THE EFFECTS OF HIGH SPEED CASTING ON THE MOULD HEAT TRANSFER, BILLET SOLIDIFICATION, AND MOULD TAPER DESIGN OF CONTINUOUSLY CAST STEEL BILLETS

By

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ABSTRACT

As the final study in a five-year project, analysing the high speed casting of high quality continuously cast steel billets, an industrial trial was conducted at Company H. The mould was instrumented with thermocouples and a linear variable displacement transducer to study the thermal response of the system to changes in casting conditions. Billet samples were collected to study the effects of high casting speeds on internal and surface quality.

Two existing mathematical models were used to approximate the heat transfer response in the mould and predict the billet solidification characteristics. The results of both models were verified using mould water heat transfer calculations, data from the literature, and empirical correlations. The mathematical model of the mould has allowed the quantification of the different characteristics of the heat transfer behaviour at the midfaces and corners of the mould. The analysis has shown that the effect of carbon content on the mould heat transfer was less pronounced at high casting speed due to the dominating effect of the reduced residence time. As well, the magnitude of change in heat transfer was less sensitive at casting speeds exceeding 3.0 m/min. Casting speeds above ~3.5 m/min were also found to increase the metal level standard deviation and local mould thermal standard deviation at the meniscus. However, the increased casting speeds were found to decrease the average heat transfer standard deviation at each mould wall, but increase the variability of the responses between each mould wall face. These last two effects have not previously been studied in conventional or high-speed applications.

The results of the billet evaluation could not be disclosed for proprietary reasons. However, a qualitative evaluation has established that the internal and surface quality of these particular high speed continuously cast billets were not significantly worse than observed in conventionally cast billets. As well, the observed defects could not be
quantitatively linked to metal level standard deviations, casting speed, superheat, or tundish nozzle diameter.

In the area of mould design, the longer mould lengths used in high speed casting were found to have a strong effect on the response of the inside curved wall in curved mould machines at casting speeds exceeding 3.0 m/min. This effect was observed at both the midfaces and corners of the mould. This was explained in terms of the interaction of several variables, including: gravity, mould length and curvature, high casting speed, and carbon grade. This phenomenon has not been observed previously, hence mould tapers were recommended for the casting of plain low and high carbon grades at high speed (3.0 to 4.5 m/min) to improve the heat transfer characteristics in the long mould. These mould tapers were found to be relatively insensitive to casting speeds within 3.5 to 4.0 m/min for both high and low plain carbon grades.

From the analysis of moulds with sufficient, inadequate, and excessively steep meniscus-level mould tapers, a new mechanism was proposed to explain the high rates of local heat transfer observed in excessively tapered moulds. This phenomenon was described in terms of the mould-strand interaction caused by the dynamic taper during the upstroke of mould oscillation. The local uniformity of the shell, extent of the meniscus taper, and type of mould lubricant used were shown to have an effect on the applicability of the mechanism.

This project completes a 5-year study of high speed continuous casting of high quality steel billets, and provides recommendations for further work with a brief look at future trends in the industry.
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# NOMENCLATURE AND LIST OF SYMBOLS

## Nomenclature

<table>
<thead>
<tr>
<th>Abbreviation</th>
<th>Description</th>
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<tbody>
<tr>
<td>AC-SM</td>
<td>Alternating Current Stirring Modifier</td>
</tr>
<tr>
<td>ADI</td>
<td>Alternating Direction Implicit</td>
</tr>
<tr>
<td>EMS</td>
<td>Electromagnetic Stirring</td>
</tr>
<tr>
<td>FDM</td>
<td>Finite Difference Method</td>
</tr>
<tr>
<td>FEM</td>
<td>Finite Element Model</td>
</tr>
<tr>
<td>ICW</td>
<td>Inside Curved Wall (of Mould Tube)</td>
</tr>
<tr>
<td>LSW</td>
<td>Left Straight Wall (of Mould Tube)</td>
</tr>
<tr>
<td>M-EMBR</td>
<td>Mould Electromagnetic Braking</td>
</tr>
<tr>
<td>M-EMS</td>
<td>Mould Electromagnetic Stirring</td>
</tr>
<tr>
<td>OCW</td>
<td>Outside Curved Wall (of Mould Tube)</td>
</tr>
<tr>
<td>RSW</td>
<td>Right Straight Wall (of Mould Tube)</td>
</tr>
<tr>
<td>S-EMS</td>
<td>Strand Electromagnetic Stirring</td>
</tr>
<tr>
<td>SEN</td>
<td>Submerged Entry Nozzle (used with mould flux lubrication)</td>
</tr>
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## Symbols

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
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<tbody>
<tr>
<td>C</td>
<td>Constant, Parabolic Growth Law, mm</td>
</tr>
<tr>
<td>C_p</td>
<td>Specific Heat, kJ/(kgK)</td>
</tr>
<tr>
<td>h</td>
<td>Specific Enthalpy, kJ/kg</td>
</tr>
<tr>
<td>H</td>
<td>Enthalpy, kJ</td>
</tr>
<tr>
<td>K</td>
<td>K-factor, Parabolic Growth Law, mm/min^{1/2}</td>
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<td>L</td>
<td>Length, m</td>
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<td>Mass (liquid steel), kg</td>
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<td>MT</td>
<td>Mould Taper, %/m</td>
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<td>t</td>
<td>Dwell time, sec</td>
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<td>t_N</td>
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<td>W</td>
<td>Width of mould (internal), m</td>
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<td>x</td>
<td>Shell thickness, mm</td>
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I would like to extend my sincerest thanks to Mr. B. Neil Walker and Mr. Gary Lockhart, from the Centre for Metallurgical Process Engineering (CMPE) of the University of British Columbia (UBC). Neil’s insight, guidance, technical support and knowledge throughout the plant trial and the analysis have played an enormous role in the evolution of this thesis. As well, Gary’s humour, advice, and technical support in the project were very much appreciated.

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Cindy Chow
April 2001
CHAPTER 1: INTRODUCTION

Continuous casting has largely replaced ingot casting due to its many benefits. As of 1999, over 80% of the steel produced in North America was continuously cast [1]. Continuous casting gives a higher process yield, improved metallurgical quality, improved uniformity of the product, and lower energy consumption than ingot casting, at a lower capital and operating cost [2].

The continuous casting of steel billets involves the transformation of molten steel into a solid semi-finished product. The steel is subjected to controlled cooling and solidification in a water-cooled, open-ended, copper mould (Figure 1.1). The mould is the primary heat transfer zone in the casting machine and is suspended inside of a steel sleeve, or baffle, which creates an annular channel through which the cooling water flows. As the mould is open-ended, a dummy bar is required at the start of a sequence to begin casting the strand. It is inserted into the bottom of the mould to act as a temporary bottom and create a vessel for the initial solidification of steel. Heat transfer in the mould, the primary cooling zone, occurs through:

a) convection in the liquid steel pool and at the mould-water interface,

b) conduction through the solid steel shell, mould wall, and mould-strand air gap, and

c) radiation through the mould-strand air gap [3-7].
Figure 1.1: The continuous casting process involves the continuous cooling of molten steel by conduction, convection, and radiation through three cooling zones, for the production of semi-finished products. [Reference 8, Figure 1]

After a stable shell thickness has solidified, the dummy bar is withdrawn at the casting speed, and the issues of mould lubrication, mould distortion, and thermal contraction become important as solidification continues. The semi-solid structure which is extracted is called a *strand*. For higher productivity, a casting machine will typically have numerous strands that receive molten steel from the same tundish.
Mould oscillation and mould lubrication (with oil or mould flux) are necessary to prevent the solidifying shell from sticking to the mould walls as it progressively thickens down the length of the mould. Lubrication and oscillation are complicated by the dynamic thermal distortion of the mould walls at casting temperatures, coupled with the solidification shrinkage of the steel shell. The mould distortion and steel shrinkage act concurrently to form a gap between the strand and the mould. This gap is the largest resistance to heat flow in the mould and is the main influencing factor on the rate of heat extraction in the primary cooling zone [4,7,9]. The reduction of the mould-strand gap is achieved through the use of a tapered mould, which provides adequate surface contact with the skin of the solidifying strand in its dynamically distorted form. Conventional mould designs can range from multiple- or straight- tapered configurations to variable-tapered configurations (Figure 1.2).

Figure 1.2: Conventional mould taper configurations can be designed as straight-tapered, multi-tapered or variable (shown schematically from left to right).

The continuous extraction of the strand from the mould is applied through the pinch rolls, while a constant liquid level is maintained in the mould through a continuous liquid pour from ladle to the tundish and mould. The rate of withdrawal of the strand is called the casting speed, and it determines the productivity of the process. The strand exits the mould and enters the bending unit to apply a curvature to the strand and reduce the head height of the machine. The secondary cooling zone, or spray chamber, follows for further solidification. The unbending rolls are applied after the secondary cooling...
zone in order to bring the strand to a horizontal orientation, where cooling through radiation is applied until full solidification is reached at the metallurgical length. Torch cutting is used to reduce the strand into segments called semis, which can ultimately have a variety of shapes and sizes. Billets, in particular, are square or rectangular in shape, and are typically less than 200 x 200 mm$^2$. These billets can then be directly hot-charged or cold-charged into the reheat furnace and rolled into long products, such as rods, bars, rounds and structural shapes. The cast quality is very important to the rolled quality, as defects can be rolled into long seams or linear defects that can result in the production of a high percentage of scrap. Particularly, there is a high potential for defects to form due to thermal and mechanical loads applied to the strand, which is semi-solid in character, while withdrawing it at the casting speed. Most of the cast defects, such as laps and bleeds, non-uniform oscillation marks, rhomboidity, transverse depressions, and subsurface cracks, initiate within the meniscus region [1,10,11]. In this region, there is a complex state of interacting events, including: mould distortion, the start of solidification, infiltration of the lubricant into the mould-strand gap, and two dimensional heat flow. The defects that initiate within this dynamic region have the potential to intensify as the strand travels through the machine, due to the various interacting thermal and mechanical loads that are applied through to the end of the process. For this reason, the design and maintenance of the machine, combined with a superior knowledge and control of the process is necessary to achieve a high quality semi-finished product. Personnel at the University of British Columbia’s Centre for Metallurgical Process Engineering (UBC-CMPE) have performed numerous industrial trials in the pursuit of understanding the complex state of events in the mould. Comprehensive modelling and analysis has been done on the mould and strand to understand how they interact during the conventional casting of steel billets. Key findings in the areas of mould design, mould distortion, mould oscillation, water velocity, metal level control and oil flow distribution have led to enormous improvements in continuously cast billet quality [3,4,9,12-19]. While numerous industrial trials, such as these, have been conducted to study the effects of various design and operating parameters on the response of the mould and billet quality at conventional casting speeds, few industrial
instrumented mould trials have been conducted at casting speeds exceeding 3.0 m/min. The purpose of the current project is to provide a natural extension of the established work in conventional casting, into research of the effects of high speed casting. This involved an instrumented mould study at casting speeds ranging from 3.0 to 4.5 m/min, and the mathematical modelling of the strand and mould with the aim of studying the response of the mould, under various operating conditions, at high casting speeds. A comprehensive evaluation of the heat transfer behaviour of the mould was undertaken to illustrate these effects, as well as determine billet solidification characteristics and optimum mould taper designs at high casting speeds. The present work also strives to provide a comprehensive assessment of the effect of casting speed over a global range of casting speeds, encompassing both conventional and high speed casting. This was done through an examination of a series of 8 plant trials conducted by UBC over the last 10 years.

This project has led to a characterization of the mould response at the midfaces and corners at high casting speeds, emphasized the importance of particular mould design principles on the heat transfer in the mould, and proposed a new theory for mould-strand interaction in a steeply tapered mould. The conclusion of this project brings to a close almost three decades of work in industrial instrumented mould trials and modelling analysis by the Centre for Metallurgical Process Engineering at the University of British Columbia for conventional and high speed processes.
CHAPTER 2: LITERATURE REVIEW

This chapter provides the necessary background for understanding the mould response, billet solidification characteristics, and mould-strand interaction, that result from changes in the operating parameters during steel billet casting. Most of the available literature is based on conventional casting speeds (< 2.0 m/min) and hence provides a foundation for the study of the effects of high speed continuous casting.

2.1 Heat Transfer in the Mould

The mould heat transfer (or heat flux) is a primary influencing factor in the quality of the cast structure. The control of heat transfer in the primary cooling zone is essential in preventing crack formation, promoting uniform shell development, and controlling the cast structure [6,7,20]. Mould heat transfer occurs by heat flow from the billet, conduction and radiation through the mould-strand gap, conduction through the mould wall, and heat extraction by the cooling water [3-7]. The rate of heat transfer through the mould is limited by conduction through the gap, which represents 80 to 90% of the total resistance [9,18]. The gap is formed through the simultaneous contraction of the solidifying shell and the thermal distortion of the mould walls, and it is inhibited by the ferrostatic pressure of the liquid core. Furthermore, the size of the gap is constantly changing in response to the local dynamic conditions in the mould. Therefore, any variables which affect the dynamics and properties of the air gap will have a profound impact on the heat transfer response of the mould. Generally, the size of the gap increases with distance down the mould due to solidification shrinkage and mould distortion. Therefore, the hot face¹ heat transfer profile is greatest near the meniscus and decreases in magnitude down the mould [2,4,5,7,9,18,21-23]. The heat transfer profile is also not uniform across the transverse face of the mould due to the physical differences in the corner geometry, as well as three dimensional heat flow at the corners [18].

¹The hot face is the side of the mould in contact with the steel.
2.1.1 Mould Midfaces

At the midface of the mould, the maximum heat transfer occurs about 25 to 40 mm below the meniscus for carbon grades up to 0.8 wt % C [4,5,9,18,22,23]. Typical heat transfer profiles for high and low carbon grade steels can be seen in Figure 2.1, for a casting speed of 1.3 m/min. The effect of carbon content and casting speed will be discussed in Section 2.4 (p.13).

![Figure 2.1: Typical mould midface heat transfer profiles for 0.05 and 0.70 wt % carbon grades cast at 1.3 m/min [Adapted from Reference 22, Figures 13 & 14].](image)

Experimental work on conventional continuous casters has demonstrated that the peak midface mould heat transfer is typically about twice the magnitude of the average mould heat transfer at the midface of the mould wall. Also, the local heat transfer magnitude at the exit of the mould is approximately half of the average value [5,22].
2.1.2 Mould Corners

The mould corners are inherently more rigid than the midfaces of the mould. The corner region in the mould is cooler than midface due to its rigidity and the three dimensional heat flow, which generates more shrinkage and a larger gap between the strand and the mould [4,13,21,24]. Consequently, the heat transfer is lower at the corners of the mould [18,22].

Blazek [22] conducted laboratory-scale work with mock-mould which had horizontal and vertical water channels machined in the walls. These water channels were used, instead of thermocouples, to determine the rate of heat extraction at a given location in the mould. The experiments revealed that the average corner heat transfer was lower than the average midface heat transfer by 20 to 30% at a casting speed of 1.3 m/min. This translates into an average mould corner heat transfer of 1190 to 1360 kW/m$^2$ for the low carbon grades, and 1260 to 1440 kW/m$^2$ for the high carbon grades. Unfortunately, no heat transfer profiles were given in the literature.

2.1.3 Alternate Expressions of the Mould Heat Transfer

The response of the mould, as evidenced by the previous discussion, can be described by a heat transfer profile. The response of the caster can also be expressed in terms of the average mould wall heat transfer, peak heat flux, average water heat transfer, and specific mould heat energy. These will be described below, but discussed in more detail later in terms of how they are affected by various casting variables.

2.1.3.1 Average Mould Wall Heat Transfer

The average mould heat transfer is simply the average of the heat transfer profile. However, it should be noted that the average mould wall heat transfer is a less sensitive evaluation of events in the mould, as similar value for the average heat transfer of the mould can be a result of different heat transfer profiles. For example, an increase in the
mould metal level heat transfer coupled with a decrease in the heat transfer at the bottom of the mould can balance and have little effect on the average value.

### 2.1.3.2 Peak Mould Heat Flux

In the assessment of the average heat transfer profile of the mould, the peak heat flux can be isolated and used to assess the local response at the meniscus region. This is the region where heat transfer, mould distortion, mechanical interaction, and solidification events are the most complicated and dynamic. Therefore, an increased uniformity of heat transfer in the meniscus region will improve the surface quality of the billet, particularly reducing the severity of those defects that form at the meniscus [1,6,10,15,25-27].

### 2.1.3.3 Average Water Mould Heat Transfer

As an alternate to the average mould heat transfer, the average mould water heat transfer can be used to describe the amount of heat energy extracted by the cooling water. This is a useful characterization of the system as it is a simple calculation that involves only the measurement of the in- and out- bulk water temperatures. An average mould wall heat transfer rate, on the other hand, can only be obtained through an instrumentation of the mould, or in Singh and Blazek's experiments [5,22] through a mock mould. The mould water heat transfer calculation is also beneficial for the comparison of different companies, as it inherently avoids the necessity of addressing any variations in the experimental and computational methods which may have occurred across the various analyses. As well, due to the ease of obtaining mould water heat fluxes, they can be used as an assessment against the calculated average mould heat transfer to determine the validity of the instrumented trial and mathematical models. Previous work at UBC has shown that the model-predicted average mould wall heat transfer can be up to 20% lower than the calculated average water heat transfer obtained from instrumented trial data [28].
2.1.3.4 Specific Mould Energy

Similar to the mould water heat transfer, the specific mould energy can be used to relate the responses of various companies. It is expressed as the energy removed per kilogram of steel flowing through the mould (kJ/kg). The specific energy calculation normalizes the average mould wall heat transfer over casting speed, section size, mould taper, and mould length. Hence the general responses of the casters at various companies can be assessed on an equal basis relative to one another.

2.2 Mould Thermal Response

The thermal profile of the mould is determined by the heat transfer characteristics in the mould. The thermal profile of the mould wall, in effect, results as a combination of the heat transfer from the strand through the mould-strand gap to the mould wall, conduction through the mould wall, and the heat extracted by the cooling water. The thermal response of the mould is highly influenced by the heat transfer from the strand, lubricant type and properties, mould taper, mould wall thickness, and mould material. The thermal profile of the mould follows the general behaviour of the heat transfer profile, as seen in Figure 2.2. The mould wall temperature reaches a peak around 30 to 40 mm below the metal level, due to 2-D conduction in the mould wall. Below this region, the temperatures decrease due to the solidification of the shell, which acts to increase both the mould-strand gap (through solidification shrinkage) and the total conductive resistance of the heat flow circuit. As well, the stronger and thicker shell is more able to withstand the ferrostatic pressure which would ordinarily aid in reducing the width of the mould-strand gap [4,15,23]. Furthermore, the thermal standard deviations of the temperature profiles are greatest at the meniscus (up to 21 °C) and are relatively small for the remainder of the mould (1.5 °C) [23].

Typically, heat transfer averages and profiles are used to describe the behaviour of the mould during casting. However, it is easiest to measure the thermal response of the mould in an instrumented mould trial and use Finite Difference Methods (FDM) to solve the inverse heat conduction model, and thus back-calculate the heat transfer. The
experimental method, apparatus and FDM analytical techniques for such a project in steel billet casting has been established and validated by Brimacombe and co-workers through industrial trials [3,4,9,14,15,18,23,28-34] and Finite Element Modelling (FEM) [33].

Figure 2.2: The thermal profile and standard deviation profile is shown at the midface of the mould wall. This 0.33 wt %C titanium-protected boron grade, was cast with mould flux lubrication at 1.3 m/min and a tundish temperature of 1553 °C [Reference 23, Figure 7].
2.3 Mould Distortion

Mould distortion results from the thermal expansion and mechanical bending of the mould walls during casting. Mould distortion is a dynamic event, which may result in structural damage to the mould, through the permanent distortion of the mould walls. Thermal stresses are generated as a result of the dynamic distortion of the mould wall. Permanent distortion of the mould wall will occur if these stresses exceed the local hot yield stress of the mould material at any point in the distorted region [13].

Through a mathematical elastic and plastic distortion analysis and measured industrial data, Samarasekera et al. [13] have found that the largest contributor to mould distortion is the thermal expansion of the mould wall within a region from the top of the mould to about 90 mm below the meniscus. The maximum dynamic distortion at the midface of a conventional billet mould is typically between 0.1 to 0.3 mm and is located at an axial position which corresponds to the peak midface hot face temperature. Distortion also occurs at the mould corners, and is larger than at the midfaces due to the triaxial state of stress [4,13]. Furthermore, this distortion is not necessarily symmetrical at each corner junction [15]. The region of distortion is described in terms of a dynamic negative taper (or reverse taper), which forms above the peak distortion, and a dynamic positive taper, which forms below (Figure 2.3) [4,9,13,15]. Typical average values for these dynamic tapers are -1 to -2 %/m and +0.4 %/m, respectively. These tapers are not static, but change dynamically with the mould temperature fluctuations that can result from intermittent nucleate boiling in the water channel or metal level turbulence [15]. The local distortion of the mould at the meniscus is typically quantified only in terms of the negative dynamic taper, as it can be used to assess the degree of mould-strand interaction. If the negative dynamic taper at the meniscus is too steep, due to inadequate initial mould taper, a high degree of mould-strand interaction can occur during the downstroke of the mould in its oscillation cycle, due to the large contact surface area generated by the negatively distorted shape. As the positive dynamically distorted taper is typically very shallow (−0.4 %/m), the upstroke cycle is not a primary contributor to the mould-strand interaction mechanism. The interaction of the strand and mould during the downstroke of
the cycle results in very high rates of heat transfer, which is detrimental to the surface quality of the billet [4].

![Diagram of mould interaction and heat transfer](image)

Figure 2.3: A schematic of the dynamically distorted mould is shown through the thickness of the mould wall to illustrate the dynamic tapers. A steep negative taper forms above the peak distortion, followed by a positive taper, allowing mould-strand interaction to occur during the downstroke of mould oscillation. Not to scale.

### 2.4 The Effect of Casting Variables on Mould Heat Transfer and Billet Solidification

In the pursuit of improving both quality and productivity, control must be exerted over the heat transfer and billet solidification characteristics throughout the casting process. The heat transfer characteristics in the mould are important for assessing mould life and determining optimum design and operation conditions. The variables and operating parameters that will be discussed here include: casting speed, metal level control, steel composition, superheat, water flow rate, tundish shroud type, mould electromagnetic stirring, and mould lubrication characteristics. These process parameters can have a profound impact on the mould heat transfer and are discussed below, in terms of their effect on the mould response and billet shell development.

#### 2.4.1 Casting Speed

The effect of casting speed can be illustrated using mould heat transfer profile, mould heat transfer peaks, average mould wall heat transfer, and specific mould energy.
Previous studies have found that increasing casting speeds cause the average mould heat transfer to increase at the midfaces [5,15,22,23,28,33,35], as well as at the mould corners [22]. The increased heat transfer at higher casting speed, is due to three reasons. First, the shorter residence time\(^{\text{ii}}\) of the steel, at high casting speeds, results in thinner shells that deform easily under the ferrostatic pressure, ultimately reducing the mould-strand gap. Second, these shorter residence times result in hotter billet surface temperatures, which increases the driving force (or thermal gradient) for heat flow. Third, there is less thermal contraction for the hotter strand shell temperatures, which improves the mould-strand contact by not contributing to the size of the gap [36]. Studies have found that increased casting speeds led to larger local differences in the heat transfer near the meniscus than at the bottom of the mould (Figure 2.4) [5,22,37,38]. However, an increase in casting speed was found to have the strongest effect at the bottom of the mould, in increasing the temperature profile [9]. For casting speeds up to 1.3 m/min, the peak heat transfer was found to be around 3100 to 3500 kW/m\(^2\) for the low carbon grades [15,22,23,28]. For the high carbon grades (\(\geq 0.6\) wt %C) cast at these speeds, the heat transfer peaks ranged from 3450 to 4400 kW/m\(^2\) [5,18,22,28]. At casting speeds greater than 3.0 m/min, there is little published data for the heat transfer profiles. Only one study [39] published flux peaks (for only 3 heats of data), and found that the low carbon grades were between 2500 to 3350 kW/m\(^2\). Schrewe [38] published heat transfer curves up to 3.0 m/min, showing heat flux peaks of 5600 kW/m\(^2\), but did not note the carbon grade and the experimental casting conditions.

