Introduction

Up-hill teeming only makes up a small fraction of today’s steel production, but it is still used in the production of certain steel grades where the quality rather than the quantity comes in the first room. Furthermore, up-hill teeming is used for steel grades, such as ball-bearing and tool steels that are hard to cast continuously with a good sound product quality. Therefore, it is more important than ever for companies using up-hill teeming to pay attention to the process and develop it further in order to remain competitive. Quality aspects concerning the up-hill teeming process that have been addressed is yield and surface quality and in recent years the formation of unwanted non-metallic inclusions. Non-metallic inclusions are primarily formed as de-oxidation products as a result of the addition of a metal with strong affinity to oxygen during the ladle treatment operation. Entrained ladle slag or torn of refractory lining can also be a source of non-metallic inclusions. The presence of this type of inclusions is quite scarce but very harmful since they usually are quite large in size. At the end of the ladle treatment operation, just before the casting, ball-bearing and tool steel grades contain very few non-metallic inclusions and has a low total oxygen concentration. Previous studies have shown that it is possible to find inclusions containing traces of mold flux in samples taken from the steel during the filling of ingot molds. Based on the findings in these two studies and observations made at steel plants during quality inspections of steel products, it is believed that the mold flux covering the rising steel surface could react with and or entrap into the steel during the filling of the molds. This could lead to the formation of new non-metallic inclusions at a very late stage in the process chain. Due to the late formation of these inclusions, they could be very hard to remove from the liquid steel. The size of these inclusions containing mold flux are quite large, which makes them especially harmful to the quality of the final product. Therefore, it is of the utmost interest to find ways to eliminate, or at least reduce, the risk of the formation of inclusions that originates from the mold flux in order to improve the product quality.

In this work numerical simulations have been carried out in order to investigate the effect of the geometry of the inlet on the fluid flow during the early stages of the filling of a 4.2 tonne ingot mold. In Fig. 1 a schematic diagram of the up-hill teeming process is shown. Before casting, a paper bag filled with approximately 5 kg of mold powder is

![Fig. 1. Schematic description of the up-hill teeming process.](image-url)
hung roughly 10 cm from the bottom of the mold. At the start of casting, steel driven by gravity enters the mold from the bottom through an inlet nozzle. Initially, a cascade of steel enters the mold leading to a large surface deformation where the flow is highest. When the steel surface reaches the mold powder bag, the paper burns and the mold powder pours down on the steel surface. The mold powder is applied to the rising steel surface in the mold mainly in order to: (i) protect the steel from reacting with the surrounding atmosphere and (ii) enhance the heat transfer during the filling and the subsequent solidification of the steel. As the steel rises in the mold, the mold powder gradually heats up, producing a liquid slag layer which is in contact with the steel. In between the molten slag and the unreacted powder there are layers with fused powder and semi-solid slag.

Numerical simulation has previously been used by a number of researchers to study filling of molds. As for example by Jönsson et al. who studied the filling of a cylindrical mold and by van der Graaf et al. who simulated the filling of thin rectangular molds. The findings made in these two works can unfortunately not easily be compared with the results in this work since the geometries differ from the present case. Recently Eriksson et al. made an investigation into filling of an ingot mold using five different turbulence descriptions. The results in this study were compared with the results from a water model study of the filling and the subsequent solidification of the steel. As the steel rises in the mold, the mold powder gradually heats up, producing a liquid slag layer which is in contact with the steel. In between the molten slag and the unreacted powder there are layers with fused powder and semi-solid slag.

The emphasis of this study has been on the opening angle of the inlet, which will determine the velocity and the direction of the steel entering the mold. The effect of the velocity of the steel entering the mold through the inlet nozzle on the quality of the produced steel has earlier been acknowledged by Blank and in a report by Jernkontoret. It is also known that the flow of steel in the mold during filling of the mold is important when it comes to minimizing the risk of chemical reactions between the steel and mold slag and/or entrainment of the slag into the steel. In this work the numerical simulations of the filling of an ingot mold have been carried out using the commercial software Flow3D.

