1. Introduction

Molten iron and slag flows play a critical role in the blast furnace lower zone, transporting mass and energy, whilst impairing and redistributing gas flow. In turn, molten iron and slag undergo physical and chemical changes, and are redistributed radially during descent to the hearth. In Part 1 of this report, the flows of molten iron and slag in the blast furnace were characterised using a ‘force-balance’ approach. A consistent set of equations describing liquid holdup, gas-liquid interaction and solid-liquid interaction was developed with reference to previous experimental studies and furnace conditions. The model accounts for the effect of gas, liquid and packing properties on liquid flow, as well as the effect of liquid on gas flow. Importantly, the model can be applied under both countercurrent and non-countercurrent conditions, where gas can either hinder or enhance liquid flow.

Integration of these techniques into a comprehensive numerical model of the blast furnace, such as ‘SHAFT’, is required to understand both the effect of operating conditions on the liquid flow fields and the effect of liquids on these operating conditions. SHAFT is a proprietary, two-dimensional, steady-state numerical model that considers the gas, liquid and solid flows, heat and mass transfer, and primary chemical reactions occurring in an ironmaking blast furnace. SHAFT is utilised for both on and off-line analysis to predict the furnace’s internal state and the effect of operating changes on furnace performance. In particular, the aim has been to predict variations in the shape and position of the cohesive zone in response to changes in the burden distribution.

In this paper, the incorporation of a new two-liquid (molten iron and slag) flow submodel into SHAFT will be discussed. The model is targeted at the simulation of liquid flow through a coke bed (coke-slits, dropping zone and deadman), recognising that the form of flow through softening and melting ore has not been well characterised. Simulations will be performed to examine the effect of liquid on furnace behaviour as well as the model’s response to changes in operating conditions such as bed permeability, furnace productivity and liquid physical properties.

2. Model Formulation

The flow domain considered in SHAFT extends from the burden surface to the hearth liquid surface in the axial direction, and from the axis of symmetry to the wall in the radial direction. This domain is nominally divided into 80 cells axially and 16 cells radially. The gas and solid flows with which liquids interact are modelled using Ergun’s equation and potential flow, respectively. The effect of the layered packing of coke and ore on gas flow is handled by classifying the bed as a single equivalent anisotropic structure, based on resistances to flow in the individual materials. Axial and radial resistances, \( f_x \) and \( f_r \) respectively, assume that layers are aligned with the computational grid:

\[
\alpha f_{\text{ore}} + (1-\alpha)f_{\text{coke}} = f_x \tag{1}
\]

\[
f_i = \frac{f_{\text{ore}} f_{\text{coke}}}{(\alpha f_{\text{coke}} + (1-\alpha)f_{\text{ore}})^2} \tag{2}
\]

Use of potential flow for solids necessitates imposing the
'deadman' region through which coke cannot flow. In previous versions of SHAFT, liquids were not explicitly handled, but were instead assumed to constitute a part of the ore potential flow field. However, this ore was not restricted from entering the deadman.

The cohesive zone, within which liquids are generated, is defined by the 1 200 and 1 400°C isotherms for solid. Between these temperatures, ore-based materials are assumed to undergo softening and melting to form a molten iron phase and a slag phase. The ore layer resistance in this zone is modified according to the shrinkage ratio (\(S_r\)) approach of Sugiyama et al.:

\[
\frac{\Delta P_{\text{coke}}}{\Delta x} = \frac{\Delta P}{\Delta x} \frac{f_{\text{coke}}}{\alpha f_{\text{ore}} + (1 - \alpha) f_{\text{coke}}} \quad (7)
\]

Axial and radial components of the gas pressure gradient in the coke bed are split into horizontal and vertical components by geometry and summed.

\[
\frac{\Delta P_{\text{coke}}}{\Delta r} = \frac{\Delta P}{\Delta r} \frac{f_{\text{coke}}}{\alpha f_{\text{ore}} + (1 - \alpha) f_{\text{coke}}}
\]

Liquids will only flow once the static holdup capacity of the coke bed is exceeded. The superficial velocity of dynamic holdup is found by averaging liquid fluxes across the cell faces and subtracting the flux transported by static holdup from the total flux determined by continuity:

\[
\dot{U}_{\text{d,ld}} = \max(\dot{U}_l h_{\text{continuity}} - \dot{U}_{\text{coke}} h_i, 0) \quad (8)
\]

