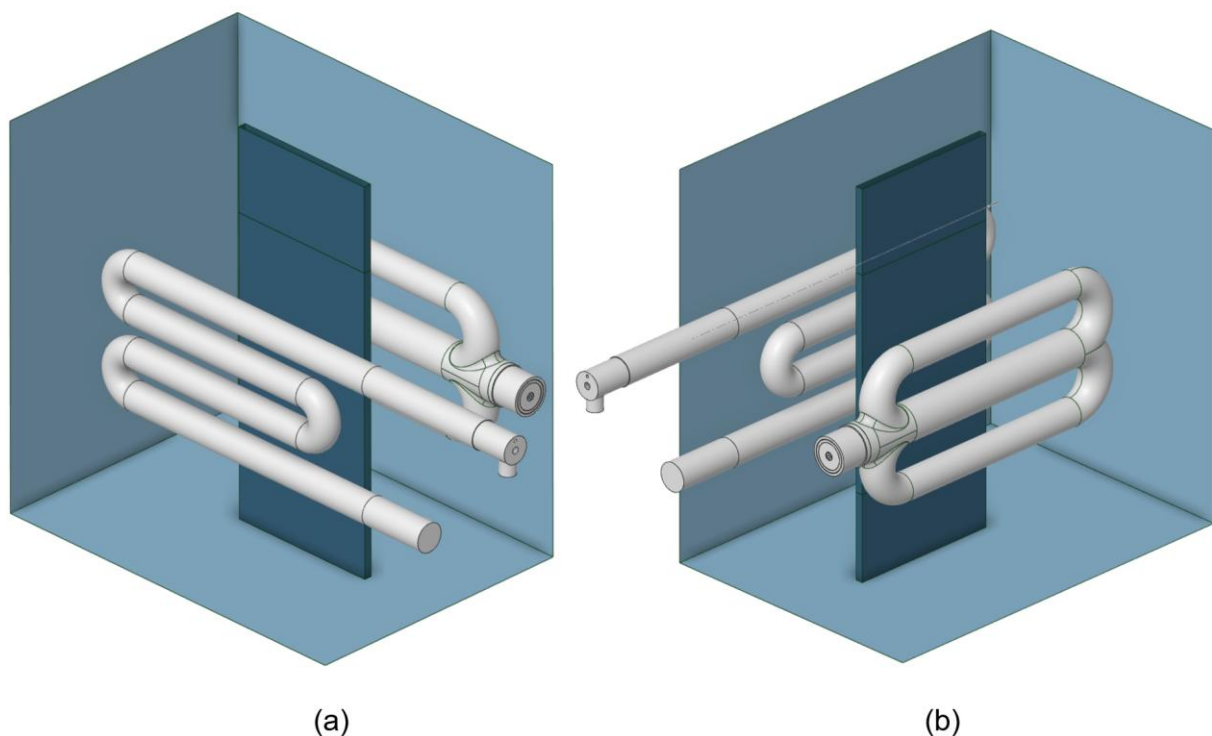


Figure 201. CAD model of the full-scale radiant tube pilot equipped with a W-type (a) and PP-type (b) radiant tube and a water-cooled plate in between.

The W-type radiant tube is fired by a non-premixed burner. Fuel gas is injected axially into the oxidizer stream from a central gas lance. The lance carries a buffer plate, that induces turbulence in the oxidizer stream. At the end of the radiant tube, flue gases pass a recuperator, where the combustion air is preheated. After the recuperator, a fraction of the flue gases is mixed with the combustion air and recirculated into the burner.

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The PP-type radiant tube is equipped with a self-recuperative burner capable of combustion in flame and flameless modes. Inside the PP-type radiant tube, a significant portion of the flue gases coming from the upper and lower legs are recirculated into the flame in the central leg. In the present investigation, the burner is operating in flameless combustion mode, where fuel is injected axially via a central gas lance.

Computational domain

For both tubes, computational domains and meshes are created. For each, the inactive tube is removed from the geometry, so the active tube and the water-cooled plate are the remaining geometric features in the furnace chamber. The domain is divided into three regions: the gas phase within the tube, where combustion takes place, the solid material of the radiant tube itself, and the atmosphere in the furnace chamber. The gas phases are meshed with hexagonal cells in the core mesh and a buffer layer of polyhedral cells between core mesh and prism layer cells close to the walls. The unrefined cell sizes are set to 24 mm in the radiant tube and 96 mm in the furnace chamber. Cell size refinements are placed in the reaction zone with cell sizes down to 1.5 mm in the vicinity of the fuel jet. The solid region of the radiant tube is not geometrically resolved, but represented by a 3 mm thick heat conducting wall in the thermal coupling between the gaseous regions inside and outside the radiant tube. To account for tangential heat conduction in the solid, one layer of shell conduction is introduced. Boundary layers are resolved by prism layer cells with the first cell height adjusted to satisfy $y^+ \approx 1$ and consecutive cell heights specified as a geometric progression with a growth rate of 1.2. This strategy produced mesh sizes of 1.29 M cells for the W-type radiant tube and 1.65 M cells for the PP-type radiant tube. The difference in mesh sizes is due to the difference in geometrical complexity of the burners used in each of the cases.

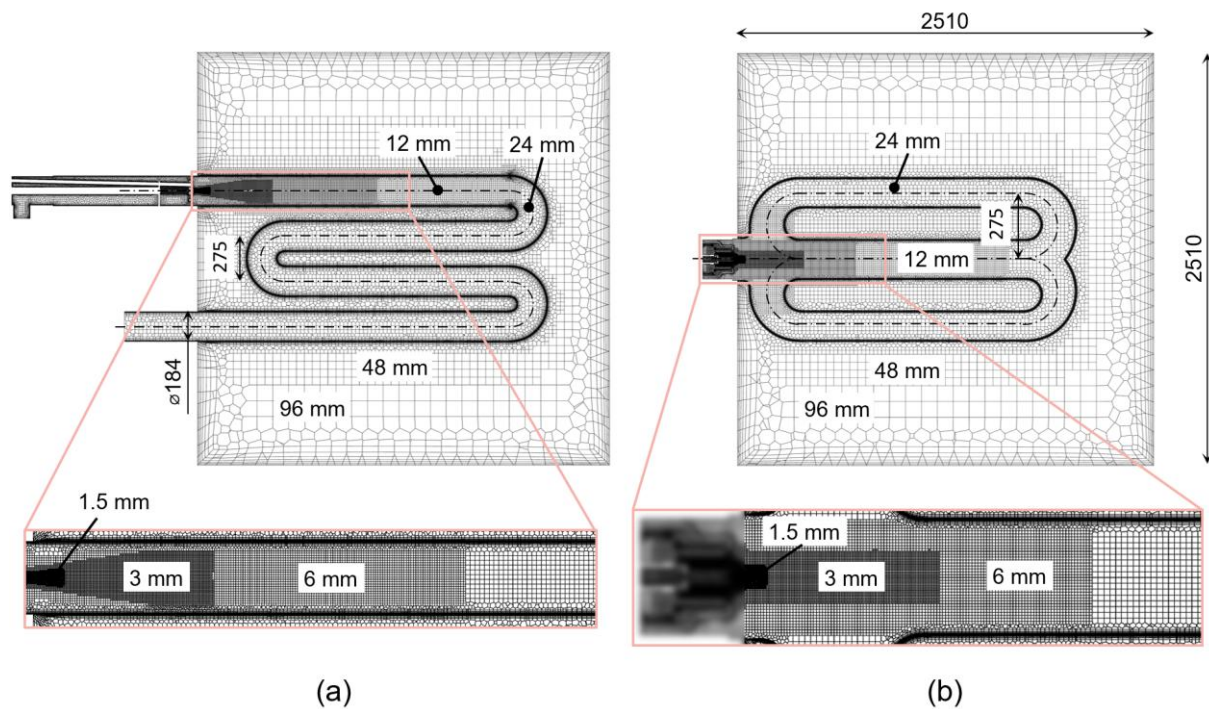


Figure 202. Computational meshes of W-type (a) and PP-type (b) radiant tubes in the pilot test rig with characteristic cell sizes, units are mm.

Numerical models

Turbulence is modelled by the realizable $k-\epsilon$ model, which is well established for the simulation of MILD combustion. The species transport model is used with the Eddy Dissipation Concept (EDC) for turbulence chemistry interaction. For the W-type radiant tube, the reacting volume fraction and reactor time scale of the EDC are computed from the Partially Stirred Reactor Model with a dynamic C_{mix} . This model is intended to improve the EDCs performance under MILD combustion conditions, which are experienced in the W-type radiant tube. For the PP-type radiant tube, the standard implementation of the EDC is used, as no flameless regime is entered by the hydrogen flame. Chemistry integration is accelerated by in-situ adaptive tabulation (ISAT) with a relative error tolerance of $1e-4$. The turbulent Schmidt number is set to $Sc_t = 0.4$. It specifies

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the proportionality between the turbulent diffusivity of species and the eddy viscosity computed from the turbulence model. Compared to the established default of $Sc_t = 0.7$, the reduction promotes turbulent diffusion of species, compensating the tendency of RANS turbulence models to underpredict the eddy viscosity. A value of 0.4 was found to sufficiently increase turbulent species diffusion, to increase the reaction zone temperature and promote thermal NO_x formation to a level that agrees with experimental measurement.

Table 48. Numerical models.