Five different types of inlet nozzles have been studied and compared, a straight nozzle and four nozzles with different angles (10°, 20°, 25° and 30°). The turbulent flow has been described using so called large eddy simulation (LES). In the first part of the paper, the mathematical model of the filling of an ingot mold including the LES formulation is described. Thereafter, predictions using different inlet nozzle angles are shown and discussed.

2. Mathematical Model
2.1. Basic Governing (Conservation) Equations
In general terms, the isothermal flow of an incompressible Newtonian fluid in a rectangular coordinate system is governed by the following partial differential equations

Continuity equation

\[ \frac{\partial u_i}{\partial x_i} = 0 \]  

where the \( A_i \) is the fractional area open to flow, \( u_i \) is the velocity and \( x_i \) is the space coordinate in the \( i \)-direction.

Momentum equation

\[ \frac{\partial u_i}{\partial t} + \frac{1}{V_i} \left( u_i A_i \frac{\partial u_i}{\partial x_i} \right) = - \frac{1}{\rho} \frac{\partial \rho}{\partial x_i} + \frac{1}{\rho V_i} \left( w_i A_i \frac{\partial \tau_{ij}}{\partial x_j} \right) + S_i \]  

where \( t \) is the time, \( V_i \) is the fractional volume open to flow, \( x_i \) is the space coordinate in the \( j \)-direction, \( u_i \) is the velocity in the \( j \)-direction, \( \rho \) is the density of the fluid, \( \rho \) is the static pressure, \( \tau_{ij} \) is the viscous stress. In this implementation the body force, \( S_i \), is the gravitational constant which only acts in the negative \( z \)-direction. The fractional volume and areas have been introduced into the equations of continuity and momentum in order to allow for solid obstacles in the computational domain. In the above expression \( w_i \) are the wall shear stresses in the \( i \)-direction, which are modeled by assuming a zero tangential velocity on any area closed to flow. For turbulent flows a law-of-the-wall function is assigned to the velocity profile, this will be described in detail in a subsequent section.

The viscous stress in Eq. (2) is given by the following relationships

\[ \tau_{ij} = -\mu \left[ 2 \frac{\partial u_i}{\partial x_j} - \frac{2}{3} (\nabla \cdot \mathbf{u}) \right] \]  

\[ \tau_{ij} = \tau_{ij} = -\mu \left( \frac{\partial u_i}{\partial x_j} + \frac{\partial u_j}{\partial x_i} \right) \]  

where \( \mu \) is the dynamic viscosity of the fluid and \( \mathbf{u} \) is the three dimensional velocity vector.

The fluid configuration is defined in terms of a volume of fluid (VOF) function, \( F \). The volume of fluid is conserved according to the following equation

\[ \frac{\partial F}{\partial t} + \frac{1}{V_i} \left( u_i A_i \frac{\partial F}{\partial x_i} \right) = 0 \]  

The fluid fraction \( F \) will be equal to 1 in cells which are completely occupied by the fluid and equal to 0 in cells which are totally empty. As a consequence of this \( 0 \leq F \leq 1 \) in cells containing the free surface.

2.2. Large Eddy Simulation
In this work turbulence have been described using large eddy simulation (LES). The idea behind the LES approach is that all turbulent flow structures that can be resolved by the computational grid should be directly computed and only those features that are too small to be resolved should be modeled. Therefore, the flow variables are decomposed in terms of resolved (grid) scales and unresolved (sub-grid) scales rather than time-averaged and fluctuating quantities.

LES was originally used in meteorological applications but has with the development of cheaper and more
powerful computer gained interest in engineering applications.15)

The LES scheme adopted in this investigation is outlined below;

1. The velocity is decomposed so the motion of the large eddies are represented by the filtered velocity field. This is the resolved velocity scale.
2. The velocity component that represents the motion of small eddies is modeled using a so called sub-grid-scale (SGS) model. The SGS model thus represents the unresolved scale velocity field.
3. The evolution of the filtered velocities is governed by equations such as Eq. (2). The main difference compared to Reynolds Averaged solutions of Navier–Stokes equation is mainly how the viscous stress tensor in Eq. (2) is calculated.