The dynamic holdup in the presence of gas flow, calculated using the superficial liquid velocity from Eq. (8), is added to the static holdup to give the calculated total holdup, \(h_c\). The total holdup can also be determined directly by mass continuity, since 

\[
h_{\text{continuity}} = \dot{U}_l / \dot{U}_g. \quad \text{If the calculated total holdup is higher than that determined by mass continuity, it implies that the dynamic liquid is flowing too fast, and that the contact area between solid and liquid should be increased to slow the liquid and achieve the expected holdup. Therefore, the difference in holdup can be used to progressively update the estimated area between iterations:}

\[
A_{l,d,\text{iteration}}^{n} = A_{l,d,\text{iteration}}^{n-1} \left[1 - \delta \frac{h_l}{h_{\text{continuity}}}\right] \quad (9)
\]

For the two-dimensional flow system, bed drag force components in axial and radial directions are determined from gravitational and gas drag forces on dynamic holdup:

\[
F_{l,d,1}^{ax} = -(F_{l,d,1}^{ax} + \rho g h_d) \quad (10)
\]

\[
F_{l,d,1}^{r} = -(F_{l,d,1}^{r} + \rho g h_d) \quad (11)
\]

Gravitational forces appear in both axial and radial directions because of the body-fitted coordinate system used in SHAFT. Gas drag force components are determined using the axial (Eq. (7)) and radial (Eq. (6)) gas pressure gradients in the coke bed. The magnitude of the bed force is calculated assuming the components are perpendicular:

\[
F_{l,d,1}^{ax} = \sqrt{(F_{l,d,1}^{ax})^2 + (F_{l,d,1}^{r})^2} \quad (12)
\]

Given the liquid–solid contact area calculated at each iteration by Eq. (9), it is possible to solve for the magnitude of liquid velocity using Eq. (12) and the bed drag force equa-

| Liquid | \( \rho \) (kgm\(^{-3}\)) | \( \mu \) (Pa·s) | \( \sigma \) (W/mK) | \( \theta \) (°) | \( \varphi \) (°) | \( d_l \) (m) | \( e \) |
|--------|----------------|-------------|----------------|-----------|--------|--------|------|
| metal  | 6600           | 0.005       | 1.1            | 90        | 0.8    | 0.035  | 0.4  |
| slag   | 2600           | 1.0         | 0.47           | 90        | 0.8    | 0.035  | 0.4  |
tion established in Part 1 of this report:\(^1\):

\[
\frac{U_{\text{old},d}}{h_d} = -x_d + \left(\frac{\mu}{\rho_l} \left(\frac{A_l}{h_d}\right)^2 + \frac{1.75}{h_d} \rho_l \left(\frac{A_l}{h_d}\right)\right)^{0.5}\]

where

\[
x_d = \frac{150}{36} \left(\frac{A_l}{h_d}\right)^2, \quad x_s = \frac{1.75}{6} \rho_l \left(\frac{A_l}{h_d}\right)
\]

Liquid velocity components for dynamic holdup are determined from the ratio of component to total bed drag forces, then superimposed on the coke velocity field:

\[
\frac{U_{\text{old},d}}{h_d} = -\frac{U_{\text{old},d}}{h_d} \left(\frac{F_{\text{ld},d}}{F_{\text{ld},t}} + U_{\text{cokex}}\right) \quad \text{(14)}
\]

\[
\frac{U_{\text{old},x}}{h_d} = -\frac{U_{\text{old},d}}{h_d} \left(\frac{F_{\text{ld},x}}{F_{\text{ld},t}} + U_{\text{cokex}}\right) \quad \text{(15)}
\]

Finally, average liquid velocity components, used together with liquid source terms to determine the superficial liquid velocity distribution satisfying mass continuity, are calculated by adding the contribution of static holdup transport by coke to Eqs. (14) and (15):

\[
\frac{U_{\text{ole},d}}{h_d} = \frac{U_{\text{ole},d} + U_{\text{cokex}}}{h_d} \quad \text{(16)}
\]

\[
\frac{U_{\text{ole},x}}{h_d} = \frac{U_{\text{ole},d} + U_{\text{cokex}}}{h_d} \quad \text{(17)}
\]

3. Method

An appropriate furnace dataset is chosen to compare results of the current version of SHAFT, with liquid flow, and the previous version, without liquid flow, as well as to study the influence of liquid and packing properties on model behaviour. BHP’s Port Kembla No. 5 Blast Furnace achieved a stable operation and production of approximately 7 700 tonnes/day during April 1996, as shown in Table 2.

| Hearth Diameter (m) | Inner Volume (m³) | Prod. Rate (t/day) | Slag Rate (kg/ton) | Fuel Rate (kg/ton) | HM Temp. (°C) | HM Si (%) |
|---------------------|------------------|-------------------|-------------------|-------------------|--------------|-----------|
| 12.04               | 3287             | 700               | 266               | 477               | 1498         | 0.31      |

Table 2. Characteristics of BHP Port Kembla 5BF.

