STUDY OF SPRAY-COOLING CONTROL FOR MAINTAINING
METALLURGICAL LENGTH OR SURFACE TEMPERATURE DURING
SPEED DROP FOR STEEL CONTINUOUS CASTING

BY

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THESIS

Submitted in partial fulfillment of the requirements for the degree of Master of Science in Mechanical Engineering in the Graduate College of the University of Illinois at Urbana-Champaign, 2016

Urbana, Illinois

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Continuous casting is an important engineering process through which nearly all of the steel is currently produced worldwide. Steel strand surface temperature and metallurgical length are two key processing variables requiring a proper real-time control to meet product quality and operational safety demands. The main focus of the control methods currently used in the steel industry is maintaining steel surface temperature. Most of these control methods are open-loop. The main reason is that the spray cooling droplets impinging on the high temperature surface induce steam clouds, which make temperature measurement unreliable. However, for operations limited by the casting speed, or for steel grades sensitive more to centerline rather than surface defects, the control of metallurgical length is more important. Operations designed to reduce centerline defects, like soft reduction, depend greatly on the metallurgical length profile. This work explores the potential of using open-loop control methods for the task of minimizing the metallurgical length deviations from the desired value during casting speed changes under temperature constraints. This objective essentially reduces to motion planning, i.e. apriori generation of spray flow rate commands that when applied to the process make the latter execute the motion that carries out the above task in the shortest time possible.

In the first part of this thesis, a simple but comprehensive heat transfer and solidification model – CON1D, and a real-time dynamic version of this model – CONOFFLINE (offline version of CONONLINE) are described. CONOFFLINE uses multiple 1-D models (CON1D) interpolated to give a 2-D prediction of transient evolution of steel temperature and shell thickness in the caster, in an Eulerian frame of reference. The accuracy of CON1D is further verified by validation through a simple test problem with an analytical solution. The phenomenon of hysteresis was introduced in this work into the CONOFFLINE model as a new feature and its effects were investigated.
In the second part, CONOFFLINE was applied to study the thermal behavior (surface temperature and shell thickness) of a thick-slab caster under different speed drop scenarios with constant secondary spray cooling. Analytical solutions are presented to estimate the surface temperature settling time, i.e. the time needed for the surface temperature to reach the neighborhood within $10 ^\circ C$ of steady state after the speed change, and the metallurgical length settling time, i.e. the time needed for the metallurgical length to reach steady state after the speed change, during sudden speed changes.

In the third part, the potential of maintaining surface temperature during speed drops was studied by investigating the following four control methods: 1), constant spray cooling (no control) 2), spray table control 3), time-constant control and 4), PI control. The results show that the time-constant control method has good performance when a good spray table, which is a set of spray patterns that produce the same surface temperature profiles at steady state under different casting conditions (mainly different casting speeds in this thesis), is available. The PI controller’s performance depends on the choice of the gains.

In the last part, the potential of maintaining the metallurgical length during small speed drops for thick-slab caster was studied by investigating four different control methods: 1), constant spray cooling (no control) 2), spray table control 3), time-constant control and 4), bang-bang control. The performance of the above control methods is evaluated in terms of the metallurgical length deviation (the maximum metallurgical length increase/decrease after the speed drop). Based on spray patterns that produce the same metallurgical length under steady state at different casting speeds, the spray table control method decreased the metallurgical length deviation by 66.1% compared with the constant spray cooling case. The time-constant control method reduced the deviation by 41.2%. Bang-bang control method has the best performance on minimizing the metallurgical length deviation. Both the two-step bang-bang control method and the three-step one reduce the metallurgical length deviation by 70.6%.
To my parents, Jingqian Chen and Saiting Zhang, for their love and support.
ACKNOWLEDGMENTS

This thesis is completed with help of a number of people. In my research and writing, I have borrowed ideas and results of hard work from numerous others whose names are not on the front page. I will try to thank as many as I could below, but the list is by no means complete. I apologize to those whose names I forgot to include.