Smagorinsky13) was the first to postulate a model for the SGS stresses. He assumed that the SGS stresses follows a gradient diffusion process,

\[ t_{ij} = 2\mu S_{ij} \]

where \( S_{ij} \) is the resolved strain rate tensor defined as

\[ S_{ij} = \frac{1}{2} \left( \frac{\partial v_i}{\partial x_j} + \frac{\partial v_j}{\partial x_i} \right) \]

Furthermore, the turbulent viscosity is here described as

\[ \mu_t = \rho(C_s L)^2(S_{ij} S_{ij})^{1/2} \]

where \( C_s \) is the empirical Smagorinsky constant. In this study \( C_s = 0.1 \). Finally, the length scale can following the Smagorinsky approach be described as the geometric mean of the grid geometry

\[ L = (\delta x \delta y \delta z)^{1/3} \]

where \( \delta x, \delta y \) and \( \delta z \) is the size of a computational cell in the x, y and z-direction, respectively.

2.3. Boundary Conditions

Figure 2(a) shows a cross section of the region of integration at \( y=0 \). At the x–y plane, the computational domain extends between \(-24.25 \leq x \leq 24.25 \) cm and \(-24.25 \leq y \leq 24.25 \) cm. The mold has a plane of symmetry which cuts through the x–y plane at \( y=0 \). However, this plane of symmetry will not be utilized here due to the nature of the LES description. In the z-direction the computational domain extends between \(-9 \) cm and \( 33 \) cm, with the bottom of the mold located at \( z=0 \). The straight part of the inlet nozzle has a diameter of 45 mm.

In this study of the early stages of filling of an ingot mold the focus has been on the effect on the velocity field of different opening angles of the inlet nozzle. The velocity field has been calculated for five different angles of the inlet, a straight inlet (0 degrees) together with four angled inlets; 10, 20, 25 and 30 degrees. The definition of the opening angle \( (\phi) \) of the nozzle is shown in Fig. 2(b). The non-rectangular geometry of the mold has been incorporated into the computational domain by defining the fractional face areas and fractional volumes of the cells that are open to flow.

At the edges of the computational domain, shown in Fig. 3, boundary conditions have been applied according to Table 1 in all the simulations carried out in this work a constant fluid velocity of 78 cm/s pointing in the positive x-direction have been assigned at the inlet, i.e. boundary WL in Fig. 3. The velocity of the fluid entering the inlet is the calculated average velocity for the filling of a 4.2 tonne ingot mold used in the production of ball-bearing steels. The physical properties of steel used in the calculations in the present study can be found in Table 2.

© 2004 ISIJ
Close to a solid boundary the effect of the viscous forces are more pronounced, which gives rise to viscous stresses which could be as large or larger than the Reynolds stresses. This region therefore calls for special methods, one of which is the so called law of the wall method.

\[ u' = \frac{\tau'}{\kappa \nu} = \frac{1}{\kappa} \ln \left( \frac{y u_t}{\nu} \right) + E = \frac{y}{\kappa} \ln(y') + E \quad \ldots (10) \]

where \( y \) is the perpendicular distance from the wall, \( \kappa = 0.41 \) (von Karman constant) and \( E = 5.5 \). \( u_t \) is the friction velocity defined as \( \sqrt{\tau_w / \rho} \), where \( \tau_w \) is the wall shear stress per unit area.

2.4. Method of Solution

The governing equations described above are discretized using a finite difference approximation. The region of integration (Fig. 2(a) has been sub-divided into a mesh with 80, 80 and 65 cells in the \( x \), \( y \) and \( z \)-direction, respectively. All dependent variables are defined at the center of the cells except the velocities which are defined at the cell faces. The governing transport and turbulence equations were solved using the commercial software Flow3D. A typical calculation of the filling of an ingot mold described above requires about 50 h of CPU time on a computer with a 2.8 GHz Intel Pentium 4 processor.