Phenomena	Model
Turbulence	Realizable $k-\epsilon$
Turbulence chemistry interaction	(PaSR) EDC
Chemical kinetic mechanism	SkeleCHy, 57 species, 582 reactions
Radiation	DOM
Gas emissivity	WSGGM

The radiative transfer equation is solved using the Discrete Ordinate Method (DOM) with a 4x4 directional discretization per octant and 3x3 pixelation of the cell surfaces. The gas absorption coefficient is determined from a full-spectrum k distribution based weighted sum of gray gases model (WSGGM), which was calibrated for blends of hydrogen and hydrocarbon fuels in Task 1.4 of the present project. The simulations are performed using the coupled solver with double precision in the commercial software Ansys Fluent release 2024 R2.

Boundary conditions

The inner furnace walls are thermally modelled by 1-dimensional heat conduction through 250 mm of refractory with a thermal conductivity $\lambda_{\text{wall}} = 0.1 \text{ W/(m K)}$ and natural convection at the outer surface. The heat transfer coefficients (htc) at the outer surfaces are computed from empirical correlations for natural convection at horizontal and vertical plates. The htc's for the side, top, and bottom walls are 5.6, 6.5, and 1.4 W/(m²K), respectively.

The amount of heat evacuated by the water-cooled strip is known from measurement of the water volume flow and inlet and outlet temperatures. The heat flow is then calculated from:

$$\dot{Q}_{\text{cool}} = \rho c_p \dot{V} (T_{\text{out}} - T_{\text{in}}).$$

and is to be evacuated from the simulation via the cooled steel strips surface. This may be achieved by imposing an evenly distributed heat flux on the respective surface. However, while one side of the strip is facing the radiant tube and is directly heated, the opposing side is heated indirectly from the walls. It is expected that the actual heat flux is greater on the radiant tube facing side, which would not be expressed by an evenly distributed heat flux boundary condition. Hence, a convective boundary condition is imposed, where the heat flux is calculated as:

$$\dot{q}''_{\text{cool}} = htc (T_{\text{surface}} - T_{\text{cool}})$$

and the total heat flow is recovered by definition of an artificial heat transfer coefficient:

$$htc = \dot{Q}_{\text{cool}} / \int (T_{\text{surface}} - T_{\text{cool}}) dS.$$

Each gas domain inside the radiant tubes has an inlet for fuel, an inlet for the oxidizer, and an outlet where the flue gases escape. At the inlets, the mass flow rate, composition, and temperature are specified. For the fuel this is straight forward in both radiant tubes, as the composition is assumed to be 100% hydrogen without impurities, the temperature is assumed to be equal to the ambient temperature at $T_{\text{fuel}} = T_{\text{amb}} = 300\text{K}$, and the norm volume flow is measured, so the mass flow is computed from the known density.

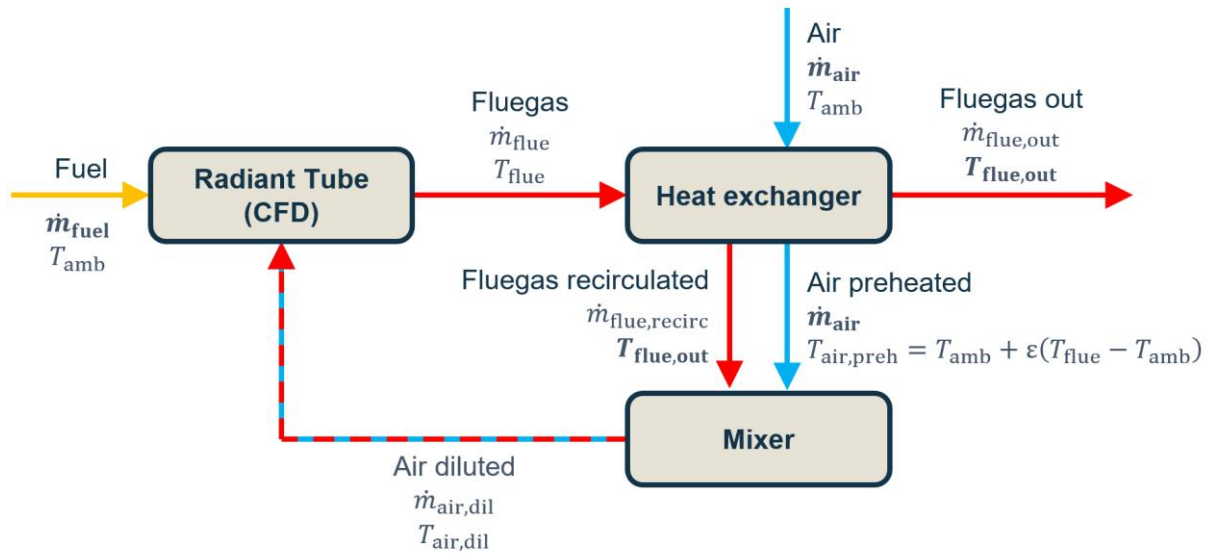


Figure 203. Mass flows in the W-tube system. Bold values are obtained from measurement.

While the configuration of the fuel inlet is equivalent for both types of radiant tubes, the oxidizer inlets differ in their specification due to the different burner design. For the W-type radiant tube, the inlet is specified according to the mass flows shown in Figure 203. Combustion air is entering the recuperator at ambient temperature, the preheated temperature is calculated from the relation

$$T_{\text{air,preh}} = T_{\text{amb}} + \varepsilon(T_{\text{flue}} - T_{\text{amb}})$$

with a relative air preheating of $\varepsilon = 0.55$. The air mass flow rate is calculated to match the measured O_2 content in the dry flue gas under the assumption of complete chemical conversion of the reactants. As the flue gas temperature is not directly measured, it is related to the radiant tube surface temperature thermocouple closest to the exit. From preliminary simulations it is found that the flue gas temperature is about **90 K** hotter compared to the temperature measured by this thermocouple. According to the manufacturer, 30% of the flue gas is recirculated. It is assumed that the preheated air is mixed with the recirculated flue gas without enthalpy losses. The resulting composition and temperature are imposed at the oxidizer inlet and are given in Table 49 for the three cases with 2%, 3%, and 4% residual oxygen in the dry flue gas.

Table 49. W-type radiant tube simulation boundary conditions.

Case	$X_{\text{O}_2,\text{flue,dry}} [\%]$	$\lambda [-]$	Fuel		Oxidizer			Cooling P [kW]
			$\dot{m}$ [g/s]	P_{th} [kW]	$\dot{m}$ [g/s]	$X_{\text{O}_2} [\%]$	T_{O_2} [K]	
1	2.0	1.08	1.131	135.6	60.411	14.29	803.5	73.8
2	3.0	1.14	1.112	133.4	62.082	14.56	798.6	71.8
3	4.0	1.21	1.129	135.4	66.005	14.84	794.1	70.4

In the PP-type radiant tube, flue gas is recirculated internally and does not have to be considered in the specification of the oxidizer inlet. Just as for the W-type radiant tube, the mass flow rate is calculated from the measured O_2 content in the dry flue gas. The air preheating temperature $T_{\text{air,preh}}$ is computed from the energy balance of the recuperator:

$$\dot{H}_{\text{flue,in}} - \dot{H}_{\text{flue,out}} = 1/\eta_{\text{rec}} c_{p,\text{air}} \dot{m}_{\text{air}} (T_{\text{air,preh}} - T_{\text{amb}}),$$

where the flue gas enthalpy flow into the recuperator $\dot{H}_{\text{flue,in}}$ is evaluated from the CFD simulation on the fly, the enthalpy flow out the recuperator $\dot{H}_{\text{flue,out}}$ is calculated from temperature measurement in the trials, the ambient temperature is set to $T_{\text{amb}} = 300 \text{ K}$, the air mass flow rate $\dot{m}_{\text{air}}$ is known from measurement as explained previously, the heat capacity of air is a value averaged over the expected air temperature range in

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the recuperator of **300 K** to **950 K**, and the recuperator efficiency is assumed to be $\eta_{\text{rec}} = 0.9$. The inlet mass flows of fuel and oxidizer are given in Table 50.

Table 50. PP-type radiant tube simulation boundary conditions.

Case	$X_{\text{O}_2, \text{flue, dry}}$ [%]	λ [–]	Fuel		Oxidizer		Cooling P [kW]
			$\dot{m}$ [g/s]	P_{th} [kW]	$\dot{m}$ [g/s]	T_{O_2} [K]	
4	2.0	1.08	1.259	151.0	46.697	–	74.5
5	3.0	1.13	1.260	151.1	48.836	–	74.4
6	4.0	1.19	1.260	151.2	51.213	–	74.5

12.1.2 Results W-type radiant tube

The simulation results are analysed for the thermal load on the system in terms of radiant tube and burner temperatures, and the formation of NO. Distributions of temperature and OH volume fraction are given in Figure 204 (a) and (b), respectively for simulation case 2 with $X_{\text{O}_2, \text{flue, dry}} = 3\%$ residual O₂ content in the dry flue gas. The flame is developing in the first leg of the radiant tube, where the peaking OH volume fraction is indicating the reaction zone.