\(^{\text{ii}}\) Residence time (or dwell time) denotes the amount of time the steel spends in contact with the mould.
Figure 2.4: Higher casting speeds lead to higher heat transfer rates, particularly in the region below the meniscus, as seen in this 0.4 wt %C grade steel [Reference 22, Figure 20].

The effect of casting speed on the average mould heat transfer is also strongly influenced by the carbon content. It was found that at higher carbon contents, a change in casting speed would effect a greater change on the average mould heat transfer (Figure 2.5) [22]. For conventional casting speeds (0.5 to 2 m/min) the average mould wall heat transfer rates were found to be between 500 to 1500 kW/m² for the low carbon grades, and 1300 to 1900 kW/m² for the high carbon grades [3,21,22,25,28,33,34,40-43]. Some studies conducted by UBC [15,18,23] tended to report heat transfer profiles as opposed to average heat transfer rates. Relatively few studies have been conducted to study the effects of high speed casting (≥ 3.0 m/min) on the mould heat transfer. These studies reported mould wall heat transfer rates of 1600 to 1900 kW/m² for the low carbon grades, and 2100 to 2600 kW/m² for the high carbon grades [24,37,43,44]. Only one study [39] used an instrumented mould to obtain a detailed thermal profile of the response of the mould and only 3 curves were published.
Figure 2.5: Increasing casting speeds lead to an increase in the average mould heat transfer, particularly at higher carbon contents [Adapted from Reference 22, Figure 22].

Although the total average heat transfer increases with casting speed, it cannot accommodate for the extra volume of steel flowing through the mould. Therefore, the specific mould heat transfer decreases with casting speed, as seen in Figure 2.6 [35,38].
Figure 2.6: Increasing the casting speed tends to decrease the specific mould heat energy (kJ/kg) in both high and low carbon grades cast with oil lubrication [Reference 35, Figure 5a].

As well, even though the heat transfer is higher at increased casting speeds, the shell thickness at the exit of the mould is reduced, due to the decreased residence time of the strand in the mould. These thin shells are weak, and poor control of the process (such as in the metal level control, oscillation, maintenance, etc.) can result in a higher incidence of non-uniform oscillation marks, laps and bleeds, transverse depressions, and off-squareness [15-17,24,26,27,29,45-49]. Therefore, a viable shell thickness is required at the entry into the spray chamber to ensure that the unsupported strand can withstand the ferrostatic pressure of the liquid core and the thermo-mechanical stresses [36]. However, a study involving peak casting speeds of up to 4.8 m/min found that the billet quality was similar to that of conventionally cast billets [39].
2.4.2 Metal Level Control

Excessive metal level fluctuation results from a rough tundish stream that entrains gas as it enters the mould. Rough tundish streams are the result of adverse fluid flow in the tundish due to the transmission of the turbulent ladle stream into the tundish. They can also be caused by poor tundish nozzle design and installation. The design parameters that are important in nozzle design include: stand-off height, nozzle diameter, internal surface roughness, inlet shape, and stream velocity. Particularly, the roughness of the stream will increase with stand-off height, as instabilities grow with height above the metal level. As well, the stream will lose cohesion as the size of the surface disturbances approach the size of the nozzle radius. Low tundish levels can also cause poor tundish flows due to vortexing of the steel around the tundish nozzle during draining. As well, the internal design of the mould, such as pour boxes, dams, and weirs, have a strong influence on the fluid flow in the tundish, and hence the stability of the surface of the tundish stream and the amount of gas entrained. As these gas bubbles, entrained from the rough surface of the tundish stream, erupt from the surface of the liquid pool in the mould, surface turbulence and waves are generated. As more gas is entrained during casting, the metal level turbulence continues to increase to the point where oil lubrication can become disrupted [16,17,50]. This can lead to defects such as non-uniform oscillation marks, laps and bleeds, transverse depressions and rhomboidity, which have been discussed in detail elsewhere [15-17,24,26,27,29,45-50]. These surface defects can lead to non-uniform heat transfer and thermal fluctuations in the mould at the meniscus of up to 25 °C, which would have a serious impact on the development of the billet shell [40,47]. Furthermore, the fluctuations can be so distinct that meniscus-level thermocouples have been used to monitor these deviations in the mould for the purpose of the on-line detection of defects such as laps and bleeds, and transverse depressions [45,47,51]. Previously, metal level fluctuations of up to 6 mm were predicted to shift the location of the peak heat transfer, but have no influence on the average mould wall heat transfer [34].
2.4.3 Steel Composition

The composition and alloying of steels is necessary to modify the material properties of the billet to ensure the ease of post-processing techniques (machining, rolling), adequate service life, and proper mechanical properties of the final product, according to the specifications required for the final application.

2.4.3.1 Carbon Content

An increase in carbon content affects the mould wall heat transfer profile in much the same way as an increase in casting speed, but with a particularly strong increase in heat transfer peaks at the meniscus region [5,9,22]. As seen in Figure 2.1, the general behaviour of the mould wall heat transfer profile remains the same with an increase in carbon content, however, the heat flux peaks are much greater. As well, for carbon contents greater than about 0.8 %, the position of the heat flux peak tends to shift from about 25 to 40 mm below the meniscus to 50 mm below the meniscus. This is due to the thicker and stronger shells of the higher carbon grades, which resist the ferrostatic pressure of the liquid core [5,22]. For a casting speed of 1.3 m/min, the peak midface mould wall heat fluxes for the low carbon grades are between 3100 to 3500 kW/m$^2$ and the high carbon grades have peaks around 3450 to 4400 kW/m$^2$ [5,15,17,18,22,28,34].

The effect of carbon content on the average midface mould wall heat transfer can be seen in Figure 2.7. For conventional casting speeds with mould oil lubrication, the lowest average midface mould heat transfer rate (1260 kW/m$^2$) is found in the 0.1 wt %C grade, due to the non-uniform shrinkage associated with the thermal contraction and phase change. For low (< 0.05 wt %) and high (0.7 to 0.8 wt %C) carbon grades the mould heat transfer rates are 1700 and 1800 kW/m$^2$, respectively. The increase in heat transfer around the 0.1 wt %C grade is due to the poor mechanical properties of these steels at high temperature, which allows the ferrostatic pressure to push the shell against the mould wall [5,22]. At the corners of the mould, the effect of carbon on the heat transfer is significantly different. For low carbon grades (≤ 0.05 wt %C) cast at 1.3
m/min, the mould corner heat transfer ranges from 1340 to 1500 kW/m². For carbon grades between 0.7 and 0.9 wt %C, the mould corner heat transfer is within 1500 to 1650 kW/m². The middle carbon grades (0.18 to 0.4 wt %C) have a large range of heat transfer rates (1260 to 1650 kW/m²), however no explanation was given in the literature for the behaviour at the mould corners for these grades [22]. It is likely that this behaviour is attributed to the solidification characteristics of the shell and its interaction with the distortion at the mould corners. The rate of solidification, and the appearance of the shell depends on the carbon content, and hence the phase diagram. The higher carbon grades tend to have thinner shells due to the larger freezing range\(^{iii}\). These shells are weaker than their low carbon counterparts, which increases the occurrence of surface defects, particularly those which form at the meniscus [47].

![Graph](image)

**Figure 2.7:** The effect of carbon content on the average mould heat transfer rate is illustrated at the mould midface and corner. The local minimum observed at 0.1 wt %C is related to the high solidification shrinkage of this composition. No explanation was given in the literature for the peak shown at the mould corner in the middle carbon range [Adapted from Reference 22, Figures 3, 5 & 6].

\(^{iii}\) The freezing range of a particular grade is the difference between the solidus and liquidus temperatures on the phase diagram.
2.4.3.2 Boron Addition

Boron (B) can be added in small quantities to unalloyed or low-alloy steels to improve the as-cast hardenability, when coupled with a boron protection method to prevent the boron from being preferentially scavenged by other elements. The hardening effect occurs only when boron is dissolved in the matrix and is at a maximum for boron contents from 10 to 40 ppm, hence protective elements (Zr, Nb, Ti, V or Al) must be added. Nitrogen has a high affinity for boron, through the formation of boron nitrides, hence preventing the dissolution of boron in the matrix and encouraging a softening mechanism instead [52-54]. This is beneficial for the high-reduction deformation processes performed subsequent to the casting operation [55-57]. In operations where billets are rolled into wire rod and subsequently drawn, for example, small amounts of boron are added (50 to 100 ppm) for the purpose of forming these boron nitrides to improve the toughness of the steel [58]. Unfortunately, there is no literature concerning the high-temperature properties of a steel shell which contains either boron nitride or titanium nitride (TiN). However, it is known that TiN forms as a stable solid phase at steelmaking temperatures, possibly strengthening the shell due to its dispersion in the solidifying shell [32]. It is possible that a steel shell containing BN may have the same effect on the high-temperature properties.

For mould flux lubricated heats, the medium titanium-protected boron grades were found to have similar thermal peaks as peritectic grades, but lower temperatures down the rest of the mould [23]. Therefore, the magnitude of the average mould wall heat transfer for plain carbon steels (< 0.17 wt %C) was found to be higher than titanium protected medium-carbon boron containing steels with mould flux lubrication [29]. However, the mould heat transfer between oil lubricated or flux lubricated boron grades were found to be very different: with mould flux lubrication, there was a significant reduction in the peak heat transfer from 5000 to 2300 kW/m² [29,50]. There are few heat transfer curves in the literature for boron grades cast with oil lubrication. The available information, however, indicated that a medium carbon, titanium-protected boron grade has a similar [33] or higher [23] heat transfer magnitude than a plain high carbon grade
throughout the length of an oil lubricated mould (Figure 2.8). There has been no characterization in the literature of the magnitude of the heat transfer in oil lubricated, unprotected boron grades compared to plain carbon grades. However, heat transfer profiles have been published for titanium-protected boron steels in both oil and mould flux lubricated moulds [23,29,32,33].

Figure 2.8: In an oil lubricated mould, a 0.32 wt %C steel with a 0.003 wt %B (+Ti) addition had a similar or higher magnitude of heat transfer over the length of the mould [Data obtained from References 23 and 33].
2.4.4 Superheat

Previous laboratory-scale experiments performed by Singh and Blazek [5,22] have shown that superheats of up to 55 °C have no effect on the mould heat transfer and temperature at casting speeds between 0.8 to 1.3 m/min. In another study, which had superheats up to 100°C, the heat transfer was found to increase the specific mould energy by 15 kJ/kg, however, the specific enthalpy (heat content) of the steel was also increased at these temperatures by about 80 kJ/kg [35]. Therefore, as the increased enthalpy could not be fully extracted by the smaller increase in the mould specific energy, the strand temperature increased, delaying the onset of solidification at the meniscus. This increased billet surface temperature results in thinner shells with lower tensile strengths, and a greater probability for the formation of surface defects or even rupture [22,35,78]. Blazek [22] subsequently explained that the effect of superheat on the thickness of the billet shell at the midface or corners was dependent on the fluid flow pattern in the mould, and hence the mould design and metal delivery practice were important. The practices of open stream pouring or submerged nozzle entry, however, were found to have no effect on the mould heat transfer rate [22].

2.4.5 Cooling Water Quality and Flow Rate

Heat transfer to the cooling water, from the surface of the mould at the cold face, occurs through forced convection. Pressurized water flows concurrent to the casting direction, through the annular water channel formed by the outside of the mould and the inside of the steel cooling jacket. Uniform and sufficient water flow rates in the cooling channel are necessary to prevent boiling of the water near the meniscus at the mould-water interface. Previous work by Samarasekera et.al. [4,59] has determined that a minimum water velocity of 10 to 12 m/s is sufficient to entirely suppress boiling in the water channel. Decreases in the water velocity below these values, have been found to

---

iv The cold face is the side of the mould in contact with the cooling water
uniformly increase the magnitude of temperature profile of the mould wall, due to the reduced heat transfer coefficient at the cold face, but have no effect on the temperature gradients [22,23,35,59]. Therefore, at low velocities, the cold face of the mould can reach temperatures that are sufficient to cause boiling in the water channel. Typically, continuous nucleate boiling will occur if the cold face of the mould reaches temperatures in excess of 160 °C. This boiling will locally increase the rate of heat extraction due to convective agitation, which increases the heat transfer coefficient. The nucleate boiling mechanism can break down, however, if the water velocity is too low to promote the agitation of the vapour bubbles at the cold face of the mould. In this situation, a vapour barrier forms at the mould face which decreases the heat transfer coefficient, inhibits heat extraction, and allows the mould to heat up and distort [22]. The discontinuity of the vapour barrier leads to intermittent boiling, which disrupts the heat extraction from the mould and results in a boiling hysteresis. This thermal cycling causes variations in mould wall temperatures and non-uniform mould distortion. In turn, these lead to the formation of off-squareness and off-corner internal cracks [2,4,14].

The nucleate boiling phenomenon is typically limited to operations where the water velocities fall below 11 m/s, as at higher velocities, comparable changes in water flow rates have less of an impact on mould wall temperature [9,22]. In conventional casting, changes in mould water velocity, in the absence of boiling, do not elicit any change in the heat flow in the mould or at the cooling water interface. The variation in the mould water velocity simply varies the thermal resistance of the mould-water interface in the heat transfer circuit. As noted previously, the mould-strand gap represents the greatest resistance to the heat flow in the system. The resistance generated by all other factors in the heat flow circuit – including mould wall thickness, mould thermal conductivity, cooling water velocity, inlet mould water temperature, cooling water flow direction and water quality – is less than 20% of the resistance of the mould-strand gap. Therefore, changes in any of these parameters have a relatively negligible impact on the heat transfer in the system, but can influence the mould wall temperature and hence distortion [9].
LITERATURE REVIEW

Water quality and baffle tube design are also important aspects of heat transfer in the cooling channel. Severe scale deposition in the baffle tube, due to poor water quality, can result in a high thermal interface resistance, which becomes a serious impediment to the extraction of heat from the strand. This can result in high mould temperatures and permanent mould wall distortion. Typically, the rate and amount of deposition is greatest at the meniscus, where the mould temperatures are the highest [60]. The baffle tube design, in terms of geometry, tolerances and alignment, is also essential in ensuring uniform and adequate water flow to all surfaces of the mould. A square baffle tube design combined with a mould with rounded corners, for example, results in high water flow rates at the corners, and insufficient flow at the midfaces. Misalignment of the mould and baffle tubes can result in a similar problem, allowing high water velocities at some faces of the mould, and poor cooling at others. Ideally, the difference in water velocity at any given location in the water channel should not exceed 1.0 m/s [14,23,61].

2.4.6 Tundish Shroud Type

There are two methods of transferring liquid steel from the tundish to the mould in a small-section billet mould: open stream and shrouded stream pouring. In open-stream pouring, the molten metal flows through the air to the mould and is unprotected against air entrainment, allowing harmful inclusions to form in the semi-finished product due to the reactions of the liquid metal with air. Liquid metal shrouding in billet machines can be used to allay this problem by protecting the surface of the tundish stream with an inert gas shroud. Two types of common tundish shrouds, Bellows and Pollard, are illustrated schematically in Figure 2.9. The Bellows shroud is a flexible enclosure around the tundish stream that uses pressurized gas injection to protect the stream. The Pollard shroud is a partial enclosure where the gas is injected at the midpoint of the stream at low velocity and is allowed to exit through openings at both the metering nozzle and the mould. In both cases, either argon or nitrogen gas can be used for the stream protection [2]. There appears to be no published data to determine the effect of shroud type on the mould heat transfer response in either conventional or high speed casting.
2.4.7 The Effect of Mould Electromagnetic Stirring and Braking

Mould electromagnetic stirring (M-EMS) is used to generate a moving magnetic field in the mould to promote fluid flow in the liquid core of the strand. M-EMS can consist of a single coil or dual coil configuration (Figure 2.10). The dual coil M-EMS configuration allows independent control of the meniscus stirrer and the main coil. In this configuration, the upper (metal level) coil, or AC Stirring Modifier (AC-SM), has reversible rotational capability. The condition in which the main coil works in conjunction with the AC-SM in reverse rotational mode, is called mould electromagnetic braking (M-EMBR). With this braking capacity, a dynamic equilibrium can be achieved at the meniscus, since the opposing flow momentum of the top and bottom coils causes the meniscus to approach zero-flow conditions [62]. Electromagnetic stirring can also be applied to the strand (S-EMS), however, the effects will not be discussed in this work.
There have been very few studies on the effect of M-EMS or M-EMBR on the mould thermal response or mould heat transfer in the continuous casting of billets or slabs [63-65]. Only one study [63] was found in the literature which measured the effect of M-EMS in billet moulds (Figure 2.11). The study found that the application of M-EMS increased the water heat transfer by 5 to 8% when casting 160 x 160 mm$^2$ billets, with mould flux lubrication, at casting speeds of 1.3 to 1.8 m/min. There have been more studies conducted on slab casters [64-65]. One laboratory model [64] indicated that M-EMS allows more heat extraction through the mould due to better heat transfer characteristics at the solidification front, but did not provide any quantification of the results. Another study [65] determined that the application of M-EMBR increases both the central temperature of the slab and the thermal gradient at the meniscus level. Therefore, they determined that the fluid flow had a strong influence on the heat transfer characteristics in the mould. This infers that the stirring practice (M-EMS, M-EMBR and stirring intensity) would have a significant impact on the average mould heat transfer.

The application of EMS in the mould alleviates the effects of high speed casting. Particularly, M-EMS improves the macrostructure of the as-cast billets, through uniform shell solidification, larger equiaxed zones, reduced columnar zones, and reduced centreline segregation and bridging. The benefits of EMS on the quality of continuously cast steel billets have been studied extensively elsewhere [62, 66-69].
Figure 2.10: A schematic of the dual coil mould electromagnetic stirring (M-EMS) and braking system (M-EMBR). The meniscus-level coil, or AC stirring modifier (AC-SM) has reversible rotational capability, allowing electromagnetic braking to be applied when used in combination with the main coil (M-EMS) [Reference 62, Figure 1].
Figure 2.11: The application of M-EMS in a flux lubricated billet mould increases the heat extracted by the water by up to 8% at any given casting speed. The oil lubricated heats are shown for reference [Adapted from Reference 63, Figure 3].

2.4.8 The Effect of Mould Lubricant Type

Studies have shown that mould flux lubrication can result in reduced mould heat transfer by 5 to 18% for grades above 0.1 wt %C. In non-peritectic low carbon grades, one study [23] found that oil lubrication had heat transfer peaks of over double the magnitude over flux lubricated heats. However, in oil casting, the metal level region can experience intermittent heat flow due to the formation of air gaps between the strand and mould where the lubricant did not penetrate. Mould fluxes, on the other hand, can fill this region and, depending on the properties, can reduce the interface resistance to improve the magnitude and uniformity of the local heat transfer characteristics. However, lower in the mould, where the contact between the strand and mould is ordinarily good, the mould flux acts as an insulating layer in the mould. The infiltration
of the mould flux into the gap in the lower mould increases the interface resistance above what would be observed with an air gap, thus reducing the heat transfer [1,22,23,40,50,63]. Only one study [44] noted the opposite trend, that flux lubrication caused higher heat removal in the mould. This study also noted that the difference in the average heat transfer between oil and flux lubrication decreased as the casting speed was increased.

2.4.8.1 Oil Lubrication

The distribution of lubricating oil around the periphery of the mould typically occurs through slots machined at the top of the mould, whereby the oil weeps down the four internal faces of the mould. The lubricating oil is drawn into the mould-strand gap during mould oscillation and wets the hot face of the mould. Typically, oil flow rates of 20 to 30 ml/min are necessary to ensure adequate, but not excessive, lubrication during both the upstroke and downstroke of mould oscillation [26,45]. The disruption of the oil flow into the mould-strand gap can result from excessive metal level fluctuations or improper oil delivery. The failure of adequate and uniform oil lubrication on the mould walls can lead to sticking and excessive rates of heat transfer. This can then result in a variety of surface defects, such as non-uniform oscillation marks, laps and bleeds, transverse depressions, and off-squareness [15-17,24,26,27,29,45-49].

As lubricating oils typically have boiling ranges within 200 to 350 °C, the oil film in immediate contact with the molten steel will pyrolyse. This causes combustion gases (H₂, H₂O, and hydrocarbons) to be released into the mould-strand gap, which aid in increasing the thermal conductivity through the gap, thus improving the heat transfer [45]. The remaining oil will be drawn into the mould-strand gap during the negative stroke of mould oscillation. As the various oil lubricants – such as Canola, Steelskin, HEAR (high euricic acid rapeseed), or Blachford – have different compositions, flashpoints and viscosities, they can also have differing effects on the heat transfer response of the mould. The effects of various oil lubricants on the mould heat transfer has been studied extensively elsewhere [6,26,28].
2.4.8.2 Mould Flux Lubrication

Mould fluxes (also called mould powders) are typically added manually to the top of the mould without any regulatory distribution system. This allows the possibility of non-uniform distribution of the fluxes to occur around the periphery of the mould, which has been known to lead to non-uniform lubrication and heat transfer [23,70].

As with oil lubrication, the type of mould flux used can be important in influencing the mould heat transfer characteristics. The viscosity, recrystallization temperature, glass transition temperature, and melting rate are the most influential properties in mould flux lubrication and the subsequent heat transfer behaviour [1,2,20,40,50,71,72]. These, as well as the casting speed and oscillation characteristics, determine the consumption rate of the mould flux into the mould-strand gap. The use of a mould flux lubricant over oil is widely known to result in deeper, but more regular oscillation marks [1]. In particular, this is because mould fluxes are more viscous than oil and a slag rim can form at the meniscus if the proper temperature-dependent properties are not achieved. This rim, which remains attached to the mould wall, increases the depth of the oscillation marks and subsequently decreases the local heat transfer [40,49]. Comprehensive studies on the properties [1,10,71] and billet quality effects [1,23,27] of mould flux lubricants have been conducted elsewhere, and will not be covered in further detail here.
2.5 Solidification and Development of the Strand Shell

Solidification of the strand shell necessarily leads to solidification shrinkage, constraining the billet profile as it descends in the mould. The magnitude of the shrinkage is dependant on either or both of two phenomena: the $\delta$ to $\gamma$ phase transformation and the thermal gradient [18]. The theoretical volumetric solidification shrinkage due to the phase transformation can be up to 4.4 vol.%. However, due to non-equilibrium cooling kinetics, carbon interstitials, and other additions, the actual solidification shrinkage due to phase transformations may vary [73]. The other shrinkage component, the thermal contraction, simply consists of the temperature-dependent change in density of the steel as it cools during casting [18].

The thickness and uniformity of the solid shell has a direct correlation with the magnitude of the mould heat transfer, particularly at the meniscus [7,22,25]. From the previous discussion, variables which affect the thickness or uniformity of the shell thickness, and hence the size of the gap, can include: casting speed, metal level fluctuation, carbon content, superheat, mould oil or flux lubricant properties, and surface defects.

