The Effect of Mould Flux Properties on Thermo-mechanical Behaviour during Billet Continuous Casting

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During continuous casting mould powder forms a pool of liquid flux which infiltrates into the solidifying shell/mould gap and forms a flux film containing liquid and/or solid layers. Subsequent crystallisation of the film results in the formation of an air gap at the mould wall. An air gap can also be formed by the shrinkage of the solidified shell. In practise, no air gap is formed by shell shrinkage in the upper mould since molten flux will flow immediately into and fill any gap formed. However, in the lower mould, if the shell temperature falls below the break temperature of the flux there is no liquid to fill any gap formed by shell shrinkage and hence an air gap can form. A model was developed which determines the effect of the various layers (including the air gap) on the horizontal heat transfer between shell and mould. A fully coupled, heat transfer/stress analysis was used to calculate: (i) the thickness of the various layers of the flux film; (ii) horizontal heat flux; and (iii) the resultant stress in the solidified shell.

The model predicted that: (a) the hoop stress increased as the interfacial thermal resistance decreases (i.e. the heat flux increases); (b) the maximum hoop stress at the exit decreased with increasing flux film thickness; (c) the ferro-static pressure tended to hinder the formation of an air gap at mid-face positions whereas in the corners the thicker shell tended to counteract the ferro-static pressure resulting in air gap formation; (d) the thickness of the liquid film at various positions in the mould was computed for fluxes with optimum break temperatures to determine if they provided liquid lubrication throughout the mould; and (e) the maximum stress occurred in the corner when casting with mould fluxes but at the mid-face when using oil.

KEY WORDS: continuous casting; mould flux; heat transfer; finite element; stress analysis; billet; oil casting.

1. Introduction

The continuous casting process has become the dominant means of producing steel, virtually eliminating ingot casting, with its usage level reaching up to 95% in countries like Japan and Canada.1) During casting, molten steel is poured from a tundish into an oscillating, water cooled copper mould. The steel solidifies against the water cooled mould forming a solid shell which increases in thickness down the length of the mould. Upon exiting the mould, the shell must be thick enough to contain the remaining liquid metal until final solidification occurs many metres from the mould. Compared to ingot casting, the continuous casting process produces higher quality cast products at a lower cost. However, insufficient control of continuous casting operations and inadequate casting machine maintenance may lead to the formation of undesirable features or defects such as longitudinal cracking, star cracking, oscillation marks, mould wear and breakouts.2) These defects are directly related to the thermo-mechanical response of the strand in the mould.

With the availability of increased computational power, ever more complex mathematical models of the continuous casting process are being developed to reduce the incidence of defects as it is difficult and expensive to carry out plant trials. The continuous casting process is complex involving turbulent fluid flow in the molten steel, solidification, heat transfer between the steel and mould, and mould oscillation. Upon solidification the metal contracts, leading to the development of thermo-mechanical stresses in the solidified strand. Early thermo-mechanical models described the behaviour of the strand by sequentially coupling heat transfer and mechanical models.3–6) More recently, fully coupled thermo-mechanical models have been developed to capture the effect of deformation on heat transfer and vice versa.7–14) Limited attempts have also been made to incorporate the effects of fluid flow on the thermo-mechanical response of the strand.15–18)

The heat transfer between the shell and the mould which governs the thermo-mechanical response is primarily dependent on the size of the mould/shell gap and the type and thickness of lubricant used, e.g. oil or mould flux.19) Mould powders or fluxes are synthetic slags which are used during continuous casting of steel to protect the steel meniscus from oxidation, provide thermal insulation to the steel surface, absorb inclusions and provide lubrication to the strand. The performance of the flux can greatly affect caster operations in terms of product quality. Mould powders normally consist of CaO, SiO2, Na2O and CaF2 along with minor additions of other mineral oxides. They are generally expected to melt with the heat available from the molten
metal. The molten flux forms a liquid pool on the top of the mould, infiltrating the mould/shell gap and partially solidifies near the mould. The flux film formed initially is glassy in nature but in time, crystallisation occurs.\textsuperscript{17,18} Since the density of the crystalline phase is greater than that of the glass phase, crystallisation results in shrinkage of the solid flux layer and the formation of an air gap at the copper mould/flux interface. Thus the crystal/glass distribution has a significant effect on the horizontal heat transfer.