I would like to express my deepest gratitude to my advisor, Professor Joseph Bentsman, for his patience, enthusiasm, immense knowledge, and providing me with an excellent atmosphere for doing research he provided me with. He is the person who lead me into the field of control and optimization, his suggestions helped me in building my background and his guidance helped me at all times in carrying out research and writing of this thesis.

My co-advisor, Professor Brian G. Thomas, has been always there to listen and give advice. The door to Prof. Thomas’s office was always open whenever I ran into a trouble spot or had a question about my research or writing. I am very grateful to him for those long discussions that helped me sort out the technical details of my work and the high standards he set for his students in their work. Especially, I am sincerely grateful for his efforts in helping me improve this thesis. Also I am grateful to the opportunity to work in the Continuous Casting Consortium, created and run by Professor Thomas.

I need to thank Kai Zheng, Xiaoxu Zhou, Bryan Petrus and all those people who have worked on programming of CONONLINE system for their previous contribution. Without them, the present work would not have been possible.

Dr. Bryan Petrus, a former PhD student of University of Illinois and now working in Nucor Decatur, taught me how to use CONONLINE and provided me with a lot of help not only in research but also in daily life.

Prathiba Duvuuri, a former MS student of University of Illinois, performed very helpful empirical investigations into mold heat flux, and also helped me
with CON1D program.

Akitoshi Matsui, of JFE Steel Corporation, calibrated the CON1D to JFE caster and gave a lot of support on designing the control algorithm for maintaining the metallurgical length. And I also want to thank JFE Steel Corporation for providing caster data and casting conditions for this project.

My sincere thanks go to the sponsors of the Continuous Casting Consortium, both in particular for funding this research, and in general for funding a group where I have been able to watch researchers at a major research university work with the best operators, engineers, and metallurgists in the industry.

Thank you to my fellow lab mates in Control System Design and Application Lab: Dr. Insu Chang, Dr. Bryan Petrus, Oyuna Angatkina, Huirong Zhao and Ya Wang, for their occasional help and continuing friendship.

I also want to thank the IT staff who maintained all the computers in my lab, especially Michale Marks for helping me setting up new Linux computers.

I am also thankful to all the staff of Department of Mechanical Science and Engineering at the University of Illinois, including Kathy Smith, Davida Bluhm, Emily Lange and others for their various forms of support during my study.

Most importantly, none of this would have been possible without the love and patience of my parents. They have been a constant source of love, concern, support, and strength all these years. I would like to express my heart-felt gratitude to my parents.
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<td>BC</td>
<td>Boundary condition</td>
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<tr>
<td>ELTM</td>
<td>Element life time method</td>
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<tr>
<td>IC</td>
<td>Initial condition</td>
</tr>
<tr>
<td>ML</td>
<td>Metallurgical length</td>
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<tr>
<td>NF</td>
<td>Narrow face</td>
</tr>
<tr>
<td>PDE</td>
<td>Partial differential equation</td>
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<tr>
<td>PI</td>
<td>Proportional-integral</td>
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<tr>
<td>RMS</td>
<td>Root mean square</td>
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<tr>
<td>WF</td>
<td>Wide face</td>
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<td>TLE</td>
<td>Thermal linear expansion</td>
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LIST OF SYMBOLS

\( K \) \quad K-factor

\( L_i \) \quad Total length for spray zone \( i \)

\( L_c \) \quad Length of whole caster

\( L_f \) \quad Latent heat

\( L_{roll\text{total}} \) \quad Total contact length with rolls

\( L_{sptotal} \) \quad Total length of strand surface length that contact with spray water

\( L_i \) \quad Total length of zone \( i \)