Billets cast at conventional speeds typically have shell thicknesses at the exit of the mould of 6 to 12 mm [19]. In high speed casting, shell thicknesses of 9 to 11 mm have been published for speeds up to 3.7 m/min [36]. The measurable thickness of the shell at the exit of the mould can be approximated using the parabolic growth law, $x = Kt^{1/2}$ [74], or some form thereof ($x = Kt^n + C$) [75]. These growth equations describe the solidification of the shell using a K-factor, and the residence time in the mould, assuming a constant thermal resistance at the interface between the mould wall and strand shell (ie. a constant mould-strand gap). The K-factor represents the heat diffusivity of steel and is not dependent on carbon grade, superheat or spray water, rather it varies with the shape and size of the strand. For billets, the K-factor can have values up to 30 mm/min$^{1/2}$, depending on the section size [74] and the casting speed [25]. The K-factor increases
with geometries that allow 2-D heat flow and decreases with increasing casting speed, hence smaller billets have higher K-factors, particularly when cast at low speeds. Sasaki et.al. [25] used K-factors of 18 to 19 mm/min$^{1/2}$ for a billet size of 132 x 132 mm$^2$, cast between 1.9 to 2.2 m/min; and Wolf et.al. [76] recommended K-factors up to 16 mm/min$^{1/2}$ for an 83 x 83 mm$^2$ section cast at 0.5 m/min. The parabolic solidification law has been found to be fairly accurate for billets cast at conventional speeds [77], but only for steady state conditions in the mould [78]. In a high speed casting application (2.4 to 3.7 m/min), the parabolic approximation was found to correlate well with experimental data only when a magnitude modification was used, $x = Kt^{1/2} - C$. However, insufficient information was published to determine the K and C values for this system [36].

2.6 Mould Design

Mould design is one of the primary variables affecting the heat extraction capability in the primary cooling zone, establishing the foundation for the quality of the billet. Poor mould design can result in the rejection of billets, as well as poor mould life [35]. Mould design is categorized in terms of mould material, and geometric characteristics such as taper, length, corner radius, wall thickness, plating thickness and material, tubular or plate construction, machine curvature, constraint, and section size [12,79]. Previous work has established the optimal parameters for the majority of these design considerations for conventional casting, as outlined by Samarasekera et.al. [12] and Hauri [79], therefore, high speed mould design will be discussed primarily in terms of mould taper and length in the present work.
2.6.1 Mould Taper

Mould tapers are designed on the basis of the billet shrinkage and mould distortion (Figure 2.12), a procedure which has been established by researchers at UBC [3,18]. The procedure involves a mould heat transfer model, mould distortion calculation, and a billet solidification model. A previous study has shown that a finite element model of the mould distortion is not necessary, as thermal expansion is the dominant factor in the distortion of the mould [4]. The definition of the mould taper is shown in Figure 2.13. Each straight-tapered portion of the mould — whether it is a single, multi, or variable tapered mould (Figure 1.2) — is calculated using Equation 1. $MT$ represents the mould taper in %/m, and the units of $W_1$, $W_2$, and $L$ are in metres, which represent the internal width dimensions at the top and bottom of the taper in question, and the length of the tapered portion, respectively [2,18].

$$MT = \left( \frac{W_1 - W_2}{W_1} \right) \left( \frac{100}{L} \right)$$  (1)

The design of the Convex (Concast) [80,81] and DANAM (Danieli) [82] moulds, on the other hand, are specifically aimed at improving heat transfer in the primary cooling zone for the purposes of increasing casting speed. These moulds have different taper specifications and cooling strategies and will not be discussed in the present work.
Figure 2.12: The mould taper design procedure involves a heat flow model of the mould, a mould distortion model, and a billet solidification model. As the dynamic distortion of the mould is primarily due to thermal expansion, the finite element modelling of the mould walls is not a necessary step [Reference 18, Figure 1].
Figure 2.13: Individual mould tapers for single, multiple or variable tapered moulds, can be defined as a mathematical relationship between the start and end width ($W_1$ and $W_2$) and the height ($L$).

Mould taper is very important in determining the heat extraction capability of the mould. Early studies have shown that the heat transfer can be improved by up to 20% through the use of a single tapered mould over an untapered mould [79]. In multiple and variable tapered moulds, the mould is tapered successively down its length in numerous sections. The steepest tapers are at the top of the mould, where the heat transfer, temperature and distortion are greatest, and the shallowest tapers are at the bottom of the mould, where the distortion is minor and the solidification shrinkage is more of an issue. An adequate taper design should account for mould distortion and shrinkage and allow adequate contact between the strand and mould in its dynamically distorted shape without causing excessive mould-strand interference. Due to the dependence of the mould taper on the solidification characteristics of the steel, a single mould taper design cannot be designed for various grades [18,61].
2.6.1.1 Initial Mould Taper

Insufficient initial mould taper will allow an excessively steep negative dynamic taper (or reverse taper) to form at the meniscus (Figure 2.3), resulting in significant mechanical interaction between the strand and mould during mould oscillation. This causes locally excessive rates of heat transfer at the meniscus and poor billet quality. Many studies [1,16,18,36,39,46-48] have recognized the need for the design of steep initial mould tapers, such that the degree of negative distortion is reduced. Typically initial tapers greater than 2%/m have been recommended to prevent surface defects from forming due to mould-strand interaction. Excessive meniscus tapers, on the other hand, have been cited for their deleterious effect on surface quality and low heat transfer at the meniscus due to binding of the strand and mould. The subsequent mould-strand interaction would result in buckling and a separation of the solidifying skin from the mould, which then results in locally reduced heat transfer at the meniscus [1,18,45]. No delineation of the amount of taper that is considered "excessive" has been found in the literature.

2.6.1.2 Final Mould Tapers

Below the meniscus region, the mould tapers are successively shallower, as there is less hot distortion of the mould walls below the meniscus, hence only the solidification shrinkage must be overcome. The lower part of the mould has less heat transfer and the lower tapers are required more for support and guidance of the strand. If the heat transfer at the bottom of the mould is too low due to inadequate taper, the shell can reheat and cracking will occur during subsequent cooling. This is a particular concern in mould flux casting due to the low heat transfer at the bottom of the mould compared to oil casting [63]. However, if the taper is too steep at the bottom of the mould, the billet can bind or jam in the mould, resulting in jerking as the pinch rolls attempt to extract the strand.
Mould taper design is highly dependent on casting speed, carbon content and resident mould length. As discussed earlier, the high and low carbon grades have widely differing freezing ranges, which result in differing amounts of solidification shrinkage, and thus different taper requirements. Casting speed influences the residence time in the mould, which impacts the amount of solidification, and hence the taper. Similarly, the resident mould length (the distance from the meniscus to the exit of the mould) directly impacts the residence time, solidification, and mould design [74].

These design considerations are all taken into account if the dynamic mould shape matches the billet profile as it travels through the mould. This will ensure that excessive binding or mould-strand interaction will not occur. As well, this eliminates the detrimental formation of air gaps between the strand and mould. While some degree of binding is necessary for the adequate extraction of heat, excessive binding will lead to high heat transfer and billet quality problems.

2.6.2 Mould Length

The design of the length of the mould, in combination with the casting speed, is essential in ensuring that there is sufficient residence time for the formation of a viable shell thickness at the exit of the mould [7,39,50,83]. The maximum casting speed will be limited by the ability of the shell at the exit of the mould to withstand the bulging load of the ferrostatic pressure of the liquid core. In conventional casting, mould lengths are in the range of 700 to 800 mm for casting speeds of up to 2.0 m/min. In high speed casting, mould lengths of about 1000 mm are used [50,83].
CHAPTER 3: SCOPE AND OBJECTIVES

The previous chapter has shown that there is not much in the literature regarding the heat transfer characteristics of the high speed casting of high quality steel billets. There appears to be little published data on the mould heat transfer profile response at casting speeds exceeding 3.0 m/min for billet casting machines [39]. Particularly, no successful instrumented trials have been conducted on the effects of M-EMS, and no heat transfer profiles for mould corners have been published. While there was some laboratory-scale work done at conventional casting speeds on the mould corner heat transfer response, the results may not be fully applicable to real casting machines due to the scale of the model, the simplifications made, and the modifications performed on the mould tube. Clearly, industrial mould trials have been instrumental in understanding and controlling the mould heat transfer and billet quality in conventional casting. Early developments in the areas of mould oscillation, mould design, mould lubricant distribution, mould constraint, cooling water flow, ladle metallurgy and electric furnaces, have made the continuous casting a cost-effective process in which to produce high quality semi-finished steel products. In this highly competitive industry, however, continual improvements and advances are being made in the areas of productivity and quality. Casting at conventional speeds is becoming less profitable in the competitive global market. More recently, further developments are in progress toward higher and higher productivity and quality. These include mould taper design, tundish flow control, electromagnetic stirring (EMS), mould flux lubrication with submerged entry nozzle (SEN) pouring, intelligent moulds [11,84], and expert systems [85,86]. With the resultant increase in billet quality and process control, the capability of casting at increasingly higher speeds has been realized.

To remain competitive in the global market, minimills are seeking to raise their productivity through increased casting speed, without sacrificing billet quality. To date, UBC has conducted instrumented plant trials at four North American minimills to study the design and operation of high-speed casting machines and the effect on billet quality. This current project explores, in particular, the findings from the fourth and final plant
trial conducted at Company H. Previously, only one instrumented study has been found which was conducted for billet casting of steel at these casting speeds [39]. However, this study considered only 0.1 to 0.19 wt %C grades, and focused on modelling rather than a comprehensive evaluation of the effect of casting speed. This project was therefore initiated to address the knowledge gap between previous work in conventional casting and future trends toward high speed casting. The objectives of this project were:

[1] To conduct an instrumented mould trial to measure mould temperature, mould oscillation, casting speed, and metal level, and obtain billet samples for analysis.
[2] To establish the thermal and heat transfer responses of the high speed continuous casting mould at the midfaces and corners for various carbon contents and casting speeds, using an existing mathematical mould model.
[3] To perform a comprehensive analysis of the effect of casting speed on the mould thermal and heat transfer responses at the midfaces of the mould, as well as determine which casting variables have the strongest effect on the heat transfer in the mould.
[4] To use existing heat transfer and billet solidification models to characterize the response of the billet in the mould during high speed casting in a long mould.
[5] To determine optimum mould tapers for plain carbon grade steels cast at 3.0 to 4.5 m/min, and evaluate the sensitivity of the design to casting speed.

As the final comprehensive experimental and analytical exploration of high speed casting in the High Speed Casting of High Quality Billets Study, this project hopes to bring together the previous work done at UBC to provide a global outlook on the effect of casting speed. The objectives are:

[1] To characterize the effect of casting speed on the mould heat transfer over a range of conventional and high casting speeds.
[2] To determine the effects of different operating practices and machine design conditions on the thermal and heat transfer response of the mould.
[3] To study the effect of mould electromagnetic stirring and mould lubricant on the heat transfer response.
CHAPTER 4: EXPERIMENTAL PROCEDURE – INDUSTRIAL PLANT TRIAL

As part of the 5-year study on the effects of high speed casting of high quality steel billets, researchers at UBC conducted 4 plant trials at various companies. The details of the most recent plant trial, conducted at Company H in December 1999, will be presented in this chapter.

4.1 Casting Machine and Operating Practices

Company H casts 120 x 120 mm\(^2\) billets for the production of wire rod. The trial was conducted on the sixth strand of the 6-strand caster. The mould tube had a quadruple taper with an initial taper of 2.7 \%/m, in a 7 m radius curved mould machine. The mould was oil lubricated and had sinusoidal oscillation. The tundish capacity was 25 tons and was of the delta type with a pour pad and no internal furniture. The casting speed was varied from 3.0 to 4.5 m/min through a change in tundish weight, and/or a change in the tundish nozzle diameter. The specifications of the casting machine are given in Table 4.1 and the trial mould tapers are given in Table 4.2.

4.1.1 Experimental Casting Parameters

The experimental variables included steel composition, casting speed, superheat, water flow rate, and tundish shroud type. These process variables were varied within the ranges listed in Table 4.3 to illustrate their effect on the response of the mould and the billet quality.
### 4.1.2 Details of Plant Trial

A total of 5 operating parameters were systematically varied to determine their effects on the mould heat transfer. These variables included casting speed, steel composition, superheat, water flow rate, and tundish shroud type. The details of the conditions during each heat are listed in Table 4.4 and the heat chemistries can be found in Table 4.5.

#### Table 4.1: Casting Machine Specifications for Company H

<table>
<thead>
<tr>
<th>Machine Type</th>
<th>Danieli</th>
</tr>
</thead>
<tbody>
<tr>
<td>Machine Radius</td>
<td>7.0 m</td>
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<tr>
<td>Number of Strands</td>
<td>6</td>
</tr>
<tr>
<td>Billet Size</td>
<td>120 x 120 mm²</td>
</tr>
<tr>
<td>Mould Details</td>
<td></td>
</tr>
<tr>
<td>Type</td>
<td>Curved</td>
</tr>
<tr>
<td>Constraint</td>
<td>4-sided</td>
</tr>
<tr>
<td>Material</td>
<td>0.07 – 0.12 wt% Ag</td>
</tr>
<tr>
<td></td>
<td>0.004 – 0.04 wt% P</td>
</tr>
<tr>
<td>Taper</td>
<td>Quadruple</td>
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<tr>
<td>Mould Length</td>
<td>1000 mm</td>
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<tr>
<td>Wall Thickness</td>
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<tr>
<td>Corner Radius</td>
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<td>Baffle Gap</td>
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<td>Mould Level Control</td>
<td>Berthold</td>
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<tr>
<td>Metal Level</td>
<td>163 mm (Low Carbon Grades)</td>
</tr>
<tr>
<td></td>
<td>134 mm (High Carbon Grades)</td>
</tr>
<tr>
<td>Casting Speed</td>
<td>3.0 to 4.5 m/min</td>
</tr>
<tr>
<td>Tundish Flow Control</td>
<td>Metering Nozzles</td>
</tr>
<tr>
<td>Tundish Level Control</td>
<td>Load Cells</td>
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<tr>
<td>Reoxidation Protection</td>
<td>N₂ Shrouding</td>
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<tr>
<td>Oscillation Type</td>
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<td>Stroke Length</td>
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<tr>
<td>Frequency</td>
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<tr>
<td>Negative Strip Time</td>
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<tr>
<td>Mould Lead</td>
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<tr>
<td>Mould Lubrication</td>
<td>Slotted Oil Delivery System</td>
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<tr>
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<td>20 to 35 ml/min</td>
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<td></td>
<td>Nalco SBQ-6</td>
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Table 4.2: Current Mould Tapers for Company H

<table>
<thead>
<tr>
<th>Distance From Top of Mould (mm)</th>
<th>Taper (%/m)</th>
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<tbody>
<tr>
<td>0 – 75</td>
<td>0</td>
</tr>
<tr>
<td>75 – 250</td>
<td>2.7</td>
</tr>
<tr>
<td>250 – 370</td>
<td>1.35</td>
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<tr>
<td>370 – 570</td>
<td>0.65</td>
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<tr>
<td>570 – 1000</td>
<td>0.45</td>
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Table 4.3: Experimental Casting Variables

<table>
<thead>
<tr>
<th>Operating Variable</th>
<th>Experimental Operating Range</th>
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</thead>
<tbody>
<tr>
<td>Carbon Content</td>
<td>0.04 to 0.08 wt %C</td>
</tr>
<tr>
<td></td>
<td>0.71 to 0.75 wt %C</td>
</tr>
<tr>
<td></td>
<td>0.81 to 0.83 wt %C</td>
</tr>
<tr>
<td>Boron Content</td>
<td>0.006 wt %B (0.05 wt %C)</td>
</tr>
<tr>
<td>Casting Speed</td>
<td>Low Carbon: 3.0 to 3.5 m/min</td>
</tr>
<tr>
<td></td>
<td>High Carbon: 3.0 to 4.5 m/min</td>
</tr>
<tr>
<td>Superheat</td>
<td>Low: 13 to 27 °C</td>
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<tr>
<td></td>
<td>High: 38 to 55 °C</td>
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<tr>
<td>Water flow rate</td>
<td>20 m/s to 15 m/s (2200 to 1700 L/min)</td>
</tr>
<tr>
<td>Tundish Shroud</td>
<td>Bellows or Pollard</td>
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**Table 4.4: List of Company H Trial Heats**

<table>
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<tr>
<th>Heat Number</th>
<th>Carbon Content (%)</th>
<th>Cast Speed (m/min)</th>
<th>Superheat (°C)</th>
<th>Water Velocity (m/s)</th>
<th>Tundish Nozzle Dia. (mm)</th>
<th>Comments</th>
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<td>0.06</td>
<td>3.5</td>
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<td>17</td>
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<td>21</td>
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</table>

**Notes:**

1. The listed casting speeds were averaged at steady-state.
2. The comments indicate the number of billets sampled and whether the heat was modelled analytically.
3. Heat 46309 contained 0.006 wt %B.
4. Heats 25692, 25693, 25694, 46308, and 46309 used the Pollard tundish shroud. The remaining heats used the Bellows tundish shroud.
Table 4.5: Heat Chemistries for the Plant Trial at Company H

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<th>C</th>
<th>Mn</th>
<th>P</th>
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<th>Si</th>
<th>Cu</th>
<th>Ni</th>
<th>Cr</th>
<th>Mo</th>
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4.2 Data Acquisition

The data was collected using Labtech Notebook (Version 7.1.1) software on an IBM PC. A Metrabyte Universal Expansion Interface, Model EXP-16, was used with a Metrabyte Multiplexer DAS-8 board for data acquisition. The board had a resolution of 0.012 mV, or approximately 0.5 °C. The data sampling frequency was 10 Hz and data was collected on 78 channels, which measured mould wall temperatures, casting speed, metal level, and water temperatures, for the duration of 23 heats.

4.2.1 Measurement of Mould Wall Temperatures

Company H provided a new mould tube for the plant trial, which was subsequently instrumented with 15 thermocouples at each face, according to UBC specifications, to measure the mould response. The thermocouple preparation, instrumentation and calibration was provided by Mr. B. Neil Walker of the University of British Columbia, Centre for Metallurgical Process Engineering. The modification of the existing mould for the installation of the thermocouples was performed by a Canadian mould manufacturer and reformer.

The procedure for the instrumentation of billet moulds and measurement of mould wall temperatures during casting, has been established by Brimacombe and co-workers [4,9,14,29,31]. The same procedure has been adopted for this investigation. A total of 68 copper/constantan (55%Cu–45%Ni), intrinsic, T-type, single-wire (0.81 mm diameter) thermocouples were installed in the mould tube. 15 thermocouples were installed at the midface of each mould wall, and a total of 8 thermocouples were installed at off-corner locations of the ICW-LSW (inside curved wall and left straight wall) junction, near the meniscus (Figure 4.1). The corner thermocouples were installed such that the centreline axes of the thermocouples were approximately 4 mm from the hot face of the mould (Figure 4.2). The thermocouples were embedded in the mould wall to a depth of 6 mm, measured from the cold face, and were held in mechanical contact with the mould using high-purity copper plugs. Heat shrinkable tube was placed over the exposed sections of the thermocouple wire.
In addition to the mould thermocouples, four commercially fabricated thermocouples were installed at the exit of each face of the water cooling channel. The thermocouples were located at the centrelines of the channel faces and were used to record the cooling water outlet temperature. The bulk inlet and outlet mould water temperatures were also recorded at the mould housing.

4.2.2 Measurement of Mould Oscillation

The mould displacement, during sinusoidal oscillation, was measured using a Linear Variable Differential Transformer (LVDT, also called Linear Variable Displacement Transducer). The LVDT is an electromagnetic device used to translate the linear displacement of the mould into an electrical signal. The LVDT was installed on the mould housing.

4.2.3 Other Miscellaneous Measurements

Ambient temperature was measured at the beginning of each heat using a hand-held digital meter, which was placed near the DAS equipment in the electrical room at Company H. Discrete readings of the water flow rate (in L/min), oil flow rate, oscillation frequency, tundish weight and tundish temperature, were recorded from the computerized control system used by the steel plant.

The metal level and casting speed signals were obtained from a direct feed from the control system at Company H. The 4 to 20 mA signals from the plant were converted, by the instrumentation described in Section 4.2, to a 0 to 20 mV output signal which was recorded by the data acquisition system.
Figure 4.1: Face view of the mould midface and ICW-LSW (inside curved wall – left straight wall) corner thermocouple layout at Company H. All distances measured in millimetres from the top of the mould. Not to scale [Figure By B. Neil Walker, 1999].
4.3 Billet Samples

Billet samples were taken strategically during the trial by Company H personnel to reflect changes in process variables across the heats. In total, 34 billet samples, measuring ~30 cm in length, were collected (Table 4.4). Billet quality will not be presented in this work due to a confidentiality agreement.

4.4 Plant Trial Difficulties

The LVDT was to provide continuous readings that were to be recorded on a separate computer from the thermocouple measurements. However, due to a short-circuit in the data acquisition board, it was not possible to continuously and concurrently record LVDT and thermal data during the plant trial. Instead, 30 seconds of oscillation data were recorded during the last heat of the trial (25705). Difficulties were also encountered during the calibration of the LVDT during the plant trial, in establishing the conversion factors between the electromechanical signal and displacement. A subsequent re-calibration was performed at UBC following the trial. The re-calibration produced a calculated stroke length that was 1.5 mm longer than reported by Company H.
During the first 3 heats cast on the instrumented mould, abnormally high mould wall temperature readings were recorded. The strand was extracted and the mould was removed to determine whether there was a water leak or if the instrumentation was at fault. An examination of the mould revealed that there was poor water pressure in the cooling channel due to the bypassing of the cooling water through the plenum seal. Measurements were performed at the plant as a dimensional check on the internal widths of the mould before continuing with the trial. It was determined that no permanent distortion resulted from the poor water flow. The thermocouples were examined and several were found to be broken. The broken thermocouples were a result of an incident which occurred during the pre-trial preparation where the instrumented mould slid several centimetres out of the steel sleeve, stretching the thermocouples. The faulty thermocouples were clipped, the sealant was reapplied, and the mould was reassembled and leak-tested in the mould shop. The 3 heats affected by this incident were not included in the analysis and are not listed in Table 4.4 and Table 4.5.

After the plant trial, as the trial mould was being disassembled for storage, another leak in the water plenum gasket was found at the ICW (inside curved wall) face. The gasket, which sealed the in- and out- water flows in the water jacket, was found to be unseated for about 100 mm along the perimeter. A subsequent analysis of the difference in the responses of the 4 mould walls revealed that the bypassed water flow had no effect on the mould water outlet temperature, the measured mould wall temperature at the meniscus, or the average mould heat transfer standard deviations. It is likely that bypassed flow did not decrease the cooling water velocity below the critical value of 11 m/s, and hence it had a negligible effect on the response of the mould.
CHAPTER 5: RESULTS OF INDUSTRIAL PLANT TRIAL

The analysis consisted of two components: the billet quality analysis, and the computational analysis. A full list of the billet samples taken and heats measured are listed in Table 4.4. The billet quality analysis consisted of both a surface and internal evaluation of the 34 samples taken during the trial. The computational component of the analysis consisted of thermal, heat transfer, and billet solidification analyses as well as mould taper design. This chapter will describe the measured thermal response of the mould and briefly describe the billet quality evaluation. The various facets of the computational analysis will be discussed in the next few chapters.

5.1 Mould Thermal Response

The temperature distribution in the mould is influenced by the heat transfer from the solidifying strand, conduction in the mould wall, radiation to the atmosphere, and the rate of heat extraction by the cooling water. The thermal response of the mould was recorded at 15 locations down the midface mid-thickness of each mould wall. Additional thermal readings were recorded at eight corner locations at the ICW-LSW junction around the meniscus region. The thermocouples that were damaged prior to the trial or that showed abnormal responses were eliminated from the analysis. The eliminated thermocouples, in the steady-state analysis, are listed in Table 5.1.

