MOULD RESPONSE AND ITS IMPACT ON BILLET QUALITY

by

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Abstract

In the past three decades, continuous casting has emerged as a dominant steel production technology. Global competition and customer expectations are driving the mini-mills to improve billet quality and increase productivity. At the core of billet casting technology is the water-cooled, oscillating copper mould. Mould interaction with the billet, both thermal and mechanical, governs billet quality and productivity. The heat extraction capability of the billet mould has been well addressed in the literature, but the mechanical response of the mould, also fundamental to the process, remains less studied. Quality issues relating to the casting operation include cracks, shape defects and breakouts. Excessive mould-billet friction can certainly contribute to these defects, in addition to restricting caster productivity. Further, wobbly mould oscillation is believed to contribute to cracks and off-squareness. It is remarkable to note that even though controlling friction is a necessity to the continuous casting process, few attempts have been made to monitor it. The main objectives of this study were: to quantify the mechanical response of the mould with force and kinematic sensors; to evaluate mould-billet binding using mathematical models; and to provide practical recommendations for on-line
A series of five industrial plant trials were conducted using instrumented moulds at two Canadian mini-mills. In addition to logging mould temperature, casting speed and metal level, new sensors were installed and tested to measure mould oscillation and mould-strand friction. Billet samples were obtained and process variables and upsets were recorded for correlation with the logged data. Near the end of this work, a prototype on-line system was tested to record mould oscillation parameters and machine forces.

This study has lead to a quantitative understanding of mould response through measurements of mould oscillation and friction on industrial casting machines. Oscillation monitoring is imperative for billet producers, since the machines were found to deviate from their design specifications. A highlight of this research was the quantification of mould-billet friction forces. Fundamental lubrication behaviour was elucidated with a force sensor, which is an excellent tool for evaluating lubrication and mould oscillation. Further, the force response varied as a function of process variables and upsets. Mathematical modelling of mould-billet binding has shown that the force signal responds mainly to lubrication effectiveness, and not the degree of binding. In the presence of a lubrication upset, however, high friction forces can be measured. When casting with oil lubrication, the friction response appeared to increase with increasing heat extraction, indicating that lubrication, heat transfer and friction are intimately linked. Modelling of binding has also lead to some recommendations for improvement in mould taper design.
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List of Symbols

\( A_b \)\quad \text{cross-sectional area of billet (m}^2\text{)}
\( A_m \)\quad \text{area of steel contact in the mould (m}^2\text{)}
\( a_{\delta c}^T \)\quad \text{lattice parameter of delta iron at temperature } T \text{ and carbon content } C \ (\text{Å})
\( a_{\gamma c}^T \)\quad \text{lattice parameter of gamma iron at temperature } T \text{ and carbon content } C \ (\text{Å})
\( C \)\quad \text{constant (function of carbon content)}
\( C_{pf} \)\quad \text{specific heat of fluid (J kg}^{-1}\text{ K}^{-1}\text{)}
\( C_{pl} \)\quad \text{specific heat of liquid (J kg}^{-1}\text{ K}^{-1}\text{)}
\( C_{pm} \)\quad \text{specific heat of mould (J kg}^{-1}\text{ K}^{-1}\text{)}
\( C_{pw} \)\quad \text{specific heat of water (J kg}^{-1}\text{ K}^{-1}\text{)}
\( C_{p,s} \)\quad \text{specific heat of solid (J kg}^{-1}\text{ K}^{-1}\text{)}
\( c \)\quad \text{the coefficient of sliding friction}
\( D_H \)\quad \text{hydraulic diameter (m)}
\( d_w \)\quad \text{width of water channel gap (m)}
\( E \)\quad \text{elastic modulus (Pa)}
\( F_{\text{cold}} \)\quad \text{cold machine force range (N)}
\[ F_{\text{liquid}} \quad \text{liquid friction or shear stress (N m}^{-2}\text{)} \]
\[ F_{\text{solid}} \quad \text{solid friction (N)} \]
\[ F_{\text{range}} \quad \text{casting force range (N)} \]
\[ f \quad \text{oscillation frequency (Hz)} \]
\[ GF \quad \text{gauge factor for strain gauge} \]
\[ g \quad \text{gravitational acceleration (m s}^{-2}\text{)} \]
\[ H_{fg} \quad \text{latent heat of vaporization (J kg}^{-1}\text{)} \]
\[ \Delta H \quad \text{latent heat of solidification (J kg}^{-1}\text{)} \]
\[ h_a \quad \text{radiant heat transfer coefficient at hot face above} \]
\[ \text{meniscus (W m}^{-2}\text{K}^{-1}\text{)} \]
\[ h_b \quad \text{heat transfer coefficient on billet face (W m}^{-2}\text{K}^{-1}\text{)} \]
\[ h_{fc} \quad \text{forced convection heat transfer coefficient (W m}^{-2}\text{K}^{-1}\text{)} \]
\[ h_m \quad \text{height of liquid steel in the mould (m)} \]
\[ h_w \quad \text{heat transfer coefficient at the mould/cooling water} \]
\[ \text{interface (W m}^{-2}\text{K}^{-1}\text{)} \]
\[ k_f \quad \text{thermal conductivity of fluid (W m}^{-1}\text{K}^{-1}\text{)} \]
\[ k_l \quad \text{thermal conductivity of liquid (W m}^{-1}\text{K}^{-1}\text{)} \]
\[ k_m \quad \text{thermal conductivity of mould (W m}^{-1}\text{K}^{-1}\text{)} \]
\[ k_s \quad \text{thermal conductivity of steel (W m}^{-1}\text{K}^{-1}\text{)} \]
\[ N \quad \text{normal force (N)} \]
\[ n \quad \text{constant (function of temperature)} \]
\[ p \quad \text{water pressure (kPa)} \]
\[ Q \quad \text{constant (K)} \]
\[ Q \quad \text{mould flux consumption (kg m}^{-2}\text{)} \]
\[ q_b \quad \text{boiling heat flux (W m}^{-2}\text{)} \]
\[ q_{fc} \quad \text{forced convection heat flux (W m}^{-2}\text{)} \]
\[ q_{in} \quad \text{heat flux at point of incipient boiling (W m}^{-2}\text{)} \]
\[ q_s \quad \text{heat flux from steel to mould (W m}^{-2}\text{)} \]
\[ q_{tr} \quad \text{heat flux transition between forced convection and nucleate boiling (W m}^{-2}\text{)} \]
\[ S \quad \text{mould stroke (mm)} \]
\[ s \quad \text{constant} \]
\[ \Delta s \quad \text{mould lead (mm)} \]
\[ T \quad \text{temperature (K)} \]
\[ T_0 \quad \text{initial mould temperature (K)} \]
\[ T_a \quad \text{ambient temperature (K)} \]
\[ T_i \quad \text{initial temperature (K)} \]
\[ T_{mp} \quad \text{melting point temperature (K)} \]
\[ T_s \quad \text{temperature of solid (K)} \]
\[ T_{s0} \quad \text{temperature of solid at surface (K)} \]
\[ T_{sat} \quad \text{saturation temperature of water (K)} \]
\[ T_{sh} \quad \text{temperature of superheated steel (K)} \]
\[ T_w \quad \text{water temperature (K)} \]
\[ t \quad \text{time (s)} \]
\( t_N \) negative strip time (s)

\( \bar{V} \) volume flow rate (m\(^3\) s\(^{-1}\))

\( V_f \) velocity of fluid (m s\(^{-1}\))

\( V_{in} \) input excitation voltage (V)

\( V_{out} \) bridge output voltage (V)

\( V_r \) voltage ratio \( \frac{\Delta V_{out}}{V_{in}} \)

\( V_w \) velocity of water (m s\(^{-1}\))

\( V_\delta \) specific volume of delta unit cell (cm\(^3\) g\(^{-1}\))

\( V_\gamma \) specific volume of gamma unit cell (cm\(^3\) g\(^{-1}\))

\( v_r \) relative velocity between mould and shell (m s\(^{-1}\))

\( v_s \) casting speed (mm s\(^{-1}\))

\( W_c \) carbon content of gamma phase (weight pct.)

\( X_c \) carbon content of delta phase (atomic pct.)

\( x \) thickness of liquid film (m)

\( x, y, z \) spacial coordinates

\( u, v, w \) nodal displacements

\( z_{ml} \) metal level (m)

\( \alpha \) mean coefficient of thermal expansion (K\(^{-1}\))

\( \alpha_s \) thermal diffusivity of solid (m\(^2\) s\(^{-1}\))

\( \mu \) viscosity (Pa s)

\( \mu_f \) viscosity of fluid (Pa s)

\( \mu_l \) viscosity of liquid (Pa s)

xxix
\( \mu \) viscosity (Pa s)
\( \nu \) Poisson’s ratio
\( \rho_f \) density of fluid (kg m\(^{-3}\))
\( \rho_l \) density of liquid (kg m\(^{-3}\))
\( \rho_m \) density of mould (kg m\(^{-3}\))
\( \rho_s \) density of steel in the mould (kg m\(^{-3}\))
\( \rho_v \) density of saturated liquid (kg m\(^{-3}\))
\( \rho_w \) density of water (kg m\(^{-3}\))
\( \varepsilon \) strain
\( \varepsilon_{th} \) thermal strain
\( \dot{\varepsilon}_p \) plastic strain rate (s\(^{-1}\))
\( \sigma \) mises stress (MPa)
\( \sigma \) surface tension at liquid/vapour interface (N m\(^{-1}\))
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Chapter 1

Introduction

Continuous casting has experienced enormous growth in the past three decades. In the 1960’s, virtually all steel was produced by the conventional ingot casting method. In 1995, continuous casting represented 75% of global steel production, and 96% of Canada’s steel production [1]. As steel producers began implementing continuous casting, they were faced with the challenges of commissioning a new technology. In 1980, a survey of billet producers indicated that casting practices varied markedly between companies [2]. Although the basic technology of the casting machine was consistent between producers, the operating practices were not. Operating experience and research knowledge gradually filtered through the industry and a follow-up survey in 1994 indicated that operating practices, such as mould oscillation and cooling water parameters, had improved [3]. However, fundamental design parameters such as mould taper still varied significantly between companies.

Global competition and customer expectations are driving the mini-
mills to improve billet quality and increase productivity. Quality issues relating to the casting operation include cracks, shape defects and breakouts. Cracks form because of thermal and/or mechanical stresses, acting internally or on the billet surface. Surface and shape defects reduce heat transfer in the mould and provide sites for shell breakouts. Further, cracking is often associated with the formation of shape defects. Poor billet quality results in reconditioning of the billets or rejection of the billets and/or rolled product. Surface cracks oxidize and create defects in the rolled product. Billets with surface cracks are subject to costly conditioning, such as scarifying, shot blasting and manual grinding to remove the cracks. Subsurface cracks may be a problem, but studies have shown that hot reductions greater than 6:1 effectively close midway cracks [4]. Surface defects like transverse depressions, in addition to being common sites for cracks, may fold during hot rolling operations and create a seam in the product. Breakouts are catastrophic shell ruptures that spill liquid steel. They are very dangerous, costly and result in excessive machine down-time.

The mould is the focal point of the casting process. The impact of the thermal and mechanical response of the mould on billet quality cannot be over-emphasized. The heat extraction capability of billet moulds has been well addressed by Brimacombe, Samarasekera and co-workers, e.g. [5, 6, 7], but the mechanical mould response has not been studied in the same detail. Moulds are tapered inwards to compensate for billet shrinkage since the air gap between the mould and shell significantly reduces heat transfer. Unfortunately, the large range of billet tapers currently employed in industry [3] indicates
that mould design is not yet fully understood. Further, the mould distorts when heated [8], and the amount of distortion can be impacted by operating practice. An inadequately tapered mould results in low heat extraction and a thin shell at the mould exit, and contributes to defects like shell bulging and off-corner internal cracks. Excessively tapered moulds cause increased mould wear and increased mechanical forces on the shell, which may lead to transverse depressions and cracks. In extreme cases, the billet may jam in the mould.

In addition to heat extraction, mould oscillation and lubrication are fundamental to continuous casting. Mould-shell friction must be minimized to eliminate shell sticking, tearing and cracking. The oscillating mould was employed in early research on continuous casting because it reduced friction and sticking, and allowed for longer casting runs than a stationary mould. Mould oscillation parameters such as stroke and negative-strip time have been empirically determined to minimize sticking and oscillation mark depth. The issue of casting machine maintenance has recently been raised in the literature, e.g. [3, 9]; poor machine alignment is believed to contribute to cracking and off-squareness [3]. At present, the industry lacks machine tolerances for wobbly oscillation. Producers have claimed to eliminate a cracking problem by servicing or replacing a machine, without knowing the cause of the problem. Further, machine displacements are often measured statically with a dial gauge, rather than at operating speed and under casting conditions.

It is remarkable to note that even though controlling friction is a ne-
cessity to the continuous casting process, few attempts have been made to monitor it [10]. The work of Brendzy et al. [11] is possibly the only published work measuring friction on an industrial billet machine. Although this work has provided significant insight into mould-strand interaction, the forces measured were only qualitative, owing to the installation of the force sensors. Most attempts to measure mould-strand friction cited in the literature were conducted on experimental slab casting machines, with the most common objective to evaluate mould flux lubricants. High friction is expected under conditions of poor lubrication, excessively tapered or distorted moulds and sub-optimal mould oscillation parameters. In addition to forming transverse cracks, high friction is also believed to accompany the formation of transverse depression defects [11].

Recently, the influence of the meniscus and process transients on billet quality has been recognized [12, 13]. Particularly problematic is the practice of open stream pouring with oil lubrication [12], commonly used with billet casters. Rough streams and/or a turbulent meniscus create variations in shell growth and mould lubrication which impact billet quality. Metal level changes have been linked to the formation of transverse depressions [14, 15]. The use of submerged entry nozzles when casting with mould fluxes can improve metal level stability, but this practice is more costly than oil casting. Since most billet casters operate without liquid steel flow rate control, metal level and casting speed are prone to being transient, owing to changes in the steel flow rate from the tundish. Thus the nature of the casting speed control system, in
the absence of flow control, may contribute to billet defects. As knowledge of
the transient behaviour of the process unfolds, the need for on-line analyses of
process information to improve billet quality becomes apparent. The concept
of an “Intelligent Mould” on-line system has been presented by Brimacombe
[16]. This system would use the rule-based reasoning of an expert system to
interpret real-time sensor signals in its knowledge base of process information.
The power of such a system lies in its ability to interpret information from
multiple sources to diagnose process upsets.

This research was undertaken to study the mechanical response of the
mould in the continuous billet casting process. Mould oscillation and mould-
billet friction were measured in five industrial plant trials at two Canadian
mini-mills. The measurement of friction and its subsequent analysis were new
aspects of this research. Sensor signals were analyzed with two objectives in
mind: firstly, to elucidate process behaviour and improve the understanding of
industrial casting machines and secondly, to develop simple tools for an on-line
monitoring system to report on process quality. The UBC casting group is well
experienced in obtaining mould temperature profiles, which were also used in
this work. Existing mathematical models were utilized and new models were
developed to investigate mould-billet binding and mould taper.

This study has lead to a *quantitative* understanding of mould response
through measurements of mould oscillation and friction on industrial casting
machines. Oscillation monitoring is imperative for billet producers, since most
machines were found to deviate from their design specifications. Fundamen-
tal lubrication behaviour was elucidated with a friction sensor, which is an excellent tool for evaluating lubrication and mould oscillation. Mathematical modelling of mould-billet binding has lead to further understanding of the response of the force sensor as well as some recommendations for improvement in mould taper design.
Chapter 2

Literature Review

Mould response significantly impacts the quality of continuously cast steel billets. Although a reasonable field of work exists involving thermomechanical mould behaviour and billet quality, very little work has been published on mould-billet friction. This chapter presents a review of knowledge available in the literature pertinent to this project. Where little information was available in the field of billet casting, scoping knowledge was obtained from the slab casting literature.

2.1 Description of the Mould Assembly

Continuous billet casting moulds are typically square copper tubes, approximately 0.8 m in length. Internal mould dimensions range from 114 to 254 mm, with the mould wall thickness varying from 9.5 to 19 mm. Figure 2.1 illustrates the mould assembly [17]. The mould is installed in a steel jacket which supports the mould and contains the cooling water. The mould is se-
Figure 2.1: Schematic of a typical billet mould assembly.
cured by plates that fit into slots cut in the outer surface of the tube, near the mould top. On top of the mould, plates secure the assembly, seal the cooling water channel and serve as part of the oil distribution system. Inside the steel jacket, water baffles are typically placed within 4 mm of the mould to facilitate a high cooling water velocity [18]. Cooling water enters between the mould and water baffle at the bottom of the mould. At the top of the assembly, the water is routed to the back of the water baffle, before it exits to the cooling water system. The mould assembly can be installed quickly on the oscillator table, to minimize down-time between mould changes.

Billet moulds are typically used for several hundred heats; an average heat lasts for 45 to 90 minutes. The moulds are then replaced because of mould wear, distortion or damage. Poor lubrication or mould taper design causes mould wear, usually at the bottom of the mould where the steel shell gouges the mould. Moulds also permanently distort with use, indicating that the thermal stresses in the mould sometimes exceed the elastic limit of the copper. Studies have shown that the mould taper slowly changes with mould use [19].

2.2 Heat Transfer in the Mould

Heat is extracted, from the liquid steel to the mould cooling water, through the following path [5]:

- Convection in the liquid steel
• Conduction through the solid steel shell

• Conduction and radiation through the mould-shell gap

• Conduction through the copper mould

• Convection to the cooling water

As the billet solidifies and the shell grows, heat transfer varies across the mould face, both vertically and horizontally. The mould-shell gap is the largest thermal resistance to heat transfer, particularly near the meniscus. Lower in the mould, the thermal resistance of the solid steel shell may provide a comparable thermal resistance [5]. Thus heat extraction lower in the mould reduces because of the increased total thermal resistance. The corners solidify more quickly because of two-dimensional cooling from two faces of the mould. The corners then shrink away from the mould wall more quickly than the midface, contributing further to non-uniform heat transfer. Based on the examination of solidification bands, the influence of non-uniform corner cooling extends about 20 mm from the mould corner [20].

Mathematical models have been successfully used to quantify heat transfer between the billet and the mould. Mould heat transfer coefficients have been estimated based on changes in mould water cooling temperature; subsequently, the heat transfer coefficients were defined based on dwell time in the mould [21]. In later studies, thermocouples were installed in industrial moulds to obtain in-situ mould temperature [22]. Axial heat flux profiles were
then calculated from the thermocouple data, using finite-difference models. Once mould heat transfer was quantified, the corresponding heat flux could be applied to a billet solidification model. Finite difference models were again appropriate for estimating the temperature distribution in the shell and shell thickness as a function of position in the mould [21].

2.2.1 Carbon Content

Singh and Blazek demonstrated a heat transfer dependence on carbon content, as shown in Figure 2.2, using a laboratory continuous caster [23]. Overall heat transfer was a minimum for 0.1 pct. carbon steels and was relatively constant for grades above 0.3 pct. carbon. The surface of billets containing approximately 0.1 pct. carbon are rough, characterized by wrinkles and indentations. The low heat flux associated with casting these steels was attributed to this rough surface.

Grill and Brimacombe [24] studied heat transfer on operating continuous casting machines. The minimum heat transfer at 0.1 pct. carbon was confirmed on the industrial machines. A small decrease in heat extraction was also seen with high carbon steels (0.85 pct. carbon) compared with medium carbon grades. Grill and Brimacombe correlated the 0.1 pct. carbon heat transfer minimum with the lower limit of the peritectic phase change, where the $\delta$ to $\gamma$ solid state shrinkage is the greatest. The $\delta$ to $\gamma$ phase change is associated with a linear shrinkage of 0.38 pct. [24]. The peritectic phase transformation has the greatest effect on grades in the range of 0.08 - 0.14 pct.
carbon [18], where the billet surface is the roughest.

The wrinkled surface is likely associated with a phase change instability of shrinking, gap formation and reheating. Grill and Brimacombe [24] presented the following mechanism:

1. The solidifying shell, in contact with the mould, cools quickly and transforms from the δ to γ phase.

2. The outer surface shrinks more than the inner surface, which is still γ phase, causing inward bending of the shell.

3. The surface in the gap reheats because of reduced heat transfer across the gap, shell strength reduces locally, and ferrostatic pressure deforms the shell back towards the mould wall. The resulting surface is wrinkled,
and retains its shape as the shell cools and strengthens.

4. The mechanism repeats continuously.

2.2.2 Influence of Process Variables on Heat Transfer

Mould Taper

As the steel billet cools and shrinks, an air gap forms between the mould and shell. As previously mentioned, the air gap usually represents the largest barrier to heat extraction. Moulds are therefore tapered to minimize the gap and improve heat transfer [20]. In early research on continuous billet casting, Aketa and Ushijima showed an increased heat extraction with increased mould taper [25]. Excessive taper, in this case 2.6 pct. m\(^{-1}\), caused the billet to bind in the mould. Moderate tapers near 0.9 pct. m\(^{-1}\) have increased heat transfer by 8 - 15 pct. [26]. Evteev reported that mould taper increased heat transfer significantly across the midface, but only had a slight effect on heat transfer near the corners [27]. Mould taper design will be discussed in more detail in Section 2.4.

Lubricant Type

Generally, heat transfer is believed to be lower when casting with mould fluxes than oil because of the additional thermal resistance of the flux film. Using an experimental stationary caster, Singh and Blazek obtained heat flux profiles for both oil and powder lubrication [28]. When casting a 0.40 pct. carbon
steel, the oil lubricated billet clearly exhibited higher heat transfer. When casting a peritectic steel (0.10 pct. carbon), Singh and Blazek reported higher heat transfer near the meniscus and lower heat transfer in the lower region of the mould with fluxes [28], as shown in Figure 2.3. The local gaps in the rough surface of peritectic steels were filled with mould flux, which facilitated improved heat transfer near the meniscus.

Klipov et al. measured heat flux profiles on a 180 x 500 mm slab caster when casting chromium-nickel steels [29]. In this case, the heat flux near the meniscus was lower with the mould flux, but higher in the lower portion of
the mould. The differences in results between the researchers is likely due to process parameters such as taper and grade, in addition to the lubrication.

When casting with oil lubrication, many gases exist in mould-shell gap in addition to nitrogen and oxygen, including shrouding gases and components of combustion. The composition of this atmosphere is believed to impact heat transfer through the mould-shell gap. The pyrolysis of oil at the meniscus creates hydrogen [19], which has a thermal conductivity seven times greater than air [30]. Chandra et al. calculated heat flux profiles when casting with oil flow rates of 20, 30 and 40 ml min$^{-1}$ [6]. The difference in heat transfer between the tests were small, and not enough to affect the shell thickness at the mould exit.

Casting Speed

Early research on continuous casting indicated increasing heat transfer into the mould with increasing casting speed [31]. This was confirmed in later studies [28, 32], where the axial heat flux profile of the mould simply increased with increasing casting speed, as shown in Figure 2.4 [33]. Singh and Blazek [28], noted that the increase of heat flux with speed was much less for a peritectic steel (0.10 pct. carbon) than a medium carbon (0.40 pct. carbon) grade. Although the heat flux increases with speed, the specific heat extraction (J kg$^{-1}$) decreases, based on the work of Singh and Blazek [28].
2.3 Thermomechanical Behaviour of the Mould

The temperature distribution of billet moulds was originally investigated by Samarasekera and Brimacombe [34] using a two-dimensional axial model. Heat flux was applied to the model hot face using a relationship developed by Savage and Prichard [31]; a heat transfer coefficient, based on cooling water parameters, was applied to the cold face. An important conclusion of this work was the sensitivity of heat extraction to the cooling water properties, particularly the cooling water velocity. The calculated cold face temperature of billet moulds was found to be approximately 140°C [34]. If the mould operated much hotter than this, boiling might commence, reducing heat transfer. In a later study,
Samarasekera et al. [22], used thermocouples embedded in the mould wall to obtain operating temperature profiles of industrial billet moulds. The mould heat transfer model was then used to estimate the heat flux profile.

Under casting conditions, the mould distorts because of differential thermal expansion. Mould distortion was investigated by Samarasekera et al. using a three-dimensional, elastic-plastic, finite element model [17]. The mould was found to bulge outwards with a maximum deflection of 0.1 - 0.3 mm, about 90 mm below the meniscus. This behaviour significantly impacts the mould taper. Near the meniscus, the mould taper may invert, forming a “negative taper”. In contrast, slab casting moulds are typically made with copper plates reinforced with steel backing plates, which are more rigid and likely less prone to acute mould distortion.

One can infer from this work that the cooling water system must be of high quality. Mould oxidation and scaling would be very detrimental to heat extraction, and may contribute to permanent mould distortion.

2.4 Mould Taper

In the first published study of billet mould taper, Dippenaar et al. [20] evaluated mould tapers by estimating billet shrinkage. The two-dimensional transverse heat transfer model originally presented by Brimacombe [21] was used to calculate the temperature field in the solidifying billet. For a given axial position in the mould, the billet dimension was assumed to the the average length of the rows of solidified nodes in the model. It was assumed that only austenite
was present, and the thermal expansion coefficient was constant at $2.3 \times 10^{-6}$ K$^{-1}$. The researchers concluded that large gaps formed in the low mould region of single-tapered moulds [20]. Double- and multiple-tapered moulds were believed to be better mould designs for heat extraction.

In later work, Chandra et al. [6] modified the model to include a thermal expansion coefficient which was a function of temperature and carbon content. Figure 2.5 shows heat flux profiles for a parabolic and a single-tapered mould [6]. The parabolic mould led to uniform heat extraction along the mould length, while the single-tapered mould had significantly higher heat extraction near the meniscus. This was surprising, since the parabolic mould had a steeper taper in the upper portion of the mould. The authors postulated that the distorted single-tapered mould mechanically interacted more strongly than the parabolic mould on the shell near the meniscus.

The work of Chandra et al. [6] was particularly important when calculating the shrinkage of peritectic steels. Although peritectic steels experience a large shrinkage due to the $\delta$ to $\gamma$ phase transformation, the low heat flux caused by the rough surface is detrimental to shell growth and further billet shrinkage. The result is that low carbon grades contract less while in the mould and require a shallower taper. If cast through a mould designed for higher carbon steels, a low carbon grade may bind in the mould, contributing to billet defects [5, 6].

Thermal stress analysis of the solidifying shell has been conducted for slab casting. Two dimensional transverse models have been used to evaluate
the sensitivity of process parameters such as casting speed and mould taper [35, 36, 37].

2.5 Process Control

2.5.1 Metal Level and Casting Speed

Metal level is commonly detected by a radioactive metal level sensor. A γ-ray radioactive source and receiver are set across the mould assembly. When the metal level is low, the received signal level is high; when the metal level is high, the signal obtained by the receiver is attenuated. Within a certain range, the received signal is a linear function of metal level. Radioactive sensors are reported to be accurate to ±5 mm [38]. In slab casting, other sensors types
such as the eddy current probe and electromagnetic cassette are commonly used [38].

Casting speed is typically regulated by the plant control system, based on a metal level set point. As the metal level rises, the casting speed increases to restore the metal level and vice-versa. On billet machines, liquid steel flow rate is not controlled, and is governed by the size of the metering nozzle installed in the tundish bed.

2.5.2 Tundish Stream

Off-centre or ropey streams create turbulence at the meniscus, and contribute to non-uniform shell growth and variable lubrication. Stream quality can be influenced by nozzle blockages, nozzle wear and fluid flow in the tundish. Rough tundish streams also entrain gas which rises to the surface in the mould, contributing further to an unstable meniscus [39, 40, 41]. Tundish design also contributes to stream quality, since turbulent flow in the tundish will initiate a turbulent stream. Tundishs may be modified with dams and weirs in order to reduce stagnant zones or turbulence near a metering nozzle [42]. The length of the open stream (i.e. distance between the nozzle and meniscus) also impacts the meniscus stability since the magnitude of stream disturbances increase with time [39].

Stream erosion of the solidifying shell is a natural concern. Of particular concern is centring of the nozzle [43, 44, 12]. Poor alignment of the stream can cause non-uniform shell growth [44] and cracking in low carbon grades
Figure 2.6: Shell uniformity for open stream and submerged entry nozzle casting.

[43]. Figure 2.6 presents experimental data of shell non-uniformity for both oil casting with an open stream and powder casting with submerged entry nozzles [44]. The powder cast billets clearly had more uniform shell growth.

2.6 Mould Oscillation

Oscillators are simple machines which reciprocate the billet mould to help prevent the steel from sticking to the mould wall. The mould is usually oscillated in a sinusoidal mode, with typical stroke and oscillation frequency parameters being 10 mm and 2 Hz respectively. A machine may be actuated hydraulically, or by an electric motor driving an eccentric cam.

Negative-strip time has been well established as an operating parameter fundamental to continuous casting. Negative-strip time is defined as the
time period during which the mould moves downward faster than the strand withdrawal rate. Assuming a sinusoidal velocity profile, negative-strip time can be calculated according to Equation 2.1 [3].

\[ t_N = \frac{\arccos \left( \frac{\frac{v_s}{S}}{\pi f J} \right)}{\pi f} \]  

(2.1)

Mould lead, defined as the distance the mould moves past the shell during negative strip, can be calculated using Equation 2.2 [3].

\[ \Delta s = S \sin(\pi f t_N) - v_s t_N \]  

(2.2)

For billet casters, the recommended mould lead and negative-strip time values are 3 - 4 mm and 0.12 - 0.15 seconds respectively [3]. Casting machines with negative-strip times below 0.1 seconds and mould leads below 2 - 3 mm are susceptible to mould-shell sticking, particularly if the meniscus is fluctuating [3]. Mould leads greater than 5 mm may contribute to deeper, non-uniform oscillation marks [3].

Casting machine maintenance is currently an active topic among steel producers. Several commercial mould oscillation monitoring systems have been noted in the literature [9, 45, 46]. Typical output from these systems includes oscillation frequency, horizontal and vertical mould movement, and casting parameters such as negative-strip time. The MO Tektor system [46] was used to detect bearing, guide system and eccentric cam defects based on non-uniform oscillation curves. British Steel developed an on-line mould oscillation monitoring system for slab casters using displacement sensors [47]. The system was
implemented to provide early warning of oscillation problems.

The Kiss Technologies system uses accelerometers to measure mould movement [9]. Through signal processing, velocity and displacement are calculated from the acceleration signal. The stroke measurement was validated using LVDT (linear variable differential transformer) displacement sensors and was shown to be accurate only within 5 pct.

2.7 Lubrication

In addition to mould oscillation, lubrication is fundamental to continuous casting. A lubricant is required to prevent the solidifying shell from sticking to the mould wall. Oil and mould fluxes are used as lubricants, with oil being the most common among billet producers.