Table 3. Plan for blast furnace liquid flow simulations.

| Run | Model | dₘ (mm) | ε (t) | θ (°) | gasflow (kg s⁻¹) |
|-----|-------|---------|-------|-------|------------------|
| A   | Previous | 35 | 0.4 | - | 113.8 |
| B   | Liquid | 35 | 0.4 | 90 | 113.8 |
| C   | Liquid | 25 | 0.4 | 90 | 113.8 |
| D   | Liquid | 35 | 0.3 | 90 | 113.8 |
| E   | Liquid | 35 | 0.4 | 60 | 113.8 |
| F   | Liquid | 35 | 0.4 | 90 | 125.2 |

4. Results and Discussion

Graphical summaries of the simulation results are given in Figs. 1, 2, 4, 6, 8 and 9 for Runs A to F, respectively. Figure 1 consists of three plots (Figs. 1(a) to 1(c)) generated using the previous version of SHAFT without liquid flow. The remaining figures consist of fifteen plots (e.g. Figs. 2(a) to 2(o)) with the addition of liquid flow results from the current version. Table 4 describes the contents of these plots. The criteria for flooding in Plot d is where an incremental increase in gas flow would result in an infinite liquid holdup. To achieve convergence, the gas drag must be artificially decreased in this region. The accumulation index in Plot e, defined as the rate of change of holdup with the dimensionless gas pressure drop \((dh/dX)\), provides an indication of the approach to flooding. Vectors overlaid on the first five plots are for comparative purposes only, so no scale is given. The shaded region in each plot corresponds to the calculated cohesive zone.

For the period of operation under investigation, SHAFT predicts a relatively flat, W-shaped cohesive zone, when liquid flow is not considered in Run A (Fig. 1). As shown in Fig. 1(a), coke descends through the cohesive zone and around the deadman. In Fig. 1(c), gas velocity vectors distribute rapidly after leaving the defined raceway region. Ascending gas essentially follows the furnace profile except in the cohesive...
zone where the anisotropic permeability distribution promotes radial flow.

Despite making changes to the definition of the cohesive zone, its shape and position are virtually unchanged when using the liquid flow model in Run B, as shown in Fig. 2. However, the presence of liquids in the lower furnace decreases the available pore space for gas flow, increasing the pressure gradient (Fig. 2(c)) compared to Run A (Fig. 1(c)). Liquid metal generated in the cohesive zone is not noticeably affected by gas flow except in the vicinity of the raceway, with some deflection of the vectors away from the gas source (Fig. 2(d)). The narrowing of the furnace profile from the belly to the hearth causes a strong flow of liquid at the wall. A similar flow is visible for slag (Fig. 2(e)). The deflection of slag by gas flow is more significant than for metal, with minimal liquid passing through the raceway region. This result must be considered in the context of the two-dimensional nature of the simulation, where liquid is unable to flow between raceways, i.e. a low gas flow region.

Total liquid holdup increases with liquid supply during descent through the cohesive zone (Fig. 2(a)). Slag holdup is visibly affected by gas flow, accumulating above the raceway in a region of strong countercurrent gas–liquid interaction before descending at its periphery, resulting in a ‘drier’ raceway (Fig. 2(k)). Holdup at the wall is enhanced by increased liquid flow due to the narrowing furnace cross-section. Directly beneath the raceway, the co-current downward flow of gas decreases the holdup of both metal (Fig. 2(f)) and slag.