Due to the volume of data, the 10 Hz sampled data was reduced to a frequency of 5 Hz for the analysis. The 5 Hz data varied, on average, by up to $-3^\circ$C below the 10 Hz data at the midface thermocouples and up to $-5^\circ$C at the corner thermocouples. These differences were acceptable as they fell within the measured standard deviations (4 to 13 °C). Previously, a sampling frequency of 1 Hz was found to be acceptable in a steady-state analysis [23].
Table 5.1: List of Eliminated Thermocouples

<table>
<thead>
<tr>
<th>Mould Face</th>
<th>Location of Eliminated Thermocouples (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>ICW</td>
<td>151.0</td>
</tr>
<tr>
<td></td>
<td>275.5</td>
</tr>
<tr>
<td></td>
<td>701.0</td>
</tr>
<tr>
<td>(corner)</td>
<td>165.5</td>
</tr>
<tr>
<td>(corner)</td>
<td>210.5</td>
</tr>
<tr>
<td>LSW</td>
<td>251.5</td>
</tr>
<tr>
<td></td>
<td>277.0</td>
</tr>
<tr>
<td></td>
<td>326.0</td>
</tr>
<tr>
<td></td>
<td>591.0</td>
</tr>
<tr>
<td></td>
<td>701.0</td>
</tr>
<tr>
<td>OCW</td>
<td>231.5</td>
</tr>
<tr>
<td>RSW</td>
<td>101.0</td>
</tr>
<tr>
<td></td>
<td>375.0</td>
</tr>
<tr>
<td></td>
<td>590.0</td>
</tr>
</tbody>
</table>

Notes: The mould wall faces are denoted as:
1. ICW, Inside Curved Wall
2. LSW, Left Straight Wall
3. OCW, Outside Curved Wall
4. RSW, Right Straight Wall
Where, ‘curved’ denotes the mould wall faces that are situated on the curvature of the machine.
5.1.1 Mould Midface Thermal Profiles

The average thermal responses of each of the mould walls, for the high and low carbon grades, are presented in Figure 5.1 to Figure 5.4. Clearly, while each mould wall had a distinct thermal profile, they shared similar characteristics. The peak mould temperatures occurred up to 100 mm below the meniscus, occasionally reaching a peak right at the meniscus. The meniscus was at 163 mm for the low carbon grades and 134 mm for the high carbon grades. The thermal profiles either decreased gradually or levelled off to a fairly constant value below the peak temperature. The complex state of events at the meniscus was one contributor to the suppression of the peak below the meniscus. The second contributor, the increased casting speed, suppressed the peak temperature to a level that was below what is expected in conventional casting. This was because the increased casting speed reduced the residence time in the mould, which decreased the solidification time. The temperatures gradually decreased from the peak value due to the increased strength and thickness of the shell.

The peak temperatures ranged from 140 to 180 °C for the high carbon grades and 100 to 155 °C for the low carbon grades. Hence, increasing the carbon content led to a hotter mould. Local minima were observed only in the OCW of all the heats analysed, with the exception of heats 46302, 46304 and 25691. The local minima ranged from 80 to 120 °C and were located at 251 mm from the top of the mould. Heats 25701, 25703, 25704, 46318 and 46319 had an additional minima of 115 to 120 °C at 326 mm from the top of the mould. The heats exhibiting the double minima were all high carbon heats cast within the last 9 heats of the trial, at high speeds (≥ 3.8 m/min) with the 17.5 mm tundish nozzle, the significance of which will be illustrated further in the analysis.

The ICW of all of the analysed heats exhibited a significant thermal drop of 15 to 55 °C at the last thermocouple, which was located at ~900 mm from the top of the mould. This could have been due a number of reasons, as listed below.
1. The cooling water may have been entering the bottom of the mould from the high-pressure water jets located in the first zone of the spray chamber, thus quenching the ICW.

2. There may have been physical deviations in the mould walls at the bottom of the mould. Unfortunately, the absolute local mould tapers could not be measured independently, as the profilometer measurements of the internal width of the mould wall used the opposite wall as a reference measurement surface. Hence these measurements resulted in a relative difference in width between two mould wall faces. A locally shallow taper at the ICW face could explain the low measured temperatures, however this could not be confirmed.

3. There is the possibility that the bottom ICW thermocouple was damaged, or not installed properly, causing an additional contact thermal resistance in the heat flow circuit. However this was unlikely, as the thermocouples were tested prior to the trial heats, at room temperature with cooling water flow through the housing.

4. There may have been an effect of the length of the mould. The curvature of the long mould (1000 mm) may have had more significance on the interaction with the strand than in a conventional mould (~700 to 800 mm), particularly since the existing mould tapers were insufficient, as will be discussed in Chapter 8. This would cause the strand to rest on the OCW during casting, resulting in gap formation at the ICW interface at the bottom of the mould. This would act as a barrier to heat transfer, reducing the mould wall temperature at the exit of the mould and allowing the ICW strand surface to reheat. Unfortunately, such an event could not be confirmed, as the size of the gap cannot be measured.

Although the thermal drop at the bottom of the ICW could not be accounted for conclusively, subsequent analysis will also show that there was a definite local event in effect.
Figure 5.1: A comparison of the average measured mould thermal responses at the inside curved wall for a low and high carbon grade cast at high speed.

Figure 5.2: A comparison of the average measured mould thermal responses at the left straight wall for a low and high carbon grade cast at high speed.
Figure 5.3: A comparison of the average measured mould thermal responses at the outside curved wall for a low and high carbon grade cast at high speed.

Figure 5.4: A comparison of the average measured mould thermal responses at the right straight wall for a low and high carbon grade cast at high speed.
5.1.2 Mould Corner Thermal Profiles

The mould corner thermal responses are illustrated in Figure 5.5. The mould corner temperature, like the midface thermal profile, reached a peak below the meniscus. However, unlike the midface peak temperatures, the corner peak temperatures were consistently much closer to the meniscus, an indication that thicker and stronger shells were initially developing at the corners. The peak temperatures were in the range of 90 to 130 °C for the high carbon grades and 65 to 95 °C for the low carbon grades, showing that the mould corners were much cooler than the midfaces. As well, unlike the midface thermal profile, the temperature of the corners of the mould dropped very rapidly below the peak. This was due to the two-dimensional heat transfer occurring at the corners of the mould that allowed it to cool much more quickly than at the midface. At a distance of about 211 mm from the top of the mould, the mould corner temperatures ranged from 70 to 130 °C for the high carbons and 55 to 65 °C for the low carbons. In comparison, the midface temperatures at a similar longitudinal location (200 mm) ranged from 130 to 175 °C in the high carbon grades and 90 to 125 °C in the low carbon grades.

Figure 5.5: A comparison of the average measured mould thermal responses at the ICW-LSW corner of a low and high carbon grade cast at high speed.
5.2 Mould Oscillation

A comparison of the measured and theoretical stroke lengths and oscillation cycle shape is shown in Figure 5.6. Figure 5.7 illustrates the calculated mould velocity from the measured and calculated data. Clearly, the oscillation displacements and velocities were well-controlled during the trial.

Figure 5.6: A comparison of the measured and theoretical stroke lengths and mould oscillation cycles during plant trial H showed that the process was well controlled.
Figure 5.7: A comparison of the measured and calculated mould oscillation velocities during plant trial H showed that the process was well controlled.
5.3 The Effect of Casting Speed and Metal Level Fluctuation

The metal level fluctuation was found to increase with casting speed only when the increase in casting speed was initiated through an increased tundish weight and when casting with the 17.5 mm diameter tundish nozzle. The smaller tundish nozzle (15.5 mm), coupled with higher tundish weights (to increase the casting speed), was found to have no effect on the metal level standard deviation. Therefore, the larger nozzle diameter likely introduced some roughness into the tundish stream. This effect will be explored further in Chapter 6.

5.4 Billet Quality

The billet quality evaluation, including sample preparation, analysis, and photography, was conducted by Mr. B. N. Walker of UBC. A total of 34 billets – 8 low carbon (including 2 boron grades), and 24 high carbon samples – were analysed for internal and surface defects. The results of this analysis are confidential and cannot be disclosed in detail. However, the analysis did not reveal any trends in which the occurrence of the defects could be quantitatively linked to metal level standard deviations, casting speed, superheat, or tundish nozzle diameter. As well, the surface and internal quality of the high speed billets did not have defects that were significantly different in type or severity than conventionally cast billets. This final observation, however, was based on a limited range of billet samples.
CHAPTER 6: MOULD HEAT TRANSFER ANALYSIS

The purpose of this analysis was to establish the effects of casting speed, metal level fluctuation, carbon content, boron addition, superheat, water flow rate and tundish shroud type on the mould heat transfer (Chapter 6) and billet solidification behaviour (Chapter 7). In the pursuit of this goal, 14 of the 23 heats were selected for analysis and are listed in Table 4.4. Ultimately, the ensuing analysis provided the basis upon which sets of optimal mould tapers were designed and recommended for a particular range of carbon contents and casting speeds (Chapter 8).

6.1 Mathematical Mould Thermal Model

An inverse heat conduction model, coded in FORTRAN, was used to determine the heat transfer profiles from the measured mould wall temperatures [4,9]. This model included heat transfer from the strand to the mould, conduction through the mould wall, and heat removal in the cooling water channel. The model used an Alternating Direction Implicit Finite Difference Method (ADI-FDM) discretizing technique, with the appropriate initial condition and boundary conditions, to aid in the determination of a 2-dimensional midface mould wall thermal profile and a midface heat transfer profile on the hot face of the mould. The system schematic is illustrated in (Figure 6.1). The figure shows the cross-section of the mould wall at the midface and a nodal mesh system for the discretization of the system.
Figure 6.1: The system schematic, used in the finite difference solution of the inverse heat conduction model, is shown at the cross-section of the mould wall through the midface [Reference 4, Figure 4].
The following conditions were imposed in the application of the model:

1. The mould was operating in steady state, such that at any snapshot in time, the mould response (heat transfer, temperature, and distortion) was identical. This assumption was considered valid, as only blocks of data containing steady casting conditions were used in the model. As well, there was no nucleate boiling in the cooling channel.
2. There was negligible heat flow in the transverse direction (y-axis). Two-dimensional heat flow occurred only through the mould wall thickness (x-axis) and down the length of the mould (z-axis).
3. The top and bottom surfaces of the mould were adiabatic.
4. There was plug flow (no friction) in the water cooling channel.
5. Heat transfer between the cooling water and mould jacket was negligible.
6. The heat transfer coefficient at the mould-water interface was constant down the length of the mould. It was evaluated at the average water temperature.
7. Local transient effects, such as mould oscillation, were ignored.
8. The thermal conductivity of the copper mould was independent of temperature. Previous work at UBC has determined that the temperature dependence of the thermal conductivity of copper has a negligible effect on the calculated thermal profile [4,9,87].
9. The model used a fixed nodal system of 5 x 141 nodes.

In this analysis, a thermal tolerance of 1°C was used in the iteration sequence. The particular details of the model have been discussed elsewhere [4,9].

6.2 Mould Heat Transfer Response

Preliminary heat transfer calculations using the aforementioned model were found to be too low, compared to the water heat transfer calculation. Using the unmodified model, the predicted average midface mould heat transfer was 15 to 30% lower than the calculated mould water heat transfer, whereas previous UBC calculations were within ~20%. Most of the analysed Company H heats were deviant by a value of 24%. It is likely that the extremely high water velocities at Company H, compared with all of the previous plant trials that used the same calculation technique, affected the calculation of the water heat transfer coefficient. The Dittus-Boelter correlation, which was used in the model to approximate the heat transfer coefficient at the mould-water interface, is known to be within a ± 25% error [88]. This error is likely attributable to the friction factor
component of the correlation, which is based on experimental measurements in circular pipe flows with smooth internal surfaces. As the conditions of the trial involved high water flows in an annular channel, it is likely that there was some flow effect which increased the error of the current calculations over previous work. Therefore, since the water velocities were significantly higher and the deviation offset was fairly constant across all of the heats, the Dittus-Boelter correlation was modified by the maximum amount, a 1.25 factor. The modified model resulted in average heat transfer rates that were 6 to 22% lower than the water heat transfer calculations, with most heats being lower by ~15%.

Using the modified heat transfer model, the mould wall heat transfer response was approximated at both the midface and ICW-LSW corner of the mould. The calculations were based on up to 15 thermocouples at the midfaces, and up to 4 meniscus-region thermocouples at each junction of the ICW-LSW corner. As a result, a detailed evaluation of the mould corner profiles was limited to the meniscus region.

### 6.2.1 Mould Midface Heat Transfer

Typical average midface heat transfer profiles at each mould wall are given in Figure 6.2 to Figure 6.5 for the low and high carbon grades. Clearly, each of the four walls had a unique response, particularly in the 200 mm band below the meniscus. The heat transfer peaks at the hot face of the mould were expected to be at the meniscus, which was at 163 and 134 mm for the low and high carbon grades, respectively. However, the standard deviations of the measured temperatures (due to metal level fluctuations) caused some of the heat flux peaks to vary around the meniscus. Typically, the heat flux peaks were in the range 160 to 250 mm below the top of the mould, hence the peaks were shifted greater distances in the high carbon grades, which had a shallower metal level and were typically cast at much higher speeds. The effect of fluctuations in the metal level will be analysed in Section 6.3.2 (p.82).
As expected, the heat transfer magnitude dropped and levelled off below the peak. As discussed previously, subsequent decrease in the heat transfer profile was attributed to the increasing resistance of the thickening shell to heat flow, and solidification shrinkage. This shrinkage produced an increasingly large air gap between the strand and mould, further impeding the heat flow. The thermal drop at the bottom of the ICW of all of the analysed heats, clearly had an impact on the heat transfer profile, as seen in Figure 6.2. It appeared that carbon grade did not have a very strong effect on the magnitude of the drop in heat transfer at the ICW at the bottom of the mould.

The peak midface heat transfer occurring at, or just below the meniscus, was in the range of 3000 to 7100 kW/m² for the low carbon grades, 4000 to 9100 kW/m² for the 0.71 to 0.75 wt %C grades, and 4300 to 8500 kW/m² for the 0.81 to 0.84 wt %C grades. These heat flux peaks were much higher, approximately 1.5 to 2.5 times the magnitude, than has been observed in conventional casting (1.3 m/min). Some of the heat transfer peaks at Company H were also much higher (by ~1.5 times) than the published high speed data, in both carbon ranges. As well, the wide ranges in the heat flux peaks indicated that this high speed casting operation had a degree of process variability, which (as will be shown) was a result of the varying responses at the different mould faces and casting speed. Distinct double peaks were also observed in the heat transfer profiles at the meniscus region of the virtually all of the mould walls of the analysed heats. The magnitude of the double peak behaviour was sufficient to manifest itself in the average mould wall heat transfer response (Figure 6.6). This was an indication that non-uniform heat extraction was occurring at the top of the mould, which was likely caused by the dynamic distortion of the working mould tapers, unsuitable taper design, and/or the action of the ferrostatic pressure against the newly formed shell.
Figure 6.2: Midface hot face heat transfer profiles are shown at the inside curved wall for a low and high carbon grade cast at high speed and various superheats. The strong heat transfer peak at the ICW is a peculiarity of the low carbon grades.

Figure 6.3: Midface hot face heat transfer profiles are shown at the left straight wall for a low and high carbon grade cast at high speed and various superheats.
Figure 6.4: Midface hot face heat transfer profiles are shown at the outside curved wall for a low and high carbon grade cast at high speed and various superheats. The excessive heat transfer peak at the OCW was a peculiarity of the high carbon grades.

Figure 6.5: Midface hot face heat transfer profiles are shown at the right straight wall for a low and high carbon grade cast at high speed and various superheats.
Figure 6.6: Average midface mould wall hot face heat transfer profiles are shown for a low and high carbon grade cast at high speed and various superheats.

Figure 6.7 illustrates the variability of the peak heat transfer at each mould wall below the meniscus for the various low carbon heats. Generally, the plain high and low carbon grades had meniscus level heat transfer standard deviations of up to 800 kW/m². Considering these fluctuations, the ICW of the low carbon grades appeared to have the greatest heat transfer peaks than any of the other mould walls. The scatter between the different heats can be attributed to an off-centre tundish stream (heats 25692, 46307) and differences in metal level standard deviation. As well, heat 46309 had a 0.006 wt % boron addition, the effect of which will be discussed in Section 6.3.4 (p.90). The high carbon grades, on the other hand, had the highest peak heat transfer peaks at the OCW, which is clearly illustrated by Figure 6.4 and Figure 6.8. At the RSW of the high carbon grades (Figure 6.8), heat 25689 had a significantly higher heat transfer peak than the other heats. This could not be explained in terms of tundish weight, shroud type, superheat, casting speed, metal level fluctuations, transient events in the metal level/tundish weight/ casting speed, or physical deviations in the mould walls.
Figure 6.7: The magnitude of the peak heat transfer was greatest at the inside curved wall for the low carbon grades. The meniscus-level heat transfer standard deviations were typically up to 800 kW/m$^2$.

Figure 6.8: The magnitude of the peak heat transfer was greatest at the outside curved wall for the high carbon grades. The meniscus-level heat transfer standard deviations were typically up to 800 kW/m$^2$. 
None of the analysed heats showed a mould wall bias for the minimum local heat transfer, which was located at the bottom half of the mould. In this region, the heat transfer standard deviations were up to 400 kW/m².

The peculiar effect of the variations in the heat transfer peaks across the four mould walls for the different carbon grades could possibly be attributed to differences in the local meniscus taper, rather than a composition effect. These deviations in the local mould tapers can result from poor tolerances in the construction of the mould tube. A high local taper at the ICW, relative to the other mould walls, would have caused high heat transfer peaks in the low carbon grades if it happened to be located near the metal level of 163 mm. In the high carbon grades, on the other hand, a high local mould taper at a distance of 134 mm down the OCW would cause high heat transfer peaks. Thus the apparent wall-bias of the peak heat transfer of different carbon grades was possibly related to the deviations in the local mould taper, combined with the change in metal level. As there is no technique to accurately determine the local absolute mould taper down the length of the mould, the cause of these peak variations could not be resolved with certainty.

The mould midface heat transfer was analysed in two ways: by a mould wall heat transfer profile, as previously discussed, and the average mould wall heat transfer. The average mould wall heat transfer and average mould wall heat transfer standard deviations were obtained for the 14 analysed heats through the averaging of the heat transfer profiles of the 4 mould walls (Table 6.1). The total average midface mould wall heat transfer for the low carbon grades ranged from about 1625 to 1860 kW/m², the 0.70 to 0.75 wt %C grades ranged from about 1980 to 2380 kW/m², and the 80C series grades ranged from about 2380 to 2470 kW/m². The two high carbon grades had different average mould heat fluxes due to the difference in casting speed, as seen in Table 6.1. These values compare very well with the published high speed data, and are significantly greater than the published data for conventional casting speeds by 24 to 45% (360 to 770 kW/m²). The corresponding mould midface heat transfer standard deviations at Company H were 200 to 300 kW/m² for the low carbons, and 200 to 240 kW/m² for the
MOULD HEAT TRANSFER ANALYSIS

high carbons. Clearly, as will be further substantiated, the average heat transfer increased with carbon content, irrespective of any other process variable. This indicated that carbon content was the dominant influencing factor on the heat transfer behaviour at Company H.

Table 6.1: Average Midface Mould Wall Heat Transfer

<table>
<thead>
<tr>
<th>Heat</th>
<th>Casting Speed (m/min)</th>
<th>Superheat (°C)</th>
<th>Midface Mould Wall Heat Transfer (kW/m²)</th>
<th>ICW</th>
<th>LSW</th>
<th>OCW</th>
<th>RSW</th>
<th>Avg</th>
</tr>
</thead>
<tbody>
<tr>
<td>25691</td>
<td>3.5</td>
<td>37</td>
<td>1668.2</td>
<td>1733.1</td>
<td>1741.8</td>
<td>1772.0</td>
<td>1728.8</td>
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</tr>
<tr>
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<td>1676.5</td>
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<td>1624.6</td>
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</tr>
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<td>29</td>
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<td></td>
</tr>
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</tr>
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<td>46309</td>
<td>3.5</td>
<td>32</td>
<td>1765.8</td>
<td>1880.8</td>
<td>1867.3</td>
<td>1914.1</td>
<td>1857.0</td>
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0.70 to 0.74 wt % Carbon Grades

<table>
<thead>
<tr>
<th>Heat</th>
<th>Casting Speed (m/min)</th>
<th>Superheat (°C)</th>
<th>Midface Mould Wall Heat Transfer (kW/m²)</th>
<th>ICW</th>
<th>LSW</th>
<th>OCW</th>
<th>RSW</th>
<th>Avg</th>
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<tbody>
<tr>
<td>25689</td>
<td>3.6</td>
<td>16</td>
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<td>2164.2</td>
<td>2216.3</td>
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<tr>
<td>25701</td>
<td>3.9</td>
<td>35</td>
<td>2349.6</td>
<td>2394.7</td>
<td>2341.8</td>
<td>2457.4</td>
<td>2385.9</td>
<td></td>
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<tr>
<td>46304</td>
<td>3.5</td>
<td>52</td>
<td>2321.3</td>
<td>2295.7</td>
<td>2231.0</td>
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<td>2298.7</td>
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</tbody>
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0.80 to 0.83 wt % Carbon Grades

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<th>Heat</th>
<th>Casting Speed (m/min)</th>
<th>Superheat (°C)</th>
<th>Midface Mould Wall Heat Transfer (kW/m²)</th>
<th>ICW</th>
<th>LSW</th>
<th>OCW</th>
<th>RSW</th>
<th>Avg</th>
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<tbody>
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<td>2383.0</td>
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</tr>
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<td>25704</td>
<td>3.8</td>
<td>58</td>
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<td>2373.4</td>
<td>2371.8</td>
<td>2559.5</td>
<td>2431.7</td>
<td></td>
</tr>
<tr>
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<td>16</td>
<td>2498.7</td>
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<td>2365.7</td>
<td>2593.4</td>
<td>2469.5</td>
<td></td>
</tr>
</tbody>
</table>

Note:
1. Heat 46309 contained 0.006 wt %B.
2. The two high carbon grades had different heat transfer responses due to the difference in casting speeds.

Interestingly, the table of values given above showed that those heats that had high heat transfer peaks did not necessarily have the highest average heat transfer. This would indicate that the local heat transfer below the meniscus region was lower than the other walls in these heats, thus dampening the overall effect of the high heat transfer peak. The low carbon grades, for instance, had high heat transfer peaks at the ICW but typically had
the lowest average heat transfer at the ICW as well. Heat 25692 was an exception due to the off-centre tundish stream. The extent of the reduction in the average heat transfer from the wall with next lowest average heat transfer was found to be 92 to 140 kW/m². These were not significant deviations, considering that the average heat transfer standard deviations were a minimum of 200 kW/m² for the low carbon grades. In the high carbon grades, only the 0.80 to 0.83 wt %C grades had a bias in the average heat transfer. While these grades had the highest heat transfer peaks at the OCW, the lowest average heat fluxes were typically found to be at both the OCW and LSW (Figure 6.9). This could be an indication that the tundish stream was off-centre, and located closer to the ICW-RSW quadrant of the mould. As well, the deviations of the OCW or LSW from the other walls were found to be not very significant, considering that the average heat transfer standard deviation was 200 to 240 kW/m². The 0.70 to 0.74 wt %C grades showed no bias in the average heat transfer at any mould wall.

Figure 6.9: In the 0.80 to 0.83 wt %C grades, the average mould heat transfer was the lowest at the OCW and LSW, although the heat transfer peaks were the greatest at the OCW. Differences in the local mould taper and/or an off-centre tundish stream may have been the cause of this.
The calculated average mould heat transfer was validated through an assessment of the mould water heat transfer, as discussed in the literature review. Figure 6.10 compares the total heat energy extracted by the water to the calculated heat extracted, and demonstrated that the model predictions (using a 1.25 modification factor in the Dittus-Boelter correlation) were within 6 to 22% (145 to 475 kW/m²). Most of the heats were about 15% lower than the mould water heat transfer. The average mould heat transfer standard deviations for the Company H heats were around 170 to 250 kW/m². The water heat transfer standard deviations could not be assessed due to noise during the measurement.