A second type of air gap can also be formed by shell shrinkage. However, in the upper part of the mould liquid flux will flow into and fill any gap formed. In the lower part of the mould if the shell temperature is lower than the break temperature of the flux there will be no liquid to fill the gap (due to shell shrinkage or inadequate mould taper). Consequently an air gap can be formed under these circumstances. In this paper the air gap formed by crystallisation of the flux film is referred to as an interfacial contact resistance, whereas the air gap formed by shell shrinkage is referred to as air gap. The heat transfer between the shell and the mould which dictates the evolution of shell thickness is primarily controlled by the crystal/glass distribution in the mould flux and the contact resistance between the solid flux and the mould, whereas the lubrication is governed by the liquid flux layer thickness.

A limited number of thermo-mechanical models of casting with mould flux lubrication have been developed. Many of these models use a simplified description of the mould flux layers. Wang \textit{et al.}\textsuperscript{8,9} developed a thermo-mechanical model to study the effect of increasing mould taper on the strain developed in the strand. A constant heat transfer coefficient was used to represent the thermal radiation occurring with mould flux lubrication.\textsuperscript{9,11} For oil casting, the heat transfer across the mould/shell interface is simplified as pyrolysis of the oil forms a gas mixture (H\textsubscript{2}, air, CO \textit{etc.}) which fills the mould/shell gap. Heat transfer coefficients based on the gas composition and the thickness of the gap are currently used to describe heat transfer across the gap.\textsuperscript{3,4} Heat transport through a mould flux system is quite complex as compared to oil due to the presence of liquid/solid flux layers.

In the present work, a coupled thermo-mechanical model of a continuously cast strand was developed. The heat transfer model was validated by comparing the predictions against experimental results reported for oil casting.\textsuperscript{23} The model was then used to analyse the effect of mould flux properties and casting speed on the gap between mould and strand.

2. Model Description

A 2-D transient, coupled thermo-mechanical model was developed (using the commercial finite element software, ABAQUS\textsuperscript{\textcopyright}), to predict the evolution of the temperature and stress/strain in a transverse slice of a square billet as it moved down the mould. In this model, time and height in the mould are related via the casting velocity using a Lagrangian frame of reference. Within each time-step a fully coupled heat transfer/stress analysis was carried out to evaluate the shell/mould gap until convergence was achieved. The size of the gap (formed by shell shrinkage) was assessed using a contact algorithm. The gap size was used to define the effective heat transfer coefficient across the shell/mould interface in a user-subroutine \textit{via} an iterative calculation. The user-subroutine is capable of estimating the heat transfer coefficient for a variety of casting conditions including with mould flux, oil or no lubricant. For mould flux casting, the gap was assumed to be filled by a flux film which contained liquid, crystalline and glass layers. By solving for the thermal resistance of each layer, the effective heat transfer coefficient was calculated for the shell/mould interface. The deformation behaviour of the mould flux in the gap was not taken into account in order to reduce the model complexity. For oil casting, the heat transfer coefficient was estimated by assuming the gap was filled by a gas containing air with a temperature dependent thermal conductivity.\textsuperscript{25}

The casting parameters and mould geometry are listed in Table 1. The model domain, shown in Fig. 1, was simplified to a quarter section of a square billet by assuming symmetry. A graduated mesh consisting of 1522 bilinear quadrilateral elements (1600 nodes) in the strand and 78 el-

<table>
<thead>
<tr>
<th>Description</th>
<th>Unit</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Billet quarter size</td>
<td>mm</td>
<td>60 \times 60</td>
</tr>
<tr>
<td>Mould length</td>
<td>mm</td>
<td>700</td>
</tr>
<tr>
<td>Mould wall thickness, (d_m)</td>
<td>mm</td>
<td>6</td>
</tr>
<tr>
<td>Mould Taper</td>
<td>%</td>
<td>0.75</td>
</tr>
<tr>
<td>Casting speed</td>
<td>m s\textsuperscript{-1}</td>
<td>0.0036</td>
</tr>
<tr>
<td>Pour temperature</td>
<td>K</td>
<td>1587</td>
</tr>
</tbody>
</table>

![Fig. 1. Schematic of the: (a) sectioned model domain; and (b) gradient mesh in the transverse section.](image)

\textsuperscript{8} ABAQUS is the registered trademark of Hibbitt, Karlson and Sorenson Inc., RI.