2.7.1 Oil

Oil is pumped from a reservoir to a distribution system at the top of the mould, where it weeps down the mould faces. Some oil lubrication systems were found to be of poor design, since the oil distribution was non-uniform [48]. The oscillating mould likely improves oil infiltration between the mould and shell during negative-strip time [10]. Oil is usually selected based on cost, how cleanly it burns and local plant experience. Certainly, much of the oil vaporizes or pyrolyzes at the meniscus due to interaction with the hot mould and solidifying steel [12, 11, 49]. The remaining oil components, likely heavier hydrocarbons, lubricate the mould-billet interface below the meniscus. Oil
properties pertinent to lubricant selection include viscosity, flash point and boiling temperature [11, 49]. Oil which is too viscous may not flow down the mould wall uniformly. Oils with low flash points and/or boiling temperatures are less likely to survive the meniscus environment and provide any effective lubrication.

2.7.2 Mould Fluxes

Mould powders are synthetic slags used for lubrication in continuous casting machines. Powder flux is continuously added on top of the meniscus where it forms a slag layer. The slag forms several sublayers, ranging from unreacted powder to liquid flux as shown in Figure 2.7 [10]. A solid slag rim forms on and oscillates with the copper mould. The liquid flux layer is consumed as the strand is withdrawn, and provides lubrication between the shell and mould.

In addition to lubrication, mould fluxes are also used to [50]:

- Control heat transfer
- Thermally insulate the meniscus
- Protect the liquid steel from oxidizing
- Absorb inclusions from the liquid steel

Mould flux behavior is complex and depends on the chemical composition of the flux, the in-situ temperature distribution and mould oscillation parameters. Figure 2.8 illustrates how mould flux consumption increases with
Figure 2.7: Schematic of mould flux in the mould.
Figure 2.8: Mould flux consumption as a function of mould oscillation parameters.

A crack will form when a tensile force generates a strain which exceeds the strain-to-fracture of the steel [51]. The crack will form perpendicular to the

2.8 Billet Quality

2.8.1 Crack Formation

A crack will form when a tensile force generates a strain which exceeds the strain-to-fracture of the steel [51]. The crack will form perpendicular to the
tensile force. Virtually all surface cracks form in the mould [51, 52]. Star cracks are an exception to this guideline, and usually form in the spray zone as a result of copper pick-up in the mould [52]. Transverse surface cracks may form due to unbending [52], but are likely initiated in the mould. Internal cracks may form in or below the mould.

In the mould, axial stresses are imposed on the shell from mould-strand interaction; the shell is in tension during the upstroke and compression during the downstroke. Below the mould bending stresses are generated in the straightener [52]. Thermal stresses in the mould are generally tensile at the surface and compressive at the solidification front [52]. Thermal stresses are complex however, and depend on heat transfer and steel grade. Reheating at the mould exit may cause tensile stress at the solidification front, forming midway cracks [52]. If the shell bulges due to ferrostatic pressure, a transverse tensile force is created [53].

In billet casting, virtually all cracks form in the zone of low ductility [51], within about 70° C of the solidus temperature [54]. This mechanism, also called “hot tearing”, is caused by solute rich liquid between dendrites. The effect is worsened with increasing concentrations of sulphur and phosphorus. Since these cracks form near the solidus temperature, the depth of a subsurface crack is a reasonable estimate of the shell thickness when the crack formed [51].
2.8.2 Oscillation Marks

Oscillation marks are fine transverse shell depressions which form due to shell interaction with the oscillating mould. Oscillation marks are typically a fraction of a millimeter wide and deep, extending around the billet perpendicular to the casting direction. The marks are clearly related to mould oscillation, since their spacing is equal to casting speed divided by oscillation frequency. Exact mechanisms for oscillation mark formation vary depending on the study, but they most likely form during negative-strip time [11]. Non-uniform oscillation marks may contribute to the formation of other defects such as rhomboidity and off-corner internal cracks [55]. Transverse cracks often form at the base of deep oscillation marks. The following operating practice should be adopted to promote the formation of uniform oscillation marks [55]:

- Low negative-strip time
- Reduce superheat
- High meniscus taper
- Four-sided mould constraint
- High cooling water velocity

Oscillation marks tend to be deeper when casting with mould fluxes. Mould oscillation parameters, flux viscosity and consumption all tend to impact the size of oscillation marks [50]. The oscillating mould creates a transient
pressure regime in the liquid flux, which contributes to the formation of oscillation marks [56].

2.8.3 Rhomboidity

Rhomboidity, or off-squareness, is usually quantified by the difference in the billet diagonals. Rhomboidity exceeding approximately 6 mm may be considered serious. Rhomboidity has been associated with non-uniform heat transfer in the mould, commencing at the meniscus. Once the off-squareness has been initiated in the mould, it can be exacerbated by non-uniform spray-cooling.

Poor mould-strand alignment and wobbly oscillation were believed to contribute to the non-uniform heat extraction [57]. Samarasekera and Brimacombe proposed that rhomboidity could be initiated by dimensional instability of the mould tube [57], caused by intermittent boiling on the mould cold face and/or non-uniform mould constraints. Further, the higher heat flux associated with medium and high carbon steels indicates that boiling, and rhomboidity, would be more severe in these grades. Subsurface cracks may be observed adjacent to the obtuse corners of the billet [57]. These cracks usually form 2 - 4 mm below the surface, where the shell is locally in tension as shown in Figure 2.9.

In a later study by Bommaraju et al., deeper oscillation marks were often found on the obtuse corners of off-square billets [58]. Since deep oscillation marks reduce heat transfer, the shell would be thinner at these corners. When the shell emerged from the mould, the billet may cool and shrink
Figure 2.9: Schematic showing a billet with varying shell thickness distorting further into an off-square shape in the spray cooling zone.
non-uniformly, creating off-squareness in the spray zone. Kumar reported that rhomboidity was worse in the medium carbon grades (0.14 to 0.45 pct. carbon) [13]. This was attributed to the fact that the combination of heat extraction and freezing range of the steel lead to thicker near-meniscus shells in these grades. Kumar also noted that rhomboidity could be initiated by metal level fluctuations, which contribute to non-uniform heat transfer [13].

2.8.4 Shell Bulging and Off-Corner Internal Cracks

Inadequate mould taper (i.e. shallow single-tapered moulds) gives rise to excessive mould-shell gaps in the lower portion of the mould [20]. Ferrostatic pressure may cause the shell to bulge slightly in these cases, until the shell midface impinges on the mould. The hinging action about the corner creates a tensile strain at the solidification front forming the off-corner internal crack [58], as illustrated in Figure 2.10 [51]. This effect is more likely to occur if shell thickness has been reduced in the off-corner region due to deep oscillation marks which have reduced heat transfer. Cracks which are within 8 mm of the surface likely formed in the mould; cracks deeper than 8 mm may have formed due to bulging below the mould [51].

2.8.5 Laps and Bleeds

Laps and bleeds are surface defects which form very near the meniscus, and tend to be most common on high carbon billets. A lap is characterized by a meniscus shell hook and subsequent overflow. A bleed is a shell tear where liq-
uid steel has filled in the ruptured shell. Kumar et al. report that these defects are initiated by metal level fluctuations coupled with poor lubrication [12]. A “hot mould” practice (i.e. when the hot face temperature of the mould in near the boiling temperature of the oil) is believed to exacerbate the problem.

2.8.6 Transverse Cracks

Transverse cracks can form during straightening if the billet surface temperature is between 700 and 900°C, a region of low ductility [52]. However, casting speeds are often too high in billet casting for this to occur [18], thus transverse cracks are believed to form in the mould. Mechanical forces due to binding, sticking or poor lubrication are believed to initiate transverse cracks [51]. Knights et al. [43] reported transverse crack severity was sensitive to stream alignment in the mould. Transverse cracking tends to be worse on steel grades
with less that 0.25 pct. carbon.

2.8.7 Transverse Depressions

Mould-Billet Binding - Mechanism 1

If the shell sticks or binds in the mould, axial withdrawal forces place the shell in tension. The shell may plastically deform and neck like a tensile test specimen as shown in Figure 2.11 [5]. As the surface of the shell deforms, the strain may form a transverse crack at the solidification front in the zone of low ductility. Assuming that the crack does not propagate into the ductile portion of the shell, the depth of the crack from the surface is indicative of the shell thickness when the depression formed [5].

Depressions in Oil Lubricated Billets - Mechanism 2

Samarasekera et al. detected depressions near the meniscus using thermocouples embedded in the mould wall [14]. The thermocouples responded with a local drop in temperature as the depression moved down the mould due to reduced heat transfer as a function of the increased mould-shell gap. In nearly all cases, the depression detection was preceded by a metal level rise noted by a thermocouple above the meniscus. Depressions were clearly forming at or very near the meniscus based on thermocouple response.

Data obtained in a previous UBC study [11] indicated that there may be a relationship between oil flow rate and depression shape or formation. The lubricating oil was postulated to influence depression formation in conjunction
Figure 2.11: Transverse depression formation by binding - Mechanism 1.

Figure 2.12: Transverse depression formation from oil vapour - Mechanism 2.
with a rise in metal level. Oil may become trapped between the steel skin and the mould given an excessive oil flow rate, downward mould movement during negative-strip time and a rising metal level. The authors argued that vaporizing oil may apply enough pressure to the steel skin at the meniscus to form a depression, shown schematically in Figure 2.12. Early research of slab casting with oil lubrication noted depression formation with variations of casting speed and metal level [59].

Cracks were observed 8 mm below the billet surface in the Samarasekera et al. study [14]. Although the depression was formed near the meniscus, the crack was formed lower in the mould below a point of binding. Tensile strains would be greatest at depression sites because the depression would be hot and thin relative to the adjacent shell. Observations by Lorento [14] indicate a critical casting speed, for a given billet size, above which depressions do not form.

**Depressions in Powder Cast Blooms - Mechanism 3**

Jenkins et al. [15] studied transverse depressions in 0.06 pct. carbon blooms with a thermocouple instrumented mould. Depressions were observed on all faces of the blooms and ranged from 1 - 4 mm in depth. Depressions were accompanied with longitudinal scrape marks, referred to as “glaciation marks”, over a 10 to 20 cm region. Cross-section samples of the depressions indicated solidification bands close to the surface under depressions; indicating a thinner shell as a result of reduced heat transfer. Consistent with the Samarasekera
et al. study [14], the formation of a depression was preceded by a rise in metal level. The depressions were determined to be slag filled rather than air filled, based on heat flux calculations. These depressions were postulated to form by capturing the slag rim as the metal level increased. If the metal level rose too quickly for the slag rim to melt, a depression would be formed as the shell solidified around the slag rim. The glaciation marks were theorized to form near the meniscus as lubrication by the slag was interrupted when the rim was captured in the depression. Bloom quality increased when a new metal level control system was installed at the subject facility.

**Thermal Distortion - Mechanism 4**

Thomas and Zhu modelled the impact of metal level fluctuations on the solidifying shell [60]. A two-dimensional, axial, thermal-stress model was
used for the investigation. The metal level was simulated to drop 30 mm for 0.6 seconds, then was restored to a level 20 mm higher. During the drop, the exposed shell was assumed to air cool. Figure 2.13 illustrates the shell distortion during the simulated metal level fluctuation. After the metal level drop, the shell had distorted inwards 0.45 mm. This was attributed to the inner face of the shell cooling in air and bending inwards, toward the centre of the billet. After the metal level rise, the shell had distorted inwards to 1.65 mm. The further distortion was caused by the liquid reheating the existing shell, causing expansion, plus the contraction of the newly solidified layers on the liquid side.

Other

In slab casting, a depression mechanism called the “plastic hinge effect” has been proposed [59, 61]. The depression is postulated to form by a local overcooling of the strand.

Sensitivity of Steel Grade on Depression Formation

Transverse depressions tend to form on low carbon [11, 6] and boron-alloyed steels [14]. In the binding mechanism, low carbon grades exhibit lower heat transfer and were believed to bind in the mould because of reduced shrinkage. For mechanisms which form depressions near the meniscus, the high temperature mechanical properties of these grades may be an influencing factor [14, 60]. It is well known that low carbon steels have a short freezing range [62], and
form a thicker solid shell near the meniscus than high carbon steels. Further, minimum segregation of sulphur and phosphorus near 0.10 pct. carbon effectively increases the high temperature strength of these steels [63]. Thus, regardless of the mechanism, low carbon steels are more likely to form and retain the shape of a depression. The high temperature behaviour of the boron steels has yet to be fully quantified. It has been argued that the stability of TiN$^1$ at steelmaking temperatures may increase the high temperature strength of these steels [14].

2.9 Friction Monitoring

Mould-strand friction has been monitored to evaluate lubrication and detect break-outs in slab casters. Most research has been conducted on experimental slab casting machines, and very little work has been reported on industrial billet casting machines.

2.9.1 Strain Gauge Force Sensors

In early research on continuous casting, strain gauge bearings were used to measure withdrawal forces caused by different mould materials [64]. The impact of oscillator condition on withdrawal force was an important conclusion of this work. Exact alignment of the mould and strand was required to minimize withdrawal forces. In addition, mould oscillation was required to be robust,

$^1$Titanium is commonly alloyed with boron to prevent boron from being consumed as BN.
and not a function of the oscillator mechanism.

A strain gauge was used on an experimental casting machine to study lubrication between the mould and shell [65]. The strain gauge was installed on a pull rod used to withdraw the shell. In this study, the friction force was found to increase with reduced mould flux thickness.

Strain gauges have been installed on the drive arm of a slab caster [66]. The signal was processed into a constant force plus an oscillating force. The oscillating force was noted to increase by 10 pct. during sticker breakouts. A surprising result of this study was that the force response did not change with casting powder or the width of the slab.

2.9.2 Load Cells

The use of load cells to measure mould-strand friction in billet machines has been described by Brendzy et al. in previous work at UBC [11]. Compressive load buttons were installed between the removable mould jacket flange and the oscillator table. Figure 2.14 illustrates a typical load cell response. The load was characterized during the upstroke by a compressive maximum plateau with numerous small peaks. The downstroke load decreased smoothly and increased, centred about negative-strip time. The friction-position relationship was also reported by Saucedo and Blazek, from research on a pilot caster [67].

Brendzy et al. reported higher load cell forces when casting grades with transverse depressions and transverse crack defects. Transverse depressions were noted on most of the hypo-peritectic (0.035 - 0.05 pct. carbon) and some
Figure 2.14: Typical load cell response as reported by Brendzy et al..

of the peritectic (0.10 pct. carbon) billets. The depressions were believed to be caused by mould-billet binding.

Load cells have also been installed in slab casting machines [68, 69, 70, 71, 72, 73]. In most cases, the research was directed at elucidating the behaviour of mould fluxes.

2.9.3 Accelerometer Based Systems

The use of an accelerometer to measure mould friction was proposed with the ML Tektor (mould lubrication detector) system [46, 74, 75, 76, 77] for use with slab and bloom casters. The accelerometer was mounted on the mould assembly and connected to an electronic processing unit which was not
described. The processed signal output was reported to be directly related to mould-strand friction and was reported as "percent friction", although a quantitative description of the signal was not given [46]. The ML Tektor signal was reported to vary with casting conditions and was presented as a tool for optimizing casting parameters [46, 75]. The ML Tektor signal was claimed to increase during the formation of transverse cracks [46]. Longitudinal cracks in peritectic steels were also claimed to form in excessively high and excessively low friction signal environments [46], although a mechanism for their formation was not given.

An accelerometer was used to measure friction on a billet casting machine by van der Stel et al. [49]. Friction, in the form of a slip-stick mechanism, was reported to be seen as amplitude peaks in the signal.

The function of accelerometer based friction monitoring systems remains vague in the literature. Emling describes the function of the ML Tektor as follows [78]:

The mechanical vibrations transmitted through the mould are converted to discrete electrical pulses by the accelerometer. These electrical signals are, in turn, assimilated by a computerized data acquisition facility. The signal generated is directly related to friction and, after some data processing, yields a relative friction factor.

Wolf notes that these signals are derived from resonance effects [10].
The author is not aware of any correlations between force sensor response and accelerometer signals. Accelerometers are effective vibration sensors, and these systems likely infer a “friction” signal from machine vibration.

2.10 Quantifying Friction

In a Bethlehem study [69], load cells were used to evaluate mould powders for increased casting speed. In this study, solid and liquid lubrication regimes were used to describe mould-strand friction. The liquid friction force was quantified by Equation 2.3 [69].

\[ F_{\text{liquid}} = \frac{\mu v_r}{x} \]  

(2.3)

Equation 2.3 assumes a constant velocity gradient through the film and uniform viscosity. Thus for a liquid friction mode, the maximum friction occurs at the point of maximum velocity. Solid friction was described by the simple relationship of Equation 2.4 [69].

\[ F_{\text{solid}} = cN \]  

(2.4)

Solid friction is independent of the magnitude of relative velocity, but simply depends on the direction of relative velocity. Solid friction simply results in a square wave force response. The friction response of mould fluxes was often a combination of solid and liquid friction [69].

The Bethlehem study defined the friction force as the difference between
the maximum and minimum forces over a 10 second period; thus eliminating
the force of the mould weight [69]. In another study using an experimental
casting machine, the friction force was determined by subtracting the mould
inertial force from the measured force [72]. In later research at Bethlehem, the
work per oscillation cycle was calculated, then divided by the mould stroke
to determine a "work-averaged" force [68]. In this study, the cold force was
subtracted from the casting force to obtain the net friction force.

2.11 Influence of Process Variables on Friction

2.11.1 Lubrication

In a study of oil lubrication, Brendzy et al. [11] noted slightly higher loads when
casting with excessively low oil flow rates. At oil flow rates above 34 ml min⁻¹,
little difference was noted in lubrication effectiveness. Friction measurements
have shown that oil lubrication leads to higher and more variant friction forces
than mould fluxes [49, 79], thus mould powder is preferred to oil lubrication
in reducing friction [10].

2.11.2 Casting Speed

Ohmiya et al. reported a relationship between mould friction and casting speed
in an excellent study of mould fluxes, as illustrated in Figure 2.15 [70]. For
a given mould powder, it appeared that a friction minimum existed at an
intermediate casting speed.
Figure 2.15: Mould friction response using powder lubrication.
2.11.3 Breakouts

A breakout is a catastrophic shell rupture that allows liquid steel to escape uncontrolled. In slab casting, sticker breakouts can be initiated by a disturbance in mould flux lubrication. The steel sticks, or welds, to the mould wall near the meniscus. Mould oscillation then tears the shell, and liquid steel fills the gap. In subsequent oscillation cycles, the shell is repeatedly torn and does not fully heal. The sticker moves down the mould, and causes a breakout at the mould exit. Sticker breakouts have been detected as hot spots in thermocouple response; propagating down the mould at approximately one half of the casting speed [80]. Sticker breakouts can also cause increased friction [66, 67, 75], and the detection of sticker breakouts has been a key objective in the use of force sensors in experimental slab casters. Sticker breakouts may be more reliably detected by thermocouples however [10].

2.11.4 Steel Grade

Singh and Blazek measured withdrawal forces on a bench-scale stationary caster [23]. Figure 2.16 illustrates the friction forces as a function of carbon content. The researchers concluded that high carbon steels (i.e. greater than 0.40 pct. carbon) exhibited low friction because of the smooth billet surfaces. Using a pilot oscillating caster, Saucedo and Blazek reported similar friction when casting carbon steels in the range 0.05 to 0.50 pct. carbon [67]. In the same study, higher friction was reported when casting free-machining\(^2\) steels.

\(^2\)Alloyed with Pb, Bi, or Te.
Figure 2.16: Mould friction response as a function of carbon content on a laboratory caster.
Using an accelerometer on a billet machine, van der Stel et al. reported a friction index for 0.10, 0.50 and 0.70 pct. carbon grades [49], as shown in Figure 2.17. The 0.10 pct. carbon steel was shown to have the highest friction index. This was attributed to reduced shrinkage of low carbon grades and higher normal forces in the mould, although heat extraction data was not presented. The opposite effect was noted using an accelerometer when slab casting with powder lubrication [46]. A peritectic steel (0.13 pct. carbon) yielded a friction signal of 25 pct., while a 0.40 pct. carbon steel produced a signal level of 75 pct. In this case, the low friction signal was attributed to the high shrinkage associated with the δ to γ phase transformation.

The effect of carbon content, and other process variables, remains in-

Figure 2.17: Output from accelerometer based "friction" signal.
complete and somewhat inconsistent in the literature. This is likely due to various sources of a "friction" signal and the different machines used: experimental casters, billet and slab machines.
Chapter 3

Scope and Objectives

Mould behaviour profoundly impacts billet quality and productivity, as was evident in the literature review. The mould interacts with the billet thermally and mechanically, and both are fundamental to process operation. Excellent material is available in the literature with respect to mould heat extraction, but very little information exists regarding mould-billet friction. Mould oscillation and lubrication facilitate casting by reducing friction so the shell will not crack, tear or stick. It is therefore surprising to note that few attempts have been made to monitor friction, and likely only one published work exists on measuring billet machine forces.

Friction is certainly impacted by key machine design parameters: mould oscillation and mould taper. The billet producer must ascertain what parameters are desired, and this has been determined empirically though local experience, and machine/mould suppliers. Once design parameters have been chosen, does the producer ensure that machine specifications are maintained
in casting operations? Unfortunately, the answer to this question is usually "no". This may be due to the history of billet casting, where low-cost, low-quality products like reinforcing bar were produced. Also, mini-mills often lack the resources and tools to perform such checks. But with the industry shift to higher quality billet steels and near-net-shape casting, these issues must be addressed. Further, there is widespread interest in industry to move towards higher speed casting for increased productivity. This requires tighter tolerances in all aspects of process quality, as well as the monitoring of process upsets which contribute to poor billet quality.

In a single statement, this research investigates the mould response in the context of process variables and upsets. The following sub-tasks were designed to meet this objective.

1. Conduct industrial plant trials to measure mould temperature, mould oscillation, mould-billet friction, casting speed and metal level. Record process and billet quality.

2. Measure mould oscillation using displacement and acceleration sensors. Is the machine operating at design specifications?

3. Test new sensor types to measure machine forces.

4. Evaluate mould-billet friction quantitatively.

5. Develop techniques for measuring mould friction and oscillation using an on-line system.
6. Investigate friction when high forces are expected, such as during the formation of transverse depressions and when mould-billet binding is occurring.

7. Investigate friction as a function of process variables: casting speed, lubricant and steel grade.

8. Develop a thermal-stress billet shrinkage model.

9. Use the billet shrinkage model to interpret mould-billet binding with the force sensor and to evaluate mould taper design.
Chapter 4

Experimental: Industrial Plant Trials

Five industrial plant trials were conducted at two Canadian mini-mills, designated as Companies A and D. Two main experimental trials were conducted at Company D, and one at Company A. As the knowledge of this work unfolded, two subsequent force sensor tests were conducted, one at each facility. This chapter discusses the instrumentation of the casting machines and casting conditions of the plant trials.

4.1 New Aspects of Industrial Plant Trials

Industrial plant trials have been conducted by the UBC continuous casting group in the past. The use of mould thermocouples has been well established in the work of Bommaraju [22], Chandra [81] and Kumar [13]. The research of Brendzy [82] is likely the only published work measuring mould-strand friction on industrial billet machines. Although Brendzy’s work has provided significant insight into the process, the forces measured were only qualitative,
owing to the installation of the load cells. This work endeavours to study the mechanical response of the billet machine quantitatively. New sensors were tested, and a separate data acquisition system was used to meet this objective. Highlights of this new work conducted as part of these plant trials are listed below.

1. **Data sampling.** Data was sampled at a higher sampling frequency than in the past. This was necessary to more fully quantify the machine response and to identify electrical line noise.

2. **Signal isolation.** Ground-isolated sensors were used to minimize electrical problems such as ground looping.

3. **Data acquisition.** A separate data acquisition system was developed and used independently of the thermocouple data acquisition system. This was required for high frequency data sampling and signal isolation.

4. **Accelerometer.** An accelerometer was tested for the following reasons:
   - As a kinematic sensor to verify the LVDT response.
   - As a candidate kinematic sensor to measure mould movement online.
   - To investigate the accelerometer “friction” signal noted by a slab casting vendor.

5. **Oscillator motor current.** The drive motor current was tested as a possible indicator of mould-strand friction.
Table 4.1: Summary of sensor use.

<table>
<thead>
<tr>
<th>Plant Trial Machine ID</th>
<th>D1 A</th>
<th>A1 B</th>
<th>D2 C</th>
<th>D3 C</th>
<th>A2 B</th>
</tr>
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<tr>
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<td>x</td>
<td>x</td>
<td>x</td>
<td>x</td>
</tr>
<tr>
<td>Accelerometer</td>
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<tr>
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<tr>
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<td>x</td>
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<td>x</td>
</tr>
</tbody>
</table>

6. Strain gauge. Strain gauges were tested as a candidate force sensors.

7. Piezoelectric strain sensor. Near the end of this research, an experimental strain sensor was tested to measure mould-strand friction.

The strain gauge sensor provided, for the first time, quantitative force measurements on an industrial billet casting machine. As will be discussed later, the motor current and accelerometer friction signals were abandoned because of the success of the strain gauge.

4.2 Sensors

A summary of sensors employed in the five plant trials is given in Table 4.1. Figure 4.1 illustrates the sensor locations in the casting machine.
Figure 4.1: Schematic of sensor locations in the casting machine.
4.2.1 Linear Variable Differential Transformers

A linear variable differential transformer (LVDT) was used for measuring mould displacement. A LVDT is an electromagnetic device which consists of a cylindrically wound primary coil with a secondary coil wound at the ends of the primary coil [83], as illustrated in Figure 4.2. The object of interest moves a steel rod which slides through the centre of the coils. The primary coil is excited with an AC voltage; an AC voltage of the same frequency is induced in the secondary coil. The output signal of the secondary coil is processed such that the resulting signal is a linear function of displacement [83]. The LVDT, attached to a magnetic base, was anchored to the shop floor near the oscillator. The LVDT was placed as close to the mould as possible, near the centre of the oscillator table.
The AC excitation frequency of the LVDT should be 10 times greater than the highest frequency of interest in the measured signal [83]. The Datronic 3230 signal conditioners excited the LVDTs at 3000 Hz, indicating that the maximum frequency of interest should be 300 Hz. Data was sampled at frequencies as high as 1000 Hz, but no frequency in the measured signal was near the 300 Hz limit. This was later confirmed by a fast Fourier transform analysis. The Schlumberger C51 signal conditioner operated at 5000 Hz. Schlumberger Sangamo ACR-15 LVDTs were used for the trials.

4.2.2 Accelerometer

Accelerometers are sensors that output a voltage proportional to acceleration. Piezoelectric accelerometers use a piezoelectric crystal as a transducer, which generates a charge when a strain is applied. This effect occurs with asymmetric crystal structures that become charge imbalanced when distorted. In the case of an accelerometer, the strain is caused by the inertial loading of the crystal by its mass or by a fixed mass attached to the crystal. The charge generated is then proportional to the acceleration of the crystal [84]. Direction is identified by charge polarity since the crystal generates opposite charges depending on tensile or compressive strain. For a constant acceleration or strain, the crystal charge leaks off. The piezoelectric device is therefore dynamic, and is not useful for constant acceleration or strain applications. In the case of most vibratory systems, constant accelerations are not encountered. Piezoelectric accelerometers are usually small, robust and have a wide dynamic range.
The piezoelectric sensor output requires signal conditioning prior to measurement by a data acquisition system. A charge amplifier is used to reduce the charge leak-off rate and provide a signal appropriate for measuring. The charge amplifier outputs a voltage proportional to the generated charge, and in the case of an accelerometer, acceleration.

A PCB Piezotronic model 326A13 accelerometer was selected and is illustrated in Figure 4.3. It was a low frequency, hermetically sealed industrial sensor, with an operating bandwidth of 0.3 - 4000 Hz. Full scale acceleration output was ±5 g (±49 m s\(^{-2}\)); the sensor was calibrated to 994 mV g\(^{-1}\). A PCB 482A16 was used to power and process the output signal of the accelerometer. The 482A16 powered the accelerometer with a constant current source of 4 mA; the 482A16 also had selectable output gains of 1, 10 and
100. The accelerometer was mounted on the mould jacket flange as close to the mould and LVDT as possible. The sensor was attached with a mounting stud, threaded into a 6.35 mm hole which was drilled and tapped in the mould jacket flange.

4.2.3 Oscillator Motor Current

The direct current motor signal was taken from a resistor in the motor power circuit. At trial D2, the potential across the resistor was 50 mV for the motor’s maximum current of 150 amps. Since the resistor voltage was not relative to ground, an isolation amplifier was required for the signal to be logged by the data acquisition system. An Analog Devices 289K isolation amplifier was used.

4.2.4 Force Sensors

Load Cells

Load cells are commercially available force sensors. Omega LCG-10K compression load cells were used for the trials. The Omega load cells utilized foil strain gauges as transducers. The load cell output voltage was proportional to the applied force; output was 2 mV per excitation volt. A regulated 10 volt excitation was employed, yielding an output of 20 mV for the full scale load of 45000 N [85].

Load cells were installed at Company D for the first two main experimental trials, D1 and D2. The load cells were installed between the oscillator table and the mould housing flange, as previously described by Bakshi et al.
Three load cells were installed in recessions in the mould jacket flange. Adjustable spacers centred the cells during installation as the oscillator table bolts were tightened. The cells were installed on the opposite side of the housing to the water inlets and outlets. Previous experience indicated that the water O-rings dampened adjacent load cell signals [11]. It has been recognized that the load cells sense only a fraction of the total load. Each cell responds to its fraction of the combined bolt and cell loading. Thus the load cell response was significantly desensitized by the mould bolts which have a higher stiffness than the load cells. The objective for this configuration was to obtain a qualitative understanding of mould load.

**Strain Gauges**

The function of a strain gauge is based on the principle that the electrical resistance of a material will change when it is mechanically deformed [83]. Metal foil is the most common strain gauge material, which is formed into a grid and oriented longitudinally in the direction that the strain is to be measured [85]. The foil is mounted on an electrically insulating backing material. The resistance change of the gauge is linear with strain, thus for a linear-elastic material the force is a also a linear function of strain. The change in resistance is measured with a simple electrical circuit.

Strain gauge output can be increased by mounting four strain gauges in a “Wheatstone bridge” configuration as shown in Figure 4.4 [83]. Two of the gauges measure axial strain, while the other two measure transverse
strain. The bridge is said to be "balanced" if the four resistances are equal. As the gauges are strained, the resistance bridge becomes imbalanced and a voltage is created. For axial loading, the bridge strain can be calculated from Equation 4.1 [85].

\[ \varepsilon = \frac{-2V_r}{GF[(\nu + 1) - V_r(\nu - 1)]} \]  \hspace{1cm} (4.1)

The strain gauge bridge needed to be installed on a load bearing member of the machine. The drive arm of the oscillator was selected for the following reasons:

- The drive arm actuates the entire machine, and the full machine force would be measured.
- The arm is located in an accessible area to install the sensor.
• The location is not likely to be exposed to a break-out, which would damage the sensor.

