Simulation of Iron Flow and Heat Transfer in the Hearth of a Blast Furnace

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ABSTRACT

The aim of this research is to numerically simulate iron flow and heat transfer in the hearth of a blast furnace by solving the three-dimensional turbulent Navier-Stokes equation coupled with the transport equation of energy at steady state. Under the effects of conjugate heat transfer and natural convection, a computational fluid dynamic (CFD) was performed to generate flow field in the hearth and temperature distribution in the refractories during the tapping process. The accuracy and computation of the model is validated using operation data from Bluescope Steel’s No. 5 blast furnace. The shear stress and heat flux on the wall were then predicted for the different vertical movements, shapes of the coke zone (deadman), and the lengths of the tap-hole. As shown in the results, it is worth noticed that an increase in the tap hole length causes the peak values of shear stress to shift in the increasing azimuthal direction at a particular plane, and the location of the peak value of shear stress coincides with the location of higher temperature actually measured on the hearth wall, signifying enhanced heat transfer to the wall at location of peak stress.

KEYWORDS
Simulation, Iron flow, Heat transfer, Hearth of blast furnace

INTRODUCTION

The erosion rate at the region of a blast furnace hearth is expected to be significant when high productivity operations are practiced, and the erosion is considered to be the main limitation of the campaign life of the blast furnace. Therefore, it is of a general interest to know the phenomena of the hot metal in the hearth region during casting. Conducting a qualitative assessment of these phenomena, Paschkis and Miresepassi (1954; and 1956) and Cowling (1946) suggested that the hearth erosion was largely a thermal phenomenon. The thermal penetration mode, or variations thereof (Kern and Brummett, 1967; Van and Van, 1967; Summers and Morgan, 1969; Leonard, 1971; Evans and Workman, 1973; Pehlke and Henning, 1976; and Saunders, 1977) has been used quite extensively in hearth-erosion studies. It should be noted that there are some difficulties in the application of thermal penetration models to interpret the shape of the salamanders, i.e. the metallic intrusions resulting from hearth erosion.

An attractive model has to be proposed by Elliott and Popper (1972) to represent the hearth erosion phenomena and potentially alternative suggested that erosion takes place owing to the circulation of molten iron within the hearth. Distributions of liquid iron flow and refractory temperatures have a significant influence on alkali and zinc attack, oxidation and dissolution of carbon refractories, heat load and thermal stress, static pressure, erosion from hot liquids, and mechanical stress (Van et al., 1987; Dzermejko, 1991; and Stothart et al, 1997). The formulation of a comprehensive computational fluid dynamics of the blast furnace hearth is complex, as it must address conjugate heat transfer, natural convection, and flow through porous media.
Additionally, a hearth/tap-hole diameter ratio of above 100 and a maximum/minimum velocity ratio of 10000 must be considered. Nevertheless, CFD models are powerful exploratory approaches, particularly in situations where plant trials are impractical or expensive, and realistic physical models are complicated.

Several interesting hearth models have been announced. Yoshikawa and Szekely (1981) considered a situation where the tap hole was plugged, and assumed the hearth to be coke free, which is unrealistic for investigating the recirculatory flow induced by natural convection and its effect on dissolution of carbonaceous refractories into the melt. Peters et al. (1985) and Preuer et al. (1992) have studied the flow field in the hearth but have not highlighted the effect of the length of the tap hole, which can strongly influence the stress field at the wall. In a detailed study by Vats and Dash (2000), a mathematical model was developed to examine the influence of the tap hole length on the shear stresses to the hearth. However, the effects of the conjugate heat transfer and natural convection on the flow induced stress distribution at the wall have not been considered in the study.

Considering the refractory walls explicitly, but assuming that natural convection was negligible, Leprince et al. (1993) and Venturini et al. (1998) numerically studied the effect of a coke free gutter filled with low porosity material on the temperature and velocity distribution of the hot metal. Tomita and Tanaka (1994) considered mass transport of titanium in the hearth, when the ilmenite was injected through tuyeres or charged with the burden.