\textsuperscript{9} ISIJ International, Vol. 47 (2007), No. 1
The thickness of the mould flux film is dependent on the powder con-summation by:  

\[ Q_s = \frac{2}{R - 5} \]  (2)  

where \( R \) is the surface area to volume ratio of the strand, \( R = \frac{2}{h \cdot (w + t)} \cdot \frac{w \cdot t \cdot h}{h_{\text{tube}}} \) where \( w, h \) and \( t \) are the width, height and thickness of the strand, respectively. Through an analysis of plant data, \( R \) has been shown that the most significant factors affecting \( Q_s \) are casting speed, \( V_c \), and mould flux viscosity, \( \eta \). Ogibayashi et al. \( R \) proposed that these parameters are related to the optimum powder consumption by:

\[ Q_s = \frac{0.6}{\eta V_c} \]  (3)

Recently, Fox \( R \) showed that the break temperature or the melting temperature, \( T_b \), of the mould flux can be expressed as a function of mould flux viscosity:

\[ T_b = 1103 + 68.5 \ln \eta \]  (4)

Combining Eqs. (2) and (3), the optimum viscosity of the mould flux can be calculated as a function of the cast geometry and velocity. The break temperature can then be evaluated using the viscosity from Eq. (4). The geometry of the billet used is listed in Table 1.

2.2.2. Billet/Mould Interfacial Heat Transfer

The heat transfer between the billet and the mould occurs via both conduction and radiation. The thermal resistance between the billet and the cooling water in the mould can be represented as an equivalent electric circuit as shown in Fig. 2. The effective resistance includes the radiation and conduction components across the air gap (if present), liquid/solid flux layers, and the interfacial resistance between the flux and the mould. Radiation across the crystalline flux layer was ignored due to the high extinction coefficient. The contact resistance between the shell and the liquid flux layer has been ignored because it is an order of magnitude lower than the resistance of any single component in the system.

The net heat flux from billet to the mould surface can be written as:

\[ q(T_m) = \frac{T_b - T_m}{r_1 + r_2 + r_3 + r_4 + r_5} \]  (5)

where \( r_1, r_2, r_3, r_4, r_5 \) are the thermal resistances of the air gap (due to shell shrinkage), liquid, glass and crystalline mould flux layers, and the solid flux/mould interfacial resistance, respectively. \( T_m \) is the billet surface temperature and \( T_m \) is the mould hot face temperature. The total resistance in the mould including the convection in the water channel can be written as:

\[ r_m = \frac{d_m}{k_m} + \frac{1}{h_u} \]  (6)

where \( d_m \) is the mould thickness, \( k_m \) is the mould thermal conductivity and \( h_u \) is the water heat transfer coefficient.

The thermal resistances, \( r_m \) in Eq. (5) are defined in terms

<table>
<thead>
<tr>
<th>Description</th>
<th>Unit</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Liquidus Temperature</td>
<td>K</td>
<td>1802</td>
</tr>
<tr>
<td>Solidus Temperature</td>
<td>K</td>
<td>1784</td>
</tr>
<tr>
<td>Coefficient of Thermal Expansion, TLE</td>
<td>K(^{-1})</td>
<td>1 (\times) 10(^{-5})</td>
</tr>
<tr>
<td>Heat Capacity, ( c_p )</td>
<td>kg m(^{-1})s(^{-1})K(^{-1})</td>
<td>0.004</td>
</tr>
<tr>
<td>Latent Heat, ( L )</td>
<td>J kg(^{-1})</td>
<td>250000</td>
</tr>
<tr>
<td>Density, ( \rho )</td>
<td>kg m(^{-3})</td>
<td>7400</td>
</tr>
<tr>
<td>Thermal conductivity, below liquidus, ( k_{\ell} )</td>
<td>W m(^{-1})K(^{-1})</td>
<td>270</td>
</tr>
<tr>
<td>Thermal conductivity, above liquidus, ( k_s )</td>
<td>W m(^{-1})K(^{-1})</td>
<td>27</td>
</tr>
</tbody>
</table>