• The arm is pinned at both ends, and in a simple state of axial stress. Thus the forces measured should be an accurate measure of machine loading.

The surface of the drive arms were degreased, and sanded with progressively finer grades of emery cloth. The strain gauges were installed using Micro-Measurements M-Bond 200 adhesive, following the installation guidelines of Micro-Measurements Bulletin B-127-2 [86].

For the main experimental trials, a regulated 10 volt power supply was used to power the bridge. For the force sensor tests, an integral strain gauge power supply-amplifier was used. A Sensor Developments Inc. model 90131 Bridgesensor was installed as close to the bridge as possible, to minimize electrical noise. Appendix A details the strain gauge force calculation.

**Piezoelectric Force Sensor**

A piezoelectric strain sensor was tested as a candidate force sensor late in this research. This sensor was recently employed measuring a low-frequency oscillating force on a ball mill [87, 88]. The strain gauge had been verified as a capable sensor for measuring mould-strand interaction, but the installation was time-consuming and tedious. Since part of this research was directed towards on-line monitoring, this sensor was tested for industrial use.
The new sensor employed a piezoelectric crystal as a transducer, and was developed by Kistler Instrument Corporation. A unique feature of this sensor is that it is installed with a single bolt, and strain is measured relative to two contact pads. To the author's knowledge, Kistler is the only supplier of this sensor type. The principle of operation is similar to that of a piezoelectric accelerometer. As the crystal is strained, the charge generated is converted to a voltage, which is proportional to strain. As mentioned previously, the charge generated by these crystals leaks off, thus this strain sensor is only useful for dynamic, or quasi-static [89], strain measurements.

The Kistler 9233B strain sensor, shown in Figure 4.5, and 5038A charge amplifier were tested at trial A2. The 9233B has an operating strain range of ±300 \( \mu \varepsilon \), and was installed with a single M6x35-12.9 machine screw. The oscillator arm was degreased and prepared with emery cloth; a hole was drilled and tapped for mounting.
4.2.5 Process Control Signals

Casting Speed

Casting speed was taken directly from the plant control system as a voltage source. The signal originated from the withdrawal roller tachometer, and was typically in the range of 0 - 10 V.

Metal Level

The metal level signal was taken from the system controller. For trial D1, a 1 Ω resistor was used in series with the 4 - 20 mA current loop; the associated output voltage was 4 - 20 mV. For trial A1, a voltage signal was obtained directly from the controller. The second stage of trials used an M-System signal isolator (discussed in Section 4.3.6), which mapped the 4 - 20 mA current loop signal to a 1 - 5 V output signal.

4.2.6 Mould Temperature

Axial temperature profiles of the mould were required for mould heat transfer calculations. The technique for measuring mould wall temperatures had been developed by Brimacombe, Samarasekera and co-workers in past research [48, 90]. Although the thermocouple instrumentation technique was not an original part of this research, it will be discussed briefly for completeness. B.N. Walker at UBC conducted the thermocouple installation.

To install a single thermocouple, threaded holes were prepared through
the water baffle and approximately half-way into the copper mould. Single-
wire, Type T, copper-constantan thermocouples were employed. Constantan
thermocouple wire was prepared by forming a bead on the wire with a TIG
welder. The bead was filed flat, then heat-shrink tubing was applied to the
wire to insulate it. The bead was inserted through the water baffle into the
hole in the mould and was held in place with a copper plug. A plug was
also installed in the water baffle thread to secure the wire and prevent water
cross-flow. The constantan wire was then attached to insulated copper wire
in the water channel. Inlet and outlet water temperatures were also measured
by installing Type T two-wire thermocouples in the water channel. Groups of
wires were bunched together and run through a pipe fitting in the side of the
mould jacket. Rubber plugs and silicone sealant were placed in the fitting for
a water-tight seal, which was retained by a pipe thread collar. Thermocouple
wires were then tested for electrical continuity and the assembly was pressure
tested with water.

The thermocouple layout for plant trials D1 and D2, and the depths of
the thermocouples are detailed in Appendix B.

4.2.7 Sensor Calibration

Mechanical Sensors

1. LVDT. The LVDT was calibrated on-site with a block of known dimen-
sion.
2. **Accelerometer.** The PCB accelerometer and signal conditioner were calibrated by the supplier.

3. **Load cells.** The load cells were calibrated by the supplier. The sensor calibration with checked with an Instron machine [91].

4. **Strain gauges.** The strain gauges were supplier calibrated, by supplying the gauge factor.

5. **Kistler strain sensor.** The piezoelectric strain sensor was calibrated by the manufacturer. The associated charge amplifier was adjustable and was not supplied calibrated. Since calibration of the charge amplifier required special instrumentation, the strain sensor was calibrated on-site to a strain gauge.

6. **Metal level.** Metal level was calibrated by inserting a billet into the mould at various distances from the mould top.

7. **Casting speed.** Casting speed calibration was obtained from the plant technicians. In trial D1, the calibration was checked using a tachometer on the withdrawal roll.

The net calibration constants, for converting logged voltages to the appropriate units, are located in Appendix C.
Table 4.2: Polynomial coefficients for Type T thermocouple voltage-to-temperature conversion.

<table>
<thead>
<tr>
<th>Coefficient</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>$a_0$</td>
<td>0.100860910</td>
</tr>
<tr>
<td>$a_1$</td>
<td>25727.94369</td>
</tr>
<tr>
<td>$a_2$</td>
<td>-767345.8295</td>
</tr>
<tr>
<td>$a_3$</td>
<td>78025595.81</td>
</tr>
<tr>
<td>$a_4$</td>
<td>-9247486589.</td>
</tr>
<tr>
<td>$a_5$</td>
<td>6.97688 E11</td>
</tr>
<tr>
<td>$a_6$</td>
<td>-2.66192 E13</td>
</tr>
<tr>
<td>$a_7$</td>
<td>3.94078 E14</td>
</tr>
</tbody>
</table>

Mould Temperature

Thermocouple voltage was converted to temperature using a polynomial equation. For Type T thermocouples, a seventh order polynomial of the form

$$ T = a_0 + a_1 x + a_2 x^2 + \ldots + a_7 x^7 \quad (4.2) $$

yields an accuracy of ±0.5°C over the range -160 to 400°C [92]. The polynomial coefficients are listed in Table 4.2.

It should be noted that the voltage measured by a mould thermocouple wire was the sum voltage of two copper-constantan junctions; one in the mould, the other in the water channel. Since the potential of these junctions opposed each other, the mould thermocouple signal was reduced by the voltage associated with the thermocouple in the cooling water channel. The two-wire thermocouples in the cooling water were used to compensate for the copper-constantan junction in the cooler water. Thus to calculate mould temperature,
the mould thermocouple voltage was added to the water channel thermocouple voltage plus reference temperature voltage \(^1\), then converted to temperature using the calibration equation.

### 4.3 Data Acquisition System

The data acquisition system for the mechanical sensors evolved in several stages. The first three experimental trials, D1, A1 and D2, tested new sensor types and implemented devices such as isolators and amplifiers for superior data quality. The force sensor tests, D3 and A2, focussed on on-line monitoring with new devices. The electrical schematics for the plant trials D1, A1, D2, D3, A2 (ordered chronologically) are illustrated in Figures D.1 to D.5 respectively, located in Appendix D.

#### 4.3.1 Personal Computer

An IBM PC clone was used for data acquisition and storage. The PC featured an Intel 80486DX2-66 microprocessor, 16 megabytes of RAM and a Toshiba 870 megabyte hard disk. A 1.2 gigabyte Colorado tape drive was installed for data transfer and back-up.

#### 4.3.2 Analog-to-Digital Converter

A Metrabyte DAS-8 data acquisition card was installed into the PC-ISA bus slot. The card featured a 12-bit successive approximation analog-to-digital converter.

\(^1\)The reference temperature voltage is simply the voltage associated with the ambient temperature, since the calibration equation is referenced at 0°C.
converter, and was powered by the PC power supply. Conversion times were typically 25 microseconds, 35 microseconds maximum. The theoretical data throughput rate was 30000 Hz, but this could not be achieved by the PC. The nominal analog input range was ±5 V. The associated signal resolution for a 12 bit analog-to-digital converter can be calculated using equation 4.3.

\[
\text{resolution} = \frac{\Delta V}{2^{12} - 1}
\]  

(4.3)

In this case, the voltage resolution was 2.44 mV \((\frac{5}{4095})\).

### 4.3.3 Multiplexer

A Metrabyte EXP-16 analog multiplexer and amplifier was employed. The board multiplexes 16 analog input channels into 1 analog output channel. The channels were selected by software through the DAS-8 using a 4 bit TTL-CMOS compatible address. The EXP-16 connected directly to the DAS-8 with a standard 37 pin ribbon cable. The multiplexer was powered by the PC through the DAS-8.

The board contained an amplifier for applying a common gain to all channels. In the plant trials D1 and D2, a gain of 200 was used allowing an analog input range of ±25 mV. The associated signal resolution was 0.012 mV using equation 4.3.
4.3.4 Software

Data Acquisition

Keithley Labtech Notebook for Windows data acquisition software was used: The software allowed the user to customize the input channels to be read, the data sampling rate, the output file format and real time graphical display of selected inputs. Data acquisition was such a burden for the PC that the graphical display was often updated only once per minute. The data was stored in binary files rather than ASCII because this was more efficient for the software. Data files sometimes exceeded 100 megabytes.

Labtech contained features that allowed input data to be modified or processed in real time. These features included algebraic, differential and integral functions. These features would be ideal for calibrating signals, and for calculating parameters such as negative-strip time on-line. Unfortunately, the PC was not capable of doing this in real-time, so only raw voltages were logged.

Data Extraction and Calibration

Convert [93], a program developed by summer student K. Wilder, was used to extract the binary data. The program was also capable of thinning the data, extracting specified columns, and calibrating data with polynomial equations. Extracting and calibrating data often took several hours, so batch jobs were submitted to the PC overnight.
Fast Fourier Transforms

Fast Fourier transforms (FFT) were used to present signals in a frequency domain format. The FFT is a useful tool for identifying frequency components in time-based signals. In particular, logged signals were checked on-site for 60 Hz electrical line noise, which would indicate a data acquisition or sensor problem. Labtech Notebook contained a simple FFT function, and the output could be viewed using a PC spreadsheet. FFT program code is readily available, e.g. [94, 95].

4.3.5 Parallel Data Acquisition Systems

Thermocouple System

A parallel data acquisition system was used to sample mould thermocouple signals during the main experimental plant trials. This system was used by other researchers [13, 96, 97] during some of the plant trials. The system consisted of a similar PC clone and analog-to-digital converter. The multiplexer consisted of 8 cascaded EXP-16 boards called an EXP-ENC device. A power supply powered the multiplexer through a splitter in the ribbon cable between the DAS-8 and EXP-ENC. The multiplexer was capable of handling 128 input channels.
Real-Time System

A real time data acquisition system has been under development by V. Rakocevic [98], for use with the Intelligent Mould. The system operates under QNX, a unix-like, real-time operating system. The system uses a Metabyte DAS-20 card for data acquisition. A unique feature of this system is that it calibrates and processes data at the driver level, and therefore is very efficient. COMDALE ProcessVision was employed as the user interface to display process parameters. The COMDALE/C expert system shell was implemented to interpret signals on-line. The environment is capable of displaying warnings, alarming, and generating reports of system findings.

This system was under development and was tested by Rakocevic during the experimental trial D2. The prototype was used for this research by the author during the force sensor test D3. Since the system interprets and displays data in real-time, this system was very useful in identifying upsets on-line. Further, four meniscus thermocouples were installed during this test, allowing thermal and mechanical data to be logged simultaneously.

4.3.6 Data Acquisition Issues

Ground Looping and Electrical Noise

Ground looping may be caused by circuits which are grounded in multiple locations. Consider the circuit in Figure 4.6 [83]. If a potential exists between the two ground points, which is likely in a high-power industrial environment,
then a circuit is induced between the ground points and the sensor negative lead. The resulting voltage is then superimposed on the sensor signal, leading to an offset or noise. The ground loop circuit can be broken using ground isolated sensors.

This condition was particularly problematic in plant trials where a PC and a sensor may be grounded 30 m apart. In the case of the thermocouple data acquisition system, the thermocouples were grounded to the mould and it was not possible to ground isolate the thermocouples. Thus the PC ground
and thermocouple ground would likely form a ground loop and reduce data
good. Electrical noise can also be induced in signal leads from AC power
lines. This noise can lead to excessive error on low-level signals such as those
from strain gauges [83]. Shielded cables were used for the plant trials in order
to minimize the impact of electrical noise.

Aliasing is the representation of a high frequency signal by a low fre­
quency signal [94], and is caused by an inadequate sampling frequency. The
Nyquist sampling theorem states that the sampling frequency must be greater
than twice the frequency of the highest frequency component in a signal [94].
For example, if 60 Hz line noise exists in a signal, and the sampling frequency
was 100 Hz, then aliasing would occur. Aliasing has been noted in past plant
trial signals [99]. In this research, signals were sampled at a very high fre­
cquency (up to 1000 Hz), and the frequency components in the signal were
established prior to selecting a sampling frequency.

**Common-Mode Voltage**

Although voltage levels from different devices may be small, their voltage
relative to a common ground may be large. This is referred to as common-
mode voltage. The oscillator motor current signal is an example of a common-
mode ground problem. The current signal, taken from a resistor in the motor
circuit, was small (50 mV), but the signal was several hundred volts relative
to ground. If this signal was connected directly to a data acquisition system,
it may damage the hardware.
A similar problem existed when tapping into 4 - 20 mA circuits such as the metal level signal. The common-mode voltage will vary depending on where the signal is extracted in the circuit.

**Signal Levels and Isolation**

A downside of the data acquisition system was that it only allowed for a single gain setting on the EXP-16 multiplexer. For the early trials, signals were acquired at the multiplexer in the range of ±25 mV, using a gain setting of 200. This signal level was appropriate for logging load cell, strain gauge and metal level signals. Sensor signals with voltages higher than 25 mV (e.g. LVDT, accelerometer) were reduced with voltage dividers, shown schematically in Figure 4.7. Voltage dividers are simple to build, and were often adapted on-site to optimize signal levels.

For the later trials, isolation amplifiers were employed to meet the following objectives:

- To reduce data acquisition problems such as ground looping.

- To sample signals at a higher voltage level (~5 V) to reduce the impact of electrical noise.

- To allow parallel data acquisition between this system and thermocouple data acquisition system.

An M-System Technologies modular isolation amplifier system was used. The base unit was capable of containing 10 - 2 channel isolation amplifier mod-
4.4 Plant Trials

4.4.1 Casting Conditions

The machine design and casting practices varied between the Companies. Most noteworthy was the difference in mould taper. Company A used a 0.8 pct. m\(^{-1}\) single-tapered mould, while Company D used a steeply tapered (4 - 5 pct. m\(^{-1}\) at the meniscus) parabolic mould. Casting machine details are given in Tables
Table 4.3: Casting machine details at Company A.

<table>
<thead>
<tr>
<th>Plant Trial</th>
<th>Machine ID</th>
<th>A1</th>
<th>A2</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Machine ID</td>
<td>B</td>
<td>B</td>
</tr>
<tr>
<td>Machine type</td>
<td>straight</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Mould taper</td>
<td>single</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Taper at meniscus (pct. m⁻¹)</td>
<td>0.8</td>
<td>0.8</td>
<td></td>
</tr>
<tr>
<td>Mould size (mm)</td>
<td>203</td>
<td>152</td>
<td></td>
</tr>
<tr>
<td>Mould length (mm)</td>
<td>734</td>
<td>734</td>
<td></td>
</tr>
<tr>
<td>Mould material</td>
<td>DHP</td>
<td>DHP</td>
<td></td>
</tr>
<tr>
<td>Mould constraint</td>
<td>4-sided</td>
<td>4-sided</td>
<td></td>
</tr>
<tr>
<td>Water channel gap (mm)</td>
<td>~4</td>
<td>~4</td>
<td></td>
</tr>
<tr>
<td>Water velocity (m s⁻¹)</td>
<td>~10</td>
<td>~12</td>
<td></td>
</tr>
<tr>
<td>Oil flow rate (ml min⁻¹)</td>
<td>30</td>
<td>35</td>
<td></td>
</tr>
<tr>
<td>Stroke (mm)</td>
<td>12.7</td>
<td>12.7</td>
<td></td>
</tr>
<tr>
<td>Osc. Frequency (Hz)</td>
<td>1.7 &amp; 2.7</td>
<td>1.7 &amp; 2.7</td>
<td></td>
</tr>
<tr>
<td>Casting Speed (mm s⁻¹)</td>
<td>~19</td>
<td>~19 - 30</td>
<td></td>
</tr>
<tr>
<td>Negative-strip time (s)</td>
<td>0.24 &amp; 0.17</td>
<td>0.21 &amp; 0.15</td>
<td></td>
</tr>
<tr>
<td>Mould Lead (mm)</td>
<td>7.5 &amp; 9.3</td>
<td>5.0 &amp; 7.6</td>
<td></td>
</tr>
</tbody>
</table>

4.3 and 4.4.

With the exception of the instrumented moulds, casting conditions during the plant trials were normal operating practice. The mould design and machine parameters were common operating practice for the individual plants. Data were obtained for as many casting conditions as possible, subject to plant constraints and production schedules. In fact, the plant trial schedules were very dynamic, owing to the current marketing conditions which changed hourly in some cases. The main parameters of interest for a given mould were steel grade and the type of lubrication, oil or mould flux. Appendix E details
Table 4.4: Casting machine details at Company D.

<table>
<thead>
<tr>
<th>Plant Trial Machine ID</th>
<th>D1</th>
<th>D2</th>
<th>D3</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>A</td>
<td>C</td>
<td>C</td>
</tr>
<tr>
<td>Machine type</td>
<td>curved</td>
<td>curved</td>
<td>curved</td>
</tr>
<tr>
<td>Mould taper</td>
<td>parabolic</td>
<td>parabolic</td>
<td>parabolic</td>
</tr>
<tr>
<td>Taper at meniscus (pct. m$^{-1}$)</td>
<td>5</td>
<td>5</td>
<td>4</td>
</tr>
<tr>
<td>Mould size (mm)</td>
<td>203</td>
<td>203</td>
<td>171 &amp; 194</td>
</tr>
<tr>
<td>Mould length (mm)</td>
<td>813</td>
<td>813</td>
<td>813</td>
</tr>
<tr>
<td>Mould material</td>
<td>DHP</td>
<td>DHP</td>
<td>DHP</td>
</tr>
<tr>
<td>Mould constraint</td>
<td>4-sided</td>
<td>4-sided</td>
<td>4-sided</td>
</tr>
<tr>
<td>Water channel gap (mm)</td>
<td>~5</td>
<td>~5</td>
<td>~5</td>
</tr>
<tr>
<td>Water velocity (m s$^{-1}$)</td>
<td>~10</td>
<td>~10</td>
<td>10 - 12</td>
</tr>
<tr>
<td>Oil flow rate (ml min$^{-1}$)</td>
<td>50</td>
<td>50</td>
<td>50</td>
</tr>
<tr>
<td>Stroke (mm)</td>
<td>9</td>
<td>6 &amp; 9</td>
<td>6 &amp; 9</td>
</tr>
<tr>
<td>Osc. Frequency (Hz)</td>
<td>1.9</td>
<td>variable</td>
<td>variable</td>
</tr>
<tr>
<td>Casting Speed (mm s$^{-1}$)</td>
<td>~19</td>
<td>~19</td>
<td>20 - 35</td>
</tr>
<tr>
<td>Negative-strip time (s)</td>
<td>.20</td>
<td>.17 &amp; .20</td>
<td>.10 &amp; .16</td>
</tr>
<tr>
<td>Mould Lead (mm)</td>
<td>4.6</td>
<td>2.0 &amp; 4.8</td>
<td>0.5 &amp; 2.8</td>
</tr>
</tbody>
</table>

@2 Hz, @2 Hz, 30 mm s$^{-1}$
the chemical compositions of the heats monitored in the main plant trials.

4.4.2 Billet Samples

During the main trials, 300 mm billet samples were acquired routinely, typically at a rate of two samples per heat. Samples were also obtained corresponding to process upsets and large defects; these samples were 300 to 1000 mm in length. The billet cooling bay and storage yard were routinely inspected for surface defects, in an attempt to understand what sizes and grades of billets were disposed to forming observed defects. The billet samples were marked with heat number, casting direction and face orientation, then shipped back to UBC for inspection.

The preparation and analysis of billet samples have been documented in other sources [81, 90]. Macro-etching was conducted on prepared billet sections to observe internal billet quality. Samples were immersed in a solution of 50 pct. HCl and 50 pct. water and heated to 85°C for 30 minutes. Samples were then cooled, scrubbed with steel wool in water, dried with ethanol, and finally photographed.

Billet quality can be outlined in the following categories:

1. Internal quality.

   • Internal cracks
   • Inclusions
   • Porosity
• Microstructure

2. Dimensional quality - rhomboidity.

3. Surface quality

• Oscillation marks
• Surface roughness
• Depressions: longitudinal and transverse
• Surface cracks
• Bleeds and laps
• Pinholes
• Slag and zipper marks

Depressions and transverse cracks were of particular interest to this research because the formation of these defects has been linked to mould-strand friction. The shape of surface depressions were mapped using a profilometer table normally used for measuring the depth of oscillation marks. A billet sample was placed on the profilometer table, which moved at a regulated speed. An LVDT was mounted on an arm which was fixed above the sample. The LVDT signal was logged using a Toshiba 286 laptop and Metrabyte DAS-8 and EXP-16 components, described previously. In a single pass, depth data were logged with time, which was converted to axial distance by multiplying time with the speed. The LVDT was moved transversely 10 mm, and the procedure
was repeated until the surface defect was completely mapped. This process was conducted to quantify both the orientation and shapes of depressions as repeatable or random in nature.
Chapter 5

Industrial Plant Trial Results

Immediate results of the plant trials, including sensor response and billet quality, are presented in this chapter. Mould oscillation and process control observations are also discussed. Mould-strand friction required more detailed analysis, and is presented in Chapter 6.

5.1 Base Sensor Response

5.1.1 Kinematic Sensors

Figure 5.1 illustrates typical mould displacement and acceleration responses, as measured by a LVDT and an accelerometer. The displacement trace was reasonably sinusoidal, and the acceleration signal was rough due to jerk in the oscillator mechanism. The displacement and acceleration signals were 180° out of phase, as expected with sinusoidal motion. Figure 5.2 shows the accelerometer signal and acceleration calculated from the LVDT signal \(^1\). The traces

\(^1\)Acceleration was calculated by taking the second derivative of the displacement signal with respect to time.
overlaid reasonably well, yielding confidence in the sensor response. Also, the magnitudes of the accelerations measured by these independent instruments were consistent.

Figure 5.1: Typical LVDT and accelerometer signals. Trial D3.
Figure 5.2: Mould acceleration as measured by an accelerometer and calculated from a displacement signal. Trial D3.
5.1.2 Force Sensors

Strain Gauge

Strain gauge and load cell force sensors were tested simultaneously during trial D2. Figure 5.3 illustrates that the signals were in phase and had a reasonable match. This was one of the most significant findings of this work. Firstly, force can be measured on the oscillator mechanism, alleviating the difficulties of conventionally installing load cells every time the mould is changed [48]. Secondly, quantitative forces have been measured possibly for the first time on an industrial billet machine.

Force and Displacement Response

Figure 5.4 illustrates a typical force and displacement trace during casting operations. The character of the force response is similar to that reported by Brendzy et al. [11], with the exception that force is measured with a strain gauge rather than load cell. The force signal leads the displacement signal by approximately 90°. The periodic reduction of force corresponds with the downstroke of the mould, or negative-strip time, which will be discussed in more detail later.

Piezoelectric Strain Sensor

As discussed in Section 4.2.4, a Kistler piezoelectric strain sensor was tested late in this research as a candidate force sensor for industrial on-line moni-
Figure 5.3: Load cell and strain gauge force sensor signals. Trial D2.
Figure 5.4: Typical oscillator force and mould displacement responses. Trial D3.
the strain gauge using data from a cold oscillation cycle, shown in Figure 5.5. Figure 5.6 shows that the piezoelectric sensor appeared less sensitive to higher frequency signal components such as machine harmonics and the friction peaks seen during positive strip time. However, for tracking the basic force response the Kistler sensor seemed acceptable. The piezoelectric sensor is more sensitive to small strains than a conventional strain gauge, and may be useful for machines with large drive arm sections (and small strains).

5.1.3 Cold Work - Machine Not Casting

During a cold oscillation test, the mould displacement was 180° out of phase with force, as shown in Figure 5.7. Acceleration, shown in Figure 5.8, was in phase with force, indicating that the force sensor was responding to the inertial forces of the machine. The consistency of the kinematic and force sensing provides further confidence that the sensors are providing meaningful and correct information.

5.1.4 Oscillator Motor Current

The current of the DC electric drive motor was measured as a candidate friction indicator in this study. Figure 5.9 shows that the current signal was periodic and in phase with the force sensor. Note that the motor current should increase when the motor load increases, regardless of whether the mould movement is up or down. Although the signal may be useful as a qualitative indicator of
Figure 5.5: Data used to calibrate the piezoelectric strain sensor with a strain gauge. Trial A2.
Figure 5.6: Strain gauge and piezoelectric strain sensors during casting operations.
Figure 5.7: Force and displacement signals $180^\circ$ out of phase during a cold oscillator test.
Figure 5.8: Force and acceleration signals in phase during a cold test. The force sensor was responding to inertial forces.
mould-strand interaction, further analysis of the signal was abandoned because of the success of the strain gauge.

5.2 Oscillator Characteristics

Three different oscillation machines were tested during the plant trials; the oscillators were designated as Machines A, B and C in Table 4.1. All machines were designed to be operated in a sinusoidal mode. As will be shown, the
Table 5.1: Summary of oscillator stroke measurements.

<table>
<thead>
<tr>
<th>Machine</th>
<th>Design stroke (mm)</th>
<th>Operating stroke (mm)</th>
<th>Sinusoidal profile (visual inspection)</th>
</tr>
</thead>
<tbody>
<tr>
<td>A</td>
<td>9.0</td>
<td>5.5 - 6.5</td>
<td>no</td>
</tr>
<tr>
<td>B</td>
<td>12.7</td>
<td>9.0 - 11.5</td>
<td>no</td>
</tr>
<tr>
<td>C</td>
<td>6.0</td>
<td>4.2 - 5.4</td>
<td>yes</td>
</tr>
<tr>
<td>C</td>
<td>9.0</td>
<td>~ 7</td>
<td>yes</td>
</tr>
</tbody>
</table>

oscillation characteristics of these machines varied greatly.

5.2.1 Oscillator Stroke

All oscillators showed a variance of stroke from their design values, as summarized in Table 5.1. Machine A had a design stroke of 9 mm and was operating near 6 mm as shown in Figure 5.10. Machine B, illustrated in Figure 5.11, operated from 9 - 11 mm and had a design stroke of 12.7 mm. Machine C exhibited a high quality sinusoidal oscillation profile while Machines A and B had non-sinusoidal profiles. Figure 5.12 shows the non-sinusoidal profile of Machine B.

Machine A had an unloaded stroke of 8 mm; the displacement profile was a smooth waveform but the peaks of the sinusoid were flattened in the centre. This profile was not seen during casting. The difference between load (6 mm) and no-load (8 mm) performance was likely due to either clearances in machine joints or elastic deformation of machine components under load.
Figure 5.10: Mould displacement and load cell response - Machine A.
Figure 5.11: Mould displacement and casting speed during the beginning of a heat - Machine B. Note that increasing casting speed increased the oscillation frequency.
Figure 5.12: Non-sinusoidal displacement profile and casting speed variation - Machine B.
5.2.2 Stroke and Machine Loading

The operating stroke of Machine A ranged from 5.5 - 6.5 mm, as shown in Figure 5.10 which presents the displacement and load cell responses. The stroke was seen to vary as much as 0.5 mm between periods. The variance of stroke was believed to be a function of loading as the load range was greatest opposite short stroke cycles.

5.2.3 Negative-Strip Time and Mould Lead

Negative-strip time is illustrated schematically in Figure 5.13 using sensor data. Mould velocity was calculated by taking the time derivative of the mould displacement signal. It is evident that the velocity profile was not sinusoidal, due to imperfections in the oscillator mechanism. Of course, an "ideal" oscillator would exhibit perfectly sinusoidal displacement, velocity and acceleration profiles. In this case, the negative-strip time was seen to be approximately 0.15 seconds.

Operating negative-strip time can differ from the design value given the observed variance of stroke, and can be further impacted by non-sinusoidal velocity profiles as illustrated in Figure 5.14. The negative-strip time was calculated to be 0.20 seconds for Machine A using Equation 2.1. Sensor data indicated the negative-strip time was varying from 0.12 to 0.17 seconds. Thus, the operating negative-strip time was significantly less than expected.

The expected mould lead for Machine A \((s=9 \text{ mm}, f=1.9 \text{ Hz}, v_s=19 \text{ mm s}^{-1})\) was 4.6 mm. Under the operating stroke of 6 mm, the actual mould lead was
Figure 5.13: Illustration of negative-strip time using real data from Machine C. Trial D2.
Figure 5.14: Mould velocity and load cell force - Machine A. Trial D1.
Figure 5.15: Mould velocity, casting speed and force corresponding to a period of zero negative-strip time. Trial D3, 0.70 pct. C, 171 mm mould, oil lubrication.

1.9 mm. For billet casters, the recommended mould lead and negative-strip time values are 3 - 4 mm and 0.12 - 0.15 seconds respectively [3]. Although the operating negative-strip time of Machine A was satisfactory, the operating mould lead was less than desired.

During the on-line sensor test at trial D3, the machine was found to be operating with a negative-strip time of nearly zero during one heat. The
stroke was set at 6 mm, operating near 4.8 mm, and the oscillation frequency was 1.4 Hz. Figure 5.15 illustrates the mould velocity, casting speed and force. Force was still in phase with velocity, and the load plateau in positive-strip time was apparent. Fortunately, the oil lubrication in this case was adequate to prevent excessive sticking. The billet surface was smooth; the oscillation marks were small and spaced at non-uniform intervals with the spacing likely impacted by metal level fluctuations. This is a clear example of how a "report card" of process parameters, created by an on-line system, would be useful to plant staff. It is likely that operating practices may not be implemented correctly at times given the confusion of changing moulds, lubricants, steel grades, and perhaps casting machines.