\( L_x \) \quad Characteristic length in \( x \)-direction

\( L_z \) \quad Characteristic length in \( z \)-direction

\( T \) \quad Temperature

\( \hat{T} \) \quad Surface temperature estimate from Consensor

\( T_{amb} \) \quad Ambient temperature

\( T_{ambK} \) \quad Ambient temperature expressed in Kelvin units

\( T_s \) \quad Strand surface temperature

\( T_{set} \) \quad Temperature setpoint

\( T_{sK} \) \quad Strand surface temperature expressed in Kelvin units

\( T_{spray} \) \quad Temperature of the colling water spray

\( Pe \) \quad Peclet number

\( Q_m \) \quad Mold heat flux

\( Q_{sw} \) \quad Spray cooling water flow rate
$Q_{sw_{\text{max}}}$ Maximum spray cooling water flow rate allowed in spray zone

$Q_{sw_{\text{min}}}$ Minimum spray cooling water flow rate allowed in spray zone

$Q_{\text{water}}$ Water flux

$S$ Shell thickness

$c_p$ Specific heat

$c'_p$ Effective specific heat

$e$ Error

$f_s$ Solid fraction

$h_{\text{test}}$ Average heat transfer coefficient used for the validation of CON1D, Eq (2.19)

$h_{\text{conv}}$ Heat transfer coefficient for heat removed by natural convection in spray zone

$h_{\text{rad-spray}}$ Heat transfer coefficient for heat removed by radiation in spray zone, Eq. (2.10)

$h_{\text{roll}}$ Heat transfer coefficient for heat removed by conduction to support rolls in spray zone, Eq. (2.11)

$h_{\text{spray}}$ Heat transfer coefficient for heat removed by water sprays in spray zone, Eq. (2.9)

$h'_{\text{spray}}$ Adjusted heat transfer coefficient of the hysteresis effect and the Leidenfrost effect

$h_{\text{multi}}$ H multipliers, adjust coefficients for the hysteresis effect and the Leidenfrost effect

$k$ Thermal conductivity

$k_i^P$ Proportional controller gain for spray zone $i$

$k_i^I$ Integral controller gain for spray zone $i$

$m_r$ The number of rows of spray nozzles in zone $i$

$q_{sw}$ Total amount of water in one spray zone

$t$ Time variable

$t^*$ Specified time variable
\( t_i(z) \) Time at which the \( i^{th} \) CON1D slice in CONSENSOR is located a distance \( z \) from the meniscus

\( t_i^0 \) Time at which the \( i^{th} \) CON1D slice used by CONSENSOR is at the meniscus

\( t_s \) Time from the meniscus to the metallurgical length

\( u_i \) Spray water flow rate in spray zone \( i \) from CONCONTROLLER, Eq. (2.18)

\( u_i^p \) Proportional component of \( u_i \)

\( u_i^I \) Integral component of \( u_i \)

\( u(t) \) Control signal of PI controller

\( v_c \) Casting speed

\( x \) Spatial variable, the narrow face cross-sectional dimension, \( x = 0 \) is usually at the center of the strand

\( y \) Spatial variable, to be the wide face cross-sectional dimension

\( z \) Spatial variable in casting direction, \( z = 0 \) is usually at the meniscus

\( z_{\text{total}} \) Total length of the simulation

\( z_i(t) \) Location in the \( z \) direction of slice \( i \) at time \( t \)

\( z_{\text{start}}(i) \) the beginning location of spray zone \( i \)

\( z_{\text{end}}(i) \) the end location of spray zone \( i \)

\( z^* \) Specified spatial variable in the \( z \) direction

\( \Delta z \) Printout frequency distance interval

\( \Delta T_i(t) \) Average surface temperature error in spray zone \( i \)

\( \rho \) Density

\( \delta \) Stefan-Boltzman constant

\( \varepsilon_{\text{steel}} \) Emissivity of the strand surface

\( \theta \) Scaling variable

\( \tau \) Dwell time

\( \tau' \) Estimated dwell time
CHAPTER 1
INTRODUCTION AND LITERATURE REVIEW

1.1 Continuous Steel Casting Process Description

Although a relatively new technology, continuous casting is the most common method of casting steel today. In 2013, 95.3% of the steel produced world-wide was made by continuous casting [1]. A schematic of the steel continuous casting process is shown in Figure 1.1. During this process, the molten steel flows from a ladle, through a tundish, into the mold, where the molten steel is maintained at a constant height through a mold level controller. In the mold, the molten steel circulates in the liquid pool and freezes against the water-cooled copper mold walls to form a solid shell. The solidifying shell and the liquid steel inside it continuously move out of the mold at a rate called the ‘casting speed’ that matches the rate of the incoming molten steel flow.