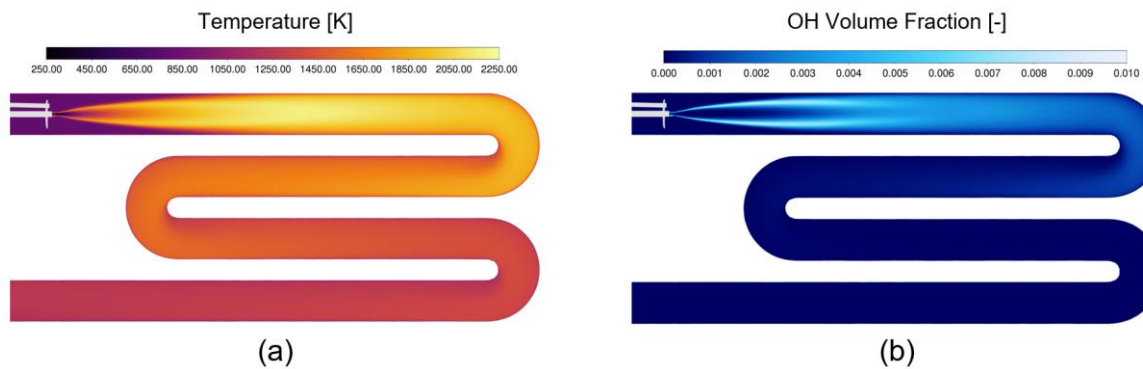


Figure 204. W-type radiant tube temperature (a) and OH volume fraction (b) distributions in the central plane for simulation case 2 with $X_{\text{O}_2, \text{flue, dry}} = 3\%$.

The evolution of gas temperature along the central axis of the first leg is given in Figure 205 (a). Depending on the case, the gas temperature peaks at 1.5m to 1.6m length of the first leg. The maximum gas temperature is increased with higher air excess, for 2%, 3%, and 4% dry flue gas O₂, the maximum temperatures are 2164K, 2179K, and 2202K in the same order. This observation may not be expected, as the adiabatic flame temperature is lowered by an increase in air excess due to the increased amount of mass that does not contribute to the heat release from combustion. However, the combustion is not adiabatic as heat is transferred from the radiant tube to the furnace. In Figure 205 (b) the running integral of the total surface heat flux is given over the first legs length, evaluated as:

$$\dot{Q}_{\text{rt}}(s) = \int_{s' \leq s} \dot{q}_{\text{rt}}(s') \, dA.$$

The integration starts from the location of the burner tip at $s=0.4\text{m}$ and the positive direction of the heat flux is defined from the radiant tube towards the furnace. The initial negative gradient of the integrated heat flux suggests that the radiant tube gains heat from the furnace up to 0.8m and from there on releases heat. At the location of the gas temperature hotspots between 1.5m and 1.6m it is observed in the integrated heat flux already that the fewer the air excess, the more heat is transferred to the furnace. The lower maximum temperature in this case suggests that the temperature decreasing effect of heat loss is dominant over the effect of a higher adiabatic flame temperature in the considered cases.

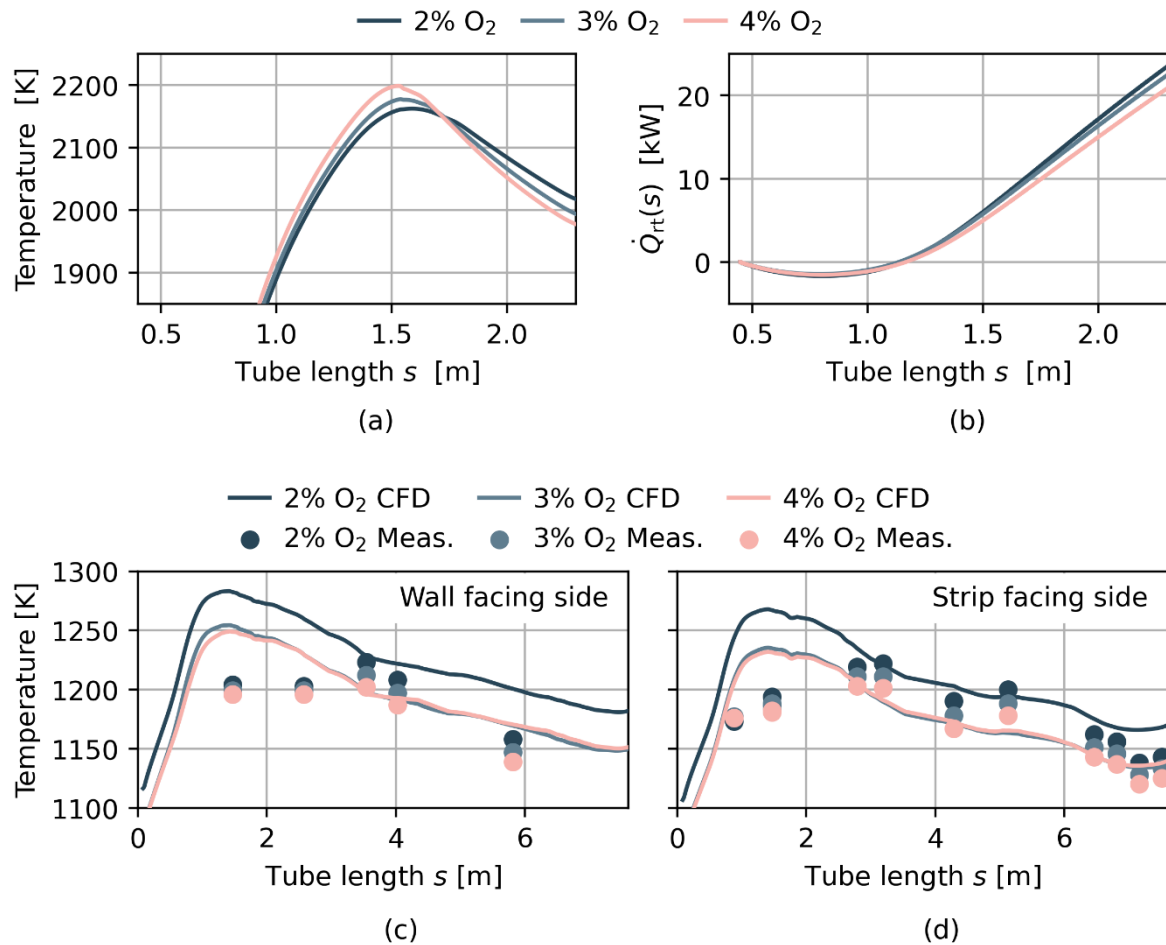


Figure 205. Gas temperature along the first legs centreline and accumulated total surface heat flux along the first leg.

The resulting radiant tube surface temperature distribution from simulation is given in Figure 206 for the wall facing side (a) and the strip facing side (b). Moreover, the locations of radiant tube surface thermocouples are indicated, with the measured temperatures given in plots (c) and (d), together with surface temperatures from simulation evaluated along the centrelines of both sides. In the CFD, the wall facing side experiences about **15 K** higher temperatures compared to the strip facing side, directly seeing the heat sink. The surface temperature evolution follows the trend of the gas temperature, where a hotspot is observed in the first leg and temperatures decay from thereon along the radiant tube's length. Considering the thermocouples from T9 on downstream, this trend is found in experimental measurement as well, while the hotspot in the first leg could not be identified. Between the runs at different air ratios, a clear trend is observed in the measured values, where each thermocouple from T9 on downstream shows about 10K lower temperatures for each increase in air fuel ratio. The 20K difference between 2% and 4% O₂ in the dry flue gas is recovered by the CFD simulation. However, the surface temperatures of cases 2 and 3 with 3% and 4% O₂ in the dry flue gas almost coincide in the CFD, while in the measurement they are clearly distinguished. For assessment of the thermal load on the equipment, maximum temperatures of the radiant tube and burner are evaluated from the CFD. The highest temperatures of the radiant tube occur in the second half of the first legs' lower surface, where the surface is closest to the second leg, and the surfaces of both legs radiate heat onto each other. For 2%, 3%, and 4% dry flue gas O₂, the maximum radiant tube temperatures are 1295K, 1266K, and 1263K. The hot spot of the burner is located at the tip of the central gas lance. In a similar manner as for the radiant tube, the maximum temperatures decrease over air fuel ratio increase with values of 1047K, 1040K, and 1038K.