5.2.4 Stroke and Oscillation Frequency

Many billet casters operate a control scheme where the oscillation frequency is a function of casting speed. An unexpected dependence of stroke on oscillation frequency was also observed. The stroke of Machine B (Figure 5.11) increased from 9 to 11.5 mm when the casting speed was increased from 10 to 20 mm s\(^{-1}\). The oscillator mechanism of Machine B contained springs designed to reduce drive train loading. The increase in stroke was likely due to either inertial forces or spring resonance. Contrary to the performance of Machine B, the stroke of Machine C decreased with increasing casting speed and frequency. The stroke was observed to vary from 5.4 to 4.2 mm over the normal range of casting speeds experienced when casting 203 mm square billets.
Figure 5.16: Negative-strip time for an 8 mm sinusoidal oscillator.
The practice of varying oscillation frequency with casting speed is common among billet producers. With this technique it is possible to adjust the frequency to maintain a constant negative-strip time. Figure 5.16 presents the negative-strip time relationship for an 8 mm oscillator with changing casting speed and oscillation frequency. Thus, if negative-strip time is to be held constant with mould oscillation frequencies above 2 Hz, the frequency should be reduced with increasing casting speed. In contrast, in many operations, the frequency is changed in direct proportion to the casting speed. Increasing oscillation frequency with casting speed may significantly decrease negative-strip time, depending on how the machine is operating in reference to Figure 5.16.

5.2.5 Casting Speed Oscillation

Machines A and B exhibited a periodic variation in casting speed as seen in Figure 5.12. This effect was not observed on Machine C. The speed varied between 0.5 and 2 mm s\(^{-1}\), with the same period of the oscillator but out of phase. The casting speed variation may be due to the upward and downward friction created during positive and negative-strip time respectively. As the friction force acts downwards during the downstroke, the withdrawal drive system may speed up because of the reduced load. This effect may be a function of the withdrawal system. Mould-strand friction may apply enough torque to the withdrawal roll, via the billet, to temporarily speed up or slow down the withdrawal drive system. It is doubtful that this effect impacted billet quality, but it must be recognized that the casting speed varies. Infrequent sampling
of the casting speed signal may lead to erroneous results.

5.2.6 **Horizontal Movement**

Horizontal mould movement was obtained for a no-load case on Machine A. Four short-stroke LVDT displacement sensors were installed on a frame and inserted into the mould in a region of zero taper. The frame was attached to the plant floor. Horizontal movement with the sensors 50 mm from the mould top is illustrated in Figure 5.17. Movement was greatest in the north-south direction at 0.4 mm. Figure 5.18 illustrates the mould vertical-horizontal mould trajectory for one oscillation cycle; note that the mould was moving diagonally. How the horizontal movement changed under casting load was not known. In a study of caster alignment of four steel companies [100], all machines indicated greater alignment variation than the tolerances set for new machines. The threshold for excessive movement causing potential defects is not known. A suggested practice for slab machines has been to maintain horizontal movement below 0.2 mm [68].
Figure 5.17: Horizontal mould movement 50 mm from mould top - Machine A unloaded.
Figure 5.18: Mould trajectory - Machine A unloaded.
5.3 Process Control

On the machines tested, the casting speed was regulated by the plant control system based on the metal level set point. As the metal level rose, the casting speed would increase to restore the metal level, and vice versa. Radioactive metal level sensors, used in all plant trials, are reported to be accurate to \(\pm 5\) mm [38]. Further error can be introduced if the controller is not tuned correctly.

The transient nature of this process is well illustrated in Figure 5.19. Large variations in casting speed and metal level lasted for up to 50 seconds, greater than the dwell time of the steel in the mould. This behaviour was observed on all machines. The steel flow rate was calculated using Equation 5.1, which is simply the bulk flow of steel due to casting speed plus the volume flow rate associated with metal level fluctuations.

\[
\dot{V} = (v_s + \frac{\partial \rho}{\partial t})A_b
\]  

(5.1)

The casting speed and metal level signals shown in Figure 5.19 were used to calculate steel flow rate, presented in Figure 5.20. Metal level changes impacted the flow rate calculation very little. It is evident from Figure 5.20 that flow rate from the tundish is a transient process variable.

Steel flow rate was not controlled, other than by steel head in the tundish and nozzle size, on all machines tested. Steel level in the tundish is usually manually controlled by an operator activating a slide gate valve on
Figure 5.19: Transient response of metal level and casting speed typical of the machines tested. Trial D2, 203 mm mould, oil lubrication.
Figure 5.20: Steel flow rate calculated from metal level and casting speed signals. (1 l = 0.001 m$^3$)
the ladle. The operator sets the tundish level by monitoring tundish weight given by a load cell or by watching the level. Flow rate can also be affected by nozzle erosion and blockage. Since mould taper is designed for a particular casting speed and grade, flow rate variation will impact billet quality via changes in casting speed. The transient nature of this system could be reduced, and hence billet quality improved, by the addition of flow control to each casting machine. Metal level and casting speed should be set points, controlled by the flow rate.

Figure 5.21 illustrates another example of a process control problem in billet casting. The metal level fluctuated about a set point and the casting speed steadily increased, indicating an increase in steel flow rate into the mould. The increasing flow rate was likely caused by an eroding nozzle. Immediately after this data set was logged, a breakout occurred. The casting speed of 20.5 mm s\(^{-1}\) was not excessively high for casting 203 mm billets, and the force had not increased prior to the breakout. The increasing steel flow rate was the only potential “breakout warning” seen in logged signals.

The process control response also varied depending on the lubricant type: oil or powder. In powder casting, liquid steel flows into the mould through a submerged entry nozzle, so the slag layer remains undisturbed. Figures 5.22 and 5.23 illustrate typical responses for oil and powder casting respectively. The oil cast control signals were typically “rougher”. Much of this response was due to stream quality, which may cause meniscus turbulence. A rough meniscus will negatively impact billet quality, \(e.g.\) [13], and also cause
Figure 5.21: Stable metal level and increasing casting speed indicates increasing steel flow rate - Machine B.
Figure 5.22: "Rough" casting speed and metal level signals commonly seen when casting with oil lubrication. Trial D3, 0.80 pct. C, 194 mm mould.
Figure 5.23: Typical smooth casting speed and metal level signals when casting with mould fluxes. Trial D3, 0.30 pct. C + B, 194 mm mould.
a noisy metal level signal. Open stream pouring, *i.e.* oil casting, is also prone
to forming small, solidified globules of steel at the nozzle exit. The globules
form and release on the nozzle exit, and impact both stream quality and steel
flow rate.

5.4 Billet Samples

Shape defects, cracks, and bleeds and laps were common defects in the bil­
lets. Defects which have been linked to mould-strand friction (*i.e.* transverse
depressions and cracks) and mould taper were most important to this work.
Rhomboiditity, and bleeds and laps were the focus of another study [13] and
were not investigated in detail.

5.4.1 Surface Roughness

The difference in surface roughness between grades has been well es­

tablished in the literature [24]. Peritectic steels tend to have a surface char­
acterized by wrinkles and indentations, which are believed to be caused by
the $\delta$ to $\gamma$ phase transformation. Figure 5.24 shows the surface of a typical
peritectic steel billet containing 0.12 pct. carbon, taken from trial D1 using
oil lubrication. Figure 5.25 illustrates the surface of a hyper-peritectic steel
billet, in this case 0.32 pct. carbon, also taken from trial D1. As discussed in
Section 2.2, the rough surface seen in Figure 5.24 reduces heat transfer in the
mould.
Figure 5.24: Rough surface of a peritectic steel billet. Trial D1, 0.12 pct. C, 203 mm mould, oil lubrication.
Figure 5.25: Smooth surface of a hyper-peritectic steel billet. Trial D1, 0.32 pct. C, 203 mm mould, oil lubrication.
5.4.2 Shape Defects and Cracks

Figure 5.26 illustrates a 171 mm billet with serious defects. The billet was severely off-square, and contained large off-corner internal cracks caused by the billet distorting. Shell bulging was also evident on the left face, which may have formed inside or below the mould where the shell was thin. Midway cracks also formed below the mould when the shell reheated.

Transverse depressions were common shape defects, particularly at Company D. The peritectic steels and boron grades were notably prone to forming transverse depressions. However, these defects were seen on all grades from peritectic to high carbon steels. Figures 5.27 and 5.28 illustrate two large
Figure 5.27: Large transverse depression. Trial D2, 0.32 pct. C + B, east face, 203 mm mould, oil lubrication.

Figure 5.28: Large transverse depression. Trial D2, 0.32 pct. C + B, south face, 203 mm mould, oil lubrication.
Figure 5.29: Surface crack on a deep transverse depression. Trial D2, 0.32 pct. C + B, east face, 203 mm mould, oil lubrication.
Figure 5.30: Subsurface cracks under a deep transverse depression. Trial D2, 0.32 pct. C + B, south face, 203 mm mould, oil lubrication.
Figure 5.31: Transverse depression showing both subsurface and surface cracks. Trial D2, 0.32 pct. C + B, 203 mm mould, oil lubrication.
transverse depression

transverse depression

Figure 5.32: Transverse depressions on a powder cast billet. Trial D2, 0.14 pct. C, 203 mm mould.
	ransverse depressions on 203 mm billets. Cracks were often seen with transverse depressions. Figure 5.29 shows a cross-sectional view of the depression shown in Figure 5.27; a surface crack is evident. Subsurface cracks, below a depression, are shown in Figure 5.30, which is a cross-section of the depression in Figure 5.28. Both surface and subsurface cracks are apparent in the cross-section shown in Figure 5.31. The transverse surface cracks are believed to form in the top of the mould, when the shell is thin and weak. Thus depressions with surface cracks most likely formed at the meniscus. Subsurface cracks are believed to form at the solidification front when the depression is lower in the mould. Since the shell is thin at a depression site due to reduced heat transfer, axial forces place the shell in tension and initiate cracks.
Figure 5.33: Macroetch of transverse depression on a powder cast billet. Trial D2, 0.14 pct. C, 203 mm mould.
A surprising observation from trial D2 was the formation of transverse depressions in powder cast billets. Figure 5.32 illustrates typical transverse depressions seen on 0.14 pct. carbon billets. In all cases the depressions were hinged about oscillation marks; the oscillation marks were deep because of the mould flux lubrication. The transverse depressions were also more acute than those found on billets cast with oil lubrication. Since the depressions were hinged about oscillation marks, they likely formed near the meniscus when the shell was thin. The weak shell adjacent the oscillation mark was easily deformed to produce the depression shape. Surface cracks were also commonly seen with these depressions, as shown in Figure 5.33.

Longitudinal midface depressions were observed on billets from trials D1 and D2, using both oil and powder lubrication. On the oil cast billets with this defect, the surface was smoothly concave and the defect was difficult to see without viewing a cross-section. Figure 5.34 shows typical longitudinal midface depressions seen on powder cast, 0.14 pct. carbon billets from trial D2. In contrast to the oil cast longitudinal depressions, the powder cast depressions were very acute as if the shell buckled abruptly. Also apparent in this photograph is that the depression wandered about the midface. Cracks sometimes accompanied these depressions, as is evident in Figure 5.35. Thus the longitudinal depressions and cracks most certainly formed near the meniscus when the shell was thin. As will be discussed later, these defects were believed to be formed by an excessive taper (relative to cooling and shrinkage of the shell) near the meniscus, causing the shell to buckle. Figure 5.36 illustrates
Figure 5.34: Typical longitudinal midface depressions on powder cast billets. Trial D2, 0.14 pct. C, 203 mm mould.
Figure 5.35: Longitudinal midface depression with corresponding surface crack. Trial D2, 0.14 pct. C, 203 mm mould, powder lubrication.

Figure 5.36: Photograph illustrating the depth of longitudinal midface depressions when powder casting. Trial D2, 0.14 pct. C, 203 mm mould.
Table 5.2: Depths of sample billet shape defects.

<table>
<thead>
<tr>
<th>Depression Type</th>
<th>Sample</th>
<th>Lubricant Type</th>
<th>Depth (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>longitudinal</td>
<td>1</td>
<td>oil</td>
<td>3</td>
</tr>
<tr>
<td>longitudinal</td>
<td>2</td>
<td>powder</td>
<td>7</td>
</tr>
<tr>
<td>transverse</td>
<td>1</td>
<td>powder</td>
<td>3</td>
</tr>
<tr>
<td>transverse</td>
<td>2</td>
<td>powder</td>
<td>2</td>
</tr>
<tr>
<td>transverse</td>
<td>3</td>
<td>oil</td>
<td>5</td>
</tr>
<tr>
<td>transverse</td>
<td>4</td>
<td>oil</td>
<td>4</td>
</tr>
<tr>
<td>transverse</td>
<td>5</td>
<td>oil</td>
<td>4</td>
</tr>
</tbody>
</table>

The depths and severity of some of these longitudinal depressions.

Contour plots of sample shape defects are presented in Appendix F. The depths of these typical shape defects are summarized in Table 5.2. These defects create serious barriers to heat transfer considering the fact that mould tapers are designed to a fraction of a millimeter. The shell adjacent to a depression is therefore very thin, and leaves the shell susceptible to cracking and breaking out, as well as incurring non-uniform shell growth. In addition to the cracks often associated with these defects, depressions can impact rolled product quality. Depressions which are acute, or contain a steep face, may fold during rolling operations and create a seam in the product. Billet conditioners must grind depression faces, as well as cracks, to ensure a smooth rolled product.

The contour plots also indicate a clear geometric orientation of these defects. These depressions are clearly longitudinal or transverse, there are no diagonal or corner depressions. This may appear like an obvious statement,
but it implies that credible mechanisms for depression formation must include a geometric component.
Chapter 6

Force Response and Process Upsets

The use of a strain gauge force sensor has provided, for the first time, quantitative force measurements on an industrial billet machine. This has facilitated more detailed evaluations of mould friction. This chapter presents an analysis of mould-strand friction and investigates friction response as a function of process variables and upsets.

6.1 Mould-Strand Friction

6.1.1 Friction Response of Oil and Powder Lubrication

The decompression of load cell signals has been shown to occur during the negative strip period [11]. Following this concept further, Figure 6.1 illustrates that load cell and mould velocity signals were in phase when casting billets with oil lubrication. This indicates that mould-strand interaction is a function of mould velocity (recall that force and mould acceleration were in phase during the no-load case) and it dominates the inertial force during casting. The
inertial force depends on stroke, oscillation frequency and mould mass. The magnitude of the velocity-dependent force likely depends on process variables such as lubrication and mould taper.

Solid and liquid lubrication regimes have been used to describe mould-strand friction [69]. Liquid friction, or hydrodynamic lubrication, is a function of relative velocity as governed by Newton’s law of viscous flow [69, 101, 102], and was shown in Equation 2.3. Equation 2.3 assumes a constant velocity gradient through the lubricant film and uniform viscosity. Thus for a liquid friction mode, the maximum friction occurs at the point of maximum velocity. Liquid friction should favor a smooth sinusoidal force response, similar to the mould-strand relative velocity profile.

Solid friction was described by the simple relationship of Equation 2.4. Solid friction is also called boundary lubrication, which is a lubrication regime governed by the type of metal surfaces and lubricant present [102]. Solid friction is not a function of relative velocity, or lubricant viscosity, but only depends on the direction of relative velocity: positive or negative. Solid friction is therefore characterized by a square wave force response, with the duration of the square wave longer during positive strip than negative strip. The maximum load plateau seen during oil casting (Figure 6.1) is indicative of solid lubrication. However, the smooth change in force during negative strip, coincident with mould velocity, indicates at least some dependence on relative velocity. Further, the change of minimum load with casting speed noted in one study [11] also supports a lubrication condition dependent on velocity.
Figure 6.1: Typical mould velocity and force signals when casting with oil lubrication. Trial D3, 0.70 pct. C, 171 mm mould.
Figure 6.2: Typical mould velocity and force signals when casting with mould powder lubrication. Trial D3, 194 mm mould, 0.30 pct. C + B.
A typical force response using mould flux is illustrated in Figure 6.2. As is evident, the force response was more sinusoidal and lacked the maximum load plateau seen when oil casting. The sinusoidal force response closely matches the mould velocity profile, supporting a liquid friction mode.

It is evident that oil and powder lubrication fundamentally differ. As billet producers are implementing powder casting practice, a friction sensor would be helpful in evaluating mould oscillation and lubrication.

6.1.2 Quantifying Force Response

Force response can be evaluated by monitoring the force range (maximum-minimum) or by the work done per cycle [68].

Work per Oscillation Cycle

Calculating work expended during an oscillation cycle is determined by integrating force over displacement for one cycle. For completeness, the work done during no-load operation can be subtracted to yield the work expended as mould friction. Figures 6.3 and 6.4 show example force-displacement plots for oil and powder lubricants respectively, when casting boron(Ti)-alloyed steels during trial D2. Figure 6.3 (oil cast) has a force range of 25000 N and work per cycle of 68 N m; Figure 6.4 (powder cast) has a force range of 12000 N and work per cycle of 25 N m. The no-load work per cycle was less than 5 N m. The character of force signals varied greatly, but general differences existed between oil and powder casting. Since oil is a less effective lubricant, oil usually
Figure 6.3: Force vs. displacement example for oil lubrication. Trial D2, 203 mm mould, 0.3 pct. C + B.

has a larger load range and work per cycle. Also, as the oil-cast force response tends to exhibit a square wave character, the oil-cast force-displacement plot has a larger area, or work done, for a given force range. For the diagrams presented, the oil-cast plot has approximately 2 times the load range and 2.5 times the work per cycle.
Figure 6.4: Force vs. displacement example for powder lubrication. Trial D2, 203 mm mould, 0.3 pct. C + B.
Force Range

Force range is a simpler way to track mould-strand friction on-line, although it is less rigorous than the work calculation. The raw force range includes machine forces as well as the mould-strand friction, thus the machine forces should be subtracted from the force range to yield a more accurate measure of mould-strand friction. The machine force consists of inertial loading from the mass of the mould assembly, plus machine friction. The machine forces were estimated on Machines B and C, by running the machines at various oscillation frequencies under cold conditions. Mould-strand friction was estimated by measuring the casting force range, and subtracting the cold machine force at the corresponding oscillation frequency. This calculation was assumed to be valid for the following reasons:

- The casting force range was significantly less than the cold force range. The cold force range was typically 10 - 30 pct. of the casting force on Machine C. Thus small errors in the cold machine force should not significantly impact the estimate of the mould-strand friction.

- When casting, force was in phase with velocity, indicating that the casting response was indeed governed by mould-strand interaction.

- The inertial force could be theoretically calculated based on mould acceleration. However, the acceleration signal was often very chaotic and it was simpler to combine and treat the inertial forces and machine friction together as a single machine force.
Figure 6.5 illustrates the cold oscillator force response of Machine C as a function of oscillation frequency, using data from trials D2 and D3. The data were arbitrarily fitted with an exponential curve, to facilitate easy calculation of the machine forces. The cold force range was quite low, near 4000 N, at a typical oscillation frequency of 2 Hz. A similar test was conducted on Machine B during plant trial A2. Figure 6.6 shows the cold machine forces using the strain gauge and piezoelectric strain sensors. Since the piezoelectric sensor
Figure 6.6: Machine B oscillator force response under no-load condition using strain gauge and piezoelectric strain sensors. Trial A2.
was less responsive to high frequency signal components, the force range was lower for cold and casting conditions, particularly at high frequencies. The cold response of machine B was more complex than Machine C, likely due to the more complicated mechanism of Machine B which contains springs. These data sets were curve fitted with a third order polynomial. The character of both strain signals were similar, again giving confidence in the sensor response.

6.1.3 Friction Coefficient Calculation

A friction coefficient was calculated assuming the simple solid friction model of Equation 2.4. $F_{\text{solid}}$ was assumed to be the casting force range ($F_{\text{range}}$) less the cold machine force ($F_{\text{cold}}$) divided by 2, to account for friction upwards and downwards during the oscillation cycle. The normal force, $N$, was assumed to be the average ferrostatic pressure in the mould multiplied by the mould-shell contact area as shown in Equation 6.1.

$$N = \frac{\rho_s g h_m A_m}{2}$$  \hspace{1cm} (6.1)

Combining Equations 2.4 and 6.1, the friction coefficient, $c$, can be calculated using Equation 6.2.

$$c = \frac{F_{\text{range}} - F_{\text{cold}}}{\rho_s g h_m A_m}$$  \hspace{1cm} (6.2)

Equation 6.2 leads to a conservative estimate of the friction coefficient because it assumes full mould-shell contact. The true normal force would likely
be less because of local shrinkage in the corners and shell strength containing some of the ferrostatic pressure. Thus the true friction coefficient may be higher than calculated. Table 6.1 illustrates two friction coefficient calculations using the force signals shown in Figures 6.3 and 6.4. The machine force was derived from Figure 6.5. The calculation assumes a boundary lubrication mode, but hydrodynamic lubrication (dependent on relative velocity) is likely present during powder casting. The main advantage of hydrodynamic lubrication is that it yields lower friction forces than boundary lubrication. Thus if liquid friction was present, the calculated friction coefficient would only be an “effective” solid coefficient and its value would be low. High friction coefficients seen during powder lubrication might indicate that a solid friction mode was operating or sticking/binding was taking place.

The calculated friction coefficients varied from 0.38 to 1.04, a significant range, but what friction coefficient value is to be expected? Friction between clean metal surfaces is rarely seen in practice due to contamination and the oxidation of metals [102]. Dry, contaminated metal surfaces typically have a friction coefficient between 0.1 and 0.3. Also, grease films are usually present.

### Table 6.1: Example friction coefficient calculations.

<table>
<thead>
<tr>
<th>Heat</th>
<th>Lubricant</th>
<th>$F_{\text{range}}$ (N)</th>
<th>Frequency (Hz)</th>
<th>$F_{\text{cold}}$ (N)</th>
<th>Net Force (N)</th>
<th>Friction (c)</th>
</tr>
</thead>
<tbody>
<tr>
<td>312</td>
<td>powder</td>
<td>11500</td>
<td>1.80</td>
<td>3300</td>
<td>8200</td>
<td>0.38</td>
</tr>
<tr>
<td>333</td>
<td>oil</td>
<td>25000</td>
<td>1.67</td>
<td>2900</td>
<td>22100</td>
<td>1.04</td>
</tr>
</tbody>
</table>
due to handling and forming of metals. Thin grease films have a strong affinity for metal surfaces and usually cannot be removed completely even with solvents [102]. For copper on steel in a boundary lubrication condition, the friction coefficient is typically 0.09 to 0.28 [102]. Perfectly clean metal surfaces, rarely seen in practice, can exhibit very high friction coefficients due to welding of the surfaces. It is generally accepted that friction coefficients greater than unity are not possible without adhesion.

It has been reported that little lubricating oil survives the mould environment at the meniscus, particularly when the mould is operating above the boiling temperature of the oil [13]. Thus calculated friction coefficients near 0.3 or 0.5 seem reasonable. High friction coefficients (i.e. greater than 1) are believed to be caused by mould-billet binding or sticking. The concept of mould-billet binding has been postulated assuming that the mould was too steeply tapered for billet shrinkage [6]. However, this is the first work to attempt to quantify binding or sticking using actual friction measurements. If the mould is excessively tapered, it may squeeze the billet and cause an excessively high normal force, resulting in a high friction coefficient. Mould-billet binding will be discussed in further in Chapter 9.

6.1.4 Accelerometer and Force Signals

As discussed in Section 2.9.3, claims have been made that accelerometers can produce “friction” signals using suitable signal processing and/or software. Although the nature of these calculations has not been reported, they likely
Figure 6.7: FFT of accelerometer signals corresponding to high and low friction. Trial D3, 0.30 pct. C + B, oil lubrication, 171 mm mould.

originates from frequency measurements since accelerometers are often used for measuring vibration on machinery.

When casting 171 mm, oil-lubricated, boron(Ti)-alloyed billets during trial D3, a force upset occurred during heat 046. The force range increased, between logged data sets, from 17000 to 40000 N. The friction coefficient, $c$, increased significantly from 0.6 to 1.7. Since the machine, steel chemistry and lubricant type had not changed, these data sets seemed appropriate to
Figure 6.8: FFT of accelerometer signals corresponding to high and low friction. Trial D3, 0.30 pct. C + B, oil lubrication, 171 mm mould.
investigate the accelerometer response. Fast Fourier transforms (FFTs) of the accelerometer signals corresponding to these friction measurements were calculated. Figure 6.7 shows the amplitude response of the accelerometer signals versus frequency. As is evident, little frequency information existed in the signals above 25 Hz. Figure 6.8 illustrates the same data as Figure 6.7 with a reduced frequency scale. The FFT plots clearly show the oscillation frequency near 2 Hz, plus higher order harmonics and machine vibrations. It is uncertain if anything regarding friction can be inferred from this information. It appears that the low friction data (set 1) may have slightly higher amplitude peaks from 8 - 13 Hz. Perhaps one would expect higher vibration in the higher friction case. Regardless, there is nothing apparent in this information that is as clear as the force range changing from 17000 to 40000 N.

Other accelerometer data sets were investigated, and similarly an obvious correlation was not seen. This is not to imply that the accelerometer claims in the literature are false. The reports of the ML Tektor system seem promising, but that research was conducted on slab casting machines. Since acceleration is a strictly a function of mould movement, any accelerometer based system must be highly dependent on machine response. The objective of this work was to measure friction quantitatively, and strain gauge force sensing has provided this. The accelerometer signal is an implicit measure of friction at best, thus these signals were not investigated further given the success of strain gauge force sensing.
6.2 Transverse Depressions

Several mechanisms for the formation of transverse depressions have been published, as discussed in Section 2.8.7. The formation of transverse depressions was investigated using simultaneous load cell, metal level and mould temperature data from trial D1. The midface array of thermocouples on the east face was used for the detection of transverse depressions. The sequence inventor (SEQIV) animation tool, developed by John Hogg at UBC [103], was used to view the temperature data in space-time-temperature. SEQIV is a powerful tool that allows the user to view a depression clearly on an animated surface. It also serves as a filter for "false" depressions which appear as a temperature drop in meniscus thermocouple signals caused by metal level fluctuations. Once a clear, large depression was detected with SEQIV, pertinent load cell, thermocouple and metal level data were investigated.

Figure 6.9 shows an image of stable mould temperature response, i.e. the mould temperature profile was not changing in time. The vertical lines in the image represent thermocouple locations in the mould. Figure 6.10 illustrates a transverse depression as a diagonal feature in the temperature-space-time surface. The depression clearly formed at the meniscus, then propagated down the mould as a local decrease in temperature. Figure 6.11 is an image that presents two effects: multiple transverse depressions were evident and a rough meniscus was indicated by unstable temperatures in the meniscus region.

Candidate depressions were evaluated in 3 heats of 0.32 pct. carbon and
Figure 6.9: Smooth mould temperature response. Trial D1, 0.8 pct. C, 203 mm mould, oil lubrication.
Figure 6.10: Mould temperature response showing the formation of a transverse depression at the meniscus and its propagation down the mould. Trial D1, 0.32 pct. C, 203 mm mould, oil lubrication.
Figure 6.11: Mould temperature response illustrating a rough metal level and transverse depressions. Trial D1, 0.32 pct. C + B, 203 mm mould, oil lubrication.
Figure 6.12: Force sensor and mould temperature response during the formation of transverse depressions. Trial D1, 0.32 pct. C, 203 mm mould, oil lubrication.
Figure 6.13: Force sensor and mould temperature response during the formation of transverse depressions. Trial D1, 0.32 pct. C + B, 203 mm mould, oil lubrication.
0.32 pct. carbon, boron(Ti)-alloyed grades. Figure 6.10 is an excellent example of depression formation. The mould temperature response was relatively smooth and the large temperature drop, 50°C, indicated a large depression. Figure 6.12 shows the force sensor response (maximum load and load range) and mould temperature of 2 thermocouples near the meniscus during the time period of the SEQIV image in Figure 6.10. Transverse depressions can be seen at 220 and 240 seconds. As is evident in Figure 6.12, there is no clear change in force response during the formation of these depressions. Figure 6.13 illustrates sensor data under more “chaotic” operating conditions, when the metal level was very rough and many transverse depressions were forming. The load range varied significantly, perhaps due in part to variable lubrication, but again there was no correlation to individual depression events.

An unexpected result of using the SEQIV tool was the visualization of near meniscus temperature data indicating the metal level stability. Figure 6.9 illustrates a smooth metal level. A rough metal level caused by an off-centre or ropey stream is indicated by a “wavy” surface at the meniscus, apparent in Figure 6.11. The average and standard deviation of the meniscus thermocouple temperature is presented in Table 6.2 for several oil-cast heats. A smooth metal level (Figure 6.9) will exhibit a standard deviation of the meniscus thermocouple temperature of less than 3°C. A rough meniscus (Figure 6.11) will have a standard deviation near 8°C. Thus the metal level stability can be implied by the standard deviation of meniscus thermocouple temperature. This simple logic could be easily implemented in an on-line system and presented...
Table 6.2: Inferring stream quality and metal level stability from thermocouple data.

<table>
<thead>
<tr>
<th>Heat</th>
<th>Grade</th>
<th>SEQIV surface</th>
<th>Meniscus Thermocouple (°C)</th>
<th>Standard Deviation (°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>142</td>
<td>0.12 pct. C</td>
<td>smooth</td>
<td>123</td>
<td>2.47</td>
</tr>
<tr>
<td>146</td>
<td>0.32 pct. C</td>
<td>very smooth</td>
<td>190</td>
<td>2.68</td>
</tr>
<tr>
<td>147</td>
<td>0.32 pct. C+B</td>
<td>rough</td>
<td>169</td>
<td>7.15</td>
</tr>
<tr>
<td>148.1</td>
<td>0.32 pct. C+B</td>
<td>rough</td>
<td>165</td>
<td>6.85</td>
</tr>
<tr>
<td>148.2</td>
<td>0.32 pct. C+B</td>
<td>very rough</td>
<td>169</td>
<td>8.22</td>
</tr>
<tr>
<td>149</td>
<td>0.84 pct. C</td>
<td>very smooth</td>
<td>193</td>
<td>2.96</td>
</tr>
</tbody>
</table>

as a measure of process control quality.

It is evident from these examples that the formation of transverse depressions cannot be seen uniquely in force signals. Force sensing may not clearly detect local mould-strand interaction events, particularly when the shell is thin. This is supported by the weak correlation of force sensing with sticker breakouts in slab casting [78]. Metal level variation prior to a depression forming has been noted in the literature [14, 15] and observations in this work has confirmed this. Using the SEQIV tool, a short term metal level rise was seen as an increase in temperature just above the meniscus prior to the formation of many depressions. The rough metal level inferred by the meniscus thermocouple has two main implications. Firstly, more transverse depressions were seen in the presence of the turbulent meniscus, supporting the influence of metal level on depression formation. Secondly, the metal level was more unstable when casting the boron(Ti)-alloyed grades. This was believed to be
caused by the steelmaking practice of these grades. The boron(Ti) steels were aluminum-killed to achieve very low oxygen levels, which is required to successfully alloy steel with boron(Ti). The formation of Al$_2$O$_3$ in aluminum-killed steels is problematic for continuous casting operations, since Al$_2$O$_3$ plugs metering nozzles [104, 105]. Thus the Al$_2$O$_3$ blockages were believed to contribute to poor stream quality and a rough meniscus.