After the mold exit, the solidifying shell, which acts as a container to support the remaining liquid, enters the spray cooling region. In this region, the steel strand is supported by a series of rolls, which are used to support the strand and minimize bulging due to the ferrostatic pressure. Water and air mist sprays cool the surface of the strand between rolls to maintain its surface temperature until the molten core solidifies. The distance from the meniscus to the point of full steel solidification, as illustrated in Figure 1.1 is called the metallurgical length (ML). After the center is completely solid (at the ML), the steel strand can be cut into slabs for future processing and shipment.

Continuous steel casting process has two cooling stages: mold (or primary) cooling and water (or secondary) cooling. Heat transfer at the metal/mold interface in the mold is referred to as the primary cooling, and the heat transfer that happens in the spray cooling region is called the secondary cooling.
Heat is removed through the slag-formed gap between the solidifying steel shell and the mold wall while in the mold, and by natural convection, radiation, spray nozzle cooling (based on water flux), and conduction through contact with the containment rolls while in the secondary cooling region. All of the above are discussed in detail in [3, 4], but the heat transfer in the secondary cooling region deserves special mention since it highly influences the product quality and process safety. The four main heat transfer mechanisms in the secondary cooling zone mentioned above are shown in Figure 1.2.

1.2 Motivating Application

During the continuous steel casting process, casting speed may change during startups, tailouts, ladle changes, breakout detection system alarms, schedule changes, delays upstream of the caster in steel melting and making, and delays downstream of the caster in rolling. It is important to understand how these events affect the dynamic thermal behavior of the solidifying steel, including the metallurgical length and the strand surface temperature.

For many operations in the continuous casting process, surface temperature profile is very important, because it controls surface crack formation. These operations prefer to maintain surface temperature during casting speed changes and transitions. In other operations, the metallurgical length has a considerable influence on the choice of operation locations. Such operations include location of unbending to prevent cracks, location of support zone to prevent whales, and especially: location of soft reduction to prevent center-line segregation. Thus, for these operations it is very helpful to minimize the metallurgical length fluctuation during transitions and other transients.

1.2.1 Bending/straightening cracks

Crack formation is one of the defects that affects the continuous casting process. Basically there are two types of cracks: surface cracks that are initiated near the meniscus in the mold and internal cracks that are initiated at the solidification front. Cracks have been observed at almost every location of strand as schematically shown in Figure 1.3 [5]. Crack formation requires both tensile stress and embrittlement. There are three distinct temperature
ranges where steel has low strength and/or ductility and therefore is susceptible to cracking: high temperature zone from around 1340°C to solidus, intermediate temperature zone from 800°C to 1200°C, and low temperature zone from 700°C to 900°C [5].

Bending/straightening cracks are one type of internal cracks. The cause of these types of cracks is excessive deformation near the solidification front due to straightening or bending [5]. As shown in Figure 1.1, the mold and top of the secondary cooling zone are vertical, and the caster is shaped to bend the solidifying steel to horizontal direction upon exiting the machine. Bending and straightening introduce large axial strains in the solid shell. During bending, the upper surface has compressive strains and the lower surface has tensile strains, and during straightening the strains are reversed.

Bending and straightening cracks could form if bending and straightening operations are carried out on a section with liquid center, or when the center is solid but above 1340°C [5]. Therefore, it is important to make sure that the metallurgical length meets the constraints imposed by the location of bending/straightening operations, otherwise bending/straightening cracks might form. This is only one of the reasons for the importance of minimizing the metallurgical length fluctuation during transition. Other reasons will be introduced below.