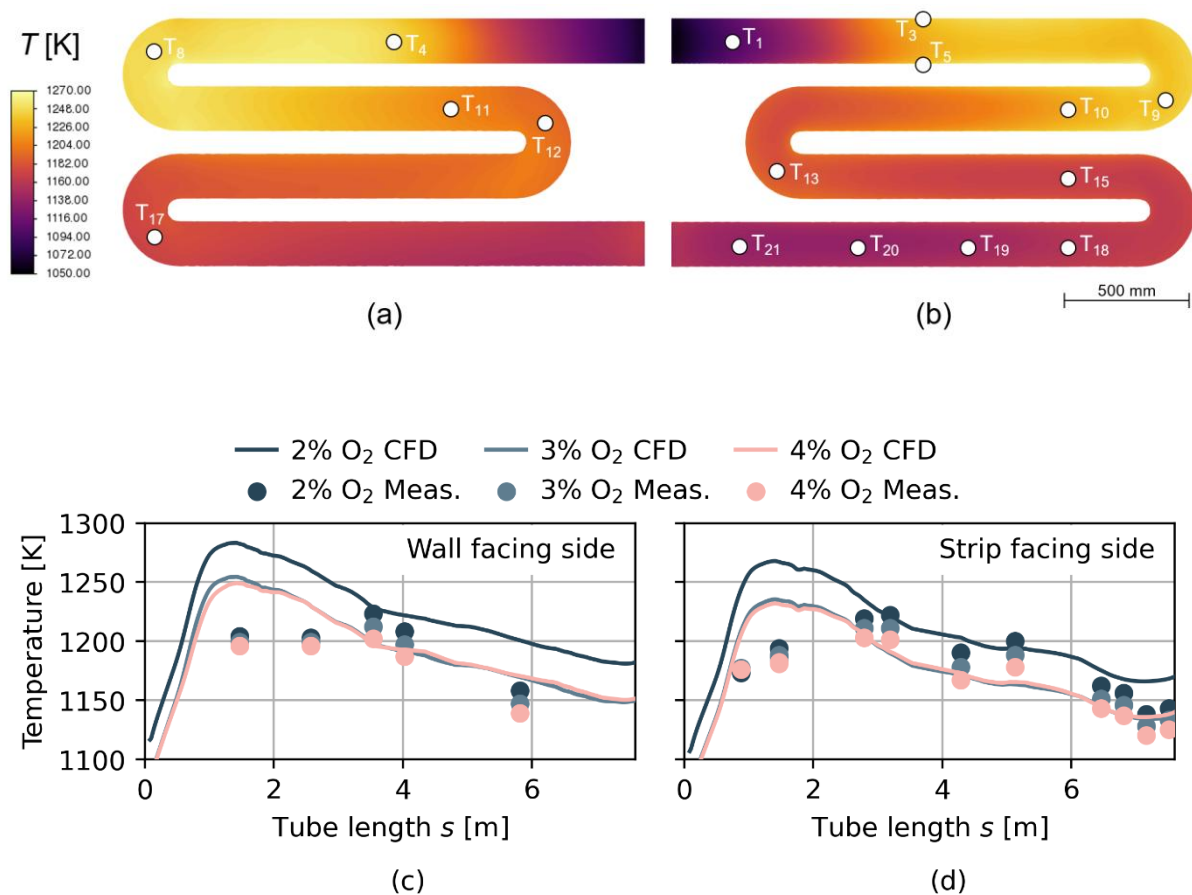
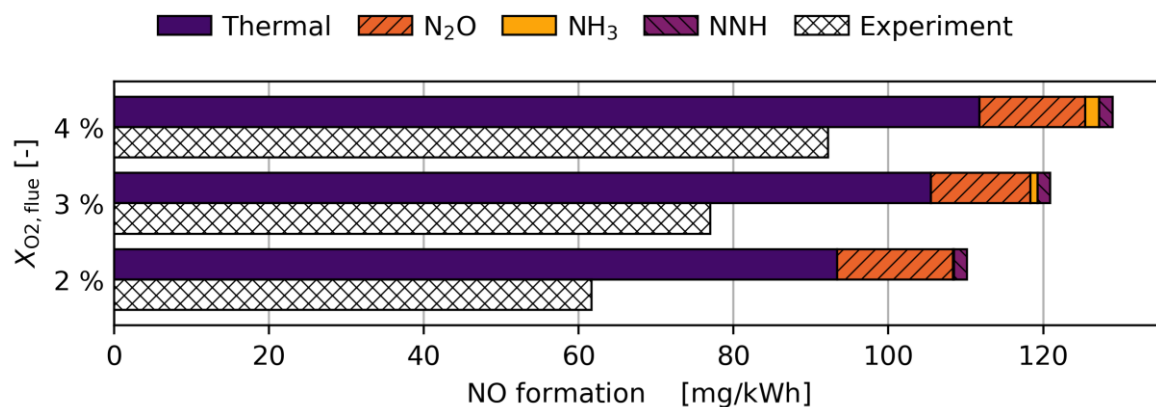


Figure 206. W-type radiant tube surface temperature distribution on the wall (a) and strip (b) facing sides for simulation case 2 with $X_{O_2,flue,dry} = 3\%$ and wall (c) and strip (d) facing side temperature evolution compared to measurement.

The chemical kinetic mechanism explicitly resolves the formation of NO_x . To gain insights into the formation, the contributions of different pathways are evaluated. As Ansys Fluent does not provide a functionality for such analysis, a post processing step is performed using the Python library Cantera. The thermodynamic state of each cell is exported in terms of temperature, pressure, and composition together with the cell volume, the reacting cell volume fraction and the integration time scale for the chemical reactor from the EDC model. The integration of each cells constant pressure reactor in time is then performed in Cantera, to compute each reactions average rate over the integrated time scale. The NO producing and consuming reactions are grouped to the thermal, N_2O , NH_3 , and NNH routes of NO formation and the contributions to the overall NO production are evaluated.



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Figure 207. Experimentally measured and numerically predicted NO emission with contributions of different formation pathways in the W-type radiant tube.

The experimentally measured and numerically predicted emission of NO is given in Figure 207. For each case, the NO formation is overpredicted by the CFD compared to the measured values. However, the trend of increasing NO formation over air fuel ratio is recovered. Therefore, the results may still be analysed to gain insights on the formation mechanisms of NO in these particular cases. In all cases, the thermal route is dominating the NO production by far, contributing over 80% to the overall formation. Between 12% and 14% are formed via the N_2O route, and between 1% and 2% via NNH. The formation via the NH_3 pathway is contributing between 1% and 2% in case 3 with 4% flue gas O_2 excess and less than 1% in cases 1 and 2. From this budget analysis it is concluded that the trend of increasing NO emission over air fuel ratio is driven by increased thermal NO formation. As thermal NO production is highly driven by temperature, these findings agree with the evaluation of gas temperature along the centreline. At increased air fuel ratio, the heat loss along the tubes first leg is decreased and the peak temperature rises, promoting the thermal formation of NO.

12.1.3 Results PP-type radiant tube

The results for the PP-type radiant tube are analysed in the same manner for radiant tube and burner temperatures, flue gas recirculation, and the formation of NO. The burner is operating in flameless oxidation (FLOX) mode, where the reactants are meant to be diluted by flue gas and the reaction zone is distributed spatially to reduce peak temperatures and mitigate the formation of thermal NO. Distributions of temperature and OH volume fraction are given in Figure 208 (a) and (b), respectively for simulation case 5 with $X_{\text{O}_2, \text{flue, dry}} = 3\%$ residual O_2 content in the dry flue gas. The reaction zone is marked by increased abundance of OH radicals. The higher concentration of OH right at the burner tip indicates that the reaction zone is attached to the burner, where fuel is injected via a central axial lance. Judging from the temperature distribution, the flame is located primarily in the first half of the central leg. From the fields of temperature and OH it is concluded that the system is not operating in flameless oxidation mode, as intended, but rather develops a round jet diffusion flame that is diluted downstream, but not before ignition. The maximum gas temperatures are reduced at higher air fuel ratio with values of 2548K, 2528K, and 2508K for 2%, 3%, and 4% O_2 by volume in the dry flue gas.

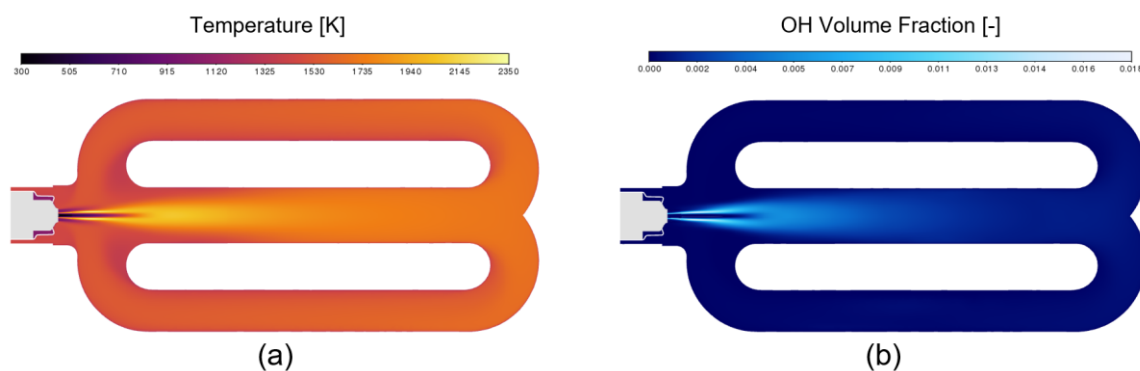


Figure 208: PP-type radiant tube temperature (a) and OH volume fraction (b) distributions in the central plane.

The resulting radiant tube surface temperature distribution from simulation is given in Figure 209 for the wall facing side (a) and the strip facing side (b). Moreover, the locations of radiant tube surface thermocouples are indicated, with the measured temperatures given in plots (c) and (d), together with surface temperatures from simulation evaluated along the projected centrelines of the central, upper, and lower tubes on both sides. In contrast to the W-type radiant tube, all thermocouples are mounted onto the strip facing side of the PP-type radiant tube. In the CFD, the wall facing side experiences 10K to 20K higher temperatures compared to the strip facing side, directly seeing the heat sink. The surface temperature evolution follows the trend of the gas temperature, where a hotspot is observed in the central leg and temperatures decay following the paths along the upper and lower legs. The hotspot in the central leg is located further downstream compared to the peak gas temperature, as the recirculated flue gas shields the hot core gas of the flame to some extent in the first half of the central leg. The surface temperatures from simulation agree with the measured values within a deviation of up to 40K. Between the runs at different air ratios, a clear trend is observed in the measured

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values, where between 2% and 3% O₂ excess each thermocouple shows about 10K higher temperatures and between 3% and 4% O₂ excess, the temperature difference is up to 5K at each thermocouple. This behaviour is recovered in the CFD, where temperatures decrease from 2% to 4% dry flue gas O₂ and the temperature profiles of cases 4 and 5 are further apart than from cases 5 and 6. However, the difference in surface temperature between the cases is overstated by CFD results when compared to the measurement.