Table 2. Thermo-physical properties of steel.  

\[ \rho = 1802, L = 250000, \rho = 7400, k_{\ell} = 270, k_s = 27, \eta = \frac{2}{R - 5} \]
of conduction and radiation components across the different layers as \(^{33}\):

**Liquid flux (l):**

\[ r_i^{\text{cond}} = \frac{d_i}{k_i} \] ...........................(7)

\[ r_i^{\text{rad}} = \frac{0.75 \alpha d_i + \left(\frac{1}{\varepsilon_i} + \frac{1}{\varepsilon_f}\right) - 1}{\sigma n^2 (T_i^c + T_i^s)(T_b + T_s)} \] ...........................(8)

**Glassy flux (g):**

\[ r_i^{\text{cond}} = \frac{d_i}{k_i} \] ...........................(10)

\[ r_i^{\text{rad}} = \frac{0.75 \alpha d_i + \left(\frac{1}{\varepsilon_i} + \frac{1}{\varepsilon_m}\right) - 1}{\sigma n^2 (T_i^c + T_i^s)(T_b + T_s)} \] for \( T_i > T_b \) ........................................(11)

\[ r_i^{\text{rad}} = \frac{0.75 \alpha d_i + \left(\frac{1}{\varepsilon_i} + \frac{1}{\varepsilon_m}\right) - 1}{\sigma n^2 (T_i^c + T_i^s)(T_m + T_s)} \] for \( T_i \leq T_b \) ........................................(12)

**Crystalline flux (c):**

\[ r_i^{\text{cond}} = \frac{d_i}{k_i} \] ...........................(13)

\[ r_i^{\text{rad}} = 0.5\sigma (\varepsilon_i + \varepsilon_f) (T_i^c + T_i^s)(T_m + T_s) \] ........................................(15)

\[ \frac{1}{r_i} = \frac{1}{r_i^{\text{cond}}} + \frac{1}{r_i^{\text{rad}}} \] ........................................(16)

where \( T_c \) is the crystalline/glass interface temperature, \( T_s \) is the solid glass flux thickness, \( \sigma \) is the Stefan–Boltzmann constant, \( \alpha \) is the absorption coefficient, \( n \) is the refractive index and \( \varepsilon \) is the emissivity. The thermo-physical properties of the mould flux used in this investigation are given in Table 3. Assuming a crystalline fraction of \( f_{cr} \), the total mould flux gap can be written as:

\[ d_i = d_s + d_c + d_g + d_i + d_a = d_i + d_c + (1 - f_{cr}) d_c + d_i + d_a \] ...............................(17)

where \( d_i \) is the interfacial gap thickness at mould/solid flux interface, \( d_s \) is the solid crystalline flux thickness, \( d_c \) is the solid glass flux thickness, \( d_g \) is the liquid flux thickness and \( d_a \) is the air gap thickness between the solidified flux and strand in the absence of liquid flux. As the heat flux across the various flux layers is the same they can be equated to give:

\[ \frac{T_a - T_m}{r_i} = \frac{T_a - T_c}{r_c} \] ........................................(18)

\[ \frac{T_a - T_m}{r_i} = \frac{T_a - T_b}{r_g} \] ........................................(19)

\[ \frac{T_a - T_m}{r_i} = \frac{T_a - T_b}{r_i} \] ........................................(20)

The resulting set of non-linear Eqs. (17)–(20) was solved using Powell’s hybrid solver (as implemented by Galassi et al.\(^{34}\)) to obtain \( T_c, T_a, d_c \) and \( d_i \) for the input flux gap size, \( d_i \), and shell temperature, \( T_s \). An initial value of 450 K was assigned to the mould temperature, \( T_m \). The total mould/strand gap size \( d_i \) at the meniscus was taken as 0.5 mm following the methodology of Yamauchi et al.\(^{21}\). Substituting the resistances in Eq. (5), the heat flux between the mould hot face and the billet can be calculated. As the same heat flux is between the hot face and water channel too, the hot face mould temperature can be re-evaluated as:

\[ T_m' = T_m + \frac{q(d_i + 1/h_w)}{k_m} \] ........................................(21)

where \( T_m \) is the temperature of the water. If the difference in the mould temperatures \( T_m \) and \( T_m' \) in an iteration is less than a desired criterion, set to 0.001 for this study, \( T_m' \) is re-
placed by $T_m$ to solve the Eqs. (17)–(20). The converged thermal resistances can then be used to calculate the net heat transfer coefficient between the shell and the cooling water given as:

$$h_i = \frac{1}{\frac{1}{h_w} + \frac{d_m}{k_m} + \frac{r_i}{r_s} + \frac{r_i}{r_a} - \frac{r_i}{r_s}} \tag{22}$$

For oil casting, the heat transfer coefficient between the billet surface and the mould can be evaluated using a simplified version of the Eq. (22). Previous authors3–4 have shown that the gap is primarily filled with air and hydrogen.5 Thus, the heat transfer coefficient between the billet surface and the cooling water channel can be represented as31:

$$h_i = \frac{1}{\frac{1}{h_w} + \frac{d_m}{k_m}} \tag{23}$$

The coefficient, $h_i$, obtained from the above equation was limited to a maximum corresponding to direct contact between the billet surface and the mould. The limiting heat transfer coefficient ($h_i$) for direct contact between the billet surface and the mould was taken as 2 223 W m$^{-2}$K$^{-1}$ based on interfacial resistance associated with an oscillation mark of 0.02 mm depth.23

2.3. Stress Model

The solidified shell experiences a complex set of forces including i) ferro-static pressure due to liquid metal, ii) contraction forces due to cooling of the shell and iii) possible contact forces from the mould due to taper and oscillation acting on the outside of the shell. The mould was treated as a rigid surface which moves as a function of the taper defined in Table 1. A contact algorithm35 was used to describe the interaction between the billet surface and the mould. In addition to contact, the stress state of the shell is influenced by the thermal history, hence a coupled temperature and stress model is needed to accurately model the deformation of the shell.

In the thermo-mechanical model, the differential equations of equilibrium are developed using an elemental volume force balance and applying compatibility conditions based on the displacement field and its relationship to strain in the body. The FEM equations are developed through the minimisation of virtual work within an element.36 The total strain is composed of 3 terms:

$$\varepsilon = \varepsilon_e + \varepsilon_i + \varepsilon_p \tag{24}$$

where $\varepsilon_e$ is the elastic strain, $\varepsilon_i$ is the thermal strain and $\varepsilon_p$ is the plastic strain.

2.3.1. Mechanical Properties

The elastic properties at high temperature are uncertain, mainly due to the effects of creep. In this investigation, the elastic modulus was developed by Kinoshito37 and the Poisson ratio was taken as constant with a value of 0.3. To correctly simulate the deformation behaviour of steel in the austenitic and ferritic phases, different constitutive behaviour is needed. In ABAQUS, rate-dependent plastic behaviour was defined using a series of flow stress curves (given as tabulated data) dependent on strain, strain rate, and temperature. Han and co-workers38 proposed an equation to determine the flow curves at various temperatures and strain rates. Seol et al.39 fitted this equation to experimental data for a plain carbon steel in the austenite phase region; this data has been used for austenite, while the equation proposed by Wray40 was used for ferrite.

The constitutive behaviour of the liquid metal was characterised using a low elastic modulus (100 MPa) and a high yield stress (100 GPa) to ensure low stress levels and no plastic strain accumulation in the liquid metal regions.

The solidified billet shell contracts (or expands) due to temperature change, exhibiting rapid change during phase transformations. The thermal strain is defined using the thermal linear expansion coefficient, TLE, as given by Won et al.41 During the $\delta$,$\gamma$ phase transformation, the specific volume was calculated using a rule of mixtures.