Figure 6.10: A comparison of the mould water heat transfer with the calculated average mould heat transfer showed that the model-predicted average heat transfer was 6 to 22% lower than the water heat transfer.
6.2.2 Mould Corner Heat Transfer

The mould corner heat transfer profiles were obtained from a combination of the responses at the ICW corner and LSW corner junctions, respectively, as seen in Table 6.2. Figure 6.11 shows the individual responses at each edge of the mould corner for the high and low carbon grades. The peak heat transfer occurred below the meniscus region and generally ranged from 2000 to 4000 kW/m$^2$ for the low carbons, 2100 to 4900 kW/m$^2$ for the high carbons. The lowest peaks in these ranges were found at the ICW side of the ICW-LSW corner junction, indicating again that gaps formed preferentially at the ICW face of the mould. It is unlikely that this was a local taper effect at the ICW, as all of the grades, which had different meniscus levels, had the same effect. The figure clearly shows that the LSW side of the corner had a significantly higher heat transfer magnitude than the ICW side. This was true in all of the analysed heats. As the measured mould wall temperatures at the left side of the corner junction were typically also higher, this indicated that there was less of a mould-strand gap in this region. It is likely that there was a larger mould-strand gap at the ICW due to the curvature of the machine and the effect of gravity acting on the strand. It was suspected that in this long mould, the arc length of the mould had a much more pronounced curvature than a conventional (short) mould. The midface mould heat transfer profiles also supported this hypothesis, as previously discussed, but the effect was of a lesser extent. Therefore, the corners appeared to be more sensitive to the changes in solidification behaviour, gap size, and mould-strand interaction, resulting from the different geometry from the midfaces. Further support for the effect of gap formation at the ICW will be given in Chapter 9.

The average mould corner response was taken as the average of the LSW and ICW corner edge responses (Figure 6.12). The low carbons had average corner heat transfer rates within 1480 to 1850 kW/m$^2$, and the high carbons were within 1960 to 2080 kW/m$^2$. These were slightly lower than the published conventional casting speed values of 1600 to 1900 kW/m$^2$ for the low carbons and 2100 to 2600 kW/m$^2$ for the high carbons. However, the data for conventional speeds was obtained from a laboratory-scale, thick-
walled mould tube mock-up, which did not simulate mould oscillation [22]. Therefore, the experimental data from this study would not fully approximate the distortion in a true production mould, and hence would over-predict the heat transfer. Therefore, the results from Company H could not be compared with the data in the literature to assess the relative degree of mould-strand gap formation at the mould corners between conventional and high speed casting operations.

At Company H, the peak mould midface heat fluxes were up to 41% (1500 kW/m$^2$) higher than the peak corner heat fluxes and the average midface heat transfer was up to 25% (485 kW/m$^2$) higher than at the corners. This corresponds very well to the literature, which reported decreases in the corner heat transfer by 340 to 540 kW/m$^2$, at a casting speed of 1.3 m/min. The exceptions to the trend were heats 46307 and 25689 which had peak and/or average corner heat fluxes that were higher than the midface heat fluxes at by up to 900 kW/m$^2$ and 141 kW/m$^2$, respectively. These exceptions could not be explained in terms of casting speed, superheat, tundish weight, or metal level fluctuation.

Table 6.2: Calculated Mould Corner Heat Transfer

<table>
<thead>
<tr>
<th>Heat Number</th>
<th>Casting Speed (m/min)</th>
<th>Mould Corner Heat Transfer (kW/m$^2$)</th>
<th>Peak</th>
<th>Average</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>ICW</td>
<td>LSW</td>
<td>ICW</td>
</tr>
<tr>
<td>0.04 to 0.08 wt % Carbon Grades</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>25693</td>
<td>3.1</td>
<td>2200</td>
<td>3500</td>
<td>941.9</td>
</tr>
<tr>
<td>46307</td>
<td>3.5</td>
<td>2700</td>
<td>3900</td>
<td>949.9</td>
</tr>
<tr>
<td>46308</td>
<td>3.5</td>
<td>2500</td>
<td>3600</td>
<td>939.5</td>
</tr>
<tr>
<td>46309</td>
<td>3.6</td>
<td>2000</td>
<td>2700</td>
<td>913.8</td>
</tr>
<tr>
<td>0.70 to 0.83 wt % Carbon Grades</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>25689</td>
<td>3.6</td>
<td>2500</td>
<td>4900</td>
<td>969.5</td>
</tr>
<tr>
<td>25701</td>
<td>3.9</td>
<td>2100</td>
<td>3000</td>
<td>970.5</td>
</tr>
<tr>
<td>46302</td>
<td>3.0</td>
<td>2300</td>
<td>3900</td>
<td>968.3</td>
</tr>
<tr>
<td>46304</td>
<td>3.5</td>
<td>2400</td>
<td>3900</td>
<td>976.6</td>
</tr>
<tr>
<td>46318</td>
<td>4.4</td>
<td>3300</td>
<td>3700</td>
<td>1003.7</td>
</tr>
<tr>
<td>46319</td>
<td>4.4</td>
<td>2500</td>
<td>3300</td>
<td>970.9</td>
</tr>
</tbody>
</table>

Note: Heat 46309 contained 0.006 wt %B.
Figure 6.11: The mould heat transfer profiles at the ICW and LSW junctions of the corner show that the LSW edge of the corner had higher temperatures due to improved contact between the strand and mould just below the meniscus.

Figure 6.12: The average mould heat transfer profiles of a high and low plain carbon grade are shown at the meniscus region of the ICW-LSW mould corner.
6.3 Effect of Operating Variables on Mould Heat Transfer Response

Knowledge of how the heat transfer profile in the mould is affected by specific process variables is important in the design and operation of continuous casters. The impact of the process variables studied in the plant trial – including casting speed, metal level control, steel composition, superheat, water flow rate, and shroud type – on the mould heat transfer profile has been assessed through a comparison of 14 of the trial heats (Table 4.4). In each case, two optimal heats, which differed in only the process variable being evaluated, were selected for comparison purposes. The heats were compared with blocks of steady-state data, in which all but one of the process parameters remained constant, to assess the effect of the operating variable in question on the heat transfer profile. The heat transfer profile assessments were based on differences in heat transfer behaviour and magnitude.

6.3.1 The Effect of Casting Speed

The effect of casting speed was evaluated based on casting speed changes of 3.1 to 3.4 m/min, 3.5 to 3.9 m/min, 3.0 to 3.9 m/min, and 3.0 to 4.4 m/min. For the first two comparisons, the effect of casting speed was either ambiguous or any observed differences in the heat transfer profiles were within the local standard deviations. The local heat transfer standard deviations were around 700 kW/m² near the meniscus and 300 kW/m² at the bottom of the mould. However, when a more significant difference in casting speed occurred, as in the last two comparisons, there were significant increases in the heat transfer profile, near the meniscus region, of 900 to 1000 kW/m² (Figure 6.13). Figure 6.13 clearly shows that the heat transfer drop at the bottom of the ICW was much more pronounced at high casting speed. On average, the drop was 600 kW/m² at a casting speed of 4.4 m/min, indicating that casting speed had a very strong effect at the bottom of the mould. The overall effect was that the average mould wall heat transfer increased by about 400 kW/m², for a casting speed range of 3.0 to 4.4 m/min (or 286 kW/m²/m/min in this particular high carbon grade comparison). This was a significant
amount, as the maximum standard deviation was 295 kW/m$^2$ for the average mould wall heat transfer.

The general effect of casting speed on all of the analysed heats is shown in Figure 6.14. In these casting speed ranges, the calculated average rate of changes of heat transfer with casting speed were 273 kW/m$^2$ / m/min for the high carbon grades (3.0 to 4.4 m/min), and 85 kW/m$^2$ / m/min for the low carbon grades (3.1 to 3.5 m/min). These were much lower than the published rates of change in average heat transfer for conventional casting (Figure 2.5). It must be realized that these rates of change are valid only for the range of specified casting speeds. But, clearly, much larger changes in casting speed are required in high speed casting in order to effect the same amount of increase in the average mould wall heat transfer.

Figure 6.13: The effect of casting speed (3.0 to 4.4 m/min) on the mould midface heat transfer profile is shown in a high carbon grade. The effect of casting speed was evident in the heat transfer profiles only for a casting speed change greater than 0.5 m/min.
6.3.1.1 Tundish Weight and Nozzle Size

Adjustments in the casting speed were effected during a heat through the modification of the tundish weight. For a large change in casting speed over the duration of a sequence, the tundish metering nozzle was changed from 15.5 to 17.5 mm in diameter prior to casting. The effect of casting speed was investigated via both of these mechanisms.

The 17.5 mm metering nozzle allowed higher casting speeds to be achieved than was previously possible through an increase in the tundish weight alone. However, the heats cast with this larger nozzle (all of which were high carbon grades) had significantly higher metal level fluctuations (Figure 6.15), particularly when combined with higher tundish weights. The scatter observed with the 17.5 mm diameter tundish nozzle at high tundish weights, emphasizes the variability induced in the process by this particular method of casting speed control. Increasing the tundish weight in combination with the 15.5 mm diameter nozzle had no effect on the metal level fluctuation.
In addition to increasing the metal level fluctuation, increasing the diameter of the nozzle, from 15.5 to 17.5 mm, increased the mould water thermal standard deviations at the outlet of the mould (Figure 6.16). The 17.5 mm diameter tundish nozzle also increased the thermal standard deviation of the mould wall near the meniscus (at the 211 mm thermocouple). The two heats (46302 and 46309) which did not follow the trend had excessive deviations due to start-up transients during the initial cast, and a 0.006 wt %B addition, respectively.

Figure 6.15: The effect of casting speed, via tundish weight and tundish nozzle size, on the metal level standard deviation in the high carbon grades. The 17.5 mm diameter tundish nozzle increased the metal level fluctuation, particularly when combined with higher tundish weights. The 15.5 mm diameter tundish nozzle had no effect.
Figure 6.16: Increased casting speeds, via the 17.5 mm diameter tundish nozzle, increased the thermal standard deviation of the mould water outlet temperature in the low and high carbon grades.

Figure 6.17: Increased casting speeds, via the 17.5 mm tundish nozzle, increased the thermal standard deviation of the mould wall at the meniscus region thermocouple (211 mm) in the low and high carbon grades. Heats 46302 and 46309 were exceptions, due to start-up transients (46302) and the addition of boron (46309).
6.3.1.2 Casting Speed Transients

During a period in which the casting speed continually increased or decreased in a heat, the metal level standard deviation was slightly higher than the metal level standard deviation for a period in which the casting speed was stable. This was true regardless of if the transient occurred from a low casting speed to a higher one, or vice versa. The 9 heats (25689, 25693, 25700, 25704, 25705, 46303, 46304, 46319 and 46317) which had increasing or decreasing casting speed transients of up to 0.8 m/min, experienced an increase in the metal level standard deviation of 0.4 to 1.5 mm. As the metal level standard deviations for stable casting speeds ranged from 3.0 to 4.4 mm, these transients could raise the standard deviations up to 5.9 mm. As the magnitude of the metal level standard deviation could not be linked to the severity of mould-related billet defects, due to a lack of sufficient data, it is unknown what the impact of the transient casting speed changes within about ±1 m/min would be at Company H. Previous work at UBC, however, has determined that metal level fluctuations in the range of 5 to 10 mm can result in a local change of the mould wall temperature of 5 to 25 °C [47], thus leading to variations in the solidification of the billet shell.

6.3.2 The Effect of Metal Level Control

The metal level fluctuated up to 4.4 mm around the nominal values of 163 and 134 mm for the low and high carbon grades, respectively. The metal level fluctuations were found to have no influence on the magnitude of the mould water heat transfer, but manifested themselves in the mould in two ways. First, as variations in the response of the mould across the various mould faces, and second, as local mould thermal standard deviations. These can be examined more closely in terms of mould heat transfer rates.
6.3.2.1 Variability of Heat Transfer Between Mould Walls

The average mould wall heat fluxes listed in Table 6.1 clearly show that there was some deviation in the response of the mould around its periphery. These deviations could not be attributed to machine misalignment, due to the lack of a consistent machine bias in the magnitudes of the average mould heat fluxes at each face. As well no significant physical deviations were found in the mould taper construction prior to the trial in separate internal dimensional checks performed by Company H and during the retrofit of the mould. The process control, in terms of mould oscillation, was also well-controlled. Therefore, the variability seen across the mould faces were, in part, attributable to the metal level standard deviation.

The variability of the heat transfer across the mould walls was found to increase with an increase in casting speed, with the exception of two heats (Figure 6.18). As will be shown in the analysis, this variability was attributed to the strong effect of casting speed at the bottom of the mould. Heat 25693, which was an exception, had a high heat transfer variation between the mould walls due to a combination of high metal level standard deviations, low tundish weight, and gap formation\(^v\) at the bottom of the mould. Heat 46308 also had a high variation between the mould walls which could not be linked to metal level fluctuations, tundish weight, superheat, or mould-strand interaction. An analysis of the effect of carbon content showed that it had no conclusive effect on the variability of the response of the different mould walls. Superheat appeared to increase the heat transfer variability across the mould walls when the 15.5 mm tundish nozzle was used, again with the exception of heats 25693 and 46308 (Figure 6.19). There was insufficient data to make a clear assessment of the effect of superheat when the 17.5 mm tundish nozzle was used. It is likely that this larger tundish nozzle diameter introduced instabilities into the process which caused consistently higher variations across all superheats.

\(^v\) As will be shown in Chapter 8.
Figure 6.18: The variation between the average heat transfer at each mould wall tended to increase with casting speed. Heats 25693 and 46308 were exceptions due to high metal level standard deviations (25693) and unexplained reasons (46308).

Figure 6.19: The variation between the average heat transfer at each mould wall tended to increase with superheat, with the 15.5 mm tundish nozzle only. Heats 25693 and 46308 were exceptions due to high metal level standard deviations (25693) and unexplained reasons (46308).
6.3.2.2 Mould Heat Transfer Standard Deviation

The effect of casting speed on the mould heat transfer standard deviation was assessed using the local heat transfer profile variability at each longitudinal node, as well as the average heat transfer standard deviation of the entire heat. These two standard deviation categories are important in assessing the significance of the differences in the response of the mould as effected by a change in the operating variables.

6.3.2.2.1 Mould Heat Transfer Profile Variability

The variability of the heat transfer profile was quantified with local heat transfer standard deviations at the calculated longitudinal nodal positions. The minimum and maximum mould heat transfer profiles were calculated in the mould heat transfer model using the average minimum and maximum thermocouple measurements. Subsequently, the standard deviations were found to be up to 800 kW/m² at the meniscus region in both the low and high carbon grades. In both grades, an increased casting speed tended to decrease the heat transfer variability, particularly at the meniscus region. These results were observed consistently across each mould wall face (Figure 6.20). It is likely that the thinner and weaker shells produced at high casting speeds allowed more consistent contact between the strand and mould through the action of the ferrostatic pressure of the liquid core.
Figure 6.20: The effect of casting speed in decreasing the heat transfer standard deviation at the meniscus is shown in two low carbon grades cast at 3.1 and 3.4 m/min.
6.3.2.2 Average Mould Wall Heat Transfer Variability

The average mould wall heat transfer standard deviation ranged from about 170 to 250 kW/m² at casting speeds of 4.3 to 3.0 m/min, respectively (Figure 6.21). This demonstrates that as the casting speed increased, the standard deviation of the mould heat transfer decreased. Heat 46309 was an exception due to the addition of 0.006 wt % boron. Furthermore, these mould heat transfer standard deviations were found to be relatively insensitive to carbon content. The average heat transfer standard deviation increased only 10 kW/m² from a low carbon grade (heat 25693) to a high carbon grade (heat 46302) when cast at about 3.0 m/min.

Figure 6.21: The average mould wall heat transfer standard deviation decreased for an increase in casting speed. Heat 46309 did not follow the trend due to the addition of 0.006 wt %B.
6.3.3 The Effect of Carbon Content

The effect of carbon content was analysed using 6 low and high carbon grade heats. On average, the higher carbon content heats exhibited increased heat transfer variability just below the meniscus and generally higher heat fluxes elsewhere in the mould (Figure 6.22). The heat transfer variability was evident in the assessment of the magnitudes of the heat transfer peaks. In the average heat transfer profiles shown, the peaks increased by 975 to 1200 kW/m² near the meniscus for an increase in carbon content. The peak mould heat transfer was even more pronounced at individual mould walls, as seen in Figure 6.4, where it increased by 1325 to 1500 kW/m² (30 to 35%) for an increase in carbon content from a low to high carbon grade. This is well above the local standard deviations (100 to 800 kW/m²) near the meniscus. The tendency for higher peaks showed that as the carbon content was increased, the heat transfer became more erratic, particularly within a 200 mm range below the metal level. This is clearly evident in Figure 6.22, where there were numerous peaks of significantly larger magnitude. There were some exceptions to this trend, namely, all of the ICW heat fluxes for the lower carbon content heats had higher peaks than at any other mould wall, as discussed in Section 6.2.1 (p.64), where a mechanism was proposed to describe this behaviour.

The heats with higher carbon contents not only had increased heat transfer peaks, but they also showed a clear increase in the average mould wall heat transfer of 250 to 700 kW/m² (15 to 41%). The corresponding average heat transfer standard deviations were between 210 to 295 kW/m². As discussed previously, the increased heat transfer of higher carbon grades was related to the increased freezing range, and hence reduced solidification shrinkage. Although it appeared that there was an increase in heat transfer between the 0.75 wt %C grades and the 0.8 wt %C grades (Figure 6.14), the differences were due to the increase in casting speed with the larger tundish metering nozzle rather than a composition effect.
Figure 6.22: Higher carbon contents increased the mould heat transfer, particularly near the meniscus, as seen in the average mould heat transfer profiles of these heats.
6.3.4 The Effect of Boron Addition

At Company H, boron was added for the purpose of lowering the as-cast tensile strength below 380 kPa (55 kpsi) when the ratio of boron to nitrogen was in the range of 0.8 to 1.2. Although the chemistries at the ladle metallurgy station (Table 4.5) had low nitrogen contents, sufficient nitrogen was entrained at the tundish stream to meet this criterion.

Heats 46307 and 46309 were examined to determine the effect of the addition of 0.006 wt %B in the 0.04 and 0.05 wt %C grades, respectively. From a comparison of the heat transfer at the curved mould walls (ICW, OCW), the heat transfer magnitudes within a 100 mm band below the meniscus were found to be suppressed in the boron grade by 1000 to 3000 kW/m$^2$. There was no significant effect of the boron addition at the straight mould walls due to the locally high heat transfer standard deviations in the meniscus region (900 to 1300 kW/m$^2$). Below the meniscus region (from 300 to 1000 mm), the addition of boron increased the local heat transfer magnitudes by up to 800 kW/m$^2$ at all faces of the mould, which was well above the local standard deviations of 200 kW/m$^2$. The effect of these changes on the total heat transfer was minimal, as the average mould wall heat transfer increased by 190 kW/m$^2$, which was within the standard deviation of 260 kW/m$^2$.

Another interesting aspect of the boron addition was that the meniscus-level standard deviations of the thermal and heat transfer profiles were much higher than without the addition. The peak thermal standard deviations increased by 3.5 to 13 °C with the boron addition. At the meniscus of the mould corners, the boron addition raised the thermal standard deviations by 4.3 to 6.4 °C. These increased thermal standard deviations were indicative of metal level fluctuations. Indeed, with the boron addition, the metal level standard deviation was 4.4 mm, as compared to the 2.8 mm without the addition. As well, the heat transfer standard deviations of the boron grade were up to 1300 kW/m$^2$ at the meniscus, compared to 500 kW/m$^2$ for the plain carbon grade.
Figure 6.23: The addition of 0.006 wt %B to a 0.05 wt %C grade, caused a suppression of the heat transfer peaks at the meniscus, and an increase in the heat transfer magnitudes for the remainder of the profile at the inside and outside curved walls.
6.3.5 The Effect of Superheat

The effect of superheat was analysed in each carbon grade using a total of 6 heats for superheats in the range of 16 to 42 °C. The heat transfer profiles at each mould wall were found to be very similar for the different superheats. The increase in the total average mould heat transfer, with an increase in superheat of 16 to 42 °C, was 171 kW/m², which was within the average heat transfer standard deviation of ~210 kW/m². Therefore, as in conventional casting, the effect of superheat had a negligible impact on the response of the mould heat transfer profile at high casting speeds. However, the metal level variability analysis did show that superheat tended to increase the variation in the responses between the mould walls (Figure 6.19). This was likely due to the effect of superheat on the solidification characteristics of the billet shell, combined with the gap size and the interaction with the mould wall, thus causing differences in the recorded response at each face of the mould.

6.3.6 The Effect of Water Flow Rate

The effect of water flow was examined using 3 heats in which the water flow rate was reduced from 20 to 15 m/s (2200 L/min to 1700 L/min) during the last half of the heat. As in conventional casting, these water flow changes had no impact on the heat transfer profile. As well, the insensitivity of the heat transfer profile to these changes in water flow indicated that the water leak in the plenum divider seal, which was discovered after the plant trial, had no effect. The water velocities in the baffle tube, even with the bypassed water flow, therefore exceeded the critical 10 to 12 m/s flow rate that would prevent nucleate boiling. On average, the total heat transfer varied up to 62 kW/m² for these changes in water velocity, which was well within the range of the average heat transfer standard deviations for these heats (195 to 250 kW/m²).
6.3.7 The Effect of Tundish Shroud Type

The effect of the Bellows shroud versus the Pollard shroud was examined using heats 46307 and 46308, respectively. These heats were 0.04 wt %C grades, with all other casting variables constant. Comparisons of these two heats revealed that the changes in the local heat transfer magnitudes and the average mould wall heat transfer were within the standard deviations. The local heat transfer standard deviation was 2200 kW/m² in the meniscus region and 300 kW/m² lower in the mould. Overall, the mould heat transfer decreased by only 27 kW/m² when the Bellows shroud was installed. As these were relatively small changes in the total mould response, the shroud type had a negligible effect on the heat transfer profile.
CHAPTER 7: BILLET SOLIDIFICATION ANALYSIS

The previous mould analysis has revealed that carbon content and casting speed had a significant impact on the response of the mould during the casting of plain carbon grades. The design and operation of the caster, for the purpose of producing high quality billets at high casting speeds, also requires an analysis of the billet behaviour in the mould. In particular, the billet solidification profile has an important relationship with the heat transfer in the mould. Solidification shrinkage is one of the main contributing factors to the width of the gap between the mould wall and solidifying strand (the other factor being the mould distortion). As discussed in the literature review, the gap that it creates provides a strong resistance to heat flow, thus giving the heat transfer in the mould its very distinct profile.

In describing the characteristics of the solidifying billet – including shell growth, temperature and shrinkage – the mathematical billet solidification model was used to translate the mould heat transfer into a billet thermal profile using principles of heat conduction and convection.

7.1 Mathematical Billet Thermal Model

A 2-dimensional, transient heat conduction model was used to predict the temperature across a transverse slice of the strand using the average heat transfer profiles obtained in the mould model. Previous work has shown that the technique of analysing incremental strand slices is a reasonable approximation, as the axial conduction in the mould is negligible compared to the longitudinal convective extraction of heat at the water channel. This convective heat transfer was applied as a boundary condition to the strand model in the form of the mould heat transfer profile. The heat conduction equation was solved using a mathematical billet solidification program, SHRINK [18,28,34], which was coded in FORTRAN. The solution path involved the application of the ADI-FDM discretizing technique, with the appropriate initial condition and boundary conditions, to
solve the 2-dimensional transient heat conduction equation and predict the billet
temperature at any point in a transverse slice of the billet. This computation process
effectively linked the billet and mould mathematical models. All further solidification
calculations were based on these billet temperatures, and were thus average values which
did not reflect local effects on the shell development, such as surface defects.