Some models did not explicitly include refractory walls. Considering natural convection, Preuer (1991) and Preuer et al. (1992; 1992; and 1993) assumed that hearth erosion was caused by carbon dissolution only. This model was validated using a water model (Preuer, 1991; Preuer et al., 1992; Preuer et al., 1992; and Preuer et al., 1993). Kurita and Ogawa (1994) simulated cases with a floating deadman with an impermeable central region, and with a non-uniform inlet temperature, which was decreased from raceways towards the center. Kowalski et al. (1998) described a model that included calculations for the dissolution rate of carbon from refractories for a sitting and a floating deadman.

A 3-D model presented by Shibata and co-workers (1990) was applied to simulate the hearth flow for four different shapes of the coke zone or deadman, namely, fully packed hearth, sitting deadman with coke free gutter, floating deadman with coke free gutter, and floating deadman with flat bottom. Assuming laminar with a constant viscosity and thermal conductivity in the presence of natural convection, Alfred et al. (1992) predicted 3D flow and temperature profiles with special regard to the influences (permeability, shape and movement of the deadman, porous free regions, temperature, tapping) during a tapping cycle. In addition, Leprince et al. (1993) analyzed the influence of circular zone porosity on the unsteady laminar thermal flow pattern without natural convection via a 3D numerical calculation using the Fluent (version 4.10) package.

Recently, Panjkovic et al. (2002) established a comprehensive model using a commercial CFX package (version 4.2). The model was considered as conjugate heat transfer, free convection, and flow through porous media with inertia force. The model was able to predict the fluid flow and heat transfer in the blast furnace hearth, specifically, the flow and temperature distributions in the liquid iron melt, and temperature profiles within the refractories. Their results permitted the model to be used for (1) an interpretation of plant data, for instance, examination of changes in hearth conditions that could produce the observed behavior; (2) assessment of various scenarios, for instance, evaluation of the effects of modified cooling controls different extent of refractory erosion, deadman conditions, and so on; and (3) examination of various parameters on the hearth performance, for instance, studying the effect of various parameters on the hearth performance.

The aim of the present study intends to examine the shear stress on the wall of blast furnace by solving the turbulent Navier-Stokes equation and thermal-energy-balance equation, as
developed by Panjkovic et al. (2002), in the hearth numerically. Under the effects of conjugate heat transfer and natural convection, the computation was performed with the use of FLUENT package (version 6.1) for the shear stress and heat transfer varying with different vertical movements, shapes of the coke zone (deadman), and the lengths of the tap-hole in the blast furnace hearth.

**PROBLEM FORMULATION**

**Physical Model**

Fig. 1 shows examined physical model of hearth geometry and refractory layout with a unit of mm for Port Kembla no. 5 (PKBF5) blast furnace (Panjkovic et al., 2002) that is symmetric with the symmetry plane being defined by central lines of the tap-hole and the hearth. In order to formulate the governing equation for the tapping process, a number of assumptions have been applied. The hearth was considered to be at steady state and only liquid iron; the tap-hole was represented as trumpet shaped and a hollow enclosure with coke free, which is inclined at 12.5° to the horizontal and is square in cross-section with 60 mm side; coke and iron temperatures are equal; and chemical reactions and solidification may be explicitly ignored.

**Mathematical Formulation**

For the mathematical description of the physical picture mentioned above, the turbulent Navier-Stokes equation and thermal-energy balance equation with natural convection have been developed to compute the velocity, which were written in vector, as follows (Gray and Giorgini, 1976; and Panjkovic et al., 2002):

Conservation of mass

\[
\vec{V} \cdot (\rho \vec{u}) = 0;
\]

Motion equation

\[
\vec{V} \cdot (\rho \vec{u} \times \vec{u}) - \vec{V} \cdot (\mu_{\text{eff}} \vec{V} \vec{u}) = -\vec{V} P + \vec{V} \cdot \left( \mu_{\text{eff}} \left( \vec{V} \vec{u} \right)^T \right) + S_u + \vec{g} \rho \beta (T - T_{\text{ref}}); \quad \text{and}
\]

Transport equation of enthalpy

\[
\vec{V} \cdot \left( \rho \vec{u} h - \left( \frac{\lambda}{C_p} + \frac{\mu_T}{0.9} \right) \vec{V} h \right) = 0.
\]