2.3.2. Ferro-static Pressure

The ferro-static pressure in the liquid metal is a function of the distance below the metal surface. For a 2-D transverse section, the ferro-static pressure, $P$, at the solidification interface is given as

$$P = \rho g V_c t \tag{25}$$

where $\rho$ is the density, $g$ is the acceleration due to gravity, $V_c$ is the casting speed and $t$ is the casting time. Since the solidification interface keeps moving towards the billet core as solidification proceeds, applying the ferro-static pressure as a boundary condition at the solidification interface requires re-meshing the domain at every time step. This procedure represents a significant computational expense for small time-steps and fine mesh sizes.

To overcome this problem in the present work, a novel strategy was developed where the ferro-static pressure is applied to all faces of every ‘liquid’ element instead of just on the solidification interface. Elements are defined as ‘liquid’ if each node in the element is above the coherency temperature (the solid fraction is less than 0.7 or the temperature is above 1 796 K).

The applicability of this approach was assessed by comparing the results of this method with the traditional approach of applying the ferro-static pressure only at the solidification interface. A uniform solid shell thickness of 4 mm was defined with a ferro-static pressure of 2 700 Pa which corresponds to 37 mm down the billet. The von Mises stresses predicted on the outer perimeter of the shell between the mid-face and the corner for the two cases, compared in Fig. 3, are the same. The stress is high at the mid-face due to bulging and further increases at the corner due to the presence of a complex biaxial stress state. This strategy was used for the more complex coupled thermo-mechanical model.

3. Model Validation

In the absence of sufficient data to validate the thermo-mechanical model for mould flux casting, the model was applied to predict billet behaviour during oil casting. Model predictions of shell thickness and mould temperature were compared with measured data reported by Park et al.21 for
oil casting of low carbon steel (Fe–0.04C–0.2Si–0.25Mn–0.010P–0.015S). Casting conditions have been summarised in Table 1. The gap heat transfer coefficient for oil casting, as given by Eq. (23), was employed for this simulation. Figure 4 shows the comparison of measured solid shell thickness with the predicted shell thickness at mid-face. The predicted shell thicknesses are in good agreement with the data measured by Park et al. Note, that the shell thickness measurement employed a tracer (FeS) whose penetration in the mushy zone was somewhat uncertain. The oscillations in the calculated shell thickness are related to the mesh size. A graduated mesh, varying between 0.2 mm close to the shell boundary to 2 mm at the centreline, was used in the current analysis. The fine mesh solidifying near the meniscus results in small oscillations as compared to the larger oscillations occurring at the mould exit where large elements are solidifying. The shell thickness predicted using a coarse mesh of uniform size (2 mm) is also plotted in Fig. 4 and reinforces that the oscillations are a mesh dependent phenomenon. The predicted cold face mould temperature, shown in Fig. 5(a), exhibit the same trends as that reported by Park et al. However, the overall heat transfer is over-predicted leading to lower temperatures compared with the measured data. The average heat flux from the current model is 1.81 MW m$^{-2}$, which agrees well with the average heat flux of 1.84 MW m$^{-2}$ calculated by Park et al. based on the 8 K increase measured in the cooling water temperature.

The model predictions were also compared against mould flux casting measurements done by Pinheiro et al. The ‘powder B’ in Pinheiro’s plant experiment with a break temperature of 1408 K was simulated. Only minimal details on the mechanical and thermophysical properties of the 0.11% C steel were provided by Pinheiro, therefore the properties and conditions described above to simulate the Park et al. data were used, changing only from oil to mould flux lubrication. Despite this approximation, the results (Fig. 5(b)) shows that measured temperature lies in between the predicted mid-face and corner temperatures, and provides confidence in the model.

4. Results and Discussion

The thermo-mechanical model has been used to study the effects of mould flux properties and casting parameters on the thermo-mechanical behaviour of a billet.

4.1. Oil vs. Mould Flux Casting

The general consensus is that mould fluxes provide better lubrication and thermal insulation than oil, leading to improved billet quality. To verify this statement, the predicted behaviour of billets cast using both oil and mould flux have been compared. The heat transfer coefficient for mould flux casting was evaluated using the approach previously described. The predicted temperature and the von Mises stress distributions at the mould exit are shown in Fig. 6 for both oil and mould flux casting cases. The isothersms of the mould flux casting are uniform compared to those for oil casting which show a decrease in shell thickness in the corners. In mould flux casting, the heat transfer conditions result in a thicker corner relative to the mid-face. The stress distributions show similar trends where the oil cast billet exhibits a complex stress state in the corner. The corner is the point of maximum stress in the
mould flux casting due to the temperature distribution whereas the mid-face is the maximum stress location for oil casting.