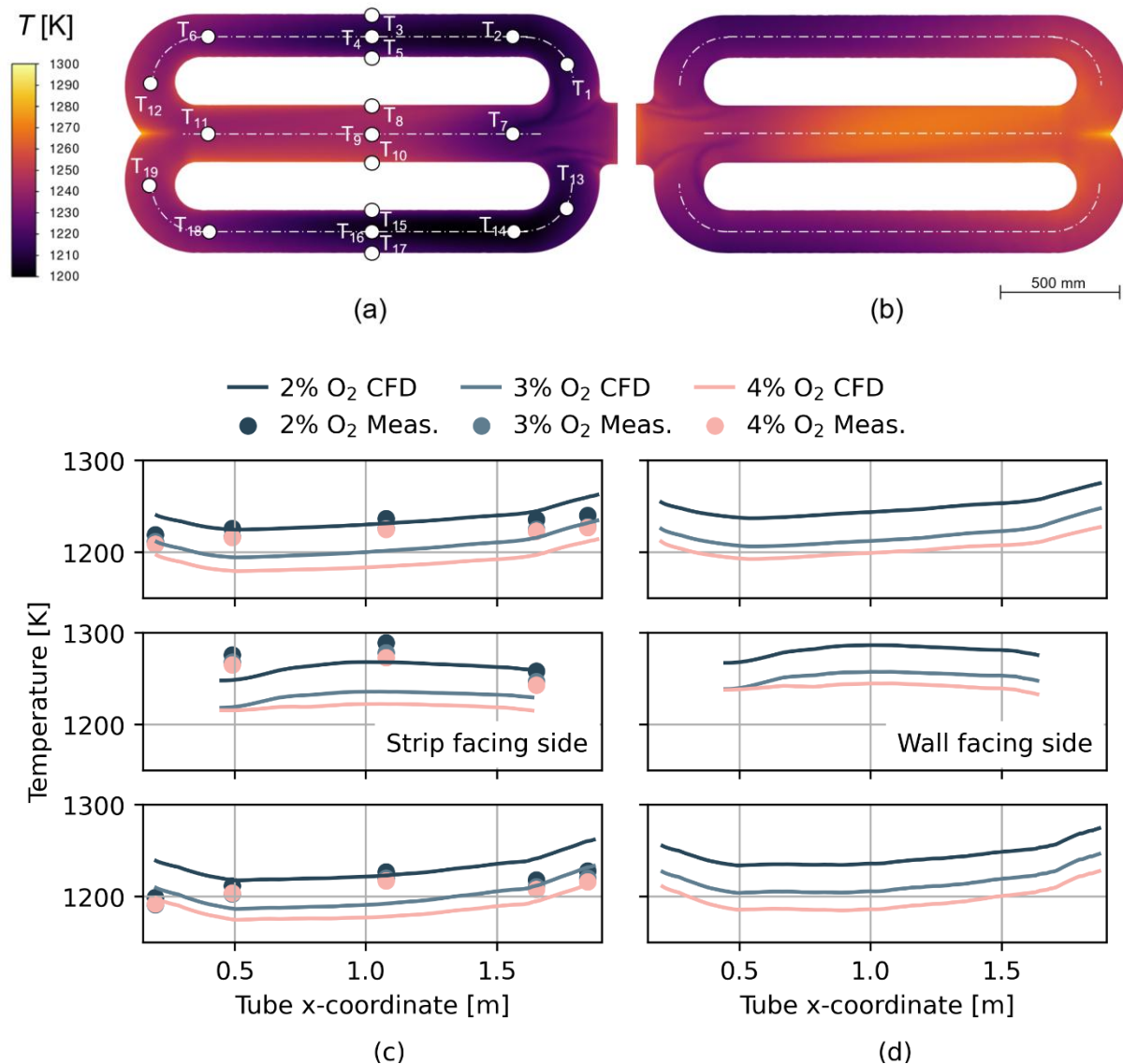


Figure 209. PP-type radiant tube surface temperature distribution on the wall (a) and strip (b) facing sides for simulation case 2 with $X_{O_2, flue, dry} = 3\%$ and wall (c) and strip (d) facing side temperature evolution compared to measurement.

As the cooling heat flow is kept constant within a range of 100W, the furnace temperature adjusts to the different air fuel ratios. As a higher amount of non-participating gas is introduced at higher air fuel ratios, radiant tube surface temperatures decrease, and fewer heat is transferred to the furnace. Consequently, the furnace temperature drops over increased air fuel ratio. Volume averaged temperatures of the furnace atmosphere are 1166K, 1132K, and 1097K, while experimentally measured values are 1133K, 1129K, and 1124K for cases 1, 2, and 3. Again, the CFD results show the same trend as found in the trials, but overpredict the impact of air-fuel ratio on the furnace temperature. For assessment of the thermal load on the burner, the combustion chamber is identified as the most critical part where a hotspot develops at the tip over the entire circumference. For cases 4, 5, and 6 the maximum temperatures are 1220K, 1188K, and 1120K. The results suggest that the influence of air fuel ratio on the equipment temperature is more pronounced in the

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immediate vicinity of the reaction zone compared to the differences observed in the radiant tube surface temperature.

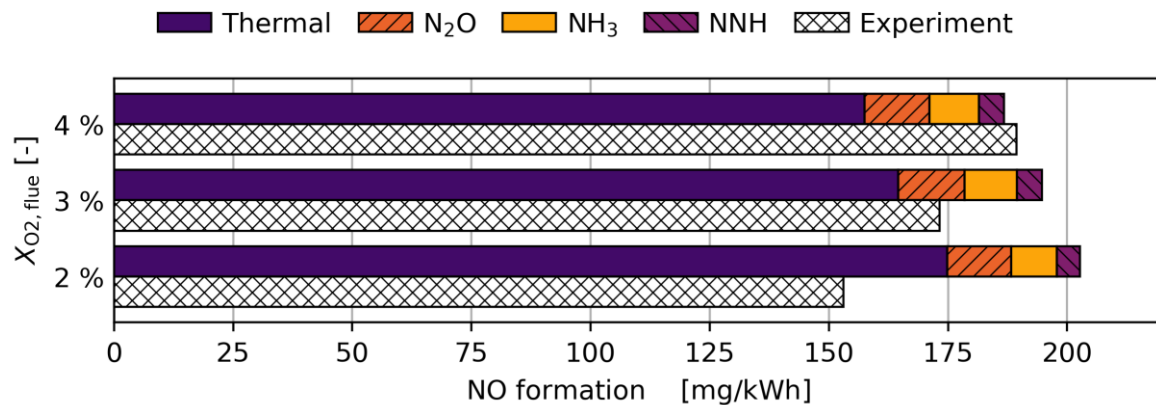


Figure 210 Experimentally measured and numerically predicted NO emission with contributions of different formation pathways in the PP-type radiant tube.

For analysis of NO emission, the formation pathway budget analysis is performed as presented for the W-type radiant tube. Again, the experimentally measured and numerically predicted emission of NO is given in Figure 210. Thermal formation is the dominant route, contributing more than 84% to the overall formation for all cases. Formation via the N₂O route contributes between 6% and 8%, and the pathway via NH₃ accounts for between 4% and 6% of the total production. Between 2% and 3% of NO are formed via the NNH route. It is found that the share of the different routes is not substantially affected by the air-fuel ratio, nor is the overall production of NO. While the prediction of NO emission is in the order of magnitude of the measured values, the trend of increasing emission over air fuel ratio is not recovered in the CFD, where the trend is vice versa. As the highly temperature dependent thermal pathway is dominating the NO formation, the trend may be explained by the altered gas temperature distribution. In contrast to the W-type radiant tube, maximum gas temperatures decay with increased air-fuel ratio for the PP-type radiant tube, and so do the NO emissions. The inversed trend of NO prediction may then be rooted in an incorrect prediction of the trend of maximum gas temperature. In the PP-type radiant tube, flue gas recirculation is a measure to mitigate NO emission by dilution of the reaction zone. Between the different cases, the recirculation defined as the mass flow of recirculating flue gas over the sum of fuel and oxidizer inlet mass flows $K = \dot{m}_{recirc} / (\dot{m}_{fuel} + \dot{m}_{oxi})$ varies from 2.18 to 2.13 for 2% to 4% O₂ excess.

12.1.4 Conclusion

Complementing the radiant tube pilot scale trials with hydrogen at ArcelorMittal's test rig, CFD simulations were performed using the RANS approach with modelling of turbulence, chemistry, and radiation. Simulation results showed close agreement for radiant tube surface temperature distribution and trends of decreasing temperatures over air-fuel ratio. The temperature field within the radiant tube was analysed with conclusions drawn to the thermal load on the equipment, namely the hotspots developing at the burner tips, and NO emission. The formation of NO is dominated by the thermal pathway contributing over 80% to the overall production in all the considered cases. As thermal NO formation is strongly driven by temperature, the gas temperature was analysed and linked to the NO emissions. The trends of NO emission dependency on air-fuel ratio were recovered for the W-type, but not for the PP-type radiant tube cases. Still, the insights gained on the share of formation pathways may be valuable for future modifications of the system regarding NO mitigation measures.

12.2 Numerical study of direct heating systems

12.2.1 Introduction

The experimental trials in the reheating furnace in the pilot plant at ArcelorMittal R&D are complemented by CFD numerical simulations. The baseline trial using coke oven gas as fuel is used as a reference for the simulations. Various combustion models and chemical kinetic mechanisms are compared in terms of

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computational cost and simulation performance to predict the temperature distribution and NO_x emissions. Simulations with fuel mixtures ranging from pure natural gas to pure hydrogen are then computed to evaluate whether the burner, originally designed and currently operated with coke oven gas, is capable of operating with hydrogen and hydrogen-enriched fuels.