In equation (2) the effective viscosity is given by

\[
\mu_{\text{eff}} = \mu_L + \mu_T,
\]

where \( \mu_L \) is laminar viscosity and the turbulent viscosity, \( \mu_T \), is expressed by...
\[ \mu_f = C\mu f \frac{1}{k} C_m \frac{d^2}{d^2} \left[ \frac{150(1-\gamma)}{1-\gamma} \frac{1.75}{\text{Re}} \right]^{0.5}, \]
\[ (5) \]

where the empirical constants are assigned the values proposed (Takeda, 1996) and Re is
Reynolds number for flow in a packed bed; and
\[ \mu_f = C \rho H|\vec{u}|, \]
\[ (6) \]

where the empirical coefficient C is dependent on the coke free layer height (H), ranging
from 0.05 (250 mm) to 0.5 (500 mm) for flow in the coke free layer under the deadman.
Furthermore, the resistance to the flow through the coke bed, \( S_u \), is calculated using Ergun’s
equation as
\[ S_u = -150 \mu_L \frac{(1-\gamma)^2}{\gamma^2 d^2} \vec{u} - 1.75 \rho \frac{1-\gamma}{\gamma d} |\vec{u}| \vec{u}. \]
\[ (7) \]

where \( \vec{u} \) is interstitial velocity of liquid iron.

In addition, the density of liquid iron, \( \rho \), is dependent on the temperature profile during
drainage period; and the reference temperature for calculation of Boussinesq term, \( T_{ref} \) is
specified as inlet iron flow temperature, namely, 1500°C. Therefore, mass conservation and
motion equations, and transport equation of enthalpy need to be simultaneously solved for
velocity distribution in the blast furnace hearth.

In this work, considering four different deadman positions, namely, (a) intact firebrick and
sitting deadman; (b) intact firebrick and floating deadman 500 mm above refractory surface; (c)
eroded firebrick and sitting deadman; and (d) eroded firebrick and floating deadman 500 mm
above refractory surface, the following boundary conditions are imposed. The surface of iron is
an inlet flow boundary with fixed temperature (1500°C), which is flat and horizontal and remains
at a constant level; the inlet velocity of liquid iron is uniform over this surface; and a no slip
condition exists on the hot face. The sidewall boundary temperatures of cases (a) and (b) and
cases (c) and (d) are 70°C and 80°C, respectively. The bottom boundary temperature for cases (a)
and (b), and cases (c) and (d), are 80°C and 100°C, respectively. Momentum and enthalpy wall
function (Fluent, 2003) are used to define the boundary conditions for flow and heat transfer at
the walls. Additionally, the upper surface of the refractory walls is adiabatic; and pressure at the
tap-hole exit is set at 1 atm.