4.1.1. Temperature Distribution

The results obtained for corner temperatures and heat fluxes are shown for both oil and mould flux casting in Fig. 7. It can be seen from Fig. 7 that:

(i) In the region just below the meniscus (0–50 mm) the billet shell is hotter with mould flux than oil due to the presence of the solid flux film, lowering the horizontal heat flux. Oil has a low flash point (around 573 K for vegetable oil) and burns within a short distance of the meniscus, producing a gas mixture that transfers heat more efficiently.

(ii) In the region 50–200 mm below the meniscus the air gap grows faster with oil, limiting the heat transfer. The billet shell temperature decreases when mould flux is used.

(iii) In the rest of the mould the heat fluxes are very similar.

The heat flux in the meniscus region for oil casting is higher than that for mould flux casting (Fig. 7(b)). These results agree with the experimental finding of Pinheiro et al., who found that for peritectic steel grades the heat flux for oil casting close to the meniscus was higher than that in mould flux casting. The increase in the shell contraction with distance down the mould causes the air gap to widen for oil casting and leads to a steep decrease in the heat flux.

On the other hand the delayed onset of air gap formation for mould flux casting allows a gradual decrease in the heat flux (up to ~200 mm). However, once the shell temperature falls below the break temperature (1468 K) of the mould flux, an air gap forms and heat flux decreases rapidly.

4.1.2. Hoop Stress Distribution

The distributions of hoop stress for the mould flux and oil casting cases at mould exit (19 s) are shown in Fig. 8. A subsurface tensile stress has developed in both cases. The tensile stress is caused by the $\delta \rightarrow \gamma$ phase transformation and the difference in thermal contraction (4%) between these two phases. The magnitudes of the subsurface hoop stresses were similar for both the oil and mould flux at the mid-face. However, the subsurface hoop stress at the off-corner location (15 mm from corner) in mould flux casting.
is twice the stress level for oil casting. This result suggests that off-corner internal cracks have a higher tendency to occur in mould flux casting.

4.2. Mould Flux Properties

The model was used to investigate the effects of varying mould flux properties on heat transfer, shell thickness and cracking.

4.2.1. Interfacial Resistance

Several researchers have shown that a significant interfacial contact resistance exists between the solid flux and the mould. Watanabe et al.\(^{43}\) suggested that volume shrinkage, which occurs when a crystalline flux is formed, leads to a surface mismatch. Cho et al.\(^{33}\) showed that the interfacial resistance increases with increasing crystallinity of the mould flux. The model was run with three interfacial resistances (\(1 \times 10^{-4}\), \(3 \times 10^{-4}\), and \(5 \times 10^{-4}\) m\(^2\)KW\(^{-1}\)) to assess the impact on heat transfer and the resulting stress distribution. As expected, the average heat flux, shown in Fig. 9, falls rapidly with increasing interfacial resistance close to the meniscus. This work clearly shows that the interfacial contact resistance (due to crystallisation of the flux) is the dominant factor in the horizontal heat transfer when there is no air gap (due to shell shrinkage) present.

In the upper mould the liquid flux will flow into and fill any gap formed by shell shrinkage. However, in the lower mould if the shell temperature is lower than the break temperature of the flux there will be no liquid available to fill the gap and hence an air gap may form. When present, this air gap becomes the dominant thermal resistance and the effect of mould flux/mould interfacial resistance is diminished. Thus, the mould flux/mould interfacial resistance does not influence the heat transfer appreciably in the lower parts of the mould (refer to Fig. 9). Since the interfacial thermal resistance has been shown to increase with increasing crystallinity (Cho et al.\(^{33}\)), it can be inferred that heat flux decreases with increasing crystallinity as is the widely accepted observation. The hoop stresses predicted for three different interfacial resistances as a function of distance from the surface at an off-corner location 100 mm below the meniscus are shown in Fig. 10. The hoop stress increases with decreasing contact resistance due to the decrease in surface temperature as shown in Fig. 11. The tensile stress is much higher for the contact resistance of \(3 \times 10^{-4}\) m\(^2\)KW\(^{-1}\) due to the occurrence of \(\delta \rightarrow \gamma\) transformation. This may cause cracks to initiate on the surface close to the meniscus.