Dynamic holdup represents approximately 10% of the total holdup for metal (Fig. 2(g)) and more than twice that amount for slag (Fig. 2(l)). Directly beneath the cohesive zone, the ratio of dynamic to total holdup is highest at furnace mid-radius, corresponding to the maximum in ore to coke ratio and, therefore, liquid generation. Variability in the distribution between static and dynamic holdup is
greater for slag due to the stronger effect of gas flow on the less dense liquid phase. The role of liquid physical properties is emphasised by the difference between the effect of gas flow on metal holdup, which increases by \(5\% - 10\%\) (Fig. 2(h)), and slag holdup which increases by \(20\% - 30\%\) (Fig. 2(m)) over much of the dropping zone.

The effective area of solid–liquid interaction for metal (Fig. 2(i)) is \(30\%\) higher than for slag (Fig. 2(n)). In Part 1 of this report, it was calculated to be similar for metal and slag in the absence of gas flow. Therefore, it appears that for slag, this area (on a liquid volume basis) decreases due to an increase in the average droplet/rivulet size with gas flow, consistent with the significant increase in holdup. For the effective area of gas–liquid interaction, the disparity is reversed, with the value for slag (Fig. 2(o)) \(30\%\) higher than for metal (Fig. 2(j)). It is plausible that the lower metal holdup can distribute more readily to regions of low gas velocity in the packing, thus minimising the effective area of interaction. For both liquids, the gas–liquid area was an order of magnitude greater than the solid–liquid area due to the non-wetting and dynamic flow conditions, as discussed in the previous report.¹

Table 4. Key for blast furnace liquid flow simulation results.

| Plot | Description |
|------|-------------|
| a    | total liquid holdup, \(h_s\) (%), \([U_{m_a}, \text{m}^3/\text{m}^2/\text{s}]\) |
| b    | solid temperature (°C), \([U_{m_a}, \text{m}^3/\text{m}^2/\text{s}]\) |
| c    | gas pressure (kPag), \([U_{m_a}, \text{m}^3/\text{m}^2/\text{s}]\) |
| d    | flooding diagram (see discussion), \([U_{m_a}, \text{m}^3/\text{m}^2/\text{s}]\) |
| e    | accumulation index, \(dh/dX\), \([U_{m_a}, \text{m}^3/\text{m}^2/\text{s}]\) |
| f    | liquid metal holdup, \(h_m\) (%) |
| g    | ratio of dynamic to total metal holdup, \(h_{dy}/h_{tot}\) (%) |
| h    | change in metal holdup due to gas, \(h_{dy}/h_{tot}\)(%) |
| i    | effective solid–liquid interaction area, \(A_{s-l}/A_{tot}\) (1/m) |
| j    | effective gas–liquid interaction area, \(A_{g-l}/A_{tot}\) (1/m) |
| k    | liquid slag holdup, \(h_s\) (%) |
| l    | ratio of dynamic to total slag holdup, \(h_{dy}/h_{tot}\) (%) |
| m    | change in slag holdup due to gas, \(h_{dy}/h_{tot}\)(%) |
| n    | effective solid–liquid interaction area, \(A_{s-l}/A_{tot}\) (1/m) |
| o    | effective gas–liquid interaction area, \(A_{g-l}/A_{tot}\) (1/m) |

Fig. 4. Effect of decreased coke particle size in the lower zone (Run C).

Fig. 5. Slag flux distribution entering the hearth for Runs B to F.
The shaded region above the raceway in Fig. 2(d) represents the region in which flooding is predicted to occur. The flooding region occurs where the flow of gas is high and countercurrent to the high volume of liquids descending adjacent to the wall. The calculation of a flooding condition for what is a relatively stable period of furnace operation is explained by the two-dimensional nature of the model, as discussed previously. The flooding diagram itself provides no indication of impending accumulation problems. However, the accumulation index (Fig. 2(e)) is indicative of the propensity to flood, increasing rapidly towards the flooded region, and decreasing in the ‘drier’ region below the raceway.

In Run A, the ‘liquid’ distribution entering the hearth is determined from the ore flow field, as shown in Fig. 3. Compared to the distribution of ore entering the furnace, the outflow is relatively uniform across the radius. Using the liquid model, a more variable distribution is obtained, with lower irrigation in the vicinity of the raceway, and higher irrigation at mid-radial and wall. Flow in the near-wall region is particularly different between Runs A and B, with the reduced furnace cross-section between belly and hearth affecting ore flow across the radius via the ore potential equation in the former, whilst only affecting liquid flow locally in the latter. Separating metal and slag illustrated the more significant effect of gas drag on the latter, with a ‘dry’ region formed below the raceway and the mid-radial peak in flow pushed towards furnace centre.