Boron(Ti)-alloyed steels have been reported to be prone to forming depressions because of possible increased strength near the solidus temperature [14]. The strength of as-cast samples from trial D1 were evaluated on a Gleeble thermomechanical testing machine. Samples with 0.32 pct. carbon and 0.32 pct. carbon boron(Ti)-alloyed were tested at a strain rate of 10$^{-2}$ s$^{-1}$ at 1200 and 1300°C [106]. Appendix G contains the stress-strain curves. The boron(Ti) steels exhibited a significantly higher flow stress, supporting the theory of Samarasekera et al. [14]. Further metallurgical research must be conducted to ensure that the testing is conducted as close to in-situ conditions as possible.

A summary of the knowledge of transverse depression formation is listed below.

1. The formation of a transverse depression is often preceded by a metal level rise.

2. Transverse depression formation cannot be seen with a force sensor.

3. Transverse depressions have not been seen to form lower in the mould.
in the mould temperature response. Thus their complete formation by binding is unlikely. However, axial friction forces can crack the thin shell of a depression at the solidification front.

4. Transverse depressions exhibit a clear geometry across the billet face. A mechanism such as "local overcooling" is incomplete since it does not include an explanation of the geometry.

5. Depressions in oil-cast billets are likely caused by thermal distortion [60], or interaction with the lubrication oil [14] during a metal level rise.

6. Depressions in powder-cast billets may be caused by thermal distortion [60] or consumption of the slag rim during a metal level rise [15]. The author postulates that the shell may buckle at the meniscus due to friction or interaction with the slag rim.

7. It is unlikely that a "unified" theory of depression formation exists; more than one type of upset may form transverse depressions.

6.3 Force Response and Steel Grade

Although the friction response may be impacted by process upsets, force was generally noted to vary between steel grades. Table 6.3 presents force range values for various steel grades cast with oil lubrication during trial D1. Each value represents an average force range taken from a 300 second data set; thus the force values represent average friction, and not instantaneous values. The
Table 6.3: Summary of load cell friction measurements from trial D1. Oil lubrication, constant oscillation frequency.

<table>
<thead>
<tr>
<th>Heat</th>
<th>Grade</th>
<th>Mould (mm)</th>
<th>Force Range (N)</th>
</tr>
</thead>
<tbody>
<tr>
<td>142.1</td>
<td>0.12 pct. C</td>
<td>203</td>
<td>3200</td>
</tr>
<tr>
<td>142.2</td>
<td>0.12 pct. C</td>
<td>203</td>
<td>3300</td>
</tr>
<tr>
<td>146.1</td>
<td>0.32 pct. C</td>
<td>203</td>
<td>4200</td>
</tr>
<tr>
<td>146.2</td>
<td>0.32 pct. C</td>
<td>203</td>
<td>3900</td>
</tr>
<tr>
<td>147.1</td>
<td>0.32 pct. C + B</td>
<td>203</td>
<td>5400</td>
</tr>
<tr>
<td>147.2</td>
<td>0.32 pct. C + B</td>
<td>203</td>
<td>5600</td>
</tr>
<tr>
<td>148.1</td>
<td>0.32 pct. C + B</td>
<td>203</td>
<td>3700</td>
</tr>
<tr>
<td>148.2</td>
<td>0.32 pct. C + B</td>
<td>203</td>
<td>6100</td>
</tr>
<tr>
<td>149.1</td>
<td>0.83 pct. C</td>
<td>203</td>
<td>4700</td>
</tr>
<tr>
<td>149.2</td>
<td>0.83 pct. C</td>
<td>203</td>
<td>4600</td>
</tr>
</tbody>
</table>

Peritectic grades exhibited the lowest forces, followed by the hyper-peritectic steels, and finally the boron(Ti)-alloyed steels showed the highest forces. The oscillation frequency was constant (1.9 Hz) during this trial, so the machine forces should have been constant. The lower forces measured when casting peritectic steel is consistent with the results of Singh and Blazek [23], who measured withdrawal forces on a bench-scale caster. The results are not consistent with the research of van der Stel et al. [49], where a “friction index” was measured with an accelerometer based system. In the case of van der Stel et al., the peritectic steels showed the highest accelerometer “friction index”.

Similar results were obtained with quantitative force measurements at trial D2. Table 6.4 presents friction measurements for the oil-cast heats. The peritectic grades exhibited an average friction coefficient of 0.53 and the hyper-
Table 6.4: Summary of oil-cast friction measurements from trial D2.

<table>
<thead>
<tr>
<th>Heat</th>
<th>Grade</th>
<th>Mould</th>
<th>Raw Force (N)</th>
<th>Frequency (Hz)</th>
<th>Friction (c)</th>
</tr>
</thead>
<tbody>
<tr>
<td>298.1</td>
<td>0.13 pct. C</td>
<td>203</td>
<td>12700</td>
<td>1.60</td>
<td>0.47</td>
</tr>
<tr>
<td>298.2</td>
<td>0.13 pct. C</td>
<td>203</td>
<td>14600</td>
<td>1.80</td>
<td>0.53</td>
</tr>
<tr>
<td>299.1</td>
<td>0.12 pct. C</td>
<td>203</td>
<td>14200</td>
<td>1.67</td>
<td>0.53</td>
</tr>
<tr>
<td>299.2</td>
<td>0.12 pct. C</td>
<td>203</td>
<td>13200</td>
<td>1.75</td>
<td>0.47</td>
</tr>
<tr>
<td>300.1</td>
<td>0.13 pct. C</td>
<td>203</td>
<td>14200</td>
<td>1.43</td>
<td>0.55</td>
</tr>
<tr>
<td>300.2</td>
<td>0.13 pct. C</td>
<td>203</td>
<td>16700</td>
<td>1.75</td>
<td>0.63</td>
</tr>
<tr>
<td>333.1</td>
<td>0.32 pct. C+B</td>
<td>203</td>
<td>12800</td>
<td>1.49</td>
<td>0.48</td>
</tr>
<tr>
<td>333.2</td>
<td>0.32 pct. C+B</td>
<td>203</td>
<td>12200</td>
<td>1.50</td>
<td>0.46</td>
</tr>
<tr>
<td>333.3</td>
<td>0.32 pct. C+B</td>
<td>203</td>
<td>23700</td>
<td>1.67</td>
<td>1.00</td>
</tr>
<tr>
<td>334.1</td>
<td>0.32 pct. C+B</td>
<td>203</td>
<td>27400</td>
<td>1.60</td>
<td>1.15</td>
</tr>
<tr>
<td>334.2</td>
<td>0.32 pct. C+B</td>
<td>203</td>
<td>24200</td>
<td>1.56</td>
<td>1.00</td>
</tr>
<tr>
<td>334.3</td>
<td>0.32 pct. C+B</td>
<td>203</td>
<td>18800</td>
<td>1.46</td>
<td>0.77</td>
</tr>
<tr>
<td>351.1</td>
<td>0.80 pct. C</td>
<td>203</td>
<td>15000</td>
<td>1.63</td>
<td>0.57</td>
</tr>
<tr>
<td>351.2</td>
<td>0.80 pct. C</td>
<td>203</td>
<td>18600</td>
<td>1.60</td>
<td>0.74</td>
</tr>
<tr>
<td>352.1</td>
<td>0.81 pct. C</td>
<td>203</td>
<td>16500</td>
<td>1.67</td>
<td>0.63</td>
</tr>
<tr>
<td>352.2</td>
<td>0.81 pct. C</td>
<td>203</td>
<td>17200</td>
<td>1.70</td>
<td>0.66</td>
</tr>
<tr>
<td>353</td>
<td>0.80 pct. C</td>
<td>203</td>
<td>16200</td>
<td>1.54</td>
<td>0.63</td>
</tr>
</tbody>
</table>

Table 6.5: Summary of powder-cast friction measurements from trial D2.

<table>
<thead>
<tr>
<th>Heat</th>
<th>Grade</th>
<th>Mould</th>
<th>Raw Force (N)</th>
<th>Frequency (Hz)</th>
<th>Friction (c)</th>
</tr>
</thead>
<tbody>
<tr>
<td>265</td>
<td>0.13 pct. C</td>
<td>203</td>
<td>14000</td>
<td>2.27</td>
<td>0.41</td>
</tr>
<tr>
<td>266</td>
<td>0.13 pct. C</td>
<td>203</td>
<td>13000</td>
<td>2.05</td>
<td>0.41</td>
</tr>
<tr>
<td>277.1</td>
<td>0.14 pct. C</td>
<td>203</td>
<td>14500</td>
<td>2.50</td>
<td>0.37</td>
</tr>
<tr>
<td>277.2</td>
<td>0.14 pct. C</td>
<td>203</td>
<td>15700</td>
<td>2.50</td>
<td>0.43</td>
</tr>
<tr>
<td>278.1</td>
<td>0.13 pct. C</td>
<td>203</td>
<td>13700</td>
<td>2.27</td>
<td>0.40</td>
</tr>
<tr>
<td>278.2</td>
<td>0.13 pct. C</td>
<td>203</td>
<td>16000</td>
<td>2.50</td>
<td>0.44</td>
</tr>
<tr>
<td>310</td>
<td>0.30 pct. C+B</td>
<td>203</td>
<td>12500</td>
<td>2.00</td>
<td>0.40</td>
</tr>
<tr>
<td>311</td>
<td>0.33 pct. C+B</td>
<td>203</td>
<td>11000</td>
<td>1.80</td>
<td>0.36</td>
</tr>
<tr>
<td>312</td>
<td>0.30 pct. C+B</td>
<td>203</td>
<td>11000</td>
<td>1.80</td>
<td>0.36</td>
</tr>
</tbody>
</table>
peritectic grades showed a slightly higher friction coefficient of 0.65. Again, the boron(Ti)-alloyed steels exhibited a higher and more varied force response, with an average friction coefficient of 0.81.

Heats cast with mould fluxes exhibited a significantly different friction response than the oil-cast heats. Table 6.5 presents the friction response of powder-cast heats from trial D2. The friction response of powder lubrication can vary significantly depending on powder properties and mould oscillation, but the friction was generally less than that of oil lubrication. Also, the average friction coefficient when powder casting was likely independent of steel grade. The average friction coefficient when powder casting was 0.40. This was less than the friction measured when oil casting peritectic steels \((c = 0.53)\) and significantly less than the corresponding oil-cast boron(Ti) steels \((c = 0.81)\). It appears that casting with mould fluxes has a very forgiving effect on the boron(Ti) grades.

Table 6.6 details force measurements from trial D3. No peritectic steels were cast during this trial, but once again the oil-cast boron(Ti) grades exhibited high and varied forces. The powder-cast grades exhibited higher friction than trial D2. This was likely caused by the use of the different mould, and higher casting speeds.

6.4 Friction and Process Control

As discussed previously, lack of flow rate control in billet casting leads to casting speed variations. As the casting speed changes, the mould taper may be
Table 6.6: Summary of friction measurements from trial D3.

<table>
<thead>
<tr>
<th>Heat</th>
<th>Grade</th>
<th>Mould (mm)</th>
<th>Lubricant</th>
<th>Force Range(N)</th>
<th>Frequency (Hz)</th>
<th>Friction (c)</th>
</tr>
</thead>
<tbody>
<tr>
<td>036</td>
<td>0.25 pct. C</td>
<td>171</td>
<td>oil</td>
<td>14500</td>
<td>2.31</td>
<td>0.43</td>
</tr>
<tr>
<td>038</td>
<td>0.45 pct. C</td>
<td>171</td>
<td>oil</td>
<td>16200</td>
<td>2.31</td>
<td>0.51</td>
</tr>
<tr>
<td>039</td>
<td>0.70 pct. C</td>
<td>171</td>
<td>oil</td>
<td>18800</td>
<td>2.27</td>
<td>0.64</td>
</tr>
<tr>
<td>041</td>
<td>0.70 pct. C</td>
<td>171</td>
<td>oil</td>
<td>18000</td>
<td>2.31</td>
<td>0.59</td>
</tr>
<tr>
<td>046.1</td>
<td>0.30 pct. C+B</td>
<td>171</td>
<td>oil</td>
<td>17000</td>
<td>2.11</td>
<td>0.59</td>
</tr>
<tr>
<td>046.2</td>
<td>0.30 pct. C+B</td>
<td>171</td>
<td>oil</td>
<td>40000</td>
<td>1.90</td>
<td>1.09</td>
</tr>
<tr>
<td>046.3</td>
<td>0.30 pct. C+B</td>
<td>171</td>
<td>oil</td>
<td>27000</td>
<td>1.90</td>
<td>1.15</td>
</tr>
<tr>
<td>046.4</td>
<td>0.30 pct. C+B</td>
<td>171</td>
<td>oil</td>
<td>40000</td>
<td>1.90</td>
<td>1.70</td>
</tr>
<tr>
<td>056</td>
<td>0.30 pct. C+B</td>
<td>194</td>
<td>powder</td>
<td>21900</td>
<td>2.31</td>
<td>0.59</td>
</tr>
<tr>
<td>058</td>
<td>0.30 pct. C+B</td>
<td>194</td>
<td>powder</td>
<td>22900</td>
<td>2.34</td>
<td>0.71</td>
</tr>
<tr>
<td>073</td>
<td>0.30 pct. C+B</td>
<td>194</td>
<td>powder</td>
<td>19300</td>
<td>2.34</td>
<td>0.58</td>
</tr>
<tr>
<td>089</td>
<td>0.30 pct. C+B</td>
<td>194</td>
<td>powder</td>
<td>16800</td>
<td>2.34</td>
<td>0.47</td>
</tr>
<tr>
<td>090</td>
<td>0.30 pct. C+B</td>
<td>194</td>
<td>powder</td>
<td>18700</td>
<td>2.34</td>
<td>0.55</td>
</tr>
</tbody>
</table>
inappropriate for the casting conditions, causing binding or excessive gap formation. Lubrication effectiveness also changes with casting speed, particularly with mould fluxes.

Trending the force range over long time periods has shown a clear dependency of force on casting speed. Figure 6.14 shows force range and casting speed signals when casting a boron(Ti) grade with mould flux in a 194 mm mould. For the first 500 seconds, the casting speed was 18.5 mm s\(^{-1}\) due to
Table 6.7: Friction and casting speed response for four powder-cast heats of 0.32 pct. C + B. Trial D2, 203 mm mould.

<table>
<thead>
<tr>
<th>Heat</th>
<th>Force Range (N)</th>
<th>Friction (c)</th>
<th>Casting Speed (mm s$^{-1}$)</th>
</tr>
</thead>
<tbody>
<tr>
<td>310</td>
<td>12500</td>
<td>0.40</td>
<td>18.4</td>
</tr>
<tr>
<td>311</td>
<td>11000</td>
<td>0.36</td>
<td>19.1</td>
</tr>
<tr>
<td>312</td>
<td>11000</td>
<td>0.36</td>
<td>18.0</td>
</tr>
<tr>
<td>313.1</td>
<td>24000</td>
<td>1.05</td>
<td>12.5</td>
</tr>
<tr>
<td>313.2</td>
<td>24500</td>
<td>1.05</td>
<td>14.3</td>
</tr>
<tr>
<td>313.3</td>
<td>26000</td>
<td>1.15</td>
<td>13.8</td>
</tr>
</tbody>
</table>

a slightly plugged metering nozzle. When the nozzle cleared and speed increased, the friction decreased. The metal level was stable during this period. The minimum recommended casting speed for this mould was 21 mm s$^{-1}$; at this speed, the force range was at its minimum. At 500 seconds (Figure 6.14), the effective friction coefficient was 1.2. When the casting speed increased from 18.5 to 21 mm s$^{-1}$, the friction coefficient dropped to 0.6. It is evident that a force sensor is a useful tool for developing process control guidelines for specific mould tapers, grades and lubricants.

Table 6.7 shows the force sensor response for four powder-cast heats when casting a 0.32 pct. carbon, boron(Ti)-alloyed steel. The friction increased significantly in heat 313, and the friction coefficient suggests that binding was occurring. Table 6.7 also indicates that the casting speed had dropped markedly during heat 313. The possibility of binding with decreasing casting speed has been reported [5]. A further complication arises when casting with mould powder lubrication; mould powder consumption and friction change.
with varying oscillation parameters and casting speed. This will be discussed in detail in Chapter 9.

Friction upsets also occur when casting with oil lubrication, but a correlation with casting speed was not established. Trending of the sensor signals showed no clear relationship between force and casting speed. In oil casting, the varying friction was believed to be caused by a combination of binding and lubrication effectiveness. This will be discussed in later sections.

6.5 Force Upsets

6.5.1 Friction and Mould Stroke

The oscillator stroke varied with mould friction, as shown in Figure 6.15, where these signals were trended for 3000 seconds. The correlation was particularly clear at 600 seconds, when the force range dropped from 35000 to 17000 N and the stroke increased from 4.05 to 4.4 mm. The difference in stroke was likely caused by elastic deformation of machine components under load. The design stroke for this machine was 6 mm, and the machine oscillated at an average stroke of 4.3 mm. The mould lead was approximately 0.7 mm, much less than the recommended value of 3 - 4 mm [3].

6.5.2 Nozzle Plugging

During trial A2, the metering nozzle began to severely plug when open stream pouring. Figure 6.16 shows the friction and casting speed signals during this
Figure 6.15: Oscillator stroke varying as a function of mould-strand friction. Trial D3.
event. When the casting operator was clearing the nozzle with an oxygen lance, the strand was stopped and restarted, causing high forces. After the upset, the friction returned to normal levels quickly. The surface quality of the billet section in the mould during the upset would likely be poor, with possibly cracks caused by the high forces. The billet corresponding to this upset should have been discarded.
6.5.3 Breakout

Two breakouts were logged during plant trials at Company A. Figure 6.17 illustrates the friction and casting speed response during a breakout when casting through a 152 mm mould using oil lubrication. There was no “breakout warning” issued by the force signal. When the breakout occurred, however, the mould was exposed to high forces as the liquid steel flowed through the mould uncontrolled, likely sticking to the mould wall. The stream quality was very poor during this heat, causing a rough meniscus. Globules of steel were forming constantly on the nozzle exit, possibly as a result of poor steelmaking.

A breakout also occurred when casting through a 203 mm mould using powder lubrication. Again, no change in the friction signal preceded the breakout.

6.5.4 Sticking and Jerking

Figure 6.18 illustrates the strain gauge response during a period of observed meniscus sticking when casting with oil lubrication. Meniscus sticking is characterized by the steel welding to the mould wall due to a combination of poor lubrication and a fluctuating metal level. The “sticks” are subsequently stripped off by mould oscillation. Despite the sticking and changing casting speed, the force response \( c = 0.3 \) lacked any correlation to these events. It appears than meniscus sticking could not be seen with force sensors. However, the poor lubrication which contributed to the sticking may have resulted in increased friction.
Figure 6.17: Friction response during a strand breakout. Trial A2, 0.90 pct. C, 152 mm mould, oil lubrication.
Figure 6.18: Force sensor response during a period of meniscus sticking. Trial A1, 0.71 pct. C, 203 mm mould, oil lubrication.
Figure 6.19: Force sensor response during a period of strand jerking. Trial A1, 0.90 pct. C, 203 mm mould, oil lubrication.
Figure 6.19 shows the strain gauge response when strand jerking was occurring. A fluctuating force range was evident; the friction coefficient varied between 0.2 and 0.5. It is unknown if the jerking was caused by binding or poor lubrication, but one would not expect excessive binding with the 0.8 pct. m$^{-1}$ tapered mould used during trial A1. Strand jerking was observed in the spray chamber below the mould and the jerking produced a squeak/groan noise, which likely indicated poor lubrication.
Chapter 7

Mathematical Modelling of Thermomechanical Mould Behaviour

This chapter discusses existing mathematical models which were used to calculate the mould temperature distribution and mould distortion. Results of these models were used to interpret mould-billet binding, and will be presented in Chapter 9.

7.1 Mathematical Modelling of Mould Heat Transfer

The mould heat transfer model was used for two reasons. First, to calculate a temperature field in the mould so mould distortion could be calculated by a stress model. Second, to calculate an axial heat flux profile to apply to a billet solidification model.

The heat transfer model calculates the mould temperature distribution through a longitudinal, midface section of the mould. Details of the model have been published in several sources [8, 34, 107, 17]; an overview of the model
Figure 7.1: Schematic of longitudinal mould heat transfer model.
will be presented here for completeness. Figure 7.1 illustrates the mould model geometry. The model simply takes an input heat flux profile, and calculates the mould temperature distribution based on cooling water parameters. The input heat flux profile was modified until the calculated temperature profile matched (within 1°C) the temperature profile measured in the subject plant trial. The following assumptions were made in the model formulation [34].

- Heat transfer in the transverse direction was negligible, from symmetry.

- Heat transfer to the water baffle was negligible.

- The cooling water between the mould and water baffle was in plug flow.

- Thermal properties of the mould were independent of temperature.

- Top and bottom surfaces of the mould were adiabatic.

- Transient variations of heat flux caused by mould oscillation and metal level fluctuations were neglected.

Heat transfer in the mould is governed by the two-dimensional transient heat conduction equation:

\[
k_m \left( \frac{\partial^2 T}{\partial x^2} + \frac{\partial^2 T}{\partial z^2} \right) = \rho_m C_{pm} \frac{\partial T}{\partial t}
\]

A steady-state model was originally employed [34]; the transient model was later developed to investigate thermal cycling caused by various boiling regimes [107]. The following mould boundary conditions were assumed.
1. Top and bottom mould wall boundaries were assumed to be adiabatic.

\[ 0 \leq x \leq X_m, \; z = 0 \text{ and } z = Z_m, \; t > 0 \]

\[ -k_m \frac{\partial T}{\partial z} = 0 \]  \hspace{1cm} (7.2)

2. Mould cold face

\[ x = 0, \; 0 \leq z \leq Z_m, \; t > 0 \]

\[ -k_m \frac{\partial T}{\partial x} = h_w(z, t)(T(0, z, t) - T_w(z, t)) \]  \hspace{1cm} (7.3)

3. Mould hot face below meniscus

\[ x = X_m, \; Z_F < z \leq Z_M, \; t > 0 \]

\[ -k_m \frac{\partial T}{\partial x} = q_s(z) \]  \hspace{1cm} (7.4)

4. Mould hot face above meniscus

\[ x = X_M, \; 0 \leq z \leq Z_F, \; t > 0 \]

\[ -k_m \frac{\partial T}{\partial x} = h_a(z, t)(T(X_M, z, t) - T_a) \]  \hspace{1cm} (7.5)

The initial temperature of the mould was assumed to be constant.

\[ 0 \leq x \leq X_M, \; 0 \leq Z \leq Z_M, \; t = 0 \]

\[ T = T_0 \]  \hspace{1cm} (7.6)

The inlet temperature of the mould water was assumed to be constant.

\[ z = Z_M, \; t \geq 0 \]

\[ T_w = T_i \]  \hspace{1cm} (7.7)
Heat transfer between the mould and cooling water (Equation 7.3) was defined in three heat transfer regimes: forced convection, transition boiling, and film boiling [81]. The following relationship defines the forced convection heat transfer coefficient, $h_{fc}$.

$$\frac{h_{fc}D_H}{k_f} = 0.023\left[\frac{\rho_fV_fD_H}{\mu_f}\right]^{0.8}\left[\frac{C_p\mu_f}{k_f}\right]^{0.4} \quad (7.8)$$

The fluid properties correspond to the bulk temperature of the fluid. The heat flux by forced convection, $q_{fc}$, was calculated by the heat transfer coefficient relationship, $q_{fc} = h_{fc}(T - T_w)$. Equation 7.8 is valid as long as the cold face of the mould is less than approximately 160°C [34]. This is a valid assumption in most cases. However, under conditions of pool boiling, the heat flux was
obtained from the following empirical relation [34].

\[
\frac{C_{pl}(T - T_{sat})}{H_{fg}} = C_s f \left[ \frac{q_b}{\mu_1 H_{fg}} \left[ \frac{\sigma}{g(\rho_l - \rho_v)} \right]^{0.5} \right]^{0.33} \left( \frac{C_{pl} \mu_l}{k_l} \right)^{0.5} \tag{7.9}
\]

A transition region exists between the commencement of boiling and when fully developed pool boiling exists, i.e. when Equation 7.9 is valid. The following relationship was used to calculate the transition boiling heat flux, \( q_{tr} \), as a function of equations 7.8 and 7.9 [34].

\[
q_{tr} = q_{fc} \left[ 1 + \frac{q_b}{q_{fc}} \left( 1 - \frac{q_{in}}{q_b} \right)^2 \right]^{0.5} \tag{7.10}
\]

The heat flux at the point of incipient boiling is given by the following relationship.

\[
q_{in} = 5.281 \cdot 10^{-3} p^{1.156} [1.8(T - T_{sat})]^{2.34} \tag{7.11}
\]

Figure 7.2 shows example heat flux values for forced convection boiling developed from the preceding equations [107].

Heat transfer in the cooling water channel was governed by the following relationship, assuming that the water was in plug flow.

\[
\rho_w V_w d_w C_{pw} \frac{\partial T_w}{\partial z} - h_w(z, t)[T(0, z, t) - T_w(z, t)] = 0 \tag{7.12}
\]

Equations 7.1 and 7.12 were solved in the finite difference model to obtain the mould and cooling water temperature distributions.
7.2 Mould Distortion

Mould distortion has been quantified by Samarasekera and Brimacombe using a three-dimensional elastic-plastic finite element model [8, 17, 107]. The model employed a one-eighth billet mould geometry, shown in Figure 7.3, because of the symmetry of the square mould tube. The temperature distribution of the mould tube from the heat transfer model was used as input to the stress model to calculate the mould distortion.

Figure 7.3: Mesh geometry used to model mould distortion.
Mould distortion depends on the mould geometry, the thermal field of the mould, and the physical mould constraints. The following boundary conditions, referring to Figure 7.3, were assumed for the stress model.

1. Mould displacement orthogonal to the midface plane of symmetry ABCD, \( v \), was zero.

2. Mould displacement orthogonal to the corner diagonal plane of symmetry EFGH was zero. This implies that displacements \( u \) and \( v \) are equal along this plane.

3. The mould constraint near the top of the mould tube was simulated by fixing displacement \( u \) to zero along the plane KLM.

4. The \( w \) displacements along the line MN were set to zero to fix the mould in the \( z \) direction.

Although the model included plasticity, only a small region of the mould near the meniscus was found to exceed the yield stress of the mould material [17]. This finding certainly explains how permanent mould distortion can occur, but for the purposes of this work a three-dimensional elastic model is sufficient to compute mould distortion.
Chapter 8

Mathematical Modelling of Billet Shrinkage

This chapter discusses the mathematical modelling of billet shrinkage using finite-element methods. Results were used in conjunction with mould distortion calculations to interpret mould-billet binding, which will be discussed in Chapter 9. Modelling of billet shrinkage is a classic thermal-stress problem. A heat transfer model was used to calculate the temperature field in the billet using the heat flux calculated from mould temperature measurements. Billet shrinkage was then calculated by a stress model using the billet temperature field.

8.1 ABAQUS Finite-Element Modelling Software

The finite-element modelling was conducted using ABAQUS commercial software [108]. ABAQUS is a general purpose finite-element package well suited for non-linear stress and heat transfer modelling discussed in this work. The finite-element mesh (i.e. node locations and element connectivity) was defined
using Patran3, a “tool-kit” for building finite-element geometries.

The ABAQUS program was run on a Silicon Graphics workstation network because of its computational efficiency and graphical capabilities. The program is set-up by creating an input file containing the finite-element geometry, material properties and a time history of boundary conditions. Complex material properties, such as creep, or boundary conditions, such as heat flux, may be defined in user-written subroutines as a function of model state.

The finite-element equations for heat transfer and stress-displacement elements are well established and are available in many sources, e.g. [109, 86]. The equations will not be presented here because they were not an original part of this research, in custom program code or otherwise. The finite-element mesh, material properties and boundary conditions will be described to quantify the problem and the model.

8.2 Model Geometry

A two-dimensional transverse geometry was adopted for this model. The approach of following a transverse “slice” of the billet through the mould has been used in past heat transfer models [21]. This is a reasonable assumption since the axial bulk heat flow by convection is significantly greater than the axial heat flow by conduction [7]. Further, convection in the liquid pool can be neglected based on the work of Szekely [110]. Although the rate of superheat extraction is significantly affected by liquid flow, the solidus isotherm is relatively insensitive to flow in the liquid phase because of the large latent heat
A one-eighth transverse section model was employed because of the symmetry of the geometry, to reduce computational time. Figure 8.1 illustrates the geometry used in the model. The mesh was terminated 20 mm from the billet surface. Since the interior mesh would be above the liquidus temperature at the bottom of the mould, it was neglected for computational efficiency. The mesh density included 100 elements across billet half-face, and 20 elements from the billet surface to the mesh termination 20 mm from billet surface. The node spacing was finer near the billet surface, becoming coarser toward the centre of the billet. The typical surface element was 1 mm wide (across the face) by 0.3 mm thick.

Linear (first order) elements were employed because of the significant evolution.
non-linearity associated with latent heat in the heat transfer model. Linear elements are reported to be superior than higher ordered elements in highly non-linear problems [108].

8.3 Heat Transfer Model

The following assumptions were made in the formulation of the heat transfer model.

1. The midface symmetry line was adiabatic.

\[ x = 0, \ 0 \leq y \leq Y_b, \ t > 0 \]

\[ -k_s \frac{\partial T}{\partial x} = 0 \quad \text{ } \tag{8.1} \]

2. The corner diagonal line of symmetry was adiabatic.

\[ (X_b - Y_b) \leq x \leq X_b, \ y = -x + X_b, \ t > 0 \]

\[ -k_s \left[ \frac{\partial T}{\partial x} \mathbf{i} + \frac{\partial T}{\partial y} \mathbf{j} \right] = 0 \quad \text{ } \tag{8.2} \]

3. The 20 mm boundary from surface was treated as a liquid isotherm.

The temperature was chosen to be the liquidus temperature plus a 25°C effective superheat.

\[ 0 \leq x \leq (X_b - Y_b), \ y = Y_b, \ t > 0 \]

\[ T = T_{sh} \quad \text{ } \tag{8.3} \]

4. The heat flux obtained from the mould heat transfer model was applied to the billet surface.
\[ 0 \leq x \leq X_b, \ y = 0, \ t > 0 \]

\[ -k_b \frac{\partial T}{\partial y} = h_b(t)[T(x,0,t) - T_w] \quad (8.4) \]

The reduced heat transfer near the corner of the billet (caused by two-dimensional cooling and shrinkage) was accounted for using a constant heat transfer coefficient, \( h_b \), across the billet face [81]. The heat transfer model was run twice to obtain the final temperature field in the billet. The first model run applied the heat flux, \( q_s(t) \), to the surface to obtain the midface temperature, \( T(0,0,t) \), which was used to calculate the effective heat transfer coefficient, \( h_b(t) \), as a function of time. The second model run applied the heat transfer coefficient, as shown in Equation 8.4. Midface temperature profiles were verified between the runs. This approach only impacted the billet temperature field near the corner, as the billet isotherms paralleled the face for most of the billet width, as illustrated in Figure 8.2.