**Numerical Method**

**Table 1. Materials properties (Panjkovic et al., 2002).**

<table>
<thead>
<tr>
<th>Iron</th>
<th>Properties</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Laminar viscosity (( \mu_L ))</td>
<td>0.00715 Pa s</td>
<td></td>
</tr>
<tr>
<td>Thermal conductivity (( \lambda ))</td>
<td>16.5 Wm(^{-1})K(^{-1})</td>
<td></td>
</tr>
<tr>
<td>Heat capacity (Cp)</td>
<td>850 J kg(^{-1})K(^{-1})</td>
<td></td>
</tr>
<tr>
<td>Density (( \rho ))</td>
<td>7000 kg m(^{-3})</td>
<td></td>
</tr>
<tr>
<td>Thermal coefficient of volumetric expansion (( \beta ))</td>
<td>1.4×10(^{-4}) K(^{-1})</td>
<td></td>
</tr>
<tr>
<td>Production rate</td>
<td>80 kg s(^{-1})</td>
<td></td>
</tr>
<tr>
<td>Height of liquid above the top of tap-hole entrance</td>
<td>0.25 m</td>
<td></td>
</tr>
<tr>
<td>Reference T for calculation of Boussinesq term (( T_{ref} ))</td>
<td>1500°C</td>
<td></td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Refractories</th>
<th>Properties</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Heat capacity (Cp)</td>
<td>1260 J kg(^{-1})K(^{-1})</td>
<td></td>
</tr>
<tr>
<td>Thermal conductivity of BC7S (( \lambda ))</td>
<td>12.0 Wm(^{-1})K(^{-1}), ( T \leq 30°C )</td>
<td></td>
</tr>
<tr>
<td>Thermal conductivity of BC30S (( \lambda ))</td>
<td>15.5 Wm(^{-1})K(^{-1}), ( T \geq 1000°C )</td>
<td></td>
</tr>
<tr>
<td>Thermal conductivity of firebrick (( \lambda ))</td>
<td>13.5 Wm(^{-1})K(^{-1}), ( T \approx 400°C )</td>
<td></td>
</tr>
<tr>
<td>Thermal conductivity of firebrick (( \lambda ))</td>
<td>13.5 Wm(^{-1})K(^{-1}), ( T \approx 400°C )</td>
<td></td>
</tr>
<tr>
<td>Thermal conductivity of ceramic cup (( \lambda ))</td>
<td>38 Wm K(^{-1})</td>
<td></td>
</tr>
<tr>
<td>Thermal conductivity of firebrick (( \lambda ))</td>
<td>2.38 Wm(^{-1})K(^{-1}), ( T \leq 800°C )</td>
<td></td>
</tr>
<tr>
<td>Thermal conductivity of firebrick (( \lambda ))</td>
<td>2.31 Wm(^{-1})K(^{-1}), ( T \geq 1200°C )</td>
<td></td>
</tr>
<tr>
<td>Thermal conductivity of firebrick (( \lambda ))</td>
<td>2.20 Wm(^{-1})K(^{-1}), ( T \leq 400°C )</td>
<td></td>
</tr>
<tr>
<td>Thermal conductivity of firebrick (( \lambda ))</td>
<td>2.00 Wm(^{-1})K(^{-1}), ( T = 500°C )</td>
<td></td>
</tr>
</tbody>
</table>
5.05 Wm⁻¹K⁻¹, T=600 °C
2.15 Wm⁻¹K⁻¹, T=800 °C
2.20 Wm⁻¹K⁻¹, T=1000 °C
2.30 Wm⁻¹K⁻¹, T=1200 °C
2.35 Wm⁻¹K⁻¹, T ≥ 1400 °C

| Coke bed | Particle diameter (d) 0.03 m | Porosity (γ) 0.35 |

The governing equations (1) to (7) and materials properties given as listed in Table 1, are solved using a commercial CFD package platform, FLUENT (version 6.1), with a segregated model. The computational domain includes refractories, liquid iron, and the deadman.

The numerical technique is based on a three-dimensional, finite volume model proposed with H grid in Cartesian coordinates, as shown in Figure 2. The total cells used to evaluate velocity and temperature are dependent on the different deadman positions, ranging from 150,000 to 200,000, in the blast furnace hearth.

RESULTS AND DISCUSSION

Validation of Computation Model

The model evaluation is carried out using temperatures measured by thermocouples embedded in the refractories of PKBF5 and numerical data from Panjkovic et al. (2002), respectively, for four cases of deadman positions, namely, (a) intact firebrick and sitting deadman, (b) intact firebrick and floating deadman 500 mm above refractory surface, (c) eroded firebrick and sitting deadman; and (d) eroded firebrick and floating deadman 500 mm above refractory surface at axial planes of z = 300 mm, 900 mm, and 1500 mm, respectively.

As shown in Figure 3, a comparison between the calculated temperature profiles of the sitting and floating deadman displays that the floating deadman increase pad temperatures significantly. This is caused by the preferential flow of hot metal towards and through the coke free zone (Panjkovic et al., 2002). Additionally, the erosion of the firebrick causes an increase of heat losses. In the practical operation, this flattens the temperature profile in the pad as it is likely that the bricks in the floor corners are also lost.

The evaluation of the model against the refractory temperature measured and the published data calculated by Panjkovic et al. (2002) shows: (1) although numerical models underestimate the pad temperatures near the central region, the agreement is reasonable, in terms both of the magnitude and the shape of temperature distribution; (2) with cases of (c) and (d), the agreements at the measured refractory temperature are improved in comparison with the results reported by Panjkovic et al. (2002) due to refining the computational grid in this model; (3) in all cases, the agreement with thermocouples in the central region is reasonable.