4.2.2. Break Temperature

The effects of break temperature (\(T_b\)) on the liquid mould flux thickness and the resulting heat flux between the billet and the mould were also investigated. Decreasing the break temperature delays the solidification of the mould flux and results in a thicker liquid flux layer and a thinner solid flux layer. Since the liquid mould flux has a high thermal conductivity, increased heat transfer results from a lower break temperature. A mould flux with a high break temperature has been reported to help reduce the cracking susceptibility of medium carbon steels owing to decreased heat transfer.\(^{44}\) At the mould exit, the maximum hoop stress predicted for a \(T_b\) of 1373 K was 3.17 MPa. The maximum hoop stress decreased by 4% and 8% as the break temperature was increased to 1473 K and 1573 K, respectively. This shows how the model can be useful to optimise flux properties when casting crack sensitive steel such as medium carbon.
4.2.3. Heat Flux at Different Positions around the Strand

The heat flux variations at both the mid-face and corner locations have been compared for different positions in the mould (Figs. 12 and 13). The smoothly varying heat flux at the mid-face suggests that ferro-static pressure prevents air gap formation throughout the mould. In contrast, in the corners the cooler temperatures result in contraction which tends to counter-balance the ferro-static pressure. Thus an air gap forms which subsequently affects the heat flux. The effects of the heat flux variation due to contraction in the corner can be seen in the thermal history shown in Fig. 14. The initial decrease in surface temperature is rapid due to the good thermal contact provided by the liquid mould flux. Once the liquid completely solidifies, the drop in the surface temperature is gradual due to the formation of air gap.

4.2.4. Bulging of the Shell

Faster casting leads to a thinner shell. If the shell thickness at the mould exit decreases it is less likely to withstand the ferro-static pressure and may bulge when the support provided by contact with the mould is removed. Bulging was simulated by removing the support provided by the mould at the mould exit and imposing a heat transfer coefficient of 500 Wm\(^{-2}\) K\(^{-1}\) typical of the spray cooling coefficient.\(^{23}\) Higher break temperature fluxes tend to decrease heat transfer and thereby produce a thinner shell (Fig. 15(a)). The mid-face displacement, presented in Fig. 15(b), is linear in the mould (up to 800 mm) due to the imposed taper and then exhibits a reversal due to bulging below the mould. Simulations of a flux with a 1 373 K break temperature exhibit considerably more bulging than that for a flux with a 1 373 K break temperature. An increased bulge along with higher temperatures may cause internal tensile strain to occur and could cause off-corner longitudinal cracks near the exit of the mould.\(^{45}\)

4.2.5. Lubrication

Maintaining a liquid flux layer in the regions of the mould where there are high contact pressures enhances lubrication helping prevent defects.\(^{46}\) Therefore, the model was used to determine the liquid flux layer thickness for a series of casting speeds where the flux properties were also altered by adjusting the \(T_b\) using Eq. (4) (Fig. 16). Increasing casting speed result in an increased liquid flux thickness due to higher billet surface temperatures and the decrease in \(T_b\). These thick layers are maintained until the end of the casting. Billany \textit{et al.}\(^{46}\) found that star cracking decreased as the relative length of mould undergoing liquid lubrication.
The hoop stress increased as the interfacial thermal resistance decreased (i.e., the heat flux increased).

The maximum hoop stress at the exit decreased with increasing flux film thickness (represented by increasing $T_b$).

The ferro-static pressure tends to hinder the formation of an air gap at mid-face positions whereas in the core the thicker shell tends to counteract the ferro-static pressure resulting in air gap formation.

Mould fluxes with a higher $T_b$ result in a thinner shell and lead to bulging.

The thickness of the liquid film at various positions in the mould was computed for fluxes with optimum break temperatures to see if they provided liquid lubrication throughout the mould.

The maximum stress occurred in the corner when casting with mould fluxes and at the mid-face for oil casting.

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