5. The initial billet temperature included a 25°C effective superheat.

\[ t = 0 \]

\[ T_i = T_{sh} \quad (8.5) \]

Thermal conductivity, specific heat, solidus and liquidus temperatures were available in the literature as a function of carbon content and temperature [111, 62]. The latent heat of solidification (272 kJ kg\(^{-1}\)) was included as specific heat in the ABAQUS input file. The conductivity of the liquid steel
Figure 8.2: Billet isotherms using constant heat transfer coefficient across the billet face. Cooling time 19 seconds, temperature in degrees K.
was doubled to simulate convection in the liquid pool [111]. Tables 8.1 to 8.3 contain the thermal properties used in the models.
Table 8.1: Thermal properties for 0.14 pct. carbon steel.

<table>
<thead>
<tr>
<th>Temperature (K)</th>
<th>Specific heat (J kg(^{-1}) K(^{-1}))</th>
<th>Temperature (K)</th>
<th>Conductivity (W m(^{-1}) K(^{-1}))</th>
</tr>
</thead>
<tbody>
<tr>
<td>1173</td>
<td>620</td>
<td>973</td>
<td>30</td>
</tr>
<tr>
<td>1723</td>
<td>700</td>
<td>1373</td>
<td>25</td>
</tr>
<tr>
<td>1762</td>
<td>900</td>
<td>1762</td>
<td>33</td>
</tr>
<tr>
<td>1769</td>
<td>10365</td>
<td>1799</td>
<td>27</td>
</tr>
<tr>
<td>1788</td>
<td>10368</td>
<td>1813</td>
<td>50</td>
</tr>
<tr>
<td>1799</td>
<td>750</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

Table 8.2: Thermal properties for 0.32 pct. carbon steel.

<table>
<thead>
<tr>
<th>Temperature (K)</th>
<th>Specific heat (J kg(^{-1}) K(^{-1}))</th>
<th>Temperature (K)</th>
<th>Conductivity (W m(^{-1}) K(^{-1}))</th>
</tr>
</thead>
<tbody>
<tr>
<td>1273</td>
<td>620</td>
<td>1073</td>
<td>30</td>
</tr>
<tr>
<td>1741</td>
<td>772</td>
<td>1373</td>
<td>25</td>
</tr>
<tr>
<td>1754</td>
<td>9421</td>
<td>1741</td>
<td>33</td>
</tr>
<tr>
<td>1771</td>
<td>9435</td>
<td>1786</td>
<td>27</td>
</tr>
<tr>
<td>1786</td>
<td>750</td>
<td>1813</td>
<td>50</td>
</tr>
</tbody>
</table>

Table 8.3: Thermal properties for 0.80 pct. carbon steel.

<table>
<thead>
<tr>
<th>Temperature (K)</th>
<th>Specific heat (J kg(^{-1}) K(^{-1}))</th>
<th>Temperature (K)</th>
<th>Conductivity (W m(^{-1}) K(^{-1}))</th>
</tr>
</thead>
<tbody>
<tr>
<td>1040</td>
<td>608</td>
<td>973</td>
<td>30</td>
</tr>
<tr>
<td>1647</td>
<td>685</td>
<td>1373</td>
<td>25</td>
</tr>
<tr>
<td>1657</td>
<td>3500</td>
<td>1662</td>
<td>33</td>
</tr>
<tr>
<td>1745</td>
<td>3500</td>
<td>1752</td>
<td>27</td>
</tr>
<tr>
<td>1752</td>
<td>670</td>
<td>1813</td>
<td>50</td>
</tr>
<tr>
<td>1873</td>
<td>750</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>
8.4 Stress Model

The stress model employed the same mesh geometry as the heat transfer model to facilitate the implementation of temperature in the stress model. The following assumptions were made in the formulation of the stress model.

1. The mould was treated as a rigid surface 1 μm from the billet surface. Contact interface elements¹ were defined between the mould rigid surface and the billet surface nodes, as illustrated in Figure 8.3. This feature was required because the shell would bend past the mould wall at the midface during initial solidification. In reality, the shell would be extremely fragile at this stage, and the reaction forces on the mould wall would be very small. The geometry of the process must be preserved however, otherwise the billet would retain its falsely distorted shape as the shell cooled and became stronger.

2. Plane strain elements were employed [112, 35, 36].

3. Transverse displacement \( (u) \), or rotation \( (\phi_z) \), about the midface line of symmetry was zero.

4. Displacement normal to the corner diagonal line of symmetry , or rotation \( (\phi_z) \) was zero. Along this line, the displacements \( u \) and \( v \) were equal and of opposite direction.

¹A contact element is not a conventional element type like a stress-displacement element, it is simply a notation used by ABAQUS to restrict nodal displacement.
5. Ferrostatic pressure was not included since the objective of this model was to calculate natural shrinkage of the shell.
Elastic and viscoplastic equations were obtained from an excellent summary of constitutive relationships applicable to continuous casting [113]. The following temperature dependent elastic modulus was employed [113].

\[
E = 968 - 2.33(T - 273) + 1.90 \times 10^{-3}(T - 273)^2 - 5.18 \times 10^{-7}(T - 273)^3 \quad (8.6)
\]

Plasticity was input through the ABAQUS creep subroutine. The viscoplastic relation used was a power law of the following form [113].

\[
\varepsilon_p = C \exp\left(\frac{-Q}{T}\right)\sigma^n \quad (8.7)
\]

\[
C = 24233 + 49973(\text{pct. } C) + 48757(\text{pct. } C)^2 \quad \text{MPa}^{-n} \text{s}^{-1}
\]

\[
Q = 49480 \text{ K}
\]

\[
n = 5.331 + 4.116 \times 10^{-3}T - 2.116 \times 10^{-6}T^2
\]

Thermal shrinkage, as a function of temperature and carbon content, was obtained from Chandra [81]. The lattice parameter for delta phase steel was calculated using the following equation.

\[
a_{sc}^T = a_{sc=0}^T + 8.40 \times 10^{-3}X_c \quad (8.8)
\]

Where the specific volume of pure delta iron was calculated using

\[
V_\delta = 0.1234 + 9.38 \times 10^{-6}(T - 20) \quad (8.9)
\]

The lattice parameter for gamma phase iron was estimated with
\[ a_{\gamma_c}^T = a_{\gamma_c=0}^T + (0.0317 - 11.65 \cdot 10^{-7}T - 0.05 \cdot 10^{-7}T^2)W_c \]  \hspace{1cm} (8.10)

The specific volume of pure gamma iron was calculated using

\[ V_\gamma = 0.1225 + 9.45 \cdot 10^{-6}(T - 20) \]  \hspace{1cm} (8.11)

With the preceding equations, the lattice parameter can be calculated as a function of temperature and carbon content. In a two phase region, Chandra employed the phase diagram and lever rule to calculate an average lattice parameter [6]. The mean coefficient of thermal expansion is calculated relative to a reference length at the solidus temperature for a given carbon content, and was implemented in ABAQUS with the following equation [108].

\[ \varepsilon_{th} = \alpha(T)(T - T_{\text{solidus}}) \]  \hspace{1cm} (8.12)

Typical mean coefficients of thermal expansion used in this work are presented in Figure 8.4. The calculated values were consistent with those reported by Chandra [81].

8.5 Model Verification

8.5.1 Heat Transfer

The ABAQUS heat transfer model was validated using an analytical solution to the one-dimensional semi-infinite heat conduction problem with latent heat evolution and an isothermal boundary [114].
Figure 8.4: Mean coefficients of thermal expansion as a function of carbon content and temperature.
\[ T_s = T_{s0} + \frac{T_{mp} - T_{s0}}{\text{erf} \lambda} \text{erf}\left[ \frac{y}{2(\alpha_s t)^{0.5}} \right] \]  

Assuming that the initial temperature of the liquid is the solidification temperature \( (i.e. \) there is no superheat or temperature gradient in the liquid), the variable \( \lambda \) can be solved for iteratively, using the following equation.

\[ \lambda e^{\lambda^2} \text{erf} \lambda = - \frac{C_{p,s}(T_{mp} - T_{s0})}{\pi^{0.5} \Delta H} \]  

The analytical and ABAQUS numerical solutions were consistent. Figure 8.5 shows the temperature response of a node 1 mm from the surface of a mesh with 0.5 mm node spacing. The initial temperature of the material was 1500°C, with the isothermal boundary set at 1200°C. The material properties were: \( k = 30 \text{ W m}^{-1} \text{ K}^{-1}, C_p = 700 \text{ J kg}^{-1} \text{ K}^{-1}, \rho = 7400 \text{ kg m}^{-3}, \) and latent heat = 272 \( \text{kJ kg}^{-1}. \) The ABAQUS solution matched the analytical solution very closely, even though the node was only 2 elements (using linear interpolation) from the surface.

The ABAQUS finite-element heat transfer model and the Samarasekera-Chandra finite-difference heat transfer model \([8, 81]\) were run using the same test heat flux profiles. Model results are compared in Table 8.4, which presents the calculated shell thickness at the mould exit. It is interesting to note that the results become more consistent as the shell thickness increases. This was likely due to the coarse node density in the finite-difference model. In test heat 3 for example, the shell thickness was only 5 nodes from the model surface at the mould exit. In verifying the finite-difference model, Chandra matched
Figure 8.5: Comparison of the analytical solution to heat conduction with phase change and the ABAQUS numerical solution.
Table 8.4: Comparison of shell thickness at mould exit between Samarasekera-Chandra model and ABAQUS model. Heat fluxes taken from trial D2.

<table>
<thead>
<tr>
<th>Test Heat</th>
<th>ABAQUS (mm)</th>
<th>Samarasekera-Chandra (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>12.2</td>
<td>12.0</td>
</tr>
<tr>
<td>2</td>
<td>10.6</td>
<td>9.5</td>
</tr>
<tr>
<td>3</td>
<td>6.4</td>
<td>5.0</td>
</tr>
<tr>
<td>4</td>
<td>14.2</td>
<td>14.0</td>
</tr>
</tbody>
</table>

The calculated shell thicknesses to the depth of solidification bands\(^2\) in billet samples [81].

8.5.2 Viscoplasticity

The viscoplastic relationship was implemented in ABAQUS using a user-written subroutine. The plastic strain rate was verified using a simple isothermal, one-dimensional problem. Equation 8.7 was implemented using \( C = 50000 \text{ MPa}^{-n} \text{s}^{-1} \), \( Q = 49480 \text{ K} \), and \( n = 7 \) at 1400 Kelvin. The model was loaded at 10 MPa for 10 seconds. The plastic strain is found by simply integrating Equation 8.7 with time. The theoretical strain of \( 2.24 \cdot 10^{-3} \) exactly matched the ABAQUS model strain. The validation of viscoplasticity in the ABAQUS code is also available in the ABAQUS verification manual [115].

\(^2\)Solidification bands indicate the location of the solidification front at the mould exit. They are caused by a sudden change in heat transfer.
8.6 Preliminary Model Results

8.6.1 Shell Shrinkage Near the Meniscus

At what point can the solidifying shell withstand enough stress to commence shrinking? For this macroscopic model, $T_{solidus}$ was used as the reference temperature for shrinkage to begin. One might argue that the zero-strength temperature or zero-ductility temperature could be used to define shell strength, but the combined shell properties of thickness, temperature distribution and plasticity likely govern when the entire billet shell can commence shrinking. Since some uncertainty exists with respect to material properties near the solidus temperature, these conditions cannot be exactly quantified. In this work, it was assumed that the shell thickness must be 0.5 mm prior to the commencement of shrinkage.

8.6.2 Billet Face

The ABAQUS results showed that the shell shrinkage was not uniform from the midface to the corners. The billet surface typically took a slightly bulged shape, with the corners shrinking away from the mould more than the midface. Figure 8.6 illustrates an example displacement profile of the one-half billet face model, with a displacement magnification of 4. Consistent with this result, the natural bending tendency of an unconstrained solidifying shell has been shown to form a convex shape [112]. The shell bends because of thermal stresses, and the severity of bending will depend on the applied heat flux and
Figure 8.6: Billet mesh at the bottom of the mould illustrating different face displacement from midface to corner - displacement magnification 4.

material properties. Thus when casting with insufficient taper, the billet will naturally form a bulged shape. Ferrostatic pressure likely enhances this effect.

The length of the curve of the billet face was calculated for several cases to check the true shrinkage of the face. The face shrinkage was nearly identical to the corner displacement; this can be reasoned since the angle of the face bending is very small.

8.6.3 Comparison with Chandra’s Work

Chandra’s model assumed that shrinkage was a function of the solid shell temperature distribution [81]. The billet dimension output from Chandra’s model was the average length of solidified rows of nodes in the model. The intuitive approach to this model was reasonable, but the model lacked complete thermophysical characterization. If the material was plastic, plastic strain may
offset thermal strain causing the overall shrinkage to differ from Chandra’s estimate. The combined effect of material properties is far from intuitive, particularly with varying heat flux on the shell surface.

Another complication when comparing model results was the differing mesh densities since the Chandra model employed a coarser mesh. A coarser element would require more time to evolve the latent heat and would result in a delayed solidification of the first row of nodes. A finer element density would result in a more accurate prediction of shell behaviour, particularly with temperature varying material properties.

In test model runs using heat flux measurements from plant trials, the Chandra model was found to both underpredict and overpredict the shrinkage of the ABAQUS model. With the billet dimension in Chandra’s model a function of temperature only, changes in billet temperature result in proportional changes in billet dimension. With the inclusion of plasticity in the ABAQUS model, plasticity tended to “soften” the change of billet dimension with temperature. The trend of shrinkage was certainly consistent between the models, and the change in billet dimension is governed mainly by thermal contraction rather than deformation caused by thermal stresses. Figure 8.7 shows an example of differing billet shrinkage profiles for the Chandra and ABAQUS shrinkage models.
Figure 8.7: Example of differing shrinkage profiles between the Chandra and ABAQUS models.
Chapter 9

Evaluation of Mould-Billet Binding and Lubrication

In Chapters 5 and 6, significant knowledge was obtained using sensor measurements to quantify the mould response. Mould oscillation was found to differ from design specifications on all three machines tested. Further, the oscillator response was dynamic, and changed as a function of oscillation frequency and machine loading. Process control in billet casting is inherently transient, owing to changes in liquid steel flow rate. Measurements of casting speed have shown that the speed may vary by as much as 30 pct., as the flow rate changes with nozzle and tundish conditions. The installation of strain sensors on the oscillator drive arm has facilitated the measurement of mould-billet friction forces, likely for the first time on industrial billet machines. Fundamental lubrication phenomena were elucidated with the force sensor. Oil lubrication responds in a solid friction mode, while mould flux lubrication exhibits liquid friction. Friction was investigated in the context of process variables and
upsets. The force signal did not respond to small, local events in the mould, such as a meniscus stick or the formation of a transverse depression. It did respond, however, to the average interaction of the billet and mould. Thus the force signal is effective in evaluating lubrication, and the variables which impact lubrication. Gross process upsets like strand plugging and breakouts show high friction during the upsets, likely because of rapid changes in metal level and large regions of sticking. Since mould flux lubrication operates in a liquid friction regime, the variables which impact hydrodynamic lubrication, namely relative velocity and lubricant thickness, impact the measured friction response. Thus mould oscillation, in addition to stripping "sticks" at the meniscus, impacts friction measurably with mould fluxes.

This chapter combines sensor measurements, the knowledge developed in Chapters 5 and 6, and the results of mathematical modelling to interpret mould-billet binding and lubrication effectiveness. The impact of material properties on billet shrinkage is also presented. The models were employed using the following logic, also outlined in Figure 9.1.

1. The mould temperature profile was taken from the subject plant trial as a 15 minute average of mould temperature, to obtain a sense of average heat transfer.

2. Mould heat flux was calculated using the Samarasekera-Chandra mould heat transfer model detailed in Section 7.1.

3. The mould temperature distribution was obtained using the mould heat
Table 9.1: Heats investigated for mould-shell binding.

<table>
<thead>
<tr>
<th>Heat</th>
<th>Grade</th>
<th>Lubricant</th>
</tr>
</thead>
<tbody>
<tr>
<td>277</td>
<td>0.14 pct. C</td>
<td>powder</td>
</tr>
<tr>
<td>298</td>
<td>0.14 pct. C</td>
<td>oil</td>
</tr>
<tr>
<td>312</td>
<td>0.32 pct. C + B</td>
<td>powder</td>
</tr>
<tr>
<td>333</td>
<td>0.32 pct. C + B</td>
<td>oil</td>
</tr>
<tr>
<td>351</td>
<td>0.80 pct. C</td>
<td>oil</td>
</tr>
</tbody>
</table>

transfer model.

4. Mould distortion was calculated by implementing the mould temperature distribution into the Samarasekera mould distortion model, described in Section 7.2. The cold mould dimensions used in the model were measured values, not mould design specifications.

5. The ABAQUS billet solidification model was run using the mould heat flux profile to obtain the billet temperature distribution.

6. The ABAQUS billet shrinkage model was run using the calculated billet temperature distribution. The initial billet dimension was taken to be the dimension of the distorted mould at the meniscus.

Several heats from trial D2 were investigated for mould-shell binding. This trial was selected because a fully instrumented mould was used, quantitative forces were obtained, and a range of grades were cast using both oil and mould flux lubrication. Table 9.1 lists the heats investigated in detail. The heats that were investigated were not unique, and represented typical sensor responses for the given grade and lubricant.
Figure 9.1: Outline of procedure used to interpret mould-billet binding and lubrication.
9.1 Mould Heat Flux

The calculated mould heat flux profiles and shell thicknesses are shown in Figures 9.2 to 9.6. Clear differences in heat transfer were observed between the heats as a function of steel grade and lubricant. With the oil-cast heats, heat 298 (peritectic) yielded lower heat extraction than heats 333 and 351 (hypoperitectic). It is well established that oil-cast peritectic steels exhibit low heat transfer because of the rough surface associated with these grades [23, 24]. When casting peritectic steels, the heat flux near the meniscus of heat 277 (powder-cast) was higher than that of heat 298 (oil-cast). This is consistent with the experimental research of Singh and Blazek [28]. Even more striking is the difference in heat flux between the boron(Ti)-alloyed steels using the different lubricants. Heat 312 (powder-cast) showed significantly less heat extraction than heat 333 (oil-cast). This is consistent with the generally accepted knowledge that heats cast with mould fluxes exhibit lower heat transfer than those cast using oil (for non-peritectic steel grades).
Figure 9.2: Mould heat flux and shell thickness. Heat 277, powder lubrication, 0.14 pct. C, trial D2.
Figure 9.3: Mould heat flux and shell thickness. Heat 298, oil lubrication, 0.14 pct. C, trial D2.
Figure 9.4: Mould heat flux and shell thickness. Heat 312, powder lubrication, 0.32 pct. C + B, trial D2.
Figure 9.5: Mould heat flux and shell thickness. Heat 333, oil lubrication, 0.32 pct. C + B, trial D2.
Figure 9.6: Mould heat flux and shell thickness. Heat 351, oil lubrication, 0.80 pct. C, trial D2.
9.2 Impact of Material Properties on Billet Shrinkage

In this section, the plastic equation in the billet shrinkage model was modified to determine its impact on shrinkage. For a given thermal stress, a strong shell would exhibit less plastic strain than a weaker shell. An excessively strong shell, as an extreme case, would behave elastically with no plastic strain. The ABAQUS model was also run assuming elastic properties only, in order to compare shrinkage with the viscoplastic model. Figure 9.7 illustrates the shrinkage profile of heat 277, assuming elastic and plastic material properties. The elastic model predicted lower shrinkage; and the effect of plasticity accumulated as the plastic shell grew and moved down the mould. In five test cases, the shrinkage predicted by the elastic model was less than that of the corresponding viscoplastic model as shown in Table 9.2. It is evident that a stronger shell (elastic in this case) would shrink less than a weaker shell, for a given heat flux. Heat 312 exhibited a low heat flux and the corresponding elastic and plastic model runs had similar billet dimensions. Heat 333 had a high overall heat flux and a large difference was observed between the elastic and viscoplastic model displacements. The impact of plasticity in the shrinkage model increases with increasing heat flux. Increased heat extraction in the mould would cause steeper temperature gradients in the solid shell and higher thermal stresses. Higher thermal stresses then cause increased plastic strain in the viscoplastic equation, which contributes to a change in billet shrinkage relative to the elastic case.
Table 9.2: One-half billet face shrinkage for elastic and viscoplastic cases.

<table>
<thead>
<tr>
<th>Test Case</th>
<th>Grade</th>
<th>Lubricant</th>
<th>Elastic (mm)</th>
<th>Viscoplastic (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>277</td>
<td>0.14 pct. C</td>
<td>powder</td>
<td>0.74</td>
<td>0.95</td>
</tr>
<tr>
<td>298</td>
<td>0.14 pct. C</td>
<td>oil</td>
<td>0.50</td>
<td>0.58</td>
</tr>
<tr>
<td>312</td>
<td>0.32 pct. C + B</td>
<td>powder</td>
<td>0.20</td>
<td>0.21</td>
</tr>
<tr>
<td>333</td>
<td>0.32 pct. C + B</td>
<td>oil</td>
<td>0.68</td>
<td>1.12</td>
</tr>
<tr>
<td>351</td>
<td>0.80 pct. C</td>
<td>oil</td>
<td>0.53</td>
<td>1.01</td>
</tr>
</tbody>
</table>

Figure 9.7: Comparison of shrinkage calculations between elastic and plastic material properties. Trial D2, heat 277.
A hyperbolic sine law of the following form was also tested in the viscoplastic model [113].

\[ \varepsilon_p = C \exp\left(-\frac{Q}{T}\right) \sinh(a_s \sigma)^n \]  

(9.1)

The hyperbolic sine law yielded nearly identical shrinkage results to the power law (Equation 8.7); the power law was retained for this work because of its simplicity.

The carbon content dependent coefficient, \( C \), of the power law equation was varied to determine its impact on shrinkage results. The shrinkage of a 0.30 pct. carbon billet was calculated with a value of \( C \) that was one half of its correct value, to simulate a stronger, i.e. less plastic, material. The stronger material exhibited only marginally less shrinkage, as shown in Figure 9.8.

Several conclusions can be made regarding the calculation of billet shrinkage. The material properties must include plasticity, since plasticity was shown to increase the calculated billet shrinkage. Further, the impact of plasticity was greater with higher heat extraction. The billet shrinkage results were not sensitive to the viscoplastic equation however, but simply the inclusion of plasticity in the stress model had a marked impact on billet shrinkage.
Figure 9.8: Sensitivity test of the viscoplastic relationship to billet shrinkage. Trial D2, heat 333.
9.3 Binding Interpretation by Mould-Billet Dimensions

Binding may be interpreted by simply comparing the billet and distorted mould dimensions at any axial position. If the billet is larger than the mould, the billet may bind or jam in the mould with the resulting generation of high axial forces on the billet shell such that cracks may form or a breakout may be initiated. As shown in previous work [8, 107], the mould bulges during casting operations due to thermal expansion. Figure 9.9 shows a typical cold mould and distorted mould profile from trial D2. The taper is most seriously influenced by distortion near the meniscus, where the in-situ taper may be reduced by 1 pct. m⁻¹. Lower in the mould, the distortion impacts the taper less severely.

In the billet solidification model, the original billet dimension was taken to be the dimension of the distorted mould when solidification commenced. Figures 9.10 to 9.14 present the calculated mould and billet dimensions for the heats investigated.
Figure 9.9: Cold mould and distorted mould dimensions. Heat 277, powder lubrication, 0.14 pct. C, trial D2.
Figure 9.10: Mould and billet dimensions from mathematical models. Heat 277, powder lubrication, 0.14 pct. C, trial D2.
Figure 9.11: Mould and billet dimensions from mathematical models. Heat 298, oil lubrication, 0.14 pct. C, trial D2.
Figure 9.12: Mould and billet dimensions from mathematical models. Heat 312, powder lubrication, 0.32 pct. C + B, trial D2.
Figure 9.13: Mould and billet dimensions from mathematical models. Heat 333, oil lubrication, 0.32 pct. C + B, trial D2.
Figure 9.14: Mould and billet dimensions from mathematical models. Heat 351, oil lubrication, 0.80 pct. C, trial D2.
It is evident that binding was occurring in heats 277, 298 and 312, while the billet was slightly smaller than the mould when casting the hyper-peritectic steels with oil lubrication, heats 333 and 351. Thus the mould was too steeply tapered for the peritectic steels, heats 277 and 298, as well as the powder-cast boron(Ti) steel, heat 312. The mould taper appears reasonable for heats 333 and 351, since the billet dimensions reasonably matched the mould dimensions.

In the past, binding may have been interpreted simply as the region where the billet dimension was greater than the mould dimension. Using heat 277 (Figure 9.10) as an example, the billet appears to be binding from 220 to 780 mm along the mould length. Since the billet can never be larger than the mould, the billet shell is plastically deformed to the mould dimension. Below 450 mm, the billet tapers more steeply than the mould, thereby fitting inside the mould. Binding in this case is restricted to 220 - 450 mm, rather than virtually the entire mould length. Therefore, in interpreting binding, it may be appropriate to look at both the tapers and dimensions of the mould and billet. Table 9.3 details the regions of binding for the heats investigated. Heat 312 exhibited excessive binding, due to low heat extraction.

Evidence of excessive mould taper can also be seen in the billet samples. As noted in Section 5.4.2, longitudinal midface depressions were observed on billets from trials D1 and D2 (the same mould was used), using both oil and powder lubrication. As was shown by the mathematical modelling of binding, the most severe binding (by mismatch of mould and billet taper) occurred in the top third of the mould where the mould taper was the steepest. The ex-
Table 9.3: Regions of mould-shell binding.

<table>
<thead>
<tr>
<th>Heat</th>
<th>Grade</th>
<th>Lubricant</th>
<th>Region of binding along mould length (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>277</td>
<td>0.14 pct. C</td>
<td>powder</td>
<td>220 - 450</td>
</tr>
<tr>
<td>298</td>
<td>0.14 pct. C</td>
<td>oil</td>
<td>meniscus - 640</td>
</tr>
<tr>
<td>312</td>
<td>0.32 pct. C + B</td>
<td>powder</td>
<td>meniscus - 720</td>
</tr>
<tr>
<td>333</td>
<td>0.32 pct. C + B</td>
<td>oil</td>
<td>none</td>
</tr>
<tr>
<td>351</td>
<td>0.80 pct. C</td>
<td>oil</td>
<td>none</td>
</tr>
</tbody>
</table>

cessive taper near the meniscus was believed to buckle the thin shell, forming these depressions. Longitudinal surface cracks were seen at the base of some of these depressions, confirming that the cracks formed high in the mould when the shell was thin, in the high temperature zone-of-low-ductility. It is interesting to note that the longitudinal depressions in peritectic powder-cast billets (e.g. Figures 5.34 and 5.36) were very abrupt, while those in boron(Ti)-alloyed powder-cast billets and oil-cast billets (e.g. Figure F.1) were smoothly concave across the billet face. The differences between the abrupt and smooth depression types could originate as a function of the lubricant or steel grade. The powder-cast peritectic shell likely buckled abruptly because of local weak areas in the shell caused by the peritectic rough surface. The weak areas would provide sites prone to acute buckling. Another factor which may have contributed is the lubricant. Small variations in mould flux film thickness would impact heat transfer, possibly leaving some regions thinner and weaker. When comparing the powder-cast peritectic (acute depression) and powder-cast boron(Ti) grades (smooth depression), two differences existed in the cast-
ing conditions: metal level and heat extraction. The metal level was much higher for the peritectic grades, 155 mm, than the boron(Ti) steels, 225 mm (the metal level was dropped after the defective peritectic billets were cast). The meniscus heat transfer was higher with the peritectic steels and mathematical modelling of billet solidification showed that the solid shell grew much more quickly. Referring to Figures 9.2 and 9.4, the peritectic shell was 2 mm thick at 210 mm from the mould top while the boron(Ti)-alloyed billet was not 2 mm thick until 330 mm from the mould top. Since the boron(Ti) steel shell was less developed in the upper region of the mould (because of the low metal level and very low heat flux) it may not have been exposed to the steep taper which acutely buckled the peritectic billet. Interestingly, this observation contrasts the behaviour of the oil-cast boron(Ti) grades, which exhibit a high heat flux and a well developed shell near the meniscus.

9.4 Design of Mould Taper

For operations convenience, mini-mills use a single mould taper for a given billet section size. As previously discussed, the mould taper should be adequate enough to minimize the mould-shell gap to improve heat extraction, but not too steep to cause mould wear and excessive axial forces. It is evident from the literature and this research that the heat extraction and billet shrinkage are largely a function of steel grade and lubricant type. Thus it is difficult to design an "all-purpose" mould taper.

Ideally, mould taper should be designed to match billet shrinkage. Ta-
Table 9.4: Overall billet shrinkage taper.

<table>
<thead>
<tr>
<th>Heat</th>
<th>Grade</th>
<th>Lubricant</th>
<th>Billet shrinkage (pct. m(^{-1}))</th>
</tr>
</thead>
<tbody>
<tr>
<td>277</td>
<td>0.14 pct. C</td>
<td>powder</td>
<td>1.56</td>
</tr>
<tr>
<td>298</td>
<td>0.14 pct. C</td>
<td>oil</td>
<td>0.92</td>
</tr>
<tr>
<td>312</td>
<td>0.32 pct. C + B</td>
<td>powder</td>
<td>0.54</td>
</tr>
<tr>
<td>333</td>
<td>0.32 pct. C + B</td>
<td>oil</td>
<td>1.77</td>
</tr>
<tr>
<td>351</td>
<td>0.80 pct. C</td>
<td>oil</td>
<td>1.66</td>
</tr>
</tbody>
</table>

Table 9.4 shows the overall billet taper for the heats investigated, calculated from the meniscus to the mould exit. As is evident, these values vary greatly, by a factor of 3. The mould taper should also match the billet shrinkage profiles, as shown previously in Figures 9.10 to 9.14. The shrinkage profiles for heats 277, 298 and 312 indicate that the mould taper was too steep for casting these heats. The Company D 200 mm mould taper was parabolic, with a steep 5 pct. m\(^{-1}\) taper near the meniscus; the in-situ taper, with the mould distorted, was typically 4 pct. m\(^{-1}\).