12.2.2 Furnace description

The pilot-scale reheating furnace for direct heating testing has dimensions of 4.6 m in length, 2.79 m in height, and 1.52 m in width, as shown in Figure 211 (a). The furnace is equipped with a door for material ingress for transient heating tests and with space for the insertion of water-cooled tubes on the wall opposite the door for continuous steady-state heating tests. The off-gas duct is located in the lower part of the furnace. The furnace is insulated with a combination of various materials, including ceramic fibre, refractory bricks, and concrete, with a thickness of 0.3 m for walls and roof and 0.42 m for the floor.

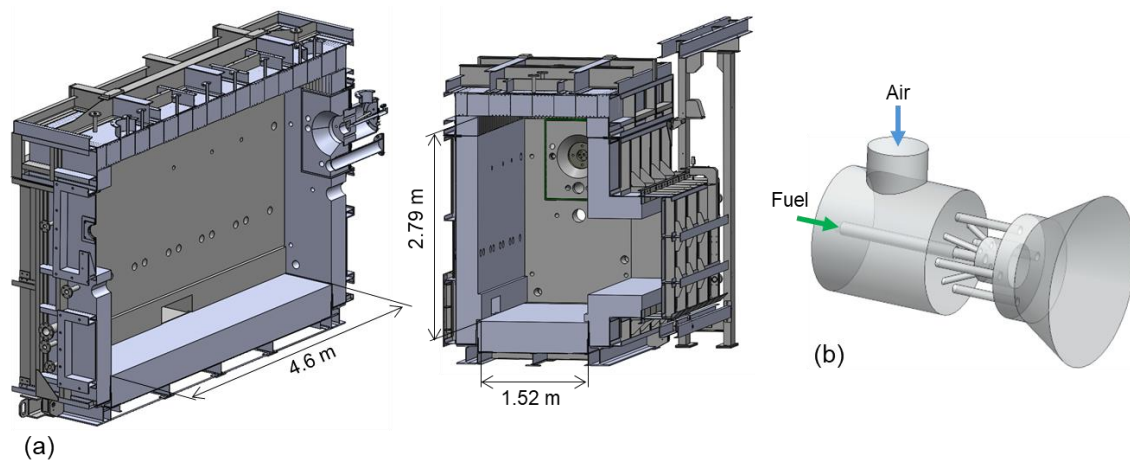


Figure 211. Schematic of (a) the pilot-scale reheating furnace and (b) the jet-flat flame burner inner volume.

A jet-flat flame burner, currently used in some reheating furnaces at ArcelorMittal plants, is installed in the pilot-scale reheating furnace. As shown in Figure 211 (b), the burner is designed with a central axial fuel injection. The air flows into a plenum, from which a portion of the air is injected close to the fuel injection to improve mixing and flame stability, while the remaining air is injected axially further downstream to increase flame volume and heat transfer homogeneity. The burner is designed to operate with coke oven gas produced during the steelmaking process. Coke oven gas is a mixture of various species with a relatively high hydrogen concentration of approximately 60% by volume. The goal of the simulations is to evaluate the capability of this burner to operate with hydrogen and hydrogen-enriched natural gas without any modifications, thereby supporting the retrofit and decarbonization of reheating furnaces at ArcelorMittal plants.

Computational domain

The computational domain for the simulations of the direct heating furnace is presented in Figure 212(a). The inner volume of the pilot-scale reheating furnace and the jet-flat flame burner are included in the simulation, while the solid structure and insulation of the furnace are excluded and represented through boundary conditions. As in the reference experimental trial, the position of the cooling tubes inside the furnace corresponds to the minimum insertion depth. The solid structure of the cooling tubes and the water flowing through them are also not included in the simulations, and only the outer surfaces of the tubes are retained. As for the simulations of the indirect heating furnace, the computational domain is discretized using an unstructured hybrid mesh with hexahedral cells in the core region and a buffer layer of polyhedral cells between the core mesh and the prism-layer cells near the walls, as shown in Figure 212 (b). Eight prism-layer elements are used at the wall surfaces, with a geometric progression growth rate of 1.2, ensuring a y^+ value below 3. Various levels of mesh refinement are applied around the burner and the cooling tubes, resulting in a total mesh size of 1.06 million elements.

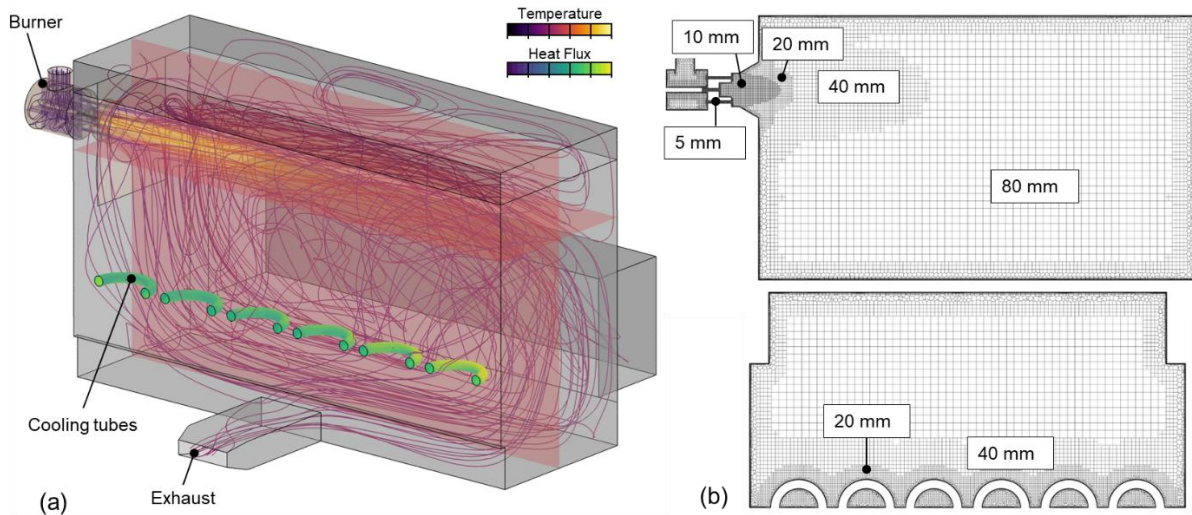


Figure 212. (a) Computational domain of the direct heating furnace and (b) detail of the mesh.

Numerical models

The pressure-based double-precision solver of the software ANSYS – Fluent release 2024 R2 was used to perform the RANS simulations of the direct heating furnace. The simulations are performed using a couple pseudo-time stepping scheme for the pressure-velocity coupling. The Second-Order Upwind special discretization scheme is used for all the transport equations, while the PRESTO! (PREssure STaggering Option) scheme for the pressure interpolation. The turbulence is modelled using the $k-\omega$ SST (Shear Stress Transport) turbulence model, which combines the $k-\omega$ model in the near-wall region with the $k-\epsilon$ model in the freestream. The turbulent diffusion of heat and species is modelled based on the gradient diffusion hypothesis with turbulent Prandtl and Schmidt numbers equal to 0.85 and 0.7, respectively. The radiation heat transfer equation is solved using the DOM with an angular discretization of 4×4 and 3×3 pixelation and the gas absorption coefficient using the WSGG model developed in Task 1.4.

Unlike the case of pure hydrogen as fuel, the chemical kinetics of fuel mixtures such as natural gas or coke oven gas involve a large number of species and elementary reactions, especially when NO_x chemistry is included to predict NO emissions. The high number of species increases the computational cost of CFD simulations, particularly for full-scale industrial and semi-industrial furnaces. On the other hand, tabulated chemistry models, such as the Flamelet Generated Manifold (FGM) model, are known to fail in accurately predicting slow-chemistry species such as NO due to the large difference in chemical time scales compared to the rapidly evolving control variables. Therefore, in the simulations of the direct heating furnace, NO emissions are computed using a post-processing approach based on the decoupled trace-species assumption (one-way coupling), using the converged solution of the species and temperature flow fields inside the furnace as input. The accuracy of this approach then relies on the accuracy of the predicted species and temperature fields in the steady state base simulation.

Four combinations of chemical kinetic schemes and turbulence–chemistry interaction models with different computational loads are compared. The standard implementation of the EDC model is used in combination with three chemical kinetic schemes: a global mechanism (GM) with 7 species and 4 semi-global reaction steps [Kim et al. (2023) IJHE], a reduced version of the San Diego mechanism (RM) with 26 species and 119 elementary reactions [Castellani et al. (2023) IJHE], and the semi-detailed mechanism SkeleCHy (DM) with 54 species and 554 elementary reactions [Schumann et al. (2025) CNF]. In all three cases, the chemical integration is performed using the stiff-chemistry solver with in-situ adaptive tabulation for chemistry acceleration, employing an error tolerance of 1×10^{-4} . Finally, the FGM model is also considered due to its low computational cost. The SDR26S mechanism is used to compute non-adiabatic 1D freely propagating premixed flame at various mixture fractions and enthalpy loss/gain levels. The laminar flamelets are tabulated as function of the progress variable, mixture fractional and normalized enthalpy. The turbulent–chemistry

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interaction is considered by integrating the laminar manifold with a β -shape probability density function (PDF), considering statistical independence between the mixture fraction and the progress variable.

For all cases, the simulations are computed until a steady state is reached. NO_x emissions are then calculated using a post-processing approach considering four NO_x formation pathways: the thermal mechanism [Zeldovich (1946) JAP], prompt mechanism [Fenimore (1971) POCI], N₂O-intermediate mechanism [Malte et al. (1974) CST], and the NNH-route mechanism [Klippenstein et al. (2011) CNF]. The latter was implemented through a UDF. The O and OH concentrations are taken from the flow-field solution, except in the case of the GM, where a partial-equilibrium model is required and NNH-route is not considered. During the post-processing step, all transport equations are frozen while the transport equations for NO and N₂O are solved. Turbulence–NO_x interaction is accounted for using a β -shaped PDF for the temperature and a transport equation for the temperature variance is solved. A total of 50 points were used for the PDF.