The gas pressure gradient in the lower furnace increases significantly with a decrease in the coke particle size (Run C, Fig. 4(c)). This is associated with an increase in the surface area to volume ratio for the packing and the increase in holdup for metal (Fig. 4(f)) and, particularly, slag (Fig. 4(k)), when compared to Run B. In turn, the effect of gas flow on metal and slag holdup is increased, as shown in Fig. 4(h) and 4(m), respectively, while the ratio of dynamic to total holdup is slightly decreased, as shown in Fig. 4(g) and 4(l), respectively. Calculated areas of gas-liquid and solid-liquid interaction are all higher than in Run B, which is consistent with a decrease in liquid droplet size, possibly caused by the more tortuous flow path through a smaller coke packing.

The flooding region adjacent to the wall and above the raceway is enlarged by the decreased coke size in Run C, as shown in Fig. 4(d). Consistently, the accumulation index (Fig. 4(e)) increases rapidly in this region, and is generally higher throughout the lower zone due to stronger gas–liquid interaction. While the change in the metal flow field with gas drag in the vicinity of the raceway is not obvious in Fig. 4(d), the dry region formed below the raceway is visibly

Fig. 6. Effect of decreased coke voidage in the deadman (Run D).
larger for slag (Fig. 4(e)). Consequently, the distribution of slag entering the hearth is skewed towards furnace centre, as shown in Fig. 5.

In Run D, poorer gas penetration into the furnace with decreased deadman porosity results in a slight drop in the height of the cohesive zone at furnace centre, as shown in Fig. 6. The decrease in porosity is also responsible for a localised increase in the total liquid holdup (Fig. 6(a)). Away from the deadman, results appear similar to Run B. However, the greater volume of gas exiting countercurrent to liquid above the raceway causes a larger increase in holdup over the zero gas flow condition (Figs. 6(h) and 6(m)) and a stronger deflection of liquid towards furnace centre, as shown for slag in Fig. 5. While the flooding region appears similar to Run B in shape and location (Fig. 6(d)), the accumulation index is a better indicator of deteriorated flow conditions above the raceway and in the deadman (Fig. 6(e)).

The effect of deadman permeability on liquid flow using the force-balance model is very different to that predicted using the potential flow model.$^4$ In potential flow, liquid at any point is influenced by the permeability distribution throughout the lower zone. Consequently, a decrease in deadman permeability, such as by decreasing the particle size, causes liquid to flow away from this region, as shown in Fig. 7.$^4$ By contrast, the resistance experienced by discrete liquid droplets/rivulets in the force-balance model has no means of propagating across the lower zone except indirectly via the gas flow field. Therefore, decreasing deadman permeability causes a local reduction in liquid velocity and, therefore, increase in holdup, but does not force liquid away from this region. In fact, as shown by Fig. 5, increased resistance may cause greater gas drag and penetration of liquid into the deadman from near the raceways.

Fig. 7. Effect of deadman permeability on liquid in potential flow.$^4$

Fig. 8. Effect of decreased liquid contact angle on coke (Run E).
Decreasing the liquid contact angle on coke in Run E increases the interaction between solid and liquid, resulting in increased liquid holdup (Figs. 8(a), 8(f) and 8(k)) and effective solid–liquid area (Figs. 8(i) and 8(n)) when compared to Run B. While the solid–liquid area increases, gas–liquid area is relatively unchanged. Therefore, despite an increased gas pressure gradient in the lower zone, the former serves to resist deflection of liquid by gas, as shown by the minimal change in slag distribution entering the hearth in Fig. 5. The flooding region shown in Fig. 8(d) is similar to Run B, though the accumulation index (Fig. 8(e)) is generally higher, consistent with the increased liquid holdup.

In Run F, gas flow is increased to simulate a change in the level of furnace productivity. The associated increase in solids flow rate is responsible for a slight thickening of the cohesive zone, as shown in Fig. 9. While slag holdup is clearly higher than in Run B due to stronger gas drag, as shown by Figs. 9(k) and 9(m), the denser metal phase is not significantly affected (Figs. 9(f) and 9(h)). With no change in the packing conditions, the effective area of solid–liquid interaction is not noticeably altered for either metal (Fig. 9(i)) or slag (Fig. 9(n)). However, the higher gas flow results in an increased area of gas–liquid interaction (Figs. 9(j) and 9(o)). Once again, the flooding region shown in Fig. 9(d) is similar to Run B, while the accumulation index (Fig. 9(e)) is generally higher, consistent with the increased gas flow moving the system closer to a flooding condition.