Figures 9.15 and 9.16 illustrate the billet and distorted mould tapers for heats 277 and 298 respectively. It should be noted that the mould taper appears “rough” because the mould distortion model was run using actual mould dimensions. Plotting the tapers assists in mould taper design, as well as visualizing when binding occurs (e.g. binding ceases for heat 277 at 450 mm). The Company D mould taper was clearly too aggressive in the upper portion of the mould. Also, the required taper in the upper portion of the mould is greater than in the lower portion, as previously recognized [20]. A reasonable
Figure 9.15: Mould and billet taper calculated from mathematical models. Heat 277, powder lubrication, 0.14 pct. C, trial D2.
Figure 9.16: Mould and billet taper calculated from mathematical models. Heat 298, oil lubrication, 0.14 pct. C, trial D2.
compromise for these peritectic grades would be to use a parabolic taper commencing at approximately 2 \text{ pct. m}^{-1} and reaching 1 \text{ pct. m}^{-1} at the bottom of the mould. A double-tapered mould could also be used, with 2 \text{ pct. m}^{-1} for the top one-third and 1 \text{ pct. m}^{-1} for the lower two-thirds of the mould. As previously mentioned, the taper is reduced near the meniscus due to acute bulging. The cold mould dimensions near the meniscus would be adjusted slightly using the results of Figure 9.9 to compensate for the loss of effective taper.

The mould taper practices varied markedly between Companies A and D. Company A employed a shallow 0.8 \text{ pct. m}^{-1} single-tapered mould, while Company D used a steeply tapered parabolic mould, commencing at 5 \text{ pct. m}^{-1} at the meniscus. Results of this work indicate that a more appropriate taper would be intermediate between the two practices. Further, a recent survey of billet casting mini-mills indicated that most employ single tapers, some as low as 0.3 \text{ pct. m}^{-1} [3]. Mould taper is of extreme importance to billet quality, and it is apparent from the varying practices in the mini-mills that improvement is needed in this area. Double- or multiple-tapered moulds should be used [3]. Results of the billet shrinkage model developed in this work and the mould distortion model are effective tools for designing mould taper.

9.5 Binding and Friction Measurements

One of the main objectives of the mathematical modelling of billet shrinkage and mould distortion was to relate the degree of binding with friction mea-
measurements. In the past, it was believed that binding would result in higher measured forces. For example, the lower overall shrinkage of a low carbon steel would causing binding in a mould designed for higher carbon steels, possibly causing transverse defects [6]. Table 9.5 shows binding results from mathematical modelling and average friction coefficients from force measurements for the heats investigated. As is evident, binding did not cause a friction increase using these average measurements. Quite the opposite was observed in heats 333 and 351, which did not exhibit binding by mould-billet dimensions but were cast with high forces.

Thus under steady-state operation, force alone is not a good indicator of binding. The missing variable in the simplified binding-friction assumption is lubrication. If the lubrication is effective, the billet will cast through an excessively tapered mould without being exposed to high axial forces. This is particularly evident in heat 312, which exhibited excessive binding and very low forces. As will be discussed later in a force upset example, the force signal does respond well to lubrication related upsets. Good lubrication is not an excuse for an excessively taper mould because if a lubrication upset occurs, the billet will be exposed to very high forces.

9.6 Friction, Heat Transfer and Steel Grade

Table 9.6 shows friction and heat extraction values from trial D2 heats. Heat extraction is simply the integrated heat flux profile, which is the total amount of heat extracted from the billet by the mould. There appeared to be a corre-
Table 9.5: Binding by mould-billet dimensions and average friction measurements.

<table>
<thead>
<tr>
<th>Heat</th>
<th>Grade</th>
<th>Lubricant</th>
<th>Binding</th>
<th>Friction Coefficient</th>
</tr>
</thead>
<tbody>
<tr>
<td>277</td>
<td>0.14 pct. C</td>
<td>powder</td>
<td>yes</td>
<td>0.40</td>
</tr>
<tr>
<td>298</td>
<td>0.14 pct. C</td>
<td>oil</td>
<td>yes</td>
<td>0.50</td>
</tr>
<tr>
<td>312</td>
<td>0.32 pct. C + B</td>
<td>powder</td>
<td>yes</td>
<td>0.36</td>
</tr>
<tr>
<td>333</td>
<td>0.32 pct. C + B</td>
<td>oil</td>
<td>no</td>
<td>0.65</td>
</tr>
<tr>
<td>351</td>
<td>0.80 pct. C</td>
<td>oil</td>
<td>no</td>
<td>0.66</td>
</tr>
</tbody>
</table>

Relation between heat extraction and friction with the oil-cast heats. Heats with high heat extraction yielded higher forces. Peritectic grades, known for their low heat extraction, also exhibited lower casting forces. One would expect that lower friction would occur with reduced mould-billet contact. What factors influence mould-billet contact? Increased heat extraction forms a colder shell and causes increased billet shrinkage. Thus from a mould-billet gap perspective, a higher heat extraction might be expected to yield lower friction forces: yet higher forces are seen with higher heat extraction. The other factor is the billet surface roughness. Peritectic billet surfaces are wrinkled, as shown in Figure 5.24. It appears that this rough surface facilitates low friction because of the intermittent mould billet contact. The hyper-peritectic billet surfaces are smooth, and the increased contact of the smooth surface likely results in both increased heat extraction and friction. It appears from the sensor data that the billet surface condition impacts friction more positively than the mould-shell gap (caused by increased heat extraction) impacts friction negatively.
Table 9.6: Heat extraction and friction measurements.

<table>
<thead>
<tr>
<th>Heat</th>
<th>Grade</th>
<th>Lubricant</th>
<th>Friction Coefficient</th>
<th>Heat Extraction (MJ m(^{-2}))</th>
</tr>
</thead>
<tbody>
<tr>
<td>277</td>
<td>0.14 pct. C</td>
<td>powder</td>
<td>0.40</td>
<td>47</td>
</tr>
<tr>
<td>298</td>
<td>0.14 pct. C</td>
<td>oil</td>
<td>0.52</td>
<td>38</td>
</tr>
<tr>
<td>312</td>
<td>0.32 pct. C + B</td>
<td>powder</td>
<td>0.36</td>
<td>25</td>
</tr>
<tr>
<td>333</td>
<td>0.32 pct. C + B</td>
<td>oil</td>
<td>0.65</td>
<td>58</td>
</tr>
<tr>
<td>334</td>
<td>0.32 pct. C + B</td>
<td>oil</td>
<td>0.97</td>
<td>64</td>
</tr>
<tr>
<td>351</td>
<td>0.80 pct. C</td>
<td>oil</td>
<td>0.66</td>
<td>55</td>
</tr>
</tbody>
</table>

The results of trial D1 also support this finding. Referring to Table 6.3, heat 142 (peritectic) exhibited the lowest load cell forces. The peritectic heat also exhibited the lowest heat flux [116]. Heat 142 (peritectic) extracted approximately 32 MJ m\(^{-2}\), while the hyper-peritectic heats extracted 48 MJ m\(^{-2}\) on average. It should be noted that casting speed was not obtained in this plant trial and the heat flux calculations assumed a constant casting speed of 19 mm s\(^{-1}\); thus these heat extraction values are not exact but should be reasonable estimates.

This correlation likely does not exist when using mould fluxes. Friction changes significantly with different oscillation settings and casting speed when using mould fluxes lubrication.

Results of this work suggest that heat extraction and friction are linked when using oil lubrication. The peritectic steels in particular, known for their low heat extraction, were also found to exhibit lower mould-billet friction in two plant trials - a significant finding. To the author’s knowledge, no industrial data has been published to confirm this finding. Singh and Blazek measured...
withdrawal forces on a bench-scale stationary caster [23], as shown in Figure 2.16. The researchers concluded that carbon steels with greater than 0.40 pct. carbon exhibited low friction because of the smooth billet surfaces. But noteworthy in this figure is the decrease in friction near 0.10 pct. carbon, indicating that the peritectic grades exhibited lower friction in this experimental research. Using a pilot oscillating caster, Saucedo and Blazek reported similar friction when casting steels with less than 50 pct. carbon, but the authors recommended further work to determine if any grade sensitivity to friction existed [67]. In the same experimental study, higher friction was reported when casting highly alloyed steels. It must be stressed that this past work was conducted on experimental casting machines [23, 67], and the results may not be applicable to industrial billet machines. Another factor which may have a small impact on friction is oscillation mark size. Large oscillation marks would slightly reduce heat transfer because of the mould-shell gap associated with the oscillation mark. It is well known that oscillation marks are more pronounced in low carbon steels, e.g. [22]. Perhaps the reduced contact resulting from large oscillation marks may have a slight effect reducing friction.

The issue of steel grade and shell strength has a profound impact on billet defects, and how friction should be considered with respect to these defects. As discussed in Sections 2.8.7 and 6.2, the formation of transverse depressions is influenced by metal level fluctuations and steel grade. Low carbon steels [11] and boron(Ti)-alloyed steels [14] tend to be sensitive to depression formation; this was confirmed in this research. Since transverse depressions
form near the meniscus the effective shell strength near the meniscus will impact how the shell can form and retain the depression shape. The effect of the narrow freezing range of low carbon steels is known to increase the shell strength near the meniscus [14]. The increased shell strength of boron(Ti)-alloyed steels was postulated by Samarasekera et al. [14] and was supported further by the thermomechanical testing detailed in Section 6.2. In contrast, high carbon steels are known for their wide freezing range and weak, perhaps semi-solid [14], meniscus shells. These steels are more prone to forming bleeds and laps [12, 13]. Mechanisms for bleeds and laps have been presented by Kumar [13]; these mechanisms involve sticking, and subsequent meniscus overflows or shell tearing. To simplify, some grades are sensitive to depressions because of a strong meniscus shell; others are sensitive to sticking because of a weak meniscus shell.

Depression-sensitive steels are prone to forming cracks about the depressions themselves, as seen in the billet samples in Section 5.4.2. Transverse surface cracks at the base of depressions (e.g. Figure 5.29) certainly form at the meniscus when the depression formed. Subsurface cracks, below the base of depressions (e.g. Figure 5.30), are believed to be formed by axial withdrawal forces [14]. Thus, high friction will crack, and perhaps widen, a depression lower in the mould because the shell is thin and hot adjacent to a depression. The solution to reducing the severity of transverse depressions appears to be in implementing mould flux lubrication. This practice has two main influences. Firstly, the meniscus remains much more stable, owing to the sub-
merged entry nozzle, and the metal level fluctuations which initiate depression formation reduce significantly. This effect was clearly seen in the process control signals of Figure 5.23. Secondly, the mould flux provides reduced heat transfer and slower shell growth for the boron(Ti) grades. Transverse depressions and cracks may still form when using mould flux lubrication (e.g. Figure 5.32, but they are generally much less severe.

In contrast, the higher carbon steels are known to stick, so friction is clearly an issue in reducing defects. This occurs because of two reasons: the shell is much weaker and prone to tearing; and because the mould may be hot (because of the high heat flux associated with high carbon grades) with little lubricating oil present [13]. Thus, a “cold mould” practice should be adopted with stick-sensitive grades so the hot face temperature of the mould is below the boiling temperature of the oil [13]. As discussed in Section 6.5.4, the individual meniscus sticks cannot be seen in the force sensor response, because the force required to strip the stick or tear the shell is small compared to the overall surface friction force. Since poor lubrication facilitates sticking and the formation of bleeds and laps, one would expect higher overall friction in the presence of poor lubrication. In addition, transverse surface cracks are believed to form by axial mechanical forces [5]. Mould oscillation also influences bleeds and laps [13]; thus mould oscillation and friction monitoring should be employed to minimize friction in stick-sensitive grades. When slab casting using mould fluxes, Wolf notes that friction control is mandatory for “sticker” grades [10].
9.7 Lubrication and Force Upsets

Lubrication plays a significant role in both heat transfer and mould friction. In general, the use of mould fluxes results in reduced heat extraction because of the increased thermal resistance associated with the mould flux film. An exception to this occurs with the peritectic steel grades. Heat 277, powder-cast, exhibited slightly higher heat extraction than heat 298, oil-cast, which is consistent with the work of Pinheiro et al. [96]; peritectic grades yielded similar heat extraction between oil and powder lubricated heats. Mould powder is also a better lubricant than oil, and consistently results in lower mould friction. With the peritectic grades, mould powder (heat 277) had a lower friction coefficient than oil (heat 298). The most striking results were noted between the boron(Ti) grades. Heat 312, powder-cast, had a friction coefficient of 0.36 while heat 333, oil-cast, had an average friction coefficient of 0.65. Thus mould powder has a very forgiving effect on the boron(Ti) grades.

The friction and mathematical modelling results presented so far in this chapter have dealt with average mould response. In the presence of transient phenomena and/or poor operating practices, the instantaneous mould response can vary markedly. Two examples will now be presented, illustrating transient changes in mould response.
9.7.1 Oil Lubrication Friction Upset

A friction upset occurred when casting heat 333 (boron(Ti)-alloyed with oil lubrication). Ten minutes after the heat commenced, the force range was stable at 12500 N \( (c = 0.48) \) as shown in Figure 9.17. Twenty minutes later, the force range was 24000 N \( (c = 1) \) as shown in Figure 9.18. Process control was excellent during this heat; casting speed and metal level remained consistent between the data sets. Figure 9.19 illustrates the casting speed and metal level stability during the period of high friction.

Figure 9.20 illustrates the mould temperature profiles corresponding
Figure 9.18: Force response and casting speed during a period of high friction. Heat 333, oil lubrication, 0.32 pct. C + B, trial D2.
to the time periods when the forces were obtained. The mould temperature clearly increased during the period of high friction: this is a significant finding in this research. The temperature profiles were not instantaneous values, but 5 minute averages of mould temperature that represent the short term steady-state response. The temperature profiles had the same shape and location in the mould, confirming that the metal level was constant. The high friction therefore could not be correlated to a process control event, since the casting speed was constant also. The mould heat transfer model was employed to calculate the heat flux profiles corresponding to the periods of low and high friction, shown in Figure 9.21. These heat flux profiles correspond to heat extractions of 49.5 MJ m\(^{-2}\) for the low friction case and 66.6 MJ m\(^{-2}\) for the high friction case. These data support the previously mentioned finding that there was a correlation between friction and heat transfer seen when comparing the response of different steel grades cast with oil lubrication.

But what event was responsible for this increase in heat extraction and friction? As seen in this work, process control and steel grade are known variables of heat extraction, but these factors were constant in this case. Varying mould flux films are known to impact heat extraction and friction, but such relationships should not exist with oil lubrication. The mould response was investigated further to determine the thermal stability during the periods of low and high friction. As noted previously in Section 6.2, the standard deviation of the thermocouple temperature at the meniscus is an indicator of the metal level stability. A fluctuating metal level has been clearly linked to
Figure 9.19: Casting speed and metal level were stable during friction upset. Heat 333, oil lubrication, 0.32 pct. C + B, trial D2.
Figure 9.20: Mould temperature profiles during periods of low and high friction. Heat 333, oil lubrication, 0.32 pct. C + B, trial D2.
Figure 9.21: Mould-billet heat flux profiles during periods of low and high friction. Heat 333, oil lubrication, 0.32 pct. C + B, trial D2.
the formation surface defects like transverse depressions [14, 15, 60]; a smooth meniscus facilitates uniform shell growth. Figure 9.22 illustrates the standard deviations of the mould thermocouple temperatures during the periods of low and high friction. As is evident, the period of low friction exhibited a noisier mould temperature profile. Particularly notable is the mould thermocouple signal at 175 mm, near the meniscus, which was very noisy in the low friction case but relatively stable during the period of high friction. Based on these data, the low friction billets were believed to contain many more shape defects like transverse depressions, which reduce heat transfer. Further, the reduction of average mould temperature for the full mould length implies reduced heat transfer, and this can certainly be caused by the poorer surface quality of the low friction billets. The cause of the rough meniscus is the tundish stream. As discussed in Section 6.2, the aluminum-killed steelmaking practice used with the boron(Ti) steels routinely causes nozzle plugging and poor stream quality. Stream quality is a chaotic process variable that can change as the nozzle plugs and releases, or as globules of steel form on the nozzle exit and are cleared manually by operators using an oxygen lance.

This finding ties in well with the observation that peritectic steels exhibit lower heat transfer and lower friction because of their rough surface. It appears that shells with surface defects can have the same effect reducing heat transfer. Thus, when casting with oil lubrication, there is a link between friction and heat transfer. For a given mould and casting speed, it appears that increased surface contact between the mould and billet raises friction as well
Figure 9.22: Standard deviation of mould temperatures corresponding to the periods of low and high friction. Heat 333, oil lubrication, 0.32 pct. C + B, trial D2.
Table 9.7: Friction response for four powder-cast heats of 0.32 pct. C + B. Trial D2, 203 mm mould.

<table>
<thead>
<tr>
<th>Heat</th>
<th>Force Range (N)</th>
<th>Friction Coefficient</th>
<th>Casting Speed (mm s(^{-1}))</th>
<th>Mould Powder</th>
<th>Oscillation Frequency (Hz)</th>
</tr>
</thead>
<tbody>
<tr>
<td>310</td>
<td>12500</td>
<td>0.40</td>
<td>18.4</td>
<td>A</td>
<td>1.87</td>
</tr>
<tr>
<td>311</td>
<td>11000</td>
<td>0.36</td>
<td>19.1</td>
<td>A</td>
<td>1.95</td>
</tr>
<tr>
<td>312</td>
<td>11000</td>
<td>0.36</td>
<td>18.0</td>
<td>A</td>
<td>1.81</td>
</tr>
<tr>
<td>313.1</td>
<td>24000</td>
<td>1.05</td>
<td>12.5</td>
<td>A</td>
<td>1.28</td>
</tr>
<tr>
<td>313.2</td>
<td>24500</td>
<td>1.05</td>
<td>14.3</td>
<td>B</td>
<td>1.45</td>
</tr>
<tr>
<td>313.3</td>
<td>26000</td>
<td>1.15</td>
<td>13.8</td>
<td>B</td>
<td>1.40</td>
</tr>
</tbody>
</table>

as heat extraction.

9.7.2 Mould Flux Friction Upset

The importance of mould oscillation and friction control when casting with mould fluxes is well illustrated in the following example. As previously noted in Section 6.4, a force upset occurred when casting heat 313, a boron(Ti)-alloyed steel using powder lubrication. Pertinent data are detailed in Table 9.7. When the casting speed dropped from 18 to 13 mm s\(^{-1}\) the friction coefficient increased significantly, from 0.36 to above 1. In evaluating this friction upset, several factors must be considered, including mould flux properties and mould oscillation settings.

Mathematical modelling has shown that there was significant mould-billet binding occurring in heat 312 (and similarly in heats 310 and 311). The reduced casting speed in heat 313 did result in some increased heat extraction.
Figure 9.23: Temperature of the billet midface and corner. Heat 312, powder lubrication, 0.32 pct. C + B, trial D2.
Figure 9.24: Temperature of the billet midface and corner. Heat 313, powder lubrication, 0.32 pct. C + B, trial D2.
Heat 312 exhibited an average heat extraction of 25.1 MJ m$^{-2}$, while in heat 313 the mould extracted 36.4 MJ m$^{-2}$. Even with this increase in heat transfer, binding was certainly still occurring since heat 351 required 55 MJ m$^{-2}$ to avoid binding. Why would heats 310 - 312 exhibit low friction and heat 313 exhibit high friction when binding was occurring in all cases? The answer likely lies in lubrication effectiveness. As shown in Table 9.7 the mould powder was changed during heat 313, but data logged before and after the change indicate that the powder composition was not responsible for the friction increase. With mould flux lubrication, a stable liquid layer of flux along the mould length is required for effective lubrication and thus the billet surface temperature must be above the break-point temperature$^1$ of the mould flux [10]. If the flux solidifies between the mould and billet, the friction will increase in a solid friction mode.

Figures 9.23 and 9.24 present the midface and corner billet temperature profiles for heats 312 (low friction) and 313 (high friction) respectively. Mould flux A had a break-point temperature of 1000°C, while flux B had a break-point temperature of 1135°C. Figure 9.23 illustrates that the billet temperature of heat 312 (low friction) was above the break-point temperature of mould flux A. Therefore, one would expect stable liquid flux along the mould length. Figure 9.24 shows that the midface billet temperature of heat 313 (high friction) was well above the break-point temperature of both fluxes. The corner billet temperature of heat 313 dropped below the break-point temperature of flux A near the mould exit, and was below the break-point temperature of flux B.

$^1$The breakpoint temperature of a mould flux is defined as the temperature at which the viscosity of the flux significantly increases, as the flux becomes semi-solid.
Figure 9.25: Rough force sensor response during high friction believed to be caused by sticking. Heat 313, powder lubrication, 0.32 pct. C + B, trial D2.

for approximately two-thirds of the mould length. However, since the midface temperature of the billet extends nearly the full width of the billet (to within 10 - 15 mm of the corners) any solidified flux would have had a very small contact area relative to the amount of liquid flux. Further, there would be only a trivial amount of solidified flux when powder A was used, and high friction forces were measured. Thus, crystallized flux was likely not the cause of the high friction.

Mould flux consumption is impacted by oscillation parameters and casting speed [50]. Mould flux consumption increases with decreasing oscillation
frequency and decreasing casting speed. The casting machine in this case had oscillation frequency synchronized with casting speed, thus a decrease in speed had a significant impact on powder consumption (the decreased oscillation frequencies are noted in Table 9.7). The impact of speed and oscillation frequency can be seen in the following equation, which estimates mould powder consumption [50].

\[
Q = 0.55f^{-1}(\eta(0.06v_s)^2)^{-0.5}
\]

(9.2)

For the low friction heats, powder consumption was approximately 0.23 kg m\(^{-2}\) \((v_s = 18.5 \text{ mm s}^{-1}, f = 1.9 \text{ Hz}, \eta = 1.3 \text{ poise for flux A}); for heat 313 the consumption had effectively doubled to 0.44 kg m\(^{-2}\) \((v_s = 13.5 \text{ mm s}^{-1}, f = 1.35 \text{ Hz})\). Flux consumption near 0.3 kg m\(^{-2}\) is a common target [10], and 0.44 kg m\(^{-2}\) does not seem like an excessive consumption. The problem in this case may involve the melting rate of the powder, and the supply of liquid flux. During this trial, the measured thickness of the liquid flux was found to be only between 1 and 3 mm [117]. It is recommended that the liquid flux layer is greater than the mould stroke [117], to facilitate smooth infiltration of the lubricant between the mould and billet. During this plant trial, the mould stroke was approximately 5 mm, and the liquid flux layer was found to be very small. When the casting speed dropped in heat 313, the flux consumption doubled, and it was likely that the liquid flux supply was insufficient for the consumption demand. This would result in local starving of flux causing sticking, or the consumption of solid material from the flux layers.
above the liquid flux. Either condition could result in unstable flux lubrication and the increased friction observed. Figure 9.25 shows the force response during the high friction in heat 313. The force signal did not exhibit the smooth response seen Figure 6.2, which is indicative of stable liquid friction. The large force range and rough signal trace was believed to be caused by sticking, as a result of the unstable flux lubrication. Thus, when mould-billet binding exists and a lubrication upset occurs, very high friction forces will be measured. In this case, the lubrication upset increased friction nearly threefold.

It is evident that a different mould flux should be used, perhaps with a lower melting temperature, to increase the liquid flux layer thickness. It should also be noted that unstable liquid lubrication may occur with mould fluxes if the viscosity is low or the relative velocity between the mould and billet is low (caused by a short stroke or low oscillation frequency). These conditions may cause increased friction in a combination hydrodynamic-boundary lubrication regime [10, 101], which is in essence a transition between solid and liquid lubrication. It is clear from this example that mould oscillation and friction monitoring is essential in selecting lubricants and setting casting parameters to minimize friction. This is particularly crucial as mini-mills are now implementing powder casting practice to improve billet quality, and relatively little powder casting experience exists in the billet industry. Further, as producers are considering high speed casting, friction monitoring becomes more critical as the shell becomes thinner and the relative velocity between the mould and
billet increases.

9.8 The Next Step

This research has shown that mathematical modelling (mould heat transfer, mould distortion, billet solidification and billet shrinkage) and sensor measurements (temperature, process control, oscillation and force) are powerful tools for evaluating the fundamental process behaviour of continuous billet casting. When used on-line, sensors may be used to evaluate process quality, and flag when process upsets occur. Mathematical models are best used in the design stage, or for diagnosing the causes of process upsets.

The goal: high quality billets and high speed casting. Before this goal can be reached, known impediments to process quality must be addressed first. Recently, great insight has been shed on the importance of the meniscus on billet defects, \( e.g. \ [12, 13, 14, 15, 60] \). Thus metal level changes, intermittent lubrication, and meniscus turbulence must be controlled. This may be achieved by a number of process variables, including: improved tundish design, reducing the height of the open stream pour, careful centring of nozzles, improved steelmaking, or the implementation of mould flux lubrication practice. This work has shown that mould oscillation parameters in practice differ significantly from those expected. Thus, mould oscillation must be measured, and the casting machine must be maintained for precise, robust oscillation; guidelines exist for machine operation, \( e.g. \ [3] \). A major influence of billet quality is mould taper. Taper practices vary widely [3], yet guidelines exist for
improving mould taper using multiple-tapered moulds [6, 17, 20]. The mathematical modelling of billet shrinkage in this work has assisted in the designing of mould taper; for improving mould-billet heat extraction and reducing the risk of mould-billet binding. The hard recommendations from past work and this research are: quiet meniscus, precise oscillation, multiple-tapered moulds.

The next step: improvements in process control, lubrication design and high speed casting. The existing, transient casting speed control system is unacceptable, particularly when using mould fluxes. Liquid steel flow control must be added, with casting speed and metal level being system set-points. As a result of this research, the tools of mould oscillation and friction monitoring are now in place to design lubrication for minimizing friction. Lubricants may be selected, and casting speed targets/ranges set for minimizing friction. With precise oscillation, a correct mould taper, stable process control and minimized friction, billet productivity may be increased.
Chapter 10

Summary and Conclusions

This study has lead to a quantitative understanding of mould response through measurements of mould oscillation and friction on industrial casting machines. Fundamental lubrication behaviour was elucidated with a friction sensor, which is a powerful tool for evaluating lubrication and mould oscillation. Mathematical modelling of mould-billet binding has lead to further understanding of the response of the force sensor as well as some recommendations for improvement in mould taper design.

10.1 Key Findings

1. Mould oscillation parameters varied significantly from design specifications.

2. Casting speed varied continuously due to lack of liquid steel flow rate control.

3. Mould-billet friction has been quantified.
• Oil and mould flux lubricants fundamentally differ.

• An effective friction coefficient may be used for on-line monitoring.

4. Mould-billet binding has been mathematically modelled; model results may be used to improve mould taper design.

5. Mould-billet friction

• The force sensor mainly responded to lubrication effectiveness. Binding likely can only be detected when lubrication is poor.

• The force signal responded to gross process upsets (e.g. strand jerking, breakout).

• The signal did not respond to small, local events in the mould like a transverse depression forming or a meniscus stick.

• Oil lubrication
  
  – Lower friction was measured when casting peritectic steels (i.e. billets with rough surfaces).
  
  – A reasonable correlation was seen between friction and heat extraction.

• Mould flux lubrication
  
  – Friction is a function of mould oscillation, casting speed and flux properties.
10.2 Summary

Sensors

1. LVDT and accelerometer sensors were successfully employed to monitor mould oscillation. The LVDT calculated acceleration and accelerometer signals matched, confirming the kinematic response of the sensors and mould.

2. The LVDT is an appropriate, readily available, oscillation monitoring sensor. An accelerometer would also likely provide appropriate mould velocity and displacement signals, but a stable integration device (either hardware or software) would be required.

3. Three force sensor types were tested: load cells, strain gauges and a Kistler piezoelectric strain device. The load cells provided a qualitative force response, due to the installation of the load cells under the mould housing flange. Both strain sensor types were installed on the oscillator drive arm to measure mould friction quantitatively. The strain gauge signal matched the character of the load cell response, indicating that installing a force sensor on the drive arm of a billet machine would provide an accurate indication of mould response. The piezoelectric strain signal and the strain gauge signal matched reasonably well, but the piezoelectric sensor was designed to be a “quasi-static” device, and did not respond to higher frequency components in the force signal.
(like oscillator jerking or mould-billet sticking).

4. A conventional strain gauge installed on the machine drive arm was the most effective force sensor tested. A Kistler piezoelectric strain sensor has the advantage of being simpler to install, but at present requires a higher operating frequency range to provide information comparable to the strain gauge.

Basic Machine Response

1. During cold operation, the casting machine force signal was in phase with mould acceleration, indicating that the force sensor was responding to inertial machine forces.

2. During casting operations, the force signal was in phase with mould velocity, indicating that mould-strand friction is a function of velocity. This also implies that mould-strand friction dominates the inertial force when casting.

3. The three industrial billet machines tested operated at strokes less than their respective design strokes (as much as 35 pct. less).


5. Machines B and C were observed to dynamically change stroke with changing oscillation frequency. Machine B increased stroke with increas-
ing oscillation frequency; Machine C decreased stroke with increasing oscillation frequency.

6. Machines A and B showed a casting speed oscillation of 0.5 - 2.0 mm s\(^{-1}\), at the same frequency as the oscillator. This was believed to be caused by mould-strand friction pushing and pulling the billet against the withdrawal rollers.

7. Machines A and C exhibited a reduced stroke with increasing mould-strand friction. The stroke of Machine C increased by 10 pet. when the friction decreased by 50 pet.

8. Since the machines operated at reduced strokes and in non-sinusoidal oscillation profiles, the operating negative-strip times and mould lead values differed significantly from design values. In one instance, the operating negative-strip time was virtually zero.

9. Oscillation frequency is commonly synchronized with casting speed on billet machines. If the scheme is set up in a haphazard fashion, it can lead to undesirable changes in negative-strip times.

10. Horizontal movement was measured on Machine A, and was found to be double the suggested tolerance for slab casting machines. The mould also moved in a diagonal vertical-horizontal trajectory.
Process Control

1. Casting speed and metal level signals often changed in phase, caused by the control system responding to changes in the steel flow rate.

2. In some instances, casting speed was noted to steadily increase as the steel flow rate increased, uncontrolled.

3. When pouring open stream with oil lubrication, the casting speed and metal level signals were often “rough”. This was caused by the open stream creating a turbulent meniscus, which may emanate from several conditions: air entrained in the tundish stream, nozzle condition, and globules of steel forming and releasing on the nozzle exit. In contrast, casting speed and metal level signals were relatively smooth when casting through submerged entry nozzles with mould flux lubrication.

4. The “roughness” of the meniscus can be implied by calculating the standard deviation of the meniscus thermocouple signal. A smooth meniscus yields a standard deviation of about 3°C; a rough meniscus about 8°C or greater.