A time step of 0.05 seconds and a nodal system of 100 x 100 nodes were used to
model a quarter slice of a transverse section of the strand as it was incremented down the
mould. A time step of 0.01 seconds was found to have little effect (0.05 mm) on the
predicted solidification shell thickness however, it significantly increased the
computation time. The stepped appearance of the calculated shell thickness profiles was
found to be due to the nodal mesh used in the FDM model and was not a physical result
of the solidification. However, a mesh of 150 x 150 did not provide significantly
different results, and a 200 x 200 mesh caused the program to become unstable.
Therefore a time step of 0.05 seconds and a mesh of 100 x 100 nodes were used in the
FDM model.

The following conditions were assumed in the billet SHRINK model:

1. The four quadrants of any transverse slice of the billet behaved identically, due to
   symmetry.
2. The phases present at any time during the solidification of the billet were assumed
to follow the Lever Rule of a linearized low-alloyed Fe-C phase diagram.
3. The billet solidification shrinkage was assumed to consist of thermal contraction
due to cooling and volumetric shrinkage due to phase transformations.
4. The effect of ferrostatic pressure was ignored.
5. The mechanical behaviour (binding and friction) of the solidified shell was
   neglected. The model did not include stress-strain analysis or creep.
6. Convection in the liquid pool was modelled through a modification factor of 7,
applied to the liquid metal thermal conductivity. This value was adopted from the
   literature [75,89].
7. The latent heat of fusion was modelled through equilibrium freezing, wherein the
   temperature-dependent specific heat of steel (Cp) was increased within the
   liquidus to solidus temperature range.
8. The total heat transfer coefficient between the billet surface and the water channel
   was assumed constant across the transverse face of the mould at any given
distance down the length of the mould.
The last assumption allowed for the variation of heat transfer across the face of the mould, thus correcting the assumed uniform heat transfer across the transverse faces in the mould model (Section 6.1, p.61, Assumption 2). While the assumption of a constant transverse heat transfer coefficient is also not entirely accurate, due to corner effects, it is a fairly good assumption with the close tolerances that can be achieved with explosion-formed baffle tubes. The remaining details of the billet model have been described in detail elsewhere [3,18,28,34].

7.2 Validation of Model-Predicted Results

The billet FDM model has been validated with measured industrial data [18,28] and a Finite Element Model (FEM) using the commercially available ABAQUS software [33]. The FEM model employed a “viscoplastic relationship” to describe the more complex mechanical and material behaviour of the solidifying strand. An evaluation of this model, compared to the model described in this work, found that the models were the most similar at larger shell thicknesses (12 mm), deviating by up to 0.2 mm. At smaller shell thicknesses (6.5 mm), the deviation was about 1.4 mm. Due to the complicating factors of thermophysical behaviour (thermal straining, plasticity) incorporated in the FEM model, the results of the billet model were not consistently larger or smaller than predicted by the FEM model [33]. Informal feedback from industry, however, has shown that the UBC recommended tapers, calculated using the FDM mould and billet models, have been consistently on the conservative side [90]. This would indicate that the predicted mould heat transfer is too low (which has been shown), the predicted solidification shrinkage is too low, and/or the mould distortion calculation is too low.

7.3 Shell Formation in the Strand

The solidification of the strand was tracked using the calculated billet temperature distributions. A nodal position in the transverse billet slice was assumed to have solidified after it had dropped below the solidus temperature, thereby contributing to the
thickness of the solidifying shell. Therefore, all of the shell profiles and thicknesses given in the current chapter indicate the location of the solidus temperature at any given time. In tracking the series of nodes that solidified, the shell thickness at any point down the length of the mould was delineated. The rate of growth of the shell was constrained by the heat transfer through the mould. Therefore, any variables that influenced the heat extraction rate also had an impact on the growth of the shell. For this reason, carbon content and casting speed affected the development of the shell.

7.3.1 The Effect of Carbon Content

The average calculated mould exit shell thickness was 8.3 mm for the low carbon steels, 7.8 mm for the 0.71 to 0.74 wt %C steels, and 6.8 mm for the 0.81 to 0.83 wt %C steels. The apparent variation in the behaviour between the two high carbon grades was related to the difference in casting speed and was not due to composition. Figure 7.1 illustrates several calculated shell thickness profiles. The stepped appearance of the calculated shell thickness profiles in Figure 7.1 was due to the time step-wise iteration of the FDM model and was not a physical result of the solidification. The influences of superheat and casting speed were confounded in the figure, hence the 0.74 wt %C grade appeared to behave more like the 0.06 wt %C grade than the other high carbon grade. However, the results indicated that the shell thickness decreased for increasing carbon contents. This is explained by the low carbon grades having higher magnitudes of shrinkage, due to the solidification phase transformation, which caused a larger gap to form between the strand and the mould. Therefore, higher carbon grades had less shrinkage (due to smaller solidification gaps) and thinner shells. They also had higher rates of heat extraction in the mould.
Figure 7.1: The effect of carbon content on the predicted shell thickness profiles is shown for three heats cast at various casting speeds and superheats (SH). The stepped appearance of the shell is related to the FDM numerical solution technique and is not a physical phenomenon.
7.3.2 The Effect of Casting Speed

For increased casting speeds, the predicted shell thickness at the exit of the mould decreased, regardless of any other casting variable, as seen in (Figure 7.2). The scatter in the figure is indicative of the different carbon grades and superheats cast during the trial. The model predictions were compared to the theoretical parabolic growth law, and the predictions were found to be in very poor agreement when K-factors of 20 to 30 m/min$^{1/2}$ were used, as suggested by the literature. For a casting speed range of 3.0 to 4.5 m/min, the calculated theoretical shell thickness, using the parabolic law Kt$^{1/2}$, was in the range of 11 to 9 mm (K = 20 m/min$^{1/2}$) and 16 to 13 mm (K = 30 m/min$^{1/2}$). A qualitative assessment of the transverse billet samples revealed that the actual shell thicknesses were closer to the values calculated by the SHRINK program (Figure 7.2) than these theoretical calculations. However, when a K-factor of 16 m/min$^{1/2}$ was applied, the results were shown to be in better agreement, particularly when the heats cast with very high or low superheats were ignored (Figure 7.2). It therefore appears that the K-factor at Company H, when using the parabolic law in the form of Kt$^{1/2}$, was highly dependent on casting speed, section size AND superheat, unlike what is stated in the literature. As well, this K-factor of 16 m/min$^{1/2}$ is the same as quoted by Wolf and Kurz [76], although a much smaller value would have been expected for the larger billet size and casting speeds at Company H. These discrepancies indicated that either the casting conditions (high speed) and/or the mould design were not ideal for the application of the parabolic law. As will be shown in Chapter 8, the constant interfacial resistance assumption of the parabolic was particularly poor for the operation at Company H due to inadequate mould taper at the bottom of the mould.
As the parabolic law was applicable to conventional but not high speeds, it appeared that the decreased residence time, for casting speeds above 3 m/min, had a more drastic effect on the solidification characteristics (and hence the mould-strand interfacial resistance) than similar increases in casting speed in a conventional caster. This is particularly true, as the residence time in a high speed mould is reduced by the high casting speed, but not a comparably longer mould length. For example, a conventional mould of approximately 800 mm length casting at 2 m/min, would have 400 mm of length for each 1 m/min casting speed change (400:1). The high speed mould at Company H, on the other hand, would require a 1.2 m mould to achieve the same length-to-speed ratio at 3 m/min, and a 1.8 m mould at 4.5 m/min. This is a simplified calculation, as the metal level was not considered. However, this clearly shows that the solidification behaviour changed at high speeds.

![Graph showing predicted shell thickness versus casting speed](image)

**Figure 7.2:** Increased casting speed had the effect of reducing the predicted shell thickness at the mould exit of various carbon grade heats cast at various superheats. The model predictions were compared to the parabolic growth law, \( x = Kt^{\frac{1}{2}} \), with a K-factor of 16 m/min\(^{1/2}\) in a metal level range of 134 to 163 mm.
Figure 7.3 illustrates the effect of casting speed on the magnitude of the shell thickness in the strand as it progressed through the mould. The higher casting speeds of the higher carbon content grades reduced the residence times to such an extent that there was reduced opportunity for shell growth.

Figure 7.3: The effect of casting speed on the predicted shell thickness profiles is shown for high and low carbon grade heats cast at various superheats. The stepped appearance of the shell is related to the FDM numerical solution technique and is not a physical phenomenon.
7.3.3 The Effect of Superheat

Superheat was also expected to have an effect on the formation of the shell, although it did not have a significant influence on the heat transfer profile. The rate of shell solidification, as outlined in Chapter 2, is governed by the competing effects of superheat and heat extraction from the mould. From an overview of the analysed heats, as seen in Figure 7.4, increasing superheats led to thinner shells, regardless of changes in the other casting conditions. The scatter is a result of the confounded factor of casting speed, which inversely affected the shell thickness. This was evident in a comparison of the 0.71 to 0.75 wt %C and 0.80 to 0.83 wt %C grades, which had different casting speeds. Therefore, for any constant rate of heat transfer, a higher superheat inhibited the formation of the shell through an increase in the temperature of the steel. The effect of superheat on the development of the shell is seen in Figure 7.5.

![Figure 7.4: Increasing superheat had the effect of reducing the predicted shell thickness at the mould exit, for various carbon grades and casting speeds.](image-url)
Figure 7.5: The effect of superheat on the predicted shell thickness profiles is shown for high and low carbon grade heats cast at various superheats. The stepped appearance of the shell is related to the FDM numerical solution technique and is not a physical phenomenon.
7.4 Solidification Shrinkage

The accumulated shrinkage, due to the thermal contraction and/or phase transformation, was quantified through the coefficient of thermal contraction for steel, which was dependent on the carbon content, temperature, and the δ (ferrite) and γ (austenite) phase fractions. The calculated values for the coefficient of thermal contraction, at the calculated billet shell temperatures at the exit of the mould (~ 800 °C), were on the order of $2 \times 10^{-5}$ to $2.3 \times 10^{-5} \, {\text{°C}}^{-1}$ for the high and low carbon grades, respectively. The solidification shrinkage was quantified through a simple linear thermal contraction calculation of the predicted shell thickness at any given longitudinal position (node) in the mould. Furthermore, the solidification shrinkage of any transverse layer nodes in the mould depended on the shrinkage of the layers of nodes previously solidified above it. The resulting average calculated billet dimensions of the low and high carbon grades are illustrated in Figure 7.6. The effect of carbon content is clearly evident. Due to the higher solidification shrinkage of the lower carbon grades, the billet widths were the smallest for these grades.
Figure 7.6: The average calculated billet width profiles of the high carbon grades were wider than the low carbon grade due to the wider solidification range of the former.
CHAPTER 8: MOULD TAPER DESIGN

The mould tapers were designed on the basis of eliminating binding and gap formation between the strand and the mould, which occurred due to the cumulative effect of the billet shrinkage and the mould distortion. The assessment of the amount of billet and dynamic mould interaction for the conditions of the trial at Company H, required an analysis of the thermal response of the mould in relation to the hot billet dimensions.

8.1 Dynamic Mould-Strand Interaction

The dimensions of the dynamic mould (also called the working mould or hot mould), were approximated though a simple linear thermal expansion calculation from room temperature to casting temperature. Since the average midface mould wall temperature was assumed to be constant across the transverse face of each mould wall, the entire width of the mould was assumed to expand uniformly. Also, because the width dimension was significantly greater than the thickness of the mould, the distortion through the mould wall thickness was neglected.

The maximum calculated mould thermal distortion, in the region of the peak temperature, ranged from 0.2 to 0.4 mm. As expected, the low carbon heats and the heats cast at the lowest speeds had the least distortion. This was because these two conditions minimized the mould heat transfer, and hence resulted in the lowest mould temperatures.

A comparison of the hot billet profile and the distorted mould revealed locations where there was interference or loss of contact between the strand and the mould. Since the mathematical model did not consider any mechanical interaction that may have occurred between the strand and the mould, some of the predicted billet dimensions were larger than the calculated dynamic mould dimensions. This situation indicated that the strand was binding in the mould during casting. On the other hand, if the calculated billet width was smaller than the hot mould dimension, this indicated that a gap formed
between the strand and mould. From the billet analysis of the analysed heats (listed in Table 4.4), all of the high carbon heats, as well as three of the low carbon heats (25691, 25693 and 46309), were calculated to have gap formation in the mould. The gap formation started near the top of the mould below the meniscus, and continued toward the mould exit (Figure 8.1). The maximum calculated gap size was about 0.2 to 0.3 mm for the low carbon grades, and 0.1 mm for the higher carbon grades. The remaining three low carbon heats exhibited both binding and gap formation, typically at the top and bottom halves of the mould, respectively (Figure 8.2). The degree of binding was within 0.1 mm and the gap size was within 0.2 mm. The transition point from binding to gap formation was at approximately 500 mm from the top of the mould. While some degree of binding is required for good contact and adequate heat transfer between the strand and the mould, excessive binding can lead to mould wear, surface defects, roughness and breakouts, as discussed previously. The correlation between the calculated binding and the nature of the observed defects in the Company H billets cannot be disclosed due to a confidentiality agreement.

The significant occurrence of calculated gap formation in the bottom of the mould in the analysed heats indicated the internal width of the trial mould was too large and the mould design required steeper mould tapers. This gap formation accounts for the uncertainties that occurred in applying the parabolic shell growth law to the high speed casting process at Company H. The applicability of the parabolic growth equation to high speed casting could therefore not be determined for the data taken during this plant trial.
Figure 8.1: A gap was predicted to have formed between the strand and mould in this high carbon grade. The gap was calculated to have formed near the top of the mould and proceeded to widen toward the exit of the mould.

Figure 8.2: In this low carbon grade, binding was predicted to occur at the top of the mould and gap formation was predicted to occur at the bottom. The calculated transition point was about 500 mm from the top of the mould.
8.2 Redesigned Mould Tapers

New mould tapers were calculated based on the heats analysed (Table 4.4) for each carbon grade, at their respective casting speeds. The recommended mould tapers for Company H are presented in Table 8.1 and Table 8.2, for the low and high carbon grades, respectively. The corresponding calculated mould taper profiles (from the meniscus to the mould exit) are depicted in Figure 8.3 and Figure 8.4. Clearly, the aggressive taper design at the bottom of the mould will eliminate the gap formation that was calculated to have occurred.

Table 8.1: Mould Taper Designs for Low Carbon Grades at Various Casting Speeds

<table>
<thead>
<tr>
<th>Taper Breakpoint (mm)</th>
<th>Taper (%/m)</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Casting Speed = 3.0 m/min</strong></td>
<td></td>
</tr>
<tr>
<td>0 – 114</td>
<td>0</td>
</tr>
<tr>
<td>114 – 302</td>
<td>2.70</td>
</tr>
<tr>
<td>302 – 501</td>
<td>1.85</td>
</tr>
<tr>
<td>501 – 700</td>
<td>1.37</td>
</tr>
<tr>
<td>700 – 1000</td>
<td>0.64</td>
</tr>
<tr>
<td><strong>Casting Speed = 3.5 m/min</strong></td>
<td></td>
</tr>
<tr>
<td>0 – 114</td>
<td>0</td>
</tr>
<tr>
<td>114 – 301</td>
<td>1.88</td>
</tr>
<tr>
<td>301 – 702</td>
<td>1.47</td>
</tr>
<tr>
<td>702 – 1000</td>
<td>0.56</td>
</tr>
<tr>
<td><strong>Casting Speed = 4.0 m/min</strong></td>
<td></td>
</tr>
<tr>
<td>0 – 114</td>
<td>0</td>
</tr>
<tr>
<td>114 – 500</td>
<td>1.91</td>
</tr>
<tr>
<td>500 – 800</td>
<td>1.17</td>
</tr>
<tr>
<td>800 – 1000</td>
<td>0.48</td>
</tr>
<tr>
<td><strong>Casting Speed = 4.5 m/min</strong></td>
<td></td>
</tr>
<tr>
<td>0 – 114</td>
<td>0</td>
</tr>
<tr>
<td>114 – 500</td>
<td>1.93</td>
</tr>
<tr>
<td>500 – 800</td>
<td>0.98</td>
</tr>
<tr>
<td>800 – 1000</td>
<td>0.44</td>
</tr>
</tbody>
</table>
Table 8.2: Mould Taper Designs for High Carbon Grades at Various Casting Speeds

<table>
<thead>
<tr>
<th>Taper Breakpoint (mm)</th>
<th>Taper (%/m)</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Casting Speed = 3.0 m/min</strong></td>
<td></td>
</tr>
<tr>
<td>0 – 114</td>
<td>0</td>
</tr>
<tr>
<td>114 – 239</td>
<td>3.27</td>
</tr>
<tr>
<td>239 – 441</td>
<td>1.43</td>
</tr>
<tr>
<td>441 – 736</td>
<td>0.86</td>
</tr>
<tr>
<td>736 – 1000</td>
<td>0.37</td>
</tr>
<tr>
<td><strong>Casting Speed = 3.5 m/min</strong></td>
<td></td>
</tr>
<tr>
<td>0 – 114</td>
<td>0</td>
</tr>
<tr>
<td>114 – 239</td>
<td>3.13</td>
</tr>
<tr>
<td>239 – 500</td>
<td>1.53</td>
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<tr>
<td>500 – 800</td>
<td>0.77</td>
</tr>
<tr>
<td>800 – 1000</td>
<td>0.21</td>
</tr>
<tr>
<td><strong>Casting Speed = 4.0 m/min</strong></td>
<td></td>
</tr>
<tr>
<td>0 – 114</td>
<td>0</td>
</tr>
<tr>
<td>114 – 300</td>
<td>2.53</td>
</tr>
<tr>
<td>300 – 498</td>
<td>1.69</td>
</tr>
<tr>
<td>498 – 773</td>
<td>0.67</td>
</tr>
<tr>
<td>773 – 1000</td>
<td>0.29</td>
</tr>
<tr>
<td><strong>Casting Speed = 4.5 m/min</strong></td>
<td></td>
</tr>
<tr>
<td>0 – 114</td>
<td>0</td>
</tr>
<tr>
<td>114 – 299</td>
<td>2.68</td>
</tr>
<tr>
<td>299 – 500</td>
<td>1.48</td>
</tr>
<tr>
<td>500 – 751</td>
<td>0.45</td>
</tr>
<tr>
<td>751 – 1000</td>
<td>0.20</td>
</tr>
</tbody>
</table>
Figure 8.3: The calculated mould taper profiles, for plain 0.04 to 0.06 wt %C steels cast at various speeds, are shown for a final cold billet dimension of 120 x 120 mm$^2$. The trial mould was designed for a plain 0.8 wt %C grade cast at 4.0 m/min.

Figure 8.4: Calculated mould taper profiles, for plain 0.71 to 0.83 wt %C steels cast at various speeds, are shown for a final cold billet dimension of 120 x 120 mm$^2$. The trial mould was designed for a plain 0.8 wt %C grade cast at 4.0 m/min.
Due to the lack of data for low carbon grades cast at speeds of 4.0 and 4.5 m/min, the corresponding mould tapers were calculated based on a scaling of the existing high carbon data. Four high and low carbon grade heats were compared at similar casting speeds (3.1 and 3.4 m/min) to obtain a divisor profile that would be used to translate the high carbon heat transfer profiles into equivalent low carbon heat transfer profiles. In this way, the high carbon grade heat transfer profiles were scaled into low carbon heat transfer equivalents at high casting speeds. These effective low carbon heat transfer profiles were subsequently used in the billet model to calculate an effective low carbon taper design. The effective low carbon taper designs were verified against the “true” experimental low carbon taper designs predicted by the traditional billet model method. At casting speeds of 3.1 and 3.5 m/min, the scaling technique was found to produce similar, but slightly more aggressive mould tapers than the calculated values obtained from the experimental low carbon data (Figure 8.5). This is because the effect of carbon content has a stronger effect at higher casting speeds (Figure 2.5 and Figure 6.14). Therefore the scaling (divisor) profile used to translate a high carbon grade into an effective low carbon grade, for casting speeds below 3.5 m/min, should actually have been more aggressive at higher casting speeds. As a result, this scaling technique overestimated the magnitude of the effective low carbon grade heat transfer profile at higher casting speeds. The resulting taper calculation allowed for more shrinkage, and was hence more aggressive than the tapers that would have been predicted by the billet model, should the high speed data have been available. Therefore, this technique should not be used for large variations in casting speed, such as scaling between 3.0 to 4.5 m/min. However, as informal feedback from previous industrial trials has indicated that the UBC recommendations were too conservative, a slightly more aggressive taper design should be more appropriate.
Figure 8.5: A validation of the scaling technique, used to calculate low carbon grade mould tapers at high casting speeds, revealed that the scaling approximation was slightly aggressive at 3.5 m/min, compared to calculations based on experimental low carbon data.

It is clear from the calculated mould taper profiles that the recommended high carbon grade tapers are relatively insensitive to changes in casting speed from 3.0 to 4.0 m/min. At higher speeds the bottom half of the mould is the most sensitive, due to the reduced residence times in the mould that allow less solidification and shrinkage to occur, requiring a comparatively wider mould (shallower tapers) to compensate. Therefore, the recommended high carbon taper designs appear to be tolerant of casting speed changes from 3.0 to 4.0 m/min and are most sensitive to increases in casting speeds above this range rather than decreases.

The sensitivity of the low carbon grade tapers cannot be assessed uniquely, as two of the designs (4.0 and 4.5 m/min) were calculated using the heat transfer scaling technique. It would be expected, however, that the low carbon grade tapers, would show a stronger dependence on casting speed at the lower end of the scale (3.0 m/min). As
seen in Figure 8.3, this indeed is the case, even though two of the designs (4.0 and 4.5 m/min) were approximated with the scaling technique. From a casting speed of 3.0 to 3.5 m/min, the quadruple tapered design translated into a triple tapered design and both designs were calculated with the billet model, based on experimental low carbon data. This clearly indicated a change in the solidification behaviour and mould-strand interaction occurred between these casting speeds. This sensitivity to low casting speed can be attributed to the incompatibility of the current mould to low carbon grades and low casting speeds. As the trial mould was designed for high carbon grades at high speed (4.0 m/min), dynamic mould dimensions could not accommodate the relatively large shrinkage that occurred during the casting of low carbon grades at low speeds. The lower casting speeds increased the residence time in the mould, allowing thicker shells to solidify with a higher solidification shrinkage for the taper to overcome. The design configuration for a low carbon grade cast at low speeds would therefore require steeper tapers to improve the surface contact between the strand and mould. The design specifications of the current mould (high carbon grade at high casting speed) provided for little solidification shrinkage, and thus resulted in a relatively wide mould since both of the design conditions inhibit shell formation. Since lower carbon grades and slower casting speeds promote shell growth, and hence shrinkage, the casting of such a heat in the trial mould would allow larger gaps to form between the strand and the mould. This would account for the expected higher sensitivity of the taper design to the casting speed for low carbon grades cast at low speeds.