Shear Stress and Heat Transfer in the Blast Furnace Hearth

With effects of turbulent flow, natural convection, and the resistance to the flow through
the coke bed, the fluid flow induced shear stresses, $\tau_w$, on the wall of the blast furnace ($r = 5.155$ m) has been calculated by

$$\tau_w = -\mu_{eff} \frac{\partial \bar{u}}{\partial n}$$  \hspace{1cm} (8)

where $n$ indicates the general coordinates at axial direction, for different lengths of the tap hole and its area. The shear stresses at three axial planes, namely, $z = 4.936$ m (below the tap hole plane), $z = 5.436$ m (at the tap hole exit plane), and $z = 5.936$ m (above the tap hole plane), will only be discussed here.

For cases of tap-hole without block, Figure 4 illustrates the effect of natural convection on wall shear stress. It is clearly that natural convection plays a significant role, as demonstrated through the recirculation loops in Figure 4-b-1, which causes larger shear stress on wall (Figure 4-b-2) than the flow situation with a constant density of iron liquid (Figures 4-a-1 and 4-a-2).
Figure 4. Comparison of velocity fields (m/s) and shear stress (Pa) on the wall corresponding to cases of (a) forced turbulent convection and (b) mixed turbulent convection in blast furnace hearth containing a tap hole without block.

In the cases of tap hole length ranging from 0 to 600 mm, the corresponding peak stresses of $\tau_w$ are decreased with the increase of the tap hole length, and a minimum peak value is obtained at the length of 600 mm at axial plane of $z = 5.936$ m, as shown in Figure 5. The Figure also shows the location of peak values of $\tau_w$ shifts away the tap-hole, as the tap hole length increases from 200 to 600 m. In other words, an increase in the length of the tap-hole has a positive effect in reducing the peak stress on the wall.

Additionally, Figure 6 demonstrates the effect of axial planes on wall shear stresses for the case of tap hole with block length of 200 mm. As displayed in the figure, the shear stress on the wall...
wall can be seen distinctly at different axial planes. Since the flow comes from the top towards the tap-hole, the influence of the tap-hole length is considered on the planes below the tap-hole level in terms of reducing the shear stress on the wall. The maximum peak shear stress $\tau_w$ may be obtained at the tap hole exit plane exits, whereas the minimum peak value of $\tau_{\theta z}$ presents at the above tap hole plane. The location of the peak value of shear stress coincides with the location of higher temperature actually measured on the hearth wall, signifying enhanced heat transfer to the wall at location of peak stress.

**Figure 7** shows the isothermal path in the firebrick and refractory for the different shape of the deadman during a tapping process. It is expected that the erosion of the firebrick cause an increase of heat losses induced by a larger thermal gradient (Figures 7-(c) and –(d)). Furthermore, the larger portion of iron flows under the deadman, more heat is transferred to the refractory due to smaller thermal resistance (Figures 7-(b) and –(d)).

**CONCLUSION**

At steady state, the turbulent Navier-Stokes equation and thermal-energy-balance equation with conjugate heat transfer and natural convection have been successfully re-solved with the FLUENT package (version 6.1) for the different vertical movements, shapes of the coke zone (deadman), and the length of the tap-hole in the blast furnace hearth (iron and refractory) during a tapping process. The accuracy and computation of the model was evaluated using plant operational data (Bluescope Steel’s no. 5 blast furnace) and the numerical solutions presented by Panjkovic et al. (2002), respectively. Overall, there is a good agreement between measured and calculated refractory temperature distributions.

Additionally, the present computation has been directed towards determining the effect of
tap hole length on the wall shear stress. The following observations have been obtained:

1) an increase in the tap hole length causes the peak value of shear stress to decrease;
2) an increase in the tap hole length causes the peak values of shear stress to shift in the increasing direction of θ.
3) the location of the peak value of shear stress coincides with the location of higher temperature actually measured on the hearth wall, signifying enhanced heat transfer to the wall at location of peak stress.
4) the presence of the tap hole block helps to significantly reduce the shear stresses on the wall of the furnace at three axial planes, namely, below the tap hole plane (z= 4.936 m), at the tap hole exit plane (z = 5.436 m), and above the tap hole plane (z = 5.936 m).

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