3. Results and Discussion

In Fig. 4 a velocity vector plot is shown after 14.75 s of filling, corresponding to a surface level of 15 cm above the bottom of the mold, for a cross section of the calculation domain at \( y = 0 \). Data are shown for the straight nozzle as well as for the three nozzles with angles of 10, 20 and 30 degree, respectively. When a straight nozzle is used the steel flow is straight up in the vertical direction, as can be seen in Fig. 4(a). When the steel flow reaches the surface, the vertical momentum force is so strong so that a "hump" is formed at the free surface. Thereafter, the steel flow is directed towards the walls of the mold. This gives rise to two similar recirculation loops on each side of the incoming steel flow "pillar". A simulation using a nozzle with a 10 degree angle, shown in Fig. 4(b), illustrates that the fluid flow pattern in the ingot mold is totally different from the results for a straight nozzle. For this case, the main flow is directed slightly towards the right side of the nozzle wall.
Furthermore, it can be seen that the surface deformation is considerably less compared to the case with a straight nozzle in Fig. 4(a). As a consequence, the recirculation flow pattern is larger on the left side of the steel plume than on the right side. In Figs. 4(c) and 4(d), predictions using a 20 degree angle and a 30 degree angle are shown. Similar to what was found for the predictions using a 10 degree nozzle, the flow is directed towards the right side of the nozzle wall. If the results in Figs. 4(b), 4(c) and 4(d) are compared, it can be seen that the incoming steel plume is moved closer to the right side of the mold when the angle is increased. As seen in Fig. 4(d), the steel plume even touches the side wall before reaching the steel surface. From a practical point of view, the effect of a changed nozzle angle can affect the casting conditions. It can, for a number of reasons, be risky to have a nozzle inlet with a 30 degree angle since the steel plume hits the side of the wall of the mold when the angle is increased. As seen in Fig. 4(d), the steel plume even touches the side wall before reaching the steel surface. From a practical point of view, the effect of a changed nozzle angle can affect the casting conditions. It can, for a number of reasons, be risky to have a nozzle inlet with a 30 degree angle since the steel plume hits the side of the wall of the mold. First, warm steel will be fed to that side of the wall which results in an uneven wear of the cast iron mold. Second, the initial solidification in this part of the mold will be less than in other parts due to that warm steel is continuously fed to that region of the mold. This may also result in an uneven heat transfer effect on the side.

Figure 5 shows the velocity vector plots for the same cases as shown in Fig. 4, but after 28.25 s of filling. At this point, the steel surface in the mold is located 25 cm above the bottom of the mold. In Fig 5(a) the result for a prediction using a straight nozzle is shown. Similar to what was found in Fig. 4(a) for a shorter filling time, the steel plume is directed straight up towards the steel surface. Furthermore, a “hump” is formed at the steel surface. The vector plots for the other nozzle angles are also very similar to what was found after 14.75 s of filling in Fig. 4. More specifically, the larger the angle the more the steel plume at the inlet is directed towards the right side of the mold. However, one difference when comparing with the results in Fig. 4 is that the free surface is almost flat for nozzle angles larger than 20 degrees at the later filling stage. The corresponding angle is 30 degrees at the shorter filling time (i.e. 14.75 s).

From the previous figures the fluid flow in the whole mold was discussed. In a metallurgical point of view, however, it is quite interesting to discuss the conditions at the rising steel surface. The reason is that the bag with mold powder will start to burn and the mold powder will be distributed on the surface sometime during the period of time covered in the study (i.e. 15 to 30 s after the start of the filling). Furthermore, our earlier studies have shown that inclusions originating from the mold powder can be found in samples taken when the mold has been filled to this level. This is an indication that mold powder already has reacted with steel and/or got entrapped into the steel at these early stages of the mold filling. Therefore, results highlighting the conditions at the rising steel surface will be presented and discussed in the following.