As illustrated in Figure 8.6, the recommended low and high carbon mould tapers are significantly different. This results from the differences in mould-strand interaction between the grades, as calculated from the billet model. As discussed previously, half of the analysed low carbon grades had binding in the upper of the mould and gap formation in the lower part of the mould. The higher carbon grades and the remaining low carbon grades, on the other hand, had consistent gap formation down the length of the mould. Clearly, these recommended designs, based on experimental data, show that a single mould cannot be designed for use with a range of carbon grades.
Figure 8.6: A comparison of the recommended low (LC) and high carbon (HC) mould taper designs revealed significant differences in the taper requirements. This showed that a single mould cannot be designed for the casting of multiple grades.
CHAPTER 9: COMPANY COMPARISONS AND DESIGN EFFECTS

Many comparisons can be made between the various experimental plant trials conducted by UBC since September 1991. Four of the most recent trials were conducted at high casting speeds (Companies C-2, F, G, and H), and the remaining trials were conducted at conventional casting speeds. In each trial, the thermal response of the mould was recorded, for the casting of different carbon grades under various casting conditions, using a similar method (as described in Chapter 4) to the trial conducted at Company H. The casting parameters that were varied within each trial, or across the trials, included carbon content, boron content, stainless steel grades, casting speed, superheat, water flow rate, tundish shroud type, lubrication media, electromagnetic stirring, and oil flow rate. Other significant differences included section size, mould taper, mould length and oscillation characteristics. The operating conditions of each company are listed in Table 9.1, and the cold profiles of selected trial moulds are shown in Figure 9.1, where the mould dimensions were normalized to a hypothetical billet dimension of 125 mm at the metal level. It should be noted that the conditions listed in Table 9.1 are not necessarily the normal operating practices, rather they are the conditions that existed during each plant trial, whether by design or inadvertently. As well, Companies A-1, A-2, C-1, D-1 and D-2 had pyrolysis of the oil lubricant due to the hot mould operation.

The comparison of the various companies that participated in the instrumented plant trials was useful to reveal general trends over a wide variety of casting conditions and caster designs. This chapter will provide a comprehensive evaluation of over a decade of trials conducted by UBC personnel as they related to the effects of speed casting and mould design.
Table 9.1: Operating Conditions During Various UBC Plant Trials

<table>
<thead>
<tr>
<th></th>
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<th></th>
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<th></th>
<th></th>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>Machine Type</td>
<td>Straight</td>
<td>Curved</td>
<td>Curved</td>
<td>Curved</td>
<td>Curved</td>
<td>Curved</td>
<td>Curved</td>
<td>Curved</td>
</tr>
<tr>
<td>Mould Material</td>
<td>DHP</td>
<td>DHP</td>
<td>Cu-Cr-Zr [DHP]</td>
<td>DHP</td>
<td>Cu-Cr-Zr</td>
<td>Cu-Cr-Zr</td>
<td>Cu-Cr-Zr</td>
<td>DHP+Ag</td>
</tr>
<tr>
<td>Section Size (mm$^2$)</td>
<td>152x152 [203x203]</td>
<td>125x125</td>
<td>140x140 [120x120]</td>
<td>208x208</td>
<td>127x178</td>
<td>114x114</td>
<td>127x178</td>
<td>120x120</td>
</tr>
<tr>
<td>Mld Length (mm)</td>
<td>735</td>
<td>813</td>
<td>835 [1000]</td>
<td>813</td>
<td>813</td>
<td>780</td>
<td>1016</td>
<td>1000</td>
</tr>
<tr>
<td>Metal Level (mm)</td>
<td>135 [152]</td>
<td>125</td>
<td>130 [100 - 141]</td>
<td>165 - 185</td>
<td>115</td>
<td>135</td>
<td>112</td>
<td>134 - 163</td>
</tr>
<tr>
<td>Metal Level Mould Taper (%/m)</td>
<td>0.71/0.85 [0.87/0.93]</td>
<td>2.8 Parabolic</td>
<td>0.4, Multi [2.6 - 2.15, Parabolic]</td>
<td>5 Parabolic</td>
<td>2.7 Dual Taper</td>
<td>1.2 Dual Taper</td>
<td>0 Parabolic</td>
<td>2.7 Quad Taper</td>
</tr>
<tr>
<td>Mould Wall Thickness (mm)</td>
<td>16 [19.8]</td>
<td>12.7</td>
<td>16 [12.7]</td>
<td>15.6</td>
<td>19</td>
<td>11</td>
<td>13</td>
<td>14.5</td>
</tr>
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<td>Channel Gap (mm)</td>
<td>3.175 [3.56]</td>
<td>4.760</td>
<td>3.175</td>
<td>4.990</td>
<td>3.175</td>
<td>5.08</td>
<td>4.763</td>
<td>3</td>
</tr>
<tr>
<td>Mould Corner Radius (mm)</td>
<td>3.175</td>
<td>3.175</td>
<td>3.175 [2.95]</td>
<td>3.175</td>
<td>3.175</td>
<td>3.175</td>
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</tr>
<tr>
<td>Mould Constraint</td>
<td>4-sided</td>
<td>4-sided</td>
<td>4-sided</td>
<td>4-sided</td>
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<td>4-sided</td>
<td>4-sided</td>
</tr>
<tr>
<td>Lubricant Type (Flow Rate, ml/min)</td>
<td>Oil, Canola [Oil/Flux] (125, 26)</td>
<td>Oil, Various (58)</td>
<td>Oil, Canola (55)</td>
<td>Oil, Canola (55)</td>
<td>Oil, Canola (55)</td>
<td>Oil, Canola (55)</td>
<td>Oil, Canola (55)</td>
<td>Oil, Canola (55)</td>
</tr>
<tr>
<td>Cast Speed (m/min)</td>
<td>1.9 [1.0]</td>
<td>2.0 - 2.5</td>
<td>1.9 [3.5]</td>
<td>1.15</td>
<td>1.65</td>
<td>2.3</td>
<td>2.4 - 2.8</td>
<td>3.0 - 4.5</td>
</tr>
<tr>
<td>Oscillation Stroke Length (mm)</td>
<td>12.7</td>
<td>9.5</td>
<td>11.2 [12 - 14]</td>
<td>6.0</td>
<td>6.4</td>
<td>6.9</td>
<td>9.525</td>
<td>12</td>
</tr>
<tr>
<td>Oscillation Frequency (cpm)</td>
<td>190, [160, 100]</td>
<td>120</td>
<td>144, 96</td>
<td>140</td>
<td>170, 130</td>
<td>222</td>
<td>132</td>
<td>160 - 240</td>
</tr>
<tr>
<td>Negative Strip Time (s)</td>
<td>0.13, [0.17, 0.25]</td>
<td>0.15</td>
<td>0.16, 0.19</td>
<td>0.15</td>
<td>0.12, 0.13</td>
<td>0.085 - 0.1</td>
<td>0.148</td>
<td>0.09 - 0.11</td>
</tr>
<tr>
<td>Mould Lead (mm)</td>
<td>8.2 [9.7, 8.1]</td>
<td>2.1</td>
<td>5.4, 3.1</td>
<td>2.4</td>
<td>2.3, 1.4</td>
<td>1.76</td>
<td>3.21</td>
<td>3.2 - 5.9</td>
</tr>
</tbody>
</table>
Figure 9.1: A comparison of the tapered mould profiles of the high carbon grade trial moulds, normalized to a hypothetical billet size (125 mm at the meniscus). The recommended casting speed for each mould is specified in the legend.
9.1 Mould Response

The various responses of the moulds used in each of the plant trials were not dependent only on the casting conditions during the heat. The design of the mould and the operating conditions under which the steel was cast were fundamental in determining how the mould responded to the particular casting conditions. The various effects – such as mould taper, mould length, mould lubricant, casting speed, superheat and carbon grade – could not be isolated individually, due to the many differing variables between the companies. However, comparisons of the thermal responses and heat transfer responses between the different companies were beneficial in recognizing the importance and impact of particular design variables and casting conditions.

9.1.1 Thermal Profiles

From the thermal profiles illustrated in Figure 9.2, it was clear that the mould design, in terms of taper and length, was very important in the response of the mould. In the cases illustrated, the thermal behaviour at the meniscus and down the length of the mould was highest for Companies A-1 and D-1. The shallow meniscus taper at Company A-1 led to mould-strand interaction (Figure 2.3), thus contributing to the high thermal response. Company D-1, on the other hand, had excessive taper which, as will be discussed in the next section, may have increased the mould heat transfer, and thus the mould temperature. All of the cases shown were cast in oil lubricated moulds.

The remaining thermal profiles had lower magnitudes, with peaks in the range of 142 to 170 °C and local meniscus-level thermal standard deviations of 8 to 14 °C. The thermal behaviour for the remainder of the mould differed dramatically for the various companies, due to the many differences in the casting operations, particularly the taper design. These differences will be discussed in more detail in Section 9.1.2 (p.122) in terms of the heat transfer.
Figure 9.2: A comparison of the measured mould thermal profiles is shown for the high carbon grades of 5 companies at various casting speeds. The high mould temperatures at Company A-1 resulted from inadequate taper and non-uniform water velocity in the baffle tube. It is proposed that the high mould temperatures at Company D-1 were caused by excessive initial taper. All the cases shown were cast with oil lubrication.
Of the companies shown in Figure 9.3, Companies F and G had the highest thermal standard deviations in the meniscus region. This was due to the shallow taper and non-uniform mould flux distribution at Company F, and the high metal level fluctuations and zero meniscus taper at Company G. Company H had the lowest thermal standard deviations at the meniscus region, but higher thermal standard deviations for the rest of the mould. The increased thermal standard deviations below the meniscus were likely caused by the insufficient taper of the trial mould, combined with the higher casting speeds. The mould-strand gap that would have been formed by this inadequate taper design would have been very dynamic in nature, due to the weak shell structure and ferrostatic pressure. The ferrostatic pressure acting on the shell would have resulted in intermittent contact of the mould and strand across this gap, increasing the variability of the heat transfer and mould thermal profile. The effect of the mould taper design will be discussed later in this chapter.

Figure 9.3: A comparison of the measured mould thermal standard deviations is shown for the high carbon grades of 3 companies at various casting speeds. The effect of inadequate taper at Companies F and G is evident at the meniscus. As well, the effect of high casting speed at the bottom of the mould is evident in Company H. All of the cases shown were cast with oil lubrication.
9.1.2 Midface Mould Heat Transfer Profiles

For clarity, the mould heat transfer profiles for various UBC plant trials have been illustrated in terms of casting speeds less than about 1.5 m/min (Figure 9.4) and greater than about 2 m/min (Figure 9.5), for oil lubricated casting. The high mould temperatures observed at Company A did not result from comparably high heat transfer rates, as discussed previously. Large gaps (up to 1.5 mm) were calculated to have formed, between the distorted mould and solidifying shell in the bottom three quarters of the mould, which acted as a barrier to heat transfer. The formation of these gaps indicated that the single 0.8 %/m taper was insufficient for these operating conditions. The high heat transfer peaks in the meniscus region, however, indicated that a large degree of mould-strand interaction occurred at the top of the mould due to the excessive negative tapers that formed during mould distortion. The operation of the mould near the boiling range of the oil lubricant also contributed to the local heat transfer peaks. Companies C-1 and D-1 also had high heat transfer peaks. At Company C-1, the high heat transfer peaks were caused due to a combination of insufficient taper (0.4 %/m) and pyrolysis of the oil lubricant. As previously discussed, the insufficient taper allowed negative taper distortion to occur at the meniscus, which led to increased mould-strand interaction and high local heat fluxes. At Company D-1, it appeared that the steep initial taper resulted in high rates of heat transfer, which is contrary to the literature. As well, these high heat transfer peaks were augmented by the pyrolysis of the oil lubricant due to the hot mould operation. A mechanism will be proposed for the occurrence of high heat transfer in excessively steeply tapered moulds in Section 9.2.1.1 (p.126), where the mould taper design is analysed in detail. Company E had poor mould water quality, thus resulting in deposit formation on the mould housing. This scaling in the cooling channel added an additional resistance in the heat flow circuit, particularly near the meniscus, thus reducing the heat transfer. However, the relatively low mould temperatures and lack of permanent distortion of the mould tube indicated that the extent of scale deposition was not severe. Therefore, it was unlikely that the heat transfer was strongly affected.
Returning now to Figure 9.5, Companies F, G and H had similar heat transfer peaks. Due to mould-strand interaction, resulting from inadequate taper, Company F was expected to have high heat transfer peaks, however, the mould flux lubrication reduced the heat transfer by adding resistance to the heat flow circuit. Company G, on the other hand, had zero taper at the meniscus, resulting in high amounts of negative local taper distortion that may have been severe enough to inhibit adequate and continuous contact between the strand and mould during mould oscillation. In fact, gap formation was predicted to occur just below the meniscus region in the high carbon grades, due to the inability of the solidifying skin to make continuous contact with the highly distorted region during the mould oscillation cycle.

![Heat Transfer Profiles](image_url)

**Figure 9.4:** A comparison of the heat transfer profiles of 3 companies is shown for casting speeds below 2 m/min and mould oil lubrication. The effects of inadequate and excessive initial taper are evident in the high heat flux peaks at Companies A-2 and D-1, respectively. The poor water quality, causing scale deposition in the water channel, may have slightly reduced the heat transfer at Company E.
Figure 9.5: A comparison of the heat transfer profiles of 5 companies is shown for casting speeds greater than ~2 m/min and mould oil lubrication. The effects of inadequate initial taper are evident in the high heat flux peaks at Companies A-1 and C-1.
9.2 Design and Operating Variables

It is important to assess several key design and operating variables that differed between the companies, in terms of their effect on the response of the mould. As discussed in the literature review, the response of the caster to differences in casting conditions can be quantified using a variety of expressions: mould heat transfer profiles, average mould heat transfer, peak heat transfer, average mould water heat transfer, and specific heat energy. Depending on the parameter being assessed, different strategies can be used in expressing the heat transfer response in the system. The peak heat transfer, for example, can be used to assess differences of in the lubricant behaviour and initial mould taper. An average mould heat transfer comparison, however, would be a poor strategy when attempting to assess the efficiency of heat removal at different companies, due to differences in the design parameters. A specific mould energy strategy would be more suitable in such a case.

The key design and operating variables that were assessed using the available data from the UBC plant trials included: mould design, mould lubricant, electromagnetic stirring and casting speed.

9.2.1 Mould Design

The key mould design variables in billet moulds are mould taper (particularly at the meniscus) and mould length. The effect of the mould material, wall thickness, corner radii, and machine curvature were not included in this analysis.
9.2.1.1 Mould Taper

From the discussion earlier in this chapter, it is clear that mould taper design, particularly at the meniscus, was a key factor in the heat transfer profile magnitude. From the mould designs at Companies A and C-1, it was clear that an inadequate (shallow) initial taper, as well as pyrolysis of the lubricant, resulted in high heat transfer peaks and metal level thermal standard deviations (Figure 9.5). Inadequate taper at the bottom of the mould, however resulted in lower mould temperatures, increased thermal standard deviations, and reduced heat transfer at Company H, compared to Company G (Figure 9.2, Figure 9.3 and Figure 9.5).

In Section 9.1.2 (p. 122), a seemingly contradictory discovery was made at Company D-1, in that it appeared that with an excessively steep initial taper also resulted in high thermal and heat transfer peaks during the casting of high carbon, oil lubricated grades (Figure 9.2 and Figure 9.4). The proposed mechanism by which a high meniscus-level heat transfer would be generated through excessive initial taper, is very similar to the established mechanism for a shallow initial taper (Figure 2.3). At Company D-1, it was calculated that the excessive meniscus taper of 5 %/m resulted in a distorted dynamic mould profile that still retained a steep positive taper of ~4 %/m during casting [33]. It is proposed that a dynamic taper of a sufficiently steep magnitude would cause interference between the strand and mould during mould oscillation. In particular, during the upstroke cycle of mould oscillation, a great deal of mechanical interaction would occur between the steep positive dynamic taper and the solidifying strand, resulting in high heat transfer at the meniscus (Figure 9.6). Furthermore, it is proposed that the dynamic positive taper formed at the meniscus must be sufficiently steep to cause mould-strand interaction, otherwise a buckling mechanism would occur, as has been described previously in the literature [1,18,45].
Figure 9.6: The proposed mechanism of excessive meniscus taper resulting in heat transfer peaks is shown in this schematic. The high meniscus heat transfer is caused by the interaction of the steep positive dynamic mould taper and the strand during the upstroke of the oscillation cycle.

Clearly, this mechanism would depend not only on the severity of the initial mould taper, but on the characteristics of the strand shell as well. Strand surfaces with smooth shells and sufficient buckling resistance would hence be more susceptible to the proposed mechanism, as these conditions promote good contact with the mould. Rough or rippled shells, or shells with sufficient strength to sustain local deformation, on the other hand, would have a higher propensity to lose contact with the mould during the proposed interaction in the upstroke cycle of mould oscillation. The shell rippling that would result in this case would inhibit the heat transfer. As well, the mould lubricant would have an impact on the proposed mechanism, as it strongly impacts the heat transfer and shell formation in the meniscus region. Therefore, the proposed mechanism would likely depend on the extent of the initial taper, composition (including carbon and alloy content), thermal and mechanical properties of the meniscus shell, and the properties of the mould lubricant.

Excessive meniscus-level tapers have previously been recognized only for their impact on surface quality [16,18,45,47]. These studies have failed to recognize the importance of initial tapers greater than 4 %/m on the magnitude of the mould heat
transfer. The previous analysis of the Company D-1 and D-2 data attributed the high mould temperatures and heat transfer to oil lubrication and pyrolysis [23]. As the results were not compared to data from outside of the study, no analysis of the effect of the initial mould taper was considered. In the current work, the effect of the excessive 5 %/m meniscus-level taper at Company D-2 was assessed against Company E, which had an initial taper of 2.7 %/m. There were only a few comparisons that could be made with the available data however, as the effects of steel composition and mould lubricant were often confounded between the companies.

In the high carbon grades cast with oil lubrication (Figure 9.7), Company D-2 clearly had higher mould heat transfer at the meniscus region than Company E. The respective tapers were 5.0 %/m and 2.7 %/m. Even though Company E had a higher casting speed, thus increasing the heat transfer, the peak heat transfer at Company D-2 exceeded that of Company E by 1700 kW/m². This was a very significant amount, considering that the meniscus-level heat transfer standard deviations were calculated to be around 800 kW/m² in the analysis at Company H, a high-speed operation. However, there were two complicating aspects in this comparison. First, there was oil pyrolysis at Company D-2. It is known that the combustion products released into the mould-strand gap likely acted to increase in the peak heat transfer, however, the effects of oil pyrolysis on heat transfer have not previously been quantified. Second, the scale deposition at Company E would have decreased the heat transfer. However, the scale deposition, and hence the effect on the heat transfer, was not found to be severe. Although there were some confounded factors, the large difference in the observed heat transfer at the meniscus between Companies D-2 and E could not be explained fully in terms of the mechanisms conventionally known to increase the heat transfer peaks. This is a clear indication that the steep initial taper at Company D-2 was a contributor to the high rate of heat transfer observed at the meniscus.
Figure 9.7: The proposed mechanism of excessive meniscus taper, causing high heat transfer at the metal level, is evident when the mould heat transfer profile is compared to the heat transfer in mould with an adequate taper. The grades shown are 1080 grades, cast with mould oil lubrication. The %/m values in the legend indicate the magnitude of the meniscus-level mould tapers.

The heat transfer profiles were assessed in the medium carbon grades of Companies D-2 and E as well, to determine the effect of composition on the heat transfer peaks in adequately and excessively tapered moulds. Unfortunately, only boron grades were cast at Company D-2 in the middle carbon range, and only plain medium carbon grades were cast at Company E, therefore little data was available for analysis. However, the literature suggests that the addition of titanium-protected boron had the effect of slightly increasing the heat transfer, depending on the magnitude of the local heat transfer standard deviation at the meniscus. With this in mind, Figure 9.8 illustrates very clearly that the excessive meniscus taper at Company D-2 resulted in higher mould heat transfer peaks, even with the slightly higher carbon content and casting speed at Company E, which are known to increase the heat transfer peaks. Again, the confounding factors at Company D-2 included the boron addition and oil pyrolysis, which acted to increase the
local heat transfer. As well, Company E had some scale deposition in the water channel, which may have decreased the heat transfer. However, the general similarity of the heat transfer profiles below the meniscus region suggested that a comparison of these two heats would be acceptable.

Although there were many complicating factors in the comparisons made in Figure 9.8 and Figure 9.7, there are solid indications that the excessive mould taper at the meniscus was partly responsible for increasing the heat transfer peaks. This was contrary to the published literature [1,18,23,45], which did not quantify or consider the extent of the initial taper. Furthermore, an examination of Figure 9.9 revealed that the excessively shallow taper and excessively steep taper mechanisms did not have the same extent of impact on the magnitude of the heat peak transfer. The meniscus tapers at Companies C-1 and D-2 were equidistant below and above the initial taper at Company E (±2.3 %/m), respectively. Company C-1, with the shallow taper, however, had a much stronger increase in the heat transfer peak than the excessive taper at Company D-2. The difference in the responses was ~2600 kW/m². It is possible that the positive dynamic taper that formed during mould distortion, in conjunction with the steep negative dynamic taper, was of sufficient magnitude to also contribute to the locally high heat transfer at Company C-1. However, the comparison of these companies was complicated by the effects of the carbon (0.31 to 0.41 wt %C) and boron content at Company D-2, however these effects have been shown to be minor for the given compositions. Both companies had pyrolysis of the mould oil lubricant. Figure 9.9 also illustrated that a small improvement in the meniscus-level taper would result in a marked reduction of the excessive local heat transfer rates. Company A-1 had double the taper of Company C-1, a taper which would still be considered too shallow. However, this 0.4 %/m improvement caused a drop in the local heat transfer of 1600 kW/m². All of the companies in Figure 9.9 had pyrolysis of the oil lubricant, which enhanced the local heat transfer at the meniscus.
Figure 9.8: The proposed mechanism of excessive meniscus taper causing high heat transfer, is evident when the mould heat transfer profile is compared to a mould with adequate taper. The grades were 0.5 wt %C and 0.31 wt % with 0.003 wt %B (+Ti), for Companies E and D-2, respectively. There was some bias in the comparison due to the lower carbon content, boron addition, and oil pyrolysis at Company D-2, as well as some scale deposition in the water channel at Company E. The %/m values in the legend indicate the magnitude of the meniscus-level mould tapers.
Figure 9.9: The proposed mechanism of excessive meniscus taper, causing high heat transfer at the metal level, had less of an impact than the shallow meniscus taper mechanism. The grades were 0.41 wt %C, 0.47 wt %C, and 0.31 wt % with 0.003 wt %B (+Ti), for Companies A-1, C-1, and D-2, respectively. There was some bias in the comparison due to the lower carbon content and boron addition at Company D-2. All of the illustrated companies had pyrolysis of the oil lubricant. The %/m values in the legend indicate the meniscus level mould tapers.
The heat transfer profiles of the peritectic grades cast with any lubricant, as well as all grades (boron, plain carbon, and peritectic) cast with mould flux lubrication, were also assessed at Company D to determine the effect of grade and lubrication on the proposed mechanism. In all cases, the heat transfer peaks were found to be between 2500 to 3300 kW/m² and the peak mould temperatures were below 160 °C. This indicated that the excessively steep initial mould taper at Company D had no effect on the behaviour of the mould in the meniscus region for peritectic grades cast with any lubricant, and all mould flux lubricated grades. In the case of the peritectic grades, the solidification behaviour, which caused non-uniform and rippled shell development, would be a barrier to the proposed steep-taper mechanism, as the mechanism depends on good surface contact to increase the heat transfer. Similarly, the infiltration of the mould flux lubricant at the meniscus acted to increase the local mould-strand resistance to heat flow, also inhibiting the proposed mechanism. Thus, it appears that the proposed mechanism is invoked only in oil lubricated moulds, with a sufficient degree of excess taper, and for grades that develop uniform and smooth shells.