5. Conclusions

Using the force-balance approach described previously, a molten iron and slag flow submodel has been integrated into the blast furnace numerical model, SHAFT. Liquids are generated from ore during descent through the cohesive zone and their flow calculated according to the prevailing gas, liquid and packing properties. Specific calculations for molten iron and slag include holdup, velocity, accumulation and flooding distributions, as well as areas of interaction between gas and liquid, and solid and liquid.

The distribution of liquids is most strongly influenced by the radial variation in ore volume fraction entering the furnace, gas flow in the vicinity of the raceway and the furnace profile. Liquids, in turn, significantly increase the gas pressure gradient in the lower furnace. Simulation results demonstrate the effect of liquids on furnace behaviour as well as the response to changes in operating conditions such as bed permeability, furnace productivity and liquid physical properties. The flow of slag is more sensitive to operating conditions than molten iron.

Two key areas have been identified for model improve-
The characterisation of meltdown and liquid flow within the cohesive zone, and three-dimensional simulation of the circumferential flow and maldistribution at tuyere level.

Nomenclature

\( A_i^j \): Area of interaction between phase \( i \) and \( j \) (\( \text{m}^2 \))

\( d_l \): Effective liquid droplet diameter (\( \text{m} \))

\( d_{lm} \): Modified effective liquid droplet diameter (\( \text{m} \))

\( d_p \): Particle diameter (\( \text{m} \))

\( d_w \): Effective packing diameter (\( \text{m} \))

\( F_{ij} \): Interaction force of phase \( j \) on phase \( i \) (\( \text{N} \cdot \text{m}^{-2} \))

\( f_i \): Gas flow resistance (\( \Delta P/\Delta L/U_{oi} \)) (\( \text{kg} \cdot \text{m}^{-2} \cdot \text{s} \))

\( g \): Gravitational acceleration (\( \text{m} \cdot \text{s}^{-2} \))

\( h_d \): Dynamic liquid holdup (–)

\( h_s \): Static liquid holdup (–)

\( h_t \): Total liquid holdup (–)

\( \Delta P/\Delta L \): Pressure gradient (\( \text{N} \cdot \text{m}^{-3} \))

\( \Delta P/\Delta r \): Radial bed pressure gradient (\( \text{N} \cdot \text{m}^{-3} \))

\( \Delta P/\Delta x \): Axial bed pressure gradient (\( \text{N} \cdot \text{m}^{-3} \))

\( S_r \): Shrinkage ratio (\( L/L_0 \)) (–)

\( U_{oi} \): Superficial velocity of phase \( i \) (\( \text{m} \cdot \text{s}^{-1} \))

\( U_i \): True velocity of phase \( i \) (\( \text{m} \cdot \text{s}^{-1} \))

\( X_p \): Dimensionless gas pressure drop (–)

Greek symbols

\( \alpha \): Ore volume fraction (–)

\( \delta \): Under-relaxation parameter (–)

\( \varepsilon \): Porosity (–)

\( \phi \): Shape factor (–)

\( \mu_i \): Viscosity of phase \( i \) (\( \text{Pa} \cdot \text{s} \))

\( \theta \): Contact angle (°)

\( \rho_i \): Density of phase \( i \) (\( \text{kg} \cdot \text{m}^{-3} \))

\( \sigma \): Surface tension (\( \text{N} \cdot \text{m}^{-1} \))

Super/subscripts

\( o,c \): Ore, coke

\( i,j \): Gas (g), liquid (l) or solid (s) phase

\( m,s \): Metal, slag

\( s,d \): Static, dynamic

\( x,r \): Axial, radial

REFERENCES

1) S. J. Chew, P. Zulli and A. B. Yu: ISIJ Int., 41 (2001), 1112.

2) P. D. Burke and J. M. Burgess, Proc. 48th Ironmaking Conf., ISS-AIME, Warrendale, PA, (1989), 773.

3) T. Sugiyama, H. Sato, M. Nakamura and Y. Hara: Tetsu-to-Hagané, 66 (1980), 1908.

4) T. Sugiyama, T. Nakagawa, H. Sibaike and Y. Uda: Tetsu-to-Hagané, 73 (1987), 2044.