In Fig. 6 the calculated velocity components in the x-direction along the free surface at are plotted, for the case with a straight nozzle as well as predictions using four dif-
ferent nozzle angles (i.e. 10°, 20°, 25° and 30°). Note that the data are taken from the first row of computational cells where there are only steel present. From Fig. 6(a), representing data after 14.75 s of filling when the steel surface is located 15 cm above the bottom of the mold, it is obvious that the largest fluctuation in surface velocity in the x-direction can be found for the straight nozzle. The most even values of the same velocity can be found for the predictions using a nozzle with a 25 degrees angle. The same tendencies are seen in Fig. 6(b), representing the velocities when the steel surface is located 25 cm above the bottom of the mold, corresponding to 28.25 s of filling.

Earlier it was discussed how the momentum from the incoming steel flow deforms the free surface. In order to illustrate the deformation of the rising steel surface more clearly, the surface contour for the different cases (i.e. straight, 10°, 20° and 30° nozzle) are plotted at 14.75 and 28.25 s of filling, in Figs. 7(a) and 7(b), respectively. For both filling times it is clear that the effect on the inflow on the surface deformation is largest for the straight nozzle. The same tendencies are seen in Fig. 6(b), representing the velocities when the steel surface is located 25 cm above the bottom of the mold, corresponding to 28.25 s of filling.

It is desirable to keep the horizontal velocities at the steel surface as low as possible in order to reduce the risk of the steel reacting chemically with the mold powder that is covering the steel surface. There is also potential danger that the mold slag gets entrained into the steel if the velocity difference at the interface between the steel and the mold slag is too high. In Fig. 8 the calculated average of the velocity component in the x-direction at $y=0$, at either side of the inlet region, is shown as a function of the opening angle of the inlet nozzle. The average velocity is calculated from velocity data taken from the first row of computational cells where all cells are completely filled with steel. As mentioned above the steel surface have been divided into two separate regions, one on either side of the inlet, defined as $-18.19 \leq x \leq -7.97$ cm and $7.97 \leq x \leq 18.19$ cm. These regions are designated as left and right respectively in Fig. 8.

The average velocity in the x-direction after 14.75 s of filling as a function of the angle of the inlet nozzle can be seen in Fig. 8(a). In the figure it can be seen that the straight inlet nozzle creates a lower average velocity in the x-direction on both sides of the inlet, compared to the 10 and 20 degree inlets. If the opening angle of the inlet is increased further to 25 and 30 degrees the average velocity is drastically decreased compared to the case with 10 and 20 degree inlets.
At the left side of the inlet the average velocity in the x-direction for the 25 and 30 degree inlets is pointing in the opposite direction compared to the straight, 10 and 20 degree inlets. The same is true on the right hand side of the inlet with an opening angle of 30 degrees. After 28.25 s of filling of the ingot mold there is a gradual decrease in the average velocity in the x-direction with the increase in the opening angle of the inlet nozzle at the right side of the nozzle, which can be seen in Fig. 8(b). It can also be seen in this figure that, at an opening angle of 25 degrees the flow in the x-direction changes direction on both sides of the inlet. At the left side of the inlet, the average velocity in the x-direction is almost zero for the case with a 25 and 30 degree angle of the inlet nozzle.

When determining the tendencies for entrapment of mold powder into steel it is useful to utilize the Weber number. The Weber number can be defined as;

\[ \text{We} = \frac{u_{\text{steel}}^2 \rho_{\text{steel}}}{\sqrt{\gamma (\rho_{\text{steel}} - \rho_{\text{slag}})}} \]  

(11)

where \( u_{\text{steel}} \) is the velocity of the steel in the x-direction relative to the mold slag, \( \rho_{\text{steel}} \) and \( \rho_{\text{slag}} \) is the density of the steel and the mold slag respectively, \( g \) is the gravitational constant and \( \gamma \) is the interfacial tension between the steel and the mold slag phase. Since no mold slag phase was included in the present numerical model and therefore no information about the velocity of the mold slag phase at the steel/mold slag interface is available, the velocity of the mold slag could be assumed to be zero. Therefore, \( u_{\text{steel}} \) would be equal to the velocity of the steel at the free surface. Xiao and co-workers used physical and mathematical modeling and the Weber number to determine when oil droplets where dispersed into steel.\(^{16}\)