The effect of the meniscus taper on the mould response could not only be seen in the analysis of thermal and heat transfer profiles; the effect could also be seen clearly in the peak heat transfer response and the specific mould energy. The peak heat transfer decreased with increasing meniscus taper, in the range of 0.4 to 2.7 %/m (Figure 9.10). As previously discussed, shallow tapers (such as 0.4 %/m at Company C-1) enhanced the mould-strand interaction, and hence the peak heat flux. However, excessively shallow tapers (0 %/m at Company G) resulted in severe mould distortion, and reduced heat transfer, due to lack of sufficient and continuous contact with the strand. Excessively steep tapers (amongst other factors), on the other hand, such as 5 %/m at Company D, resulted in increased heat transfer peaks as proposed previously. At Company E the poor water quality, causing scale formation on the cold face of the mould, was responsible for slightly reducing the heat transfer. The effect of casting speed in increasing the heat transfer peaks is also evident in the figure. The few high-speed data points that are shown indicated that the trend would be shifted toward higher peaks for increased casting speeds. The specific mould heat energy (Figure 9.11) very distinctly showed the same
trend in that it decreased as the meniscus taper increased. Again, the importance of the initial taper is emphasized, as most of the energy is extracted at the top of the mould. Company G, for example, which had no initial taper, had lower overall heat extraction capability. As well, the high initial taper of Company D enhanced the heat extraction through the mould due to the proposed mould-strand interaction mechanism. However, unlike the peak heat transfer trend, an increased casting speed led to successively lower specific mould heat energies. This was because the residence time in the mould was reduced for higher casting speeds.

From (Figure 9.12), which compares Companies A-1 and F, it is clear that the meniscus level tapers, as well as the overall taper configurations, were important in influencing the mould heat transfer response. Company A-2 had a higher heat transfer peak, even at a lower casting speed, due to the inadequate taper. The remainder of the heat transfer profile, however, was lower than Company F due to the casting speed (which was below the nominal design speed), and the inadequate taper. These contributed to the formation of a mould-strand gap that was detrimental to the heat flow.

The suitability of the mould taper design can also be seen in a comparison of Companies G and H, both of which were long moulds with high casting speeds (Figure 9.13). Unlike Company H, Company G did not have a thermal drop at the ICW, due to its more suitable trial tapers. From a comparison of the recommended and trial tapers at Company H, the recommended mould design was much more aggressive than the trial mould (Figure 9.14). This suggested that a significant degree of gap formation occurred between the strand and the mould, particularly at the ICW where it was proposed that gravity pulled the strand away from the inside mould wall face. The added resistance of this gap that formed was an impediment to the heat extraction at the mould exit of ICW. This, in turn, resulted in a thermal drop of 15 to 54°C at the bottom of the ICW. This is an intuitive result, as the effect of gravity should be felt most particularly in the longer curved moulds, especially if they are inadequately tapered. As well, in Figure 9.14, a comparison of the recommended tapers for Companies G and H, both long moulds, indicated that casting speed had a strong effect at the bottom of a long mould. Company
G, which had casting speeds around 2 m/min, required a final taper that would account for continued solidification shrinkage over a long portion of the mould. Company H, however, had a significant decrease in the amount of taper in the lower half of the mould over the last two tapers. This indicated that the high casting speeds at Company H resulted in very little solidification shrinkage over the bottom half of the mould, and thus the taper requirements in this region were not as aggressive as at Company G.

Figure 9.10: The effect of initial mould taper is shown on the peak mould heat transfer of high carbon grades. Shallow tapers and excessive tapers enhanced the peak mould heat transfer, while the zero-tapered mould suppressed the heat transfer peak. The scale deposition in the water channel at Company E may have slightly lowered the heat transfer peak. The data also indicated that increased casting speeds increased the heat transfer peaks at any given initial taper.
Figure 9.11: The effect of initial mould taper is shown on the specific mould energy of high carbon grades. Shallow tapers and excessive tapers enhanced the energy extracted by the mould, while the zero-tapered mould suppressed the heat extraction capability. The data indicated also that increased casting speeds decreased the specific heat energy for any given initial taper.
Figure 9.12: The effect of mould taper design is shown on the mould heat transfer profiles of mould flux lubricated high carbon grades at Companies A-2 and F.
Figure 9.13: The effect of mould taper design and casting speed are shown on the midface mould thermal responses of the inside curved walls of oil lubricated, high carbon heats at Companies G and H.

Figure 9.14: A comparison of the trial and recommended mould tapers of Companies G and H shows that the recommended mould design at Company H was much more aggressive than that of Company G. The dimensions are based on a hypothetical 125 mm width at the meniscus.
9.2.1.2 Mould Length

Little can be deduced from the mould thermal responses, regarding the effect of mould length, due to taper deficiencies at Companies A and C-1, and mould flux lubrication at Company F. As well, there was a pronounced variability in the heat transfer at Company C-1 at 300 mm, due to a poor taper breakpoint transition. These variables had an effect on either or both of the responses at the meniscus and further down in the mould, making cross-company comparisons difficult. The heat transfer profiles (Figure 9.15), however, indicated that the longer mould of Company G generally allowed for a slightly higher local heat transfer at the bottom of the mould than Companies A-1 and F by \(-400 \text{ kW/m}^2\). The standard deviations in this lower region of the mould were typically 100 to 300 kW/m\(^2\). The high heat transfer peaks at Companies A and C-1 were caused by the inadequate mould tapers. These observed differences in the heat transfer responses, however, were possibly partly attributable to the design and operation differences as well, which were discussed previously.

Figure 9.15: The effect of mould length is shown on the mould midface heat transfer profiles of high carbon grades, cast with oil lubrication at \(\sim 2 \text{ m/min}\). The differences in the responses at the meniscus were attributed to the inadequate taper (Companies A-1 and C-1), and mould flux lubrication (Company F).
9.2.2 The Effect of Mould Lubricant

The effects of mould flux lubrication and oil lubrication were assessed against casting speed for the various companies using the mould water heat transfer. The results were inconclusive, likely due to the many different mould oils and fluxes that were used across the various companies. With mould flux lubrication in particular, the viscosity, glass transition temperature and melting rate are very important in influencing the stability of the lubricating film, the flux consumption rate and the heat transfer. These properties can vary greatly between different mould fluxes. The varying stroke lengths at the different companies also affected the effectiveness of any of the given lubricants. Companies A, B, C and H had much longer stroke lengths than Companies D to F, further complicating the lubrication effect. Therefore, no conclusive assessment of the effect of mould lubrication could be made with the available data.

9.2.3 The Effect of Mould Electromagnetic Stirring and Braking

Of the UBC plant trials, Company C-2 was the only trial conducted with mould EMS and EMBR. During the plant trial, electromagnetic stirring and braking were achieved with the use of dual-coil mould EMS. High intensity stirring was achieved with both coils stirring in the same direction or with an increase in coil amperage. Low intensity stirring was achieved with the single main coil in operation at various amperages.

Unfortunately, due to electrical problems with the interference between the trial instrumentation and the electromagnetic stirrer, thermocouple data could not be obtained for the high speed trial conducted at Company C-2. Therefore, the heat transfer profiles could not be calculated, and the effect of electromagnetic stirring and braking was limited to an average mould water heat transfer analysis. As well, the analysis was limited to Companies C-2 and H, which had similar casting conditions but without M-EMS/EMBR. The effect of M-EMS/EMBR was assessed in the low carbon grades in terms of stirring
on or off, stirring intensity, and M-EMS with mould flux or oil lubrication. There was no EMS data gathered for high carbon grades.

In both the oil and flux lubricated heats, M-EMS had the effect of decreasing the mould water heat transfer for the low carbon grades (Figure 9.16). In the oil lubricated heats, the reduction in heat transfer was over 86 kW/m², except for heat 22739, which had no significant difference. In the flux lubricated heats, the stirred heats had lower heat rates of heat transfer by at least 70 kW/m². Heat 20909 (flux lubrication) appeared to have a slightly greater heat flux (~58 kW/m²) than heat 22738, although it used mould flux lubrication. However, the difference was just within the standard deviations of the mould water heat transfer and was therefore not significant.

![Figure 9.16: The application of M-EMS, with mould flux or oil lubrication, to low carbon grades cast at various speeds, tended to decrease the mould water heat transfer.](image)
The effect of stirring intensity in the low carbon grades, through meniscus stirring or braking, can be seen in Figure 9.17. For the low carbon grades, the heat transfer rates for the heats without EMS were the greatest. The effect of dual coil stirring (Dual M-EMS) versus braking (M-EMS + M-EMBR) could not be delineated due to casting speed differences. There was insufficient data to assess how the increase in coil current affected the heat transfer response in dual coil stirring and braking. As well, the intensity of the stirring, through increased current to the coil, could not be assessed for main coil stirring due to the lack of data in the low carbon range.

As few heats were available for analysis, due to different lubricant types, variations in carbon grade, and different stirring strategies, these observed effects of M-EMS/EMBR on the mould water heat transfer could not be established with statistical certainty.

Figure 9.17: A comparison of the effects of the various stirring strategies showed that M-EMS decreased the mould water heat transfer, in low carbon grades. The effects of M-EMBR, dual coil stirring, and stirring intensity could not be assessed due to insufficient data.
9.2.4 The Effect of Casting Speed – Revisited

To illustrate the effect of casting speed from conventional speeds to high speeds, the mould water heat transfer rates from various sources were compared. Figure 9.18 illustrates the mould water heat transfer over a spectrum of casting speeds from various sources [22-25,28,37,40-44]. Although the operating conditions vi varied between the different sources, the graph clearly indicates that as the casting speed increased, so did the average mould water heat transfer. This figure also demonstrates, through the decreasing slope of the curve, that there is a limited capability for the water to extract heat as the casting speed is increased vii. The limiting factors that apply to the ability of the mould to extract heat at high casting speeds can include lubrication kinetics and mechanical interaction of the strand and mould.

A validation of the average mould and water heat transfer is seen in Figure 9.19. The figure shows that the model-predicted average mould wall heat transfer was up to 27% lower than the calculated water heat transfer, except for at Company F, where the water heat transfer was typically lower by up to 30%. The mould water transfer values for Company F were obtained from calculations done by the plant, which could account for this difference. Company B had low calculated mould water heat transfer due to the casting of peritectic carbon grades, as well as severe transverse depression formation, which increased the resistance in to heat transfer. Due to the low rates of heat transfer in these peritectic grades, the measured differences between the bulk in- and out- water flows were small (1.5 °C) and not accurately measurable with the thermocouples, which were within ±1 °C accuracy [28]. However, overall, these results indicated that the calculation procedure and assumptions of the heat transfer model were fairly consistent over various conditions of the different casting operations.

vi Section size, composition, mould design (constraint, taper, length, wall thickness, material), lubrication (type, flow rate), cooling water velocity and quality, oscillation characteristics (stroke length, frequency, strip time), etc.

vii Note: The decreasing slope was not attributed to the conservative model-predicted mould heat transfer rates at Company H, as this figure used water heat transfer values.
Figure 9.18: Increasing the casting speed has the effect of increasing the mould water heat transfer, regardless of the differences in the caster design and operating conditions. The effect is weaker at high speeds, likely due to physical limiting factors.
Figure 9.19: A comparison of the mould water heat transfer with the calculated average mould wall heat transfer shows that the model predictions were typically up to 27% lower than the water heat transfer calculations. Companies F and B were exceptions.
The effect of casting speed on the mould water heat transfer was examined across the 8 UBC plant trials, separating the effects of carbon content and lubricant type. Figure 9.20 and Figure 9.21 show the effect of casting speed on low-medium and high carbon grades with oil lubrication, respectively. Figure 9.22 illustrates the same for mould flux lubrication in the low-medium carbon grades. The effect could not be shown for high carbon grades due to insufficient data. In all of these figures, the mould water heat transfer increased as the casting speed increased. This increasing trend was due to the reduction in residence time at higher casting speeds, which allowed less solidification to occur in the mould. Subsequently, the mould-strand gap was reduced at higher casting speeds, and the amount of heat extraction by the water increased. The apparent effect of M-EMS in increasing the water heat transfer in Company C-2 (Figure 9.22) was actually a grade effect. The 0.3 to 0.36 wt %C grades cast with M-EMS had slightly higher heat transfer than the low carbon grades without M-EMS.

![Figure 9.20: The effect of casting speed on the mould water heat transfer is shown for low and medium carbon grades cast with oil lubrication.](image-url)
Figure 9.21: The effect of casting speed on the mould water heat transfer is shown for high carbon grades cast with oil lubrication.

Figure 9.22: The effect of casting speed on the mould water heat transfer is shown for low and medium carbon grades cast with mould flux lubrication.
Due to the larger range of data, the effect of casting speed on the specific mould energy and peak heat transfer was assessed in the high carbon grades only. The low carbon grades followed the same trends, and therefore will not be shown.

Figure 9.23 shows that the specific mould energy decreased with increasing casting speed. The range of scatter seen in the figure was due to the differences in meniscus mould taper between the companies. The effect of casting speed on the peak mould heat transfer is illustrated in Figure 9.24. Companies A-1, A-2, C-1, and D-1 had excessively high peaks at low casting speeds due to various taper problems at each plant, coupled with oil pyrolysis. Companies A-1, A-2 and C-1 had shallow initial mould tapers and subsequent boiling of the oil lubricant. Company D-1 had excessive taper at the meniscus, due to poor taper construction, which was proposed to have caused high heat transfer. Company E, on the other hand, may have had slightly lower heat transfer at the meniscus due to poor water quality and scale formation in the cooling channel. The remaining companies showed that an increase in casting speed led to an increase in the peak mould heat transfer.
Figure 9.23: Increased casting speed resulted in decreased specific mould energy in the high carbon grades. The range of scatter was due to the differences in meniscus mould taper between the various companies.
Figure 9.24: Increased casting speed led to an increase in heat transfer peaks in these high carbon grades. The exceptions were those companies that had operational deficiencies, such as inadequate or excessive taper (Companies A-1, A-2, C-1, and D-1), which had high heat transfer peaks. As well, Company E had poor water quality, which may have slightly reduced the heat transfer at the meniscus.
Increased casting speed led to a decreased shell thickness at the exit of the mould in a comparison of all the carbon grades (Figure 9.25). The effect of mould flux lubrication on the reduction of the shell thickness, due to lower heat transfer, is evident in this figure at a casting speed of 1 m/min (Company A-2). The effect of carbon content is also clear, illustrating that the lower carbon contents had thicker shells. An evaluation of the effect of superheat, however, revealed no effect. It is likely that the many differences in the operating practices and mould designs affected the heat transfer response to such a degree that the effect of superheat on the solidification behaviour was masked. Recall however, that for the constant operating design conditions of Company H, increased superheat led to decreased mould heat transfer. The mould heat transfer, as previously discussed, was the major influence on the solidification behaviour in the mould. As superheat was found to have a negligible impact on the heat transfer behaviour, it is likely that its effect on the solidification of the shell cannot be determined across the various companies.
Figure 9.25: Increased casting speeds (at various superheats) had the effect of reducing the predicted shell thickness at the mould exit of any steel grade, in the UBC instrumented trials. The effect of mould flux lubrication is evident in decreasing the shell thickness.
CHAPTER 10: SUMMARY, CONCLUSIONS AND RECOMMENDATIONS

With the many ground-breaking advances that have been made in continuous casting since its inception in the early 1800s, steel plants have been able to produce high quality semi-finished sections for various consumer sectors. More recently, the steel industry has been striving to achieve higher quality billets with higher productivity machines. Many plant trials have been conducted by UBC over the last 30 years to understand the factors affecting billet quality. The most recent project, involving the high speed casting of high quality continuously cast steel billets, started in 1997. Four high speed instrumented plant trials have been conducted since the inception of this project with the hopes of understanding the effects of high casting speeds. The most recent instrumented mould trial has been conducted at Company H, a producer of plain carbon 120 x 120 mm² billets for the wire rod industry. Thermocouple data was gathered and analysed using two mathematical models, one describing the mould, and the other describing the strand. Billet samples were gathered for the assessment of the internal and surface quality over changes in casting speed and other operating variables. The details of the billet evaluation could not be disclosed, however no abnormally severe defects occurred that were consistently linked to higher casting speeds.

The predictions from the modified mould heat transfer model were verified using mould water heat transfer calculations, previous UBC findings, and data from the literature. The predictions from the billet solidification model were compared qualitatively with the billet samples, and quantitatively with the theoretical parabolic growth law for shell thickness. Although highly simplified, the two models used in this analysis appeared to provide adequate results that correlated well with existing knowledge and trends. Future modelling in high speed casting may require the use of a more accurate heat transfer correlation in the mould heat transfer model, should the predicted values be too low. However, these models have resulted in the successful quantification of the heat transfer at the midfaces and corners of the mould, thus providing a tool for the assessment of the primary cooling zone at high casting speeds.
The significant new findings that have evolved from the present work are listed below.

[1] The high speed casting operation at Company H was found to have midface heat transfer peaks that were 1.5 to 2.5 times higher than values quoted for conventional casting, and up to 1.5 times higher than published high speed data. Particularly, the effect of carbon content was found to be less pronounced at high casting speeds than in conventional casting. Generally, the sensitivity of changes in the heat transfer was found to decrease with casting speeds in excess of 3.0 m/min. Increased casting speeds above 3.5 m/min were also found to increase the metal level fluctuation and local thermal standard deviation at the meniscus. As well, higher casting speeds decreased the local heat transfer standard deviation at the meniscus region, and the average heat transfer standard deviation at each mould wall. However, the difference in heat transfer response across the four mould walls increased at higher casting speeds, due to the impact of speed at the bottom of the mould. Furthermore, in the high speed casting speed regime ($\geq$ 3.0 m/min), larger changes in casting speed ($\geq$ 1 m/min) were required to effect a significant change in the metal level standard deviation and heat transfer profile. This indicated that casting in the high speed regime was much less sensitive than conventional casting, in terms of mould response. These effects have never been characterized before in high speed casting due to the few instrumented mould trials that have been conducted.

[2] The long mould length and high casting speed at Company H were found to have a strong impact on the thermal and heat transfer response of the mould in the bottom portion of the inside curved wall (ICW). The midface thermal and heat transfer responses were lowest at the exit of the ICW face, compared to the other faces. At the mould corners, near the meniscus, the thermal and heat transfer responses were lower at the ICW edge than the LSW edge. The formation of a larger gap at the ICW was proposed to be the cause of this phenomenon. Furthermore, the size of the gap at the ICW, and the subsequent heat transfer response of the mould, was proposed to be dependent on the interaction of several variables, namely: gravity acting on the strand, the long mould length and curvature, high casting speed, and carbon grade.
This was supported by the findings at Company G, which showed that in a suitably designed mould, in terms of taper and length, the heat transfer at the bottom of the mould was higher at Company H, which had inadequate taper. As well, the heat transfer at the bottom of the well-designed long mould at Company G was slightly higher than in moulds with conventional lengths and casting speeds.

[3] Mould tapers were recommended for the casting of low and high plain carbon steels at casting speeds of 3.0, 3.5, 4.0 and 4.5 m/min. Previously, no mould taper designs have been published for high and low plain carbon grades cast at high casting speeds in a long mould. The recommended designs were found to be the most sensitive to casting speed at the bottom of the mould, for the reasons stated above. In the high-casting speed regime, the low carbon grades were most sensitive at the lower speeds, due to the strong additive effects of reducing the casting speed and solidification shrinkage on the mould-strand gap size. The high carbon grades, on the other hand, most sensitive to casting speeds above 3.5 m/min, due to the reduced residence time. As in conventional casting, the analysis revealed that the same mould design cannot be used for different carbon grades due to the differences in the solidification characteristics.

[4] A significant new theory has resulted from the comparison of the results of 4 heats from the UBC instrumented plant trials. Opposite to what has been stated in the literature, a mechanism by which excessive metal level taper resulted in higher heat transfer peaks was proposed. It was proposed that the interaction of the steep dynamic positive taper that remained during dynamic distortion of the mould, caused a high degree of mould-strand interaction to occur during the upstroke cycle of mould oscillation. Furthermore, this proposed mechanism of heat transfer increase is dependent on the extent of the positive dynamic taper, local high-temperature properties and uniformity of the shell, and mould lubricant properties, as these affect the deformation characters of the shell during oscillation. This is a very important finding, as previous heat transfer studies have concentrated on exclusively on excessively shallow meniscus-level tapers, and have thus consistently recommended
steeper initial tapers. However, these studies have concentrated primarily on the effect of taper on surface quality and have failed to realize the importance of tapers exceeding 4%/m. The characterization of such a mechanism is highly beneficial in the design of the mould, as it sets distinct guidelines for the initial taper design for specific grades cast with particular lubricants. Furthermore, the proposed steep-taper discovery is very exciting, in that it illustrates the full evolutionary circle that the continuous casting industry has moved through in the casting of steel billets. From the inception of continuous casting up until as recent as 1980, it was very common to have untapered mould tubes. Nearly 15 years later, in 1994, after improved knowledge and trial and error modifications to the casting process, very shallow initial tapers of less than 1%/m were widely used in industry [12]. Extensive research since then has shown that meniscus-level tapers of 2%/m are essential for the prevention of mould distortion, high heat transfer, and poor billet quality [61]. This recent discovery has shown that over-design of the initial mould taper can result in exactly the same problems as an under-designed taper.

10.1 Recommendations for Future Work

Unfortunately, two issues could not be assessed at high casting speeds, due to insufficient data. These included the effect of mould lubrication type (oil or flux) and the effect of stirring intensity (M-EMS). To the knowledge of this author, there are no published results for successful instrumented billet mould trials with systems using M-EMS. As this project has shown, M-EMS has a definite effect on the mould water heat transfer. Although EMS-related billet quality issues have been studied extensively, it would be highly beneficial to study the heat transfer characteristics of the mould in a system with M-EMS to determine the effects of M-EMS, M-EMBR and stirring intensity on the optimum mould taper designs. The understanding of the heat transfer profile at the hot face of the mould is important in the control of the location of maximum stirring and hence the billet solidification and quality. It will enable companies to adjust the M-EMS current, location of maximum stirring, and stirring strategies to achieve particular billet
macrostructures for a given composition and at any given casting speed and superheat, without a costly trial and error juggling of the many different casting parameters.

In terms of mould design, future work can also be done in assessing the effects of the mould length on heat transfer and billet quality. This will become an issue in the future as casting speeds are increased, and control of the dwell time in the mould becomes important through the design of the mould length. Particularly, more attention needs to be focused on the behaviour of the billet at the bottom of the mould, at the inside curved wall, at different ranges of casting speeds above 2 m/min. As well, it should be noted that this work dealt exclusively with conventionally tapered moulds. Special configurations, such as the Convex [80,81] or DANAM [82] mould designs, or the systems proposed by Fukada et.al. [24], Sasaki et.al. [25], Rokop [50], Heard et.al [91], and Kawa [92], must be acknowledged for their potential, or demonstrated, high speed casting capabilities.

Finally, a few details should be examined more closely in the proposed steep-taper mechanism of generating high local heat transfer peaks, as there were several confounding parameters in the current analysis. In particular, it should be determined whether there is a critical value for meniscus taper at which this mechanism becomes significant. As well, the magnitude of the local heat transfer increase should be linked conclusively to an increase in the meniscus taper above this critical value. The proposed mechanism should also be examined in its sensitivity to steel composition, high-temperature shell strength, and various lubricant properties.
CHAPTER 11: FUTURE DIRECTIONS

In both research and industry, there has been movement towards the implementation of expert systems for the prediction and analysis of quality problems and pinpointing their causes to a specific event or location in the machine. An expert system, such as CRAC/X [85], has the capability of providing an interface between a comprehensive database of knowledge and an operator, to teach and troubleshoot issues observed in the plant [85,86]. Intelligent moulds are integral to the success of expert systems by providing continuous real-time data to the controller for analysis and action [84]. The prediction of defects, such as transverse depressions [45], laps and bleeds [47], and breakouts [93], has already been made possible through the continuous monitoring of mould thermocouple and/or metal level signals in instrumented mould machines during day-to-day operation. As the steel industry continues to strive towards the goal of higher productivity without the sacrifice of billet quality, more focus must be trained on the understanding of events in the mould and predicting events before they occur. In recognition of this, the completion of this project has taken another step towards bridging the knowledge gap between understanding and controlling the high speed continuous casting of high quality steel billets.
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