Boundary conditions

The numerical evaluation of the direct heating furnace was conducted using the experimental trial with coke oven gas (COG) as fuel as the reference case. The average composition of the COG is reported in Table 51 alongside the average composition of the natural gas (NG) supplied to the plant. In the experiments, the thermal power based on the lower heating value was measured at 775.4 kW, and the oxygen concentration in the wet off-gas was measured at 3.5%, corresponding to an air–fuel ratio λ of 1.23. The air is preheated in the off-gas recuperator to a temperature of 681 K, while the fuel inlet temperature is 323.7 K.

Mass-flow inlet boundary conditions are defined for the fuel and air inlets, while a zero-gauge-pressure outlet boundary condition is used for the flue gas exhaust. The fuel composition used in the boundary conditions differs slightly between the simulations with the different chemical kinetic mechanisms and the values reported in Table 51, as each reaction mechanism considers only up to a certain hydrocarbon. Therefore, in the simulations, the concentrations of hydrocarbons not included in a given mechanism were added to the heaviest hydrocarbon species considered by that mechanism. For the same fuel and air mass flow rates, this results in slight variations in the thermal power and global air–fuel ratio in the simulations, typically below 2%.

Table 51. Composition of the coke oven gas (COG) the natural gas (NG) in volume basis.

Fuel	H ₂	CH ₄	C ₂ H ₄	C ₂ H ₆	C ₃ H ₆	C ₃ H ₈	C ₆ H ₆	C ₄ H ₁₀	CO	CO ₂	N ₂	O ₂	H ₂ O
COG	0.606	0.213	0.019	0.006	0.001	0.001	0.005	0.0001	0.057	0.016	0.066	0.001	0.008
NG		0.920		0.040		0.020				0.010	0.010		

The thermal power, wet off-gas O₂ concentration, and air preheating temperature from the reference case are kept unchanged for the simulations with natural gas–hydrogen mixtures. In addition to the reference COG case, four cases are considered, including pure natural gas, pure hydrogen, and two natural gas–hydrogen mixtures containing 50% and 75% hydrogen by volume. A summary of the operating conditions for the simulations is reported in Table 52.

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Table 52. Operating conditions for the numerical simulations of the direct heating furnace.

Case	Fuel mixture	P_{th} [kW]	%O ₂ wet	λ [–]	Fuel		Air	
					$\dot{m}$ [g/s]	T [K]	$\dot{m}$ [g/s]	T [K]
0	COG	775.4	3.5	1.23	19.84	323.7	293.77	681.1
1	NG			1.221	16.26		323.83	
2	0.5 NG – 0.5 H ₂			1.225	14.00		312.23	
3	0.25 NG – 0.75 H ₂			1.230	11.61		300.04	
4	H ₂			1.242	6.47		273.73	

The heat balance from the experimental trials with COG is used to define the thermal boundary conditions for the simulations. In the experiments, the off-gas temperature is estimated to be 20 K lower than the average flue-gas temperature measured by eight thermocouples distributed along the furnace roof and walls and inserted 200 mm into the furnace. This temperature is 1451 K, leading to an off-gas heat loss of approximately 470 kW. Water flows through the cooling tubes at a mass flow rate of 8.44 kg/s, with inlet and outlet temperatures of 318.3 K and 326.15 K, respectively, corresponding to a heat sink of approximately 275 kW. The remaining energy is attributed to wall heat losses of 151 kW.

The heat losses through the furnace walls to the surroundings at a temperature of 290 K are modelled by considering one-dimensional heat transfer through the insulation, with an effective thermal conductivity of 0.6 W/(m·K) and an external heat transfer coefficient of 10.3 W/(m²·K), which is internally adjusted to match the wall heat losses measured during the experimental trial. As for the water-cooled strip in the indirect heating furnace, a convective boundary condition is imposed at the surface of the water-cooled tubes, with a reference temperature of 322.2 K (average water temperature) and an effective convective heat transfer coefficient of 350 W/(m²·K), internally adjusted to match the heat sink measured during the experimental trial. Finally, the emissivity of the surfaces is set to 0.7 for the furnace walls and 1.0 for the cooling tubes.

12.2.3 Results

The simulations of the reference case with COG as fuel are first evaluated using the various combustion modelling approaches. The computational cost of the simulations differs significantly depending on the combination of chemical kinetic scheme and turbulence–chemistry interaction model. The FGM model with the reduced mechanism (FGM–RM) has an average computation time of 2.64 s per iteration (running on 45 cores), whereas the EDC model has average computation times of 6.33, 20.5, and 92.8 s per iteration when used with the global mechanism (EDC–GM), reduced mechanism (EDC–RM), and semi-detailed mechanism (EDC–DM), respectively. The temperature fields near the burner predicted by the various combustion modelling approaches are shown in Figure 213. All cases predict a flame attached to the burner, with the temperature increasing where the fuel and primary air mix. Similar flame lengths of approximately 0.48 m (based on the heat release distribution) are predicted when the RM and DM are used, whereas a slightly shorter flame of around 0.4 m is predicted by the GM. More importantly, the peak temperature predicted by the GM is almost 300 K higher than that predicted by the other cases. This is due to the higher adiabatic flame temperature predicted by the GM as a result of the absence of intermediate species. This is a known drawback of the GM and is further amplified by the preheated combustion air conditions. On the other hand, similar mean gas temperatures inside the furnace are predicted by all mechanisms when using the EDC model, whereas the mean gas temperature is approximately 38 K lower with the FGM model.

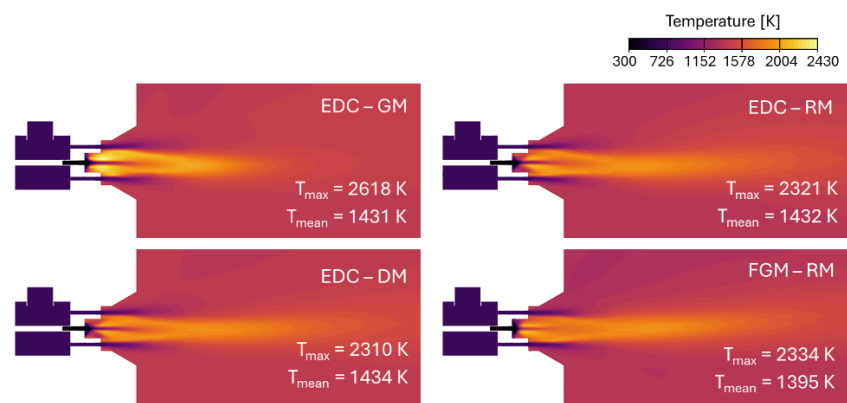


Figure 213. Temperature distribution at the burner vertical plane for COG with the various combustion modelling approaches.

The temperatures measured at the eight locations inside the furnace are reported in Figure 214, alongside the results from the numerical simulations. The experimental values are slightly higher than those predicted by the numerical simulations for all combustion modelling approaches. In general, the results obtained with the EDC model are approximately 40 K lower than the experimental measurements, while those obtained with the FGM model are around 80 K lower. This corresponds to an error of less than 3% for the case of the EDC model. The predictions of NO emissions computed using the post-processing approach are reported in Figure 215. The EDC model with the GM overpredicts NO emissions by a factor of 2.8. This is related to the overprediction of the flame temperature since, as shown for the indirect heating furnace, the thermal mechanism is the dominant route for NO formation. On the other hand, the FGM model with the RM underpredicts NO emissions by 50% due to the lower flue-gas temperatures predicted. The EDC model with the RM shows the closest agreement with the experimental value, although it still overpredicts NO emissions by 24%. Overall, the EDC-RM combination provides the best balance between accuracy and computational cost and is therefore selected for the simulations of the direct heating furnace with natural gas-hydrogen mixtures.

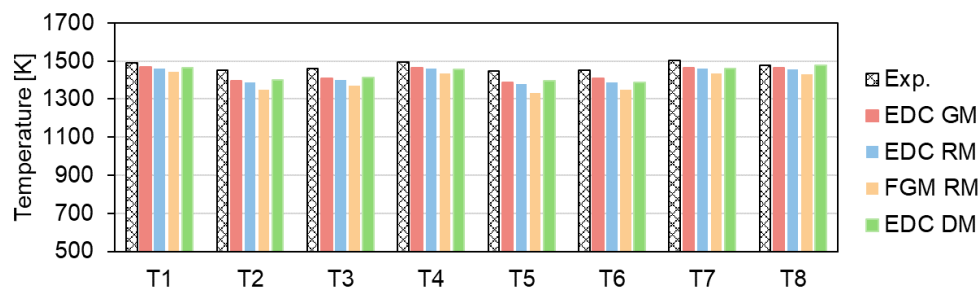


Figure 214. Flue-gas temperature measured by eight thermocouples distributed along the furnace roof and walls and inserted 200 mm into the furnace.

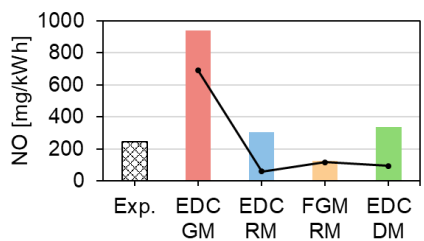


Figure 215. Experimentally measured and numerically predicted NO emission. The black line indicates the error relative to the experimental value.