From water model experiments, they found that oil dispersion into water occurs when the Weber number is larger than 12.3. Furthermore, they claimed that slag is also dispersed into steel when the Weber number is exceeds than 12.3. Later, Jonsson and Jönsson\(^{17}\) made similar findings while studying the steel/slag interface in a gas-stirred ladle using a three-phase numerical model. Based on the findings in these works, it was felt that a similar discussion could be of interest in the present work, especially from a practical point of view. However, no mold powder layer has been incorporated in the present numerical model of the filling of an ingot mold, so the following discussion will be somewhat hypothetical.

In Fig. 9 the Weber number, calculated using Eq. (11), has been plotted as a function of the relative velocity between the steel and the mold slag phase for different mold slag densities and steel/slag interfacial tensions. It can be seen in Fig. 9 that the Weber number exceeds 12.3 if the relative velocity between the steel and the slag is greater than \( \sim 50 \text{ cm/s} \) and if the steel/slag interfacial tension is 0.5 N/m at the same time as the mold slag density is 3 500 kg/m\(^3\). The steel density was chosen to be 6 900 kg/m\(^3\) in the calculations of the Weber number presented in Fig. 9. From Fig. 9 it may be concluded that the greatest risk of entraining mold slag, based on a critical value of the Weber number, is when the density of mold slag is as great as possible and at the same time the steel/slag interfacial tension is as low as possible. It is not very likely that there will be large fluctuations in the mold slag density and the effect of a variation of 1 000 kg/m\(^3\) is shown in the figure. On the other hand, it is known that reactions between the steel and the covering slag phase can drastically reduce the steel/slag interfacial tension.
interfacial tension.\textsuperscript{18)} More specifically the interfacial tension was found to be very small (almost zero) when oxygen was transferred over the slag/steel interface due to the reaction between FeO in the slag and Al dissolved in the steel. Hence, even lower critical velocities for entrainment of slag.

As mentioned earlier, the authors found that inclusions were formed at an early filling stage due to interactions with the mold powder.\textsuperscript{2)} The component FeO was present in those mold powders. Since the steel was killed with Al, it is very likely that alumina inclusions would form due to the steel/mold powder reaction. This would lead to an increased oxygen transfer across the slag/steel interface, which lowers the interfacial tension between the steel and the slag as suggested by Gaye et al.\textsuperscript{18)}

Even though it is a hypothetical discussion, we can conclude that for plant conditions the interfacial tension can be lowered so much that the velocities in the \(x\)-direction of the surface shown in Fig. 8 for nozzle angles 0, 10 and 20 degrees are large enough to reach Weber numbers in the neighborhood of 12.3. Thus, mold slag might be dispersed into steel leading to even greater risks for inclusion formation due to reactions between steel and mold powder.

In summary, there seem to be benefits with an inlet nozzle angle of about 25 degrees. This will lead to small deformations of the free surface and a reduced risk for interactions between the steel and mold powder, during and just after the mold powder bag is burned open. The use of a 25 degree nozzle angle will also lead to horizontal surface velocities in the \(x\)-direction of only up to 10 cm/s. For so small velocity differences the Weber number will not be larger than 12.3, even if the interfacial tension between the steel and the mold slag is as small as 0.1 N/m (which is almost zero), see Fig. 9. Thus, the chances for mold powder entrainment are small. Of course, the Weber number can also be kept small by altering the chemical composition of the mold powder. This can be done by removing the easily reducible oxides such as FeO and MnO from the mold powder, thus avoiding lowering of the interfacial tension by reactions with strong deoxidants such as Al.

4. Conclusions

The effect of the entrance nozzle angle on the fluid flow in an ingot mold during filling was studied using a fundamental mathematical model. The focus in the study was on the initial filling period since it is during that time the mold powder is applied to the rising steel surface. It is especially important to have control of the filling at this stage in order to minimize the steel mold/powder interactions, which could lead to the formation of new inclusions. The predictions show that a change in the nozzle angle that results in a larger volume flow, will also to a large degree effect the fluid flow pattern, the surface deformation as well as the horizontal surface velocities. The best results were obtained using a 25 degree angle of the inlet nozzle. For this case the steel rising surface was almost flat and the horizontal velocities were kept below 10 cm/s. Both of these conditions are believed to minimize the steel/mold powder interactions.