Friction

1. Mould-strand friction is a function of the relative velocity between the mould and billet.
2. An oil-lubricated mould responds in a solid friction regime, where the force signal is in the shape of a “square wave”. Solid friction is not dependent on the magnitude of relative velocity, but only the direction of relative velocity.

3. A mould lubricated with mould flux responds in a liquid friction regime. The friction response is sinusoidal, since liquid friction is a function of relative velocity.

4. Friction may be quantified by calculating the work expended per oscillation cycle or by estimating the friction coefficient.

   - Work per cycle is calculated by integrating the force over displacement for one oscillation cycle. The cold work\(^1\) per cycle can be subtracted from the casting work per cycle to yield the work expended as friction.

   - A friction coefficient can be estimated by dividing the casting force by the normal force due to ferrostatic pressure. The cold machine force range is subtracted from the casting force range to yield a more accurate estimate of the friction coefficient.

Both of these methods provide a quantitative sense of friction and can track friction changes. Although only an approximation, a friction coefficient near unity may indicate sticking or mould-billet binding.

\(^1\)Cold work refers to the cold mould state without casting.
Mathematical Modelling of Mould-Billet Binding

1. Existing mathematical models were employed to calculate mould heat flux and mould distortion.

2. A thermal-stress, elastic-viscoplastic billet shrinkage model was developed using ABAQUS commercial finite-element software. The model calculated billet shrinkage based on heat flux measurements taken during plant trials.

3. Stronger steels, i.e. less plastic, shrink slightly less than weaker steels.

4. The model was not sensitive to the exact viscoplastic equation, but the inclusion of plasticity in the model had a marked impact on overall billet shrinkage. The effect of plasticity on shrinkage was greater with increasing heat flux because thermal stresses cause plastic strain in the billet shell. Thus estimates of billet shrinkage based on temperature alone may be inaccurate.

5. Mould-billet binding can be evaluated by comparing the calculated mould and billet dimensions and tapers. Peritectic steel grades, and hyper-peritectic steels cast with mould flux were noted to bind in the parabolic mould at Company D. Hyper-peritectic steels cast with oil lubrication more closely matched the mould taper.

6. Mould taper design can be improved by using results of the billet shrinkage model. The parabolic mould at Company D employed a steep
5 pct. m\(^{-1}\) at the meniscus, which was too aggressive. Company A used a shallow single-tapered mould, which has previously been recognized as inadequate [20]. An “all-purpose” mould taper should be able to cast the peritectic grades, which are known for exhibiting lower heat transfer and shrinkage. Based on model calculations from the 200 mm mould at Company D, a suitable taper for casting the peritectic steels would be a multiple-tapered mould commencing at approximately 2 pct. m\(^{-1}\) at the meniscus, reducing to 1 pct. m\(^{-1}\) at the bottom of the mould.

Investigation of Friction Response as a Function of Process Variables and Upsets

1. Low carbon and boron(Ti)-alloyed steels are sensitive to transverse depression formation. The boron(Ti) steels generally exhibited high friction, yet the formation of transverse depressions could not be seen in the force signal. The formation of transverse depressions has been linked to metal level changes [14, 15, 60], consistent with this research. The high friction environment was believed to be caused by the metal level fluctuations, which removed oil lubricant from the mould wall intermittently, causing poor lubrication. The boron(Ti)-alloyed billets contained the highest depression population and these steels were also noted to have the greatest metal level instability. This was believed to be caused by the aluminum-killed steelmaking practice, which contributed to nozzle clogging, poor stream quality and large metal level fluctuations. Fur-
ther, thermomechanical testing of the boron(Ti) steels indicated that they have increased high temperature strength, which may contribute to depression formation at the meniscus [14].

2. Friction was noted to vary between steel grades when using oil lubrication. The peritectic grades exhibited the lowest forces, followed by the hyper-peritectic steels, and the boron(Ti)-alloyed grades exhibited the highest friction. As previously mentioned, the boron(Ti)-steel billets were believed to be poorly lubricated because of the unstable metal level associated with these grades. Peritectic steels are well known for their rough billet surface, and it appears that the rough surface results in lower mould friction because of the reduced contact area. When using mould flux lubrication, the friction was not noted to vary between grades.

3. A nozzle plugging upset may expose the shell and machine to high friction. Rapid changes in casting speed and the associated intermittent lubrication may cause sticking in the mould.

4. During two logged breakouts, the force signal did not increase prior to the breakout. During the breakout however, very large forces were experienced because of sticking in the mould.

5. Small amounts of sticking at the meniscus cannot be seen as individual events in the force response.
6. Strand jerking can be seen in the force signal as a fluctuating friction response.

7. The friction signal does not correlate with steady-state mould-billet binding calculations, i.e. if modelling results indicated significant binding, a large friction response was not necessarily seen. Therefore, the force signal responds mainly to lubrication effectiveness.

8. When casting with oil lubrication, there appeared to be a steady-state correlation between friction and heat extraction. Heats with higher heat extraction yielded higher friction. The peritectic steels exhibited low heat extraction and low friction. A relationship using mould flux lubrication was not seen.

9. When casting with oil lubrication, a transient increase in friction during a boron(Ti)-alloyed heat was clearly accompanied by an increase in heat extraction. During the period of lower friction, the standard deviations of the mould thermocouple temperatures were much greater than in the high friction case. The meniscus thermocouple was responding to a fluctuating metal level, and the fluctuations in lower mould temperatures were believed to be caused by a high population of transverse depressions causing intermittent mould-shell gaps. In this case, a defect-ridden billet reduced heat transfer and friction.

10. The friction signal responded well to lubrication upsets. When casting with powder lubrication during a period of subnormal casting speed
and low oscillation frequency, very high friction was measured (friction coefficient greater than one). Modelling results indicated that mould-billet binding was occurring. Under conditions of normal casting speed and mould oscillation parameters (with binding), low friction forces were measured. The high friction was believed to be caused by increased flux consumption exceeding the liquid flux supply, causing intermittent lubrication and sticking.

11. When casting with mould flux lubrication, trending logged data over long time periods has shown a clear relationship between friction and casting speed. As in the upset example above, the friction increased at low casting speeds. This was believed to be caused by either a flux consumption problem (as above) or a transition solid-liquid lubrication regime caused by the low relative velocity. It should be noted that excessively high casting speeds did not occur during this research. A clear relationship between casting speed and friction was not observed with oil lubrication.

10.3 Concluding Remarks

New sensors were tested to quantify the kinematic and dynamic response of the mould. The use of redundant sensors validated both the sensor responses and machine response. Simple, inexpensive sensors are available for quantifying the mechanical response of the mould.
The kinematic responses of the three machines tested varied from design specifications. Thus the fundamental mould oscillation parameters of stroke, negative-strip time and mould lead differed significantly from those expected. Further, machine response was *dynamic*, and varied as a function of oscillation frequency and machine loading. Given the importance of mould oscillation in surface quality, reducing friction, and preventing sticking, the measured machine responses were unacceptable. Since the design specifications of these machines were not being met (even on new machines), the issue of casting machine maintenance is likely even less recognized. Wobbly oscillation is believed to contribute to cracking and off-squareness [3, 38]. Based on the operating responses of the machines tested, on-line oscillation monitoring is imperative.

Process control of billet casting is inherently transient due to lack of steel flow rate control. Changes in flow rate due to tundish and nozzle conditions cause the casting speed to vary significantly. The varying casting speed leads to changing shell properties at the mould exit, in addition to varying lubrication properties when using mould flux lubrication. Casting speed control should be improved, perhaps with the addition of flow control, to improve billet quality. This is particularly important when considering high speed casting, an active topic among billet producers. Meniscus stability is also very important with respect to both billet quality and oil lubrication effectiveness. The stability of the meniscus can be quantified by monitoring the mould temperature near the metal level.

Casting friction is a function of the relative velocity between the mould
and billet. When using oil lubrication, the force signal responds in a solid friction mode; when using stable mould flux lubrication, liquid friction is evident. The estimation of a friction coefficient on-line is a simple technique for quantifying friction. Although the calculated friction coefficient is only an estimate, since the true normal force not only includes the ferrostatic pressure but also sticking, binding and mould-billet misalignment, it does provide a reference point for the friction magnitude. Further, one can imply that binding may be occurring (in the presence of poor lubrication) if the friction coefficient is near unity. In any technique for quantifying mould-billet friction, the machine forces must be subtracted from the gross force signal. In the cases of the billet machines tested, this was relatively simple since the casting forces significantly dominated the cold machine forces. If the machine forces were large relative to the casting forces, the work per oscillation cycle technique would be the superior method for quantifying friction. This method provides a more exact measure of the work expended as friction and would be less prone to error with large machine forces.

Mathematical models were employed to calculate the mould and billet dimensions to determine if binding was occurring. A thermal-stress, elastic-viscoplastic, finite-element model was developed to calculate billet shrinkage using in-situ heat flux measurements. The billet shrinkage was not sensitive to the viscoplastic model used, but the inclusion of plasticity in the model had a marked impact on billet shrinkage. Stronger shells, i.e. less plastic, shrunk slightly less than weaker shells. The parabolic mould taper at Company D,
commencing at 5 pct. m\(^{-1}\) was too aggressive. This was confirmed by binding
calculations and longitudinal midface depressions in billet samples, caused by
the excessive taper buckling the billet face. When designing an "all-purpose"
mould taper, one should look at the grades which shrink less, namely the
peritectic steels. As previously mentioned, a suitable taper in this case would
be a multiple-tapered mould, commencing at approximately 2 pct. m\(^{-1}\) at the
meniscus and reducing to 1 pct. m\(^{-1}\) at the bottom of the mould.

The development of quantitative force sensing on industrial billet ma­
chines has allowed the process to be studied from a different perspective in the
context of process variables and upsets. The friction of stable mould powder
lubrication was consistently less than oil lubrication. Interestingly, the force
response did not clearly vary as a function of mould-billet binding. Perhaps
one might expect higher forces with increased mould-billet binding, but the
lubrication effectiveness appears to dominate the force signal. In cases when
severe binding was occurring the force signal may be low because of stable
lubrication. This is not to imply that satisfactory lubrication is an excuse for
an excessively tapered mould. In the case of a lubrication upset, excessive
forces will be experienced because of the binding.

Peritectic steels exhibited less friction than hyper-peritectic steels when
casting with oil lubrication. Peritectic steels are well known for their rough
billet surface which contributes to low heat transfer; it appears that the rough
surface results in lower mould friction because of the reduced contact area.
Also, there appears to be a correlation between heat extraction and friction
when casting with oil lubrication, based on average heat extraction and friction measurements. This effect was studied in further detail with a significant friction upset during a boron(Ti)-alloyed heat. When the friction increased, there was a clear and significant increase in mould temperature, confirming a relationship between heat extraction and friction. Process control was stable during this heat, so the effect of casting speed could be neglected. The grade and heat extraction sensitivity to friction was not seen when casting with mould fluxes.

Fundamentally, the friction signal responds to lubrication effectiveness, and thus is a powerful tool for evaluating lubricants and mould oscillation parameters, particularly with mould fluxes. Casting speed changes resulted in clear changes in friction when casting with mould powders, confirming the importance of the relationship between mould oscillation, casting speed and friction. Friction upsets were also seen when casting with oil lubrication, but a clear relationship with casting speed was not seen. The friction signal represents an average force response to the billet-mould interface. Local events to an isolated area of the mould, e.g. meniscus sticking and transverse depression formation, cannot be seen in the force signal. If, however, lubrication was poor in these conditions, a slightly higher friction signal may be observed. During gross process upsets such as strand plugging or breakouts, significantly increased friction may be observed due to rapid speed changes or lubrication problems. The force signal did not change prior to the observed breakouts in this research, and thus the friction signal is likely not effective as a “breakout
warning”.

In practice, many process variables and upsets impact lubrication and friction, and ultimately billet quality. A force sensor can be used as a design tool for minimizing friction and as an on-line sensor for detecting upsets, and thus preventing the production of sub-standard billets. Process quality can be improved by implementing more appropriate mould taper designs, using results of the billet shrinkage model. Multiple-tapered moulds should be used, using intermediate tapers.
Chapter 11

New Knowledge and Recommendations

11.1 New Knowledge

Significant knowledge was obtained in this research regarding the mechanical response of the mould, and this section highlights the contributions to new knowledge. This research was conducted with two aspects of new knowledge in mind: firstly, to provide fundamental knowledge of the process, specifically involving mould-billet friction and mould taper; and secondly, to provide a practical framework for mini-mills to commence on-line monitoring of the process.

1. Simple measurements of mould oscillation have provided important quantitative information regarding the operation of industrial billet machines. Mould oscillation is fundamental in reducing friction and sticking, yet these machines operate with oscillation parameters significantly different from design values. The key parameters of stroke, negative-strip time and mould lead often operated at unacceptable values. Further,
the machine response appeared to be dynamic, and changed as a function of oscillation frequency and loading. Individual machine design also affected the response, since one machine would increase stroke with increasing oscillation frequency while another would decrease stroke with increasing frequency. Oscillation parameters may also be set incorrectly; in one instance the operating negative-strip time was virtually zero.

2. Machine forces have been measured quantitatively on industrial billet casters, possibly for the first time. Strain sensors installed on the machine drive arm (for the machines tested), facilitated these measurements. Thus the drive arm appears to be an appropriate location for robust, on-line monitoring of machine forces. The casting forces significantly dominated the cold machine forces, indicating that most of the force signal measured was caused by mould-strand friction.

3. Mould-billet friction may be evaluated by estimating the friction coefficient or by calculating the work expended per oscillation cycle, a technique developed at Bethlehem Steel in research on slab casting [68]. Both techniques involve subtraction of the cold machine forces to obtain a more accurate estimate of mould-billet friction. Thus friction can be quantified, and process variables such as lubricants, oscillation parameters and casting speed may be evaluated for their impact on friction.

4. A thermal-stress, elastic-viscoplastic, billet shrinkage model was developed using ABAQUS commercial finite-element software. Coupled with
the results of existing models of mould heat flux and mould distortion, mould-billet binding could be evaluated. Results of these models were used to evaluate mould taper design, and to interpret force sensor results when binding was occurring. The mould taper was found to be inappropriate at both companies. Company A used a shallow single-tapered mould, and Company D an aggressively tapered parabolic mould. Multiple-tapered moulds should be employed, using an intermediate taper (commencing at approximately 2 pct. m\(^{-1}\) at the meniscus, and reducing to 1 pct. m\(^{-1}\) at the bottom of the 200 mm mould).

5. Mathematical models and sensor measurements were used to interpret the mould response during friction changes. Interestingly, the force signal does not necessarily respond to mould-billet binding. If the lubrication is adequate, low forces will be sensed. Thus friction sensing responds mainly to lubrication effectiveness. If binding exists however, and lubrication is poor, exceptionally high forces will be measured.

6. A surprising result of this study is that a correlation likely exists between heat extraction and friction when casting with oil lubrication (solid friction). A rough surface impacts heat transfer and friction negatively. Thus when casting with oil lubrication, mould-billet contact governs heat transfer and friction.

7. The friction response of oil and mould flux lubricants fundamentally differ in billet casting. The difference between solid lubrication and liquid
lubrication has been noted in the slab casting literature [69] in reference to evaluating mould flux lubrication. Very little research has been published regarding friction in the field of billet casting. Further, slab casters usually employ only powder lubrication and billet machines typically use oil lubrication. During the course of this work, the two mini-mills participating in the research implemented powder casting practice for certain grades to improve billet quality. This research in evaluating friction on billet machines is timely, as billet producers are now using both lubricant types, as well as casting a large range of grades and sizes. As was evident in one example of high friction using mould powder, significant opportunity exists in billet casting to improve the usage of mould fluxes. The powder in this case was poorly selected, as the liquid flux pool was very small, and insufficient for effective lubrication. Also apparent in this example was the influence of mould oscillation and casting speed on flux consumption. Thus friction needs to be measured and minimized: the use of a force sensor is key in evaluating lubricants for both improving billet quality and increasing productivity.

11.2 Recommendations

As is evident from this research, the mechanical response of the mould is very dynamic, and changes with process variables. Since the mould response is key to both billet quality and productivity, its behaviour should not be presumed or neglected. Recommendations from this research centre on measurement
of mould response. This work contributes to the concept of the “Intelligent Mould” [16], an on-line monitoring system for billet and process quality.

The casting machine itself should not be a variable in the process. The machines tested were not operating at design specifications, since measurement of the oscillator response had not previously been conducted during operating conditions. One would expect that monitoring of the oscillator would assist in maintenance, and would flag potential oscillator problems before sub-standard billets were produced or the machine became damaged. Thus, the oscillator response should be measured at least periodically during casting conditions. A reference performance should be established (i.e. measured stroke and negative-strip time), and deviations from this performance would indicate a need for maintenance. One might not expect that on-line monitoring of the oscillator would be necessary, but “human” upsets also occur. As was noted during one plant trial, the negative-strip time was zero because of incorrect oscillation settings. Local oscillation practices may vary between grades, lubricants and mould sizes, and it is understandable that confusion may occur at times given the chaotic environment of the plant floor. On-line monitoring, reporting and alarming would ensure that target oscillation settings were being met.

Friction monitoring is an essential tool for evaluating lubrication effectiveness, and for selecting lubricants such as mould powders. Since casting with mould fluxes has only recently been employed in the mini-mill industry, this research on friction monitoring is timely. Friction sensing can be used as
a design tool for selecting lubricants and setting mould oscillation parameters. Friction sensing also responds well to some process upsets, and is an excellent on-line tool for detecting problems. In the case of a lubrication upset, the shell may stick, reducing surface quality, crack under high axial forces, or cause a breakout as an extreme case. Reduced lubrication will also cause premature mould wear.

Mould taper design varies significantly in the billet industry, as was evident in a survey of billet producers [3]. Mathematical modelling of billet shrinkage will greatly assist in mould taper design. Although the optimum mould taper, for a given mould size, will depend on steel grade and casting speed, some guidelines exist for taper design. Single-tapered moulds are inappropriate because of mould distortion at the meniscus and greater billet shrinkage in the upper portion of the mould. Thus multiple-tapered moulds should be used. Parabolic moulds which are steeply tapered (5 pct. m\(^{-1}\) at the meniscus) have been shown to be too aggressive. If peritectic steels are to be cast in a 200 mm mould, intermediate tapers are appropriate (say 2 pct. m\(^{-1}\) at the meniscus, and 1 pct. m\(^{-1}\) lower in the mould). Further, billet producers must measure the mould dimensions to confirm the operating mould taper. This would ensure that moulds were supplied to the correct tolerances, and moulds which have distorted are removed from service.

The issue of process control in billet casting is very important, especially with respect to mould flux lubrication and high speed casting. As was seen in this research, casting speed upsets may cause high friction when using mould
fluxes. In this context, the implementation of steel flow rate control would be advantageous, as the casting speed would remain fixed. The disadvantages include cost and design of such equipment, since it is rarely used (if at all) in the mini-mill industry. If flow rate control, and hence casting speed control, is available to the researcher, the impact of lubrication and friction can be further studied. For a given lubricant and mould oscillation settings, friction versus casting speed curves can be generated. Thus lubricants may be selected, and casting speed targets set for minimizing friction.

Mould response should be measured quantitatively to assist in oscillator maintenance, the setting of oscillation parameters, evaluating lubricants, and identifying upsets on-line. Ultimately, the industry needs robust, precise oscillation, and low mould-billet friction to reduce billet defects and facilitate high speed casting.

11.3 A Primer for Billet Producers

If you own a billet machine read this.

1. Mould oscillation
   - Measure it. It's not what you think it is.

2. Process control
   - Casting speed may vary significantly, by greater than 30 pct., because of lack of steel flow control. This may be unacceptable for
mould flux lubrication practice and high speed casting. Consider installing flow control.

- The meniscus is, at times, excessively turbulent due to poor stream quality. If the meniscus cannot be stabilized, mould flux lubrication practice should be implemented for depression-prone grades.

3. Friction and lubrication

- Friction may be quantified by installing a strain sensor on the machine drive arm.

- Oil and mould flux friction responses fundamentally differ.

- What impacts measured friction with oil lubrication?
  - Peritectic steel surface roughness
  - Surface defects like depressions
  - Oil supply and distribution

- What impacts measured friction with flux lubrication?
  - Casting speed
  - Mould oscillation

- What can you see with a friction sensor?
  - Overall mould-billet interaction: lubrication effectiveness.
  - Gross process upsets like strand plugging and breakouts.
  - Billet jerking caused by a lubrication problem.
- Mould-billet binding in the presence of poor lubrication.

- What can't you see with a force sensor?
  - Single, local events like meniscus sticking, or transverse depressions forming.

- What else likely affects friction?
  - Mould taper. Impacts contact with oil lubrication; flux thickness with powders.
  - Type of mould flux: break-point temperature and viscosity.

### 11.4 Simple Tools for On-Line Monitoring

The following section lists some simple features that an on-line system could use to monitor mould response. This work complements the research of Kumar [13], who studied the mould thermal response in the context of billet defects. A prototype of such an on-line system has been under development at UBC [98, 118], which is capable of most of these features.

#### 11.4.1 Sensors

The system must be capable of measuring friction and mould oscillation. Strain gauge and LVDT sensors are appropriate. The system must also log casting speed, metal level and mould thermocouple signals if available.
11.4.2 Features

Oscillator

- Mould stroke
- Negative-strip time
- Mould lead
- Oscillation frequency

Process Control

- Metal level
- Casting speed
- Metal level stability (standard deviation of the meniscus thermocouple signal).

Lubrication

- Raw force range
- Effective friction coefficient (including the rejection of the cold machine forces)

11.4.3 Display

Table 11.1 presents a list of process parameters and how they may be presented by an on-line system. The process parameter attributes used in the table are:
Operating The actual sensor reading or calculated process parameter.

Set point The target process parameter.

Heat average The process parameter averaged on a per heat basis.

Extrema The history of maximum and minimum values of the parameter taken over a specified time period. Extrema may be reported on a per heat basis, or trended on a per minute basis to visualize the operating range of a process variable.

The graphical displays (raw signal versus time) would assist in the assessment of oscillation condition and friction regime.

The system could alarm when a process parameter was out of a specified range. For example, if the casting speed became excessive, the heat could be aborted. The system could also report process parameters as part of a management information system. The parameters could be reported on a per heat basis, including average, set point, and extrema values.
Table 11.1: Suggested methods for displaying process parameters on an on-line system.

<table>
<thead>
<tr>
<th>Process Parameter</th>
<th>Attribute</th>
<th>Numeric</th>
<th>Trend</th>
<th>Graphical</th>
</tr>
</thead>
<tbody>
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<td>Stroke</td>
<td>operating</td>
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<td>x</td>
<td>x</td>
</tr>
<tr>
<td></td>
<td>set point</td>
<td>x</td>
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<td></td>
</tr>
<tr>
<td></td>
<td>heat average</td>
<td>x</td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>extrema</td>
<td>x</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Mould velocity</td>
<td>operating</td>
<td>x</td>
<td>x</td>
<td></td>
</tr>
<tr>
<td>Negative-strip time</td>
<td>operating</td>
<td>x</td>
<td>x</td>
<td></td>
</tr>
<tr>
<td></td>
<td>set point</td>
<td>x</td>
<td></td>
<td></td>
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<tr>
<td></td>
<td>heat average</td>
<td>x</td>
<td></td>
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<tr>
<td></td>
<td>extrema</td>
<td>x</td>
<td>x</td>
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<tr>
<td>Mould lead</td>
<td>operating</td>
<td>x</td>
<td>x</td>
<td></td>
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<tr>
<td></td>
<td>set point</td>
<td>x</td>
<td></td>
<td></td>
</tr>
<tr>
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<td>heat average</td>
<td>x</td>
<td></td>
<td></td>
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<tr>
<td>Oscillation frequency</td>
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<td>x</td>
<td>x</td>
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<tr>
<td></td>
<td>set point</td>
<td>x</td>
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<td>heat average</td>
<td>x</td>
<td></td>
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<tr>
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<td>x</td>
<td>x</td>
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<tr>
<td></td>
<td>set point</td>
<td>x</td>
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<td></td>
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<td></td>
<td>heat average</td>
<td>x</td>
<td></td>
<td></td>
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<tr>
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<td>x</td>
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<tr>
<td>Metal level</td>
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<td></td>
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<tr>
<td></td>
<td>heat average</td>
<td>x</td>
<td></td>
<td></td>
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<tr>
<td></td>
<td>extrema</td>
<td>x</td>
<td>x</td>
<td></td>
</tr>
<tr>
<td>Meniscus stability</td>
<td>operating</td>
<td>x</td>
<td>x</td>
<td></td>
</tr>
<tr>
<td></td>
<td>heat average</td>
<td>x</td>
<td></td>
<td></td>
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<tr>
<td>Raw force range</td>
<td>operating</td>
<td>x</td>
<td></td>
<td>x</td>
</tr>
<tr>
<td></td>
<td>heat average</td>
<td>x</td>
<td></td>
<td></td>
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<tr>
<td>Friction coefficient</td>
<td>operating</td>
<td>x</td>
<td>x</td>
<td></td>
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<tr>
<td></td>
<td>heat average</td>
<td>x</td>
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<td></td>
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<tr>
<td></td>
<td>extrema</td>
<td>x</td>
<td>x</td>
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</tr>
</tbody>
</table>
Bibliography


[18] I.V. Samarasekera. Private communication, University of British Columbia, Vancouver, Canada.


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Appendix A

Strain Gauge Force Calculation

Strain on the drive arm was calculated from the strain gauge bridge voltage, using Equation 4.1. Stress was calculated from the elastic stress-strain relationship, $\sigma = E\varepsilon$, and force was simply $F = \sigma A_{\text{arm}}$.

where:

$\sigma = \text{stress (Pa)}$

$E = \text{elastic modulus (Pa)}$

$F = \text{force (N)}$

$A_{\text{arm}} = \text{cross-sectional area of drive arm (m}^2\text{)}$

The variable values used in the calculations were:

$\nu = 0.3$

$E = 200 \cdot 10^9 \text{ Pa}$

$GF = 2$

Substituting the variable values into Equation 4.1, the strain gauge force was,
\[ F = E A_{\text{arm}} \varepsilon = E A_{\text{arm}} \left( \frac{V_r}{1.3 + 0.7V_r} \right) \]  

(A.1)

For Machine B (trials A1 and A2), the oscillator arm was made of 101.6 mm square steel tubing, with a wall thickness of 9.53 mm. The corresponding area was 0.0035 m\(^2\). For Machine C (trials D2 and D3), the oscillator arm was a tapered steel box. The dimensions of the box at the strain gauge location was 255 x 100 mm, with a 15 mm wall thickness. The area of the box was 0.00975 m\(^2\).
Appendix B

Mould Thermocouple Layout
<table>
<thead>
<tr>
<th>Distance from mould top (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>O 1</td>
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<tr>
<td>O 2</td>
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<tr>
<td>O 3</td>
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<tr>
<td>O 5</td>
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<td>O 6</td>
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<tr>
<td>O 7</td>
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<td>O 15</td>
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<tr>
<td>O 24</td>
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<tr>
<td>O 25</td>
</tr>
</tbody>
</table>

Figure B.1: Layout of mould thermocouples on the east face; used for plant trials D1 and D2.
Table B.1: Mould thermocouple depths used in plant trials D1 and D2.

<table>
<thead>
<tr>
<th>Thermocouple Number</th>
<th>Depth (mm)</th>
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<tbody>
<tr>
<td>1</td>
<td>7.81</td>
</tr>
<tr>
<td>2</td>
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<td>25</td>
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Appendix C

Calibration Data

This appendix contains calibration equations to convert logged voltages to SI units. The equations are linear, of the form:

\[ \text{output unit} = a_0 + a_1 \times \text{voltage} \quad (C.1) \]

Table C.1: Sensor calibration data for plant trial D1. Signals logged in millivolts.

<table>
<thead>
<tr>
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<th>(a_1)</th>
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<tr>
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Table C.2: Sensor calibration data for horizontal mould movement test during plant trial D1. All LVDTs were short stroke.

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Table C.4: Sensor calibration data for plant trial D2. Signals logged in milli-volts.

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Table C.5: Sensor calibration data for plant trial D3, force sensor test. Signals logged in volts.

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Table C.6: Sensor calibration data for plant trial A2, force sensor test. Signals logged in volts.

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Appendix D

Plant Trial Sensor Schematics
Figure D.1: Electrical schematic for plant trial D1.
Figure D.2: Electrical schematic for plant trial A1.
Figure D.3: Electrical schematic for plant trial D2.
Figure D.4: Electrical schematic for on-line force sensor test D3.
Figure D.5: Electrical schematic for on-line force sensor test A2.
Appendix E

Chemical Compositions of Heats Monitored

This appendix contains the chemical analyses of heats monitored during the main experimental plant trials.
Table E.1: Chemical composition, in weight percent, of heats monitored at plant trial A1.

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<th>Cu</th>
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Table E.2: Chemical composition, in weight percent, of heats monitored at plant trial D1.

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Appendix F

Contour Plots of Surface Defects
Figure F.1: Longitudinal midface depression believed to be caused by excessive mould taper. Sample 1, trial D1, 0.3 pct. C + B, 203 mm mould, oil lubrication, north face.
Figure F.2: Longitudinal midface depression believed to be caused by excessive mould taper. Sample 2, trial D2, 0.14 pct. C, 203 mm mould, powder lubrication, north face.
Figure F.3: Transverse depression, sample 1. Trial D2, 0.14 pct. C, 203 mm mould, powder lubrication, east face.
Figure F.4: Transverse depression, sample 2. Trial D2, 0.14 pct. C, 203 mm mould, powder lubrication, south face.
Figure F.5: Transverse depression, sample 3. Trial D2, 0.3 pct. C + B, 203 mm mould, oil lubrication, east face.
Figure F.6: Transverse depression, sample 4. Trial D1, 0.3 pct. C + B, 203 mm mould, oil lubrication, west face.
Figure F.7: Transverse depression, sample 5. Trial D2, 0.3 pct. C + B, 203 mm mould, oil lubrication, south face.
Appendix G

Thermomechanical Tests of Boron Steels

The simple tests described in Table G.1 were conducted on as-cast samples to determine if the boron steels exhibited increased hot strength. Further metallurgical research must be conducted to test samples at in-situ conditions.

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<th>Sample</th>
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<th>$T_{\text{ aust}}$ (°C)</th>
<th>$T_{\text{ deform}}$ (°C)</th>
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Figure G.1: Thermomechanical tests of 0.32 pct. C and 0.32 pct. C + B as-cast samples at a strain rate of $10^{-2}$ s$^{-1}$ at 1200°C.

Figure G.2: Thermomechanical tests of 0.32 pct. C and 0.32 pct. C + B as-cast samples at a strain rate of $10^{-2}$ s$^{-1}$ at 1300°C.