The simulations of the pilot reheating furnace with the jet-flat flame burner fuelled by natural gas-hydrogen mixtures are conducted under the same operating conditions as the case with COG, as reported in Table 52.

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Moreover, the convective heat transfer coefficients used to model wall heat losses and cooling-tube heat sink are kept constant, analogous to maintaining the same water mass flow rate in the experiments. The temperature and OH mass-fraction distributions are shown in Figure 216. The simulation results indicate a change in the reaction zone when switching from coke oven gas to natural gas as fuel. For the latter, the fuel reactivity is insufficient to keep the flame attached to the burner; therefore, combustion occurs further downstream in a flameless-like regime, as the fuel and air become partially premixed and diluted by the surrounding flue gases. This behaviour is reflected by a mild temperature rise, with a peak value of approximately 1650 K, and a reaction zone distributed over a larger volume, as observed in the OH field. On the other hand, the flame remains attached to the burner for the mixtures with hydrogen. As the hydrogen content increases, the intensity of the reaction near the primary air injection increases, and the flame becomes more robust and compact. The peak temperature increases with hydrogen addition, reaching approximately 2070 K, 2350 K, and 2510 K for 50%, 75%, and 100% hydrogen, respectively. However, the maximum temperature of the burner solid components does not increase significantly.

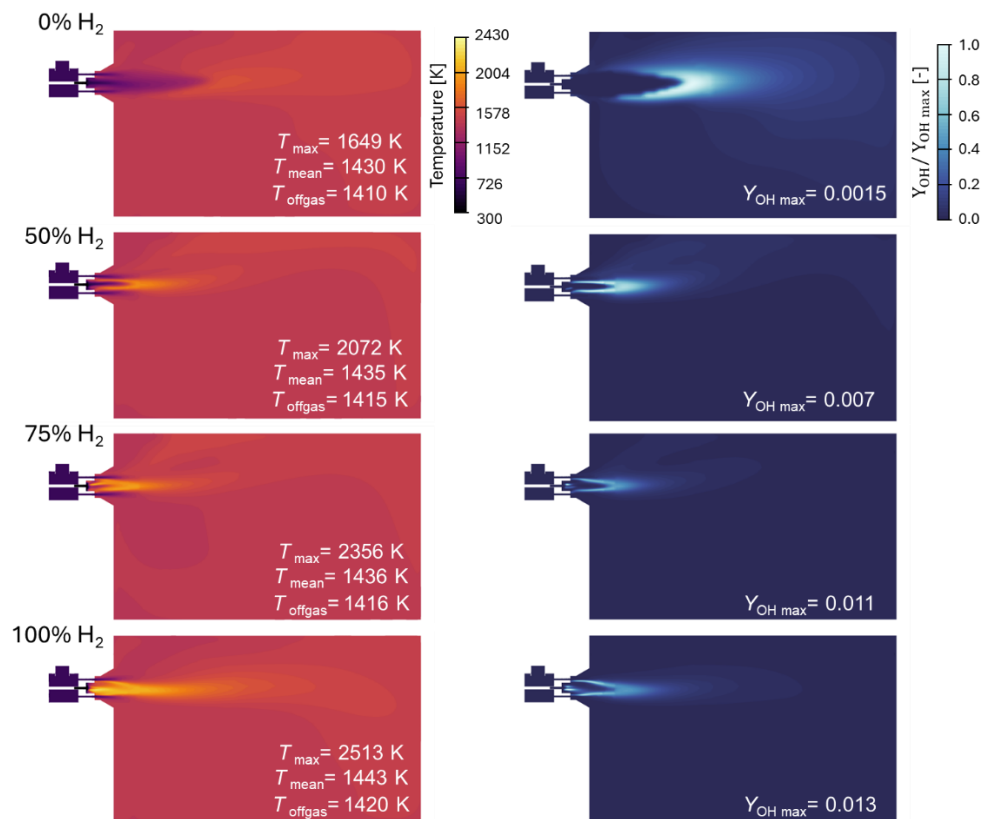


Figure 216. Temperature and OH mass fraction distributions in the central vertical plane.

The average temperature inside the furnace differs by only about 15 K between pure natural gas and pure hydrogen, while the difference in off-gas temperature is approximately 10 K. The heat balance in the direct heating furnace is only slightly affected by hydrogen addition. Figure 217(a) shows the heat flows in the furnace relative to the fuel thermal power (775.4 kW). While the wall heat losses remain nearly constant with increasing hydrogen content, the off-gas losses decrease despite the increase in off-gas temperature, due to the reduction in flue-gas mass flow rate at the same thermal power and off-gas oxygen concentration. The same trend is observed for the heat required to preheat the lower mass flow of combustion air to 681 K. The heat transferred to the cooling tubes corresponds to approximately 38% of the fuel thermal power and increases slightly with hydrogen addition due to enhanced radiative and convective heat transfer from the hotter flame and flue gases. The contributions of radiation and convection to the heat transfer to the cooling tubes are presented in Figure 217(b), where it can be seen that approximately 89% of the total heat is transferred by radiation from the walls and combustion gases.

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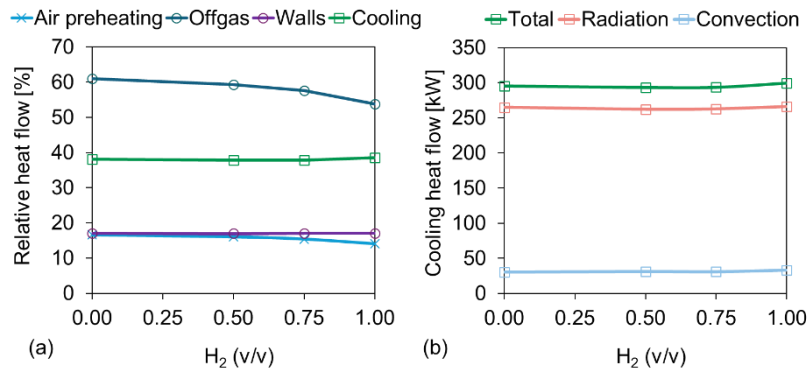


Figure 217. (a) heat flows in the furnace relative to the fuel thermal power and (b) heat transfer to the cooling tubes.

The evolution of the wet off-gas composition when transitioning from natural gas to hydrogen is shown in Figure 218(a), where the decrease in CO_2 and the increase in H_2O achieved through hydrogen enrichment can be observed. Approximately 76% hydrogen by volume is required to reduce CO_2 emissions by half, while the H_2O concentration increases from about 16% for pure natural gas to 28% for pure hydrogen. The CFD simulations predict CO and H_2 emissions in the wet offgas below 1 ppm in all cases. The NO emissions are reported in Figure 218 (b) both as a volume fraction in the dry off-gas and as mass per unit of energy released by the fuel mixture. The NO emissions predicted for the COG case are included as a reference. An increase in NO emissions is observed with hydrogen enrichment, reaching a value of approximately 614 mg/kWh for pure hydrogen. The NO emissions for COG lie between those of the 50% and 75% hydrogen cases, in agreement with the hydrogen concentration in the COG. The pure natural gas case exhibits the lowest emissions, at approximately 87 mg/kWh. These relatively low emissions are associated with the much lower peak temperatures obtained when natural gas is used as fuel in the jet-flat flame burner, due to flame detachment and flameless-like combustion. However, it remains unclear whether stable combustion can be achieved with pure natural gas as fuel when the furnace starts from cold-wall conditions.

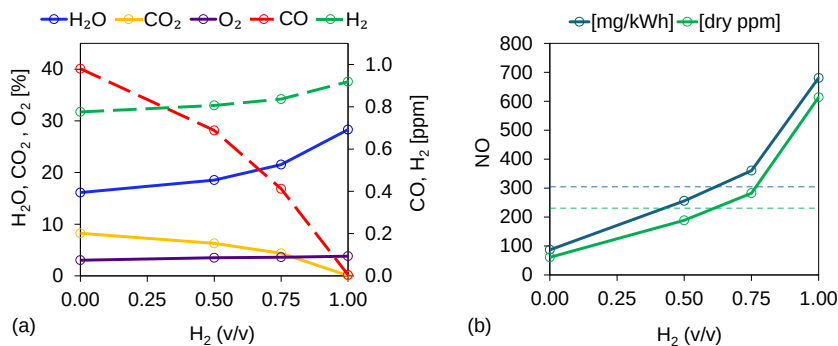


Figure 218. (a) Wet offgas composition and (b) NO emissions with dotted lines indicating the value for COG.

12.2.4 Conclusion

Numerical simulations of the reheating furnace in the pilot plant at ArcelorMittal R&D were conducted to complement the experimental trials. The trials using coke oven gas as fuel were taken as the reference and baseline for evaluating the performance of the reheating furnace equipped with the jet-flat flame burner. As in the case of the indirect heating furnace, a RANS approach including turbulence, chemistry, and radiation modeling was employed. However, due to the high computational cost associated with simulations using coke oven gas or mixtures with natural gas, a post-processing approach was adopted to predict NO emissions. The combination of the EDC model and a reduced chemical mechanism provided the best balance between accuracy and computational cost. The simulation results show that the jet-flat flame burner currently used in ArcelorMittal plants with COG as fuel can be operated with hydrogen and hydrogen-enriched natural gas without significant changes in furnace performance, thereby facilitating the decarbonization of reheating furnaces at ArcelorMittal without requiring burner replacement. However, NO emissions are predicted to increase with hydrogen enrichment, and alternative strategies, such as staging with oxygen lancing, can be

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implemented to mitigate NO emissions while retaining the existing burner design, as explored in the experimental trials.

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