The specific conclusions from this study are:

- In general, the surface deformation decreases with increased inlet nozzle angle.
- The incoming flow is moved closer to the right side of the mold when the inlet nozzle angle is increased.
- When the mold is filled to a 25 cm level, the surface is almost flat if the inlet nozzle angle is larger than 20 degrees. This is advantageous since the chances for interactions with the mold powder decreases if the surface is undisturbed.
- The most even horizontal velocities (i.e. in the \(x\)-direction) were found for an inlet nozzle angle of 25 degrees.
- At the shorter filling time (i.e. 14.75 s) both the inlet angles 25 and 30 degrees results in average velocities in the \(x\)-direction that are smaller than 10 cm/s. At the longer filling time (i.e. 28.25 s) only the prediction using a 25 degree inlet angle result in horizontal velocities lower than 10 cm/s.

A hypothetical discussion was made on the possible influence of a mold flux layer on top of the rising steel surface. From this it can be concluded that the possibility of mold slag dispersion into the steel during filling of the mold would be decreased if an angled inlet nozzle was employed. However, the incorporation of a mold flux layer on top of the rising steel surface into the numerical model is not yet done but is a natural continuation of this work.

Acknowledgments

The authors wish to thank SSF which through the Brinell Center together with Ovako Steel contributed financially to this project.

REFERENCES

1) P. Sjödin, P. Jönsson, M. Andreasson and A. Winquist: Scand. J. Metall., 26 (1997), 41.
2) R. Eriksson, P. Jönsson and A. Gustafsson: Scand. J. Metall., 33 (2004), 160.
3) J. Chang, R. Eriksson and P. Jönsson: Ironmaking Steelmaking, 30 (2003), No. 1, 66.
4) M. Freiberg: Proc. 50th Electric Furnace Conf., ISS, Warrendale, PA, (1992), 235.
5) P. Jönsson, N. Saluja, O. J. Ilegbuisi and J. Szekely: AFS Trans., 99 (1991), 291.
6) G. B. van der Graaf, H. E. A. van den Akker and L. Katgerman: Metall. Mater. Trans. B, 32B (2001), 69.
7) R. Eriksson, L. T. I. Jonsson and P. G. Jonsson: Tech. Rep. ISRN MSE/KTH-03/24-SE+TILL.METALLURGI/RAPP, Dept. of Material Science and Engineering, KTH, Stockholm, Sweden, (2003).
8) R. Eriksson, A. Tillander, L. T. I. Jonsson and P. G. Jonsson: Steel Res., 74 (2003), 42.
9) J. R. Blank: Proc. 67th Steelmaking Conf., ISS, Warrendale, PA, (1984), 135.
10) M. Petersson: Final report, tech. rep., Jernkontoret, Stockholm, Sweden, (1976) TM 25-76.
11) Flow3D Manual version 8.0, Flow Science inc., SantaFe, NM, (2003).
12) C. W. Hirt and B. D. Nicholas: J. Comp. Phys., 39 (1981), 201.
13) J. Smagorinsky: Monthly Weather Review, 91 (1963), 99.
14) J. W. Deardorff: Boundary-Layer Meteorology, 5 (1974), 81.
15) B. G. Thomas, Q. Yuan, S. Srivankrishnan, T. Shi, S. P. Vanka and M. B. Assar: ISIJ Int., 41 (2001), No. 10, 1262.
16) Z. Xiao, Y. Peng and C. Liu: Chin. J. Mater. Sci. Technol., 3 (1987), 187.
17) L. Jonsson and P. Jönsson: ISIJ Int., 36 (1996), No. 9, 1127.
18) H. Gaye, D. Lucas, M. Olette and P. V. Riboud: Can. Metall. Q., 23 (1984), No. 2, 179.