1.2.2 Whale formation

As explained in section 1.1, after the solidifying steel exits the mold and enters the secondary cooling region, there are a series of rolls supporting the strand. The rolls serve two main purposes: they allow the strand to move through the caster without sticking and they prevent the strand from bulging under the ferrostatic pressure by supporting the strand shape.

The distance from the meniscus to the location where the steel is completely solid is called the metallurgical length (ML) and the distance from the meniscus to the last supporting rolls is called the machine length. The metallurgical length should be shorter than the machine length, otherwise a defect known as ‘whale’ might form. It is obvious that if the metallurgical length extends beyond the machine length, the final portion of the partially solidified strand will no longer be supported by rolls on the strand broad
faces. The ferrostatic pressure transmitted from the meniscus via the liquid pool acting internally on the steel shell will then cause this unconstrained portion of strand to bulge out excessively, as illustrated in Figure 1.7.

When a whale happens, the bulged steel cannot fit through the cut off device located at the end of the caster. The casting process must then be stopped until the steel is completely solid, and the whale subsequently cut up and removed before casting can resume. In the worst cases, liquid steel could escape through the shell, potentially causing severe damages or serious injuries [6]. To help prevent this from happening, water sprays in the secondary cooling region must cool the steel sufficiently to make sure that the metallurgical length is shorter than the machine length.

1.2.3 Segregation

Segregation is another kind of defect that is related to non-uniformity of chemistry composition during solidification. In general, solidification of steel occurs in three stages [7]:

1) nucleation, or creation of small, stable, and solid crystals.  
2) growth of these nuclei into larger crystals called dendrites  
3) continuation of dendrites growth into grains that forms the final structure.

Nucleation - the creation of tiny, stable, solid crystals, called nuclei in the liquid steel - is the first step of solidification. Undercooling is the driving force for solidification. Solid atoms get together and form clusters. Once a cluster reaches a critical size it becomes a stable nucleus and continues to grow. These small nuclei grow into larger crystals called dendrites.

The casting process always starts with rapid nucleation and growth against the cold mold wall, which creates a thin chill zone of tiny grains. During the solidification of steel, the solidification front does not remain planar - tree-shaped spikes called dendrites shoot into the liquid in the direction of the heat flow. Short perpendicular secondary arms grow on the primary arms of dendrites, as seen in Figure 1.4. The dendrites create a mushy zone between the liquid and the solid steel, which is bounded roughly by, respectively, the liquidus and the solidus temperatures.

The parallel dendrites grow away from the chill zone in the direction of
the heat flow and form a large region of grains with similar orientation called columnar zone. While columnar grains continue growing, the nuclei in the center of liquid steel grow in all directions, and form roughly round equiaxed grains. The growth of equiaxed grains limits the growth of columnar grains, and results in the final structure with three distinct zones: the chill zone, the columnar zone, and the central equiaxed zone, as depicted in Figure 1.5.

The solidification process produces differences in composition at different parts of cast material. Dissolved elements usually have a higher solubility in liquids than in solids. The solute-rich material may be trapped between the arms of growing dendrites and lead to micro-segregation, or may be trapped in the centerline of the casting slabs and lead to macro-segregation. Micro-segregation does not constitute a major quality problem, since it can be removed during thermal processing, such as subsequent soaking in the reheat furnace and annealing [8]. Macro-segregation is a persistent problem that can not be removed, even if the metal part is subjected to severe deformation, as shown in Figure 1.6.

Centerline segregation is a type of macro-segregation that appears as a line of impurities near the centerline of the slab, in which region cracks could appear and will be very harmful when the slab is rolled into thin plates [9]. Sulfur printing is the most common way to evaluate segregation and cracks in the steel slabs. An example of a sulfur print is shown in Figure 1.9.

Soft reduction operation has been developed to reduce centerline segregation. During the solidification process, the steel will shrink while transitioning from liquid phase to solid phase. Therefore, the centerline is susceptible to segregation and other defects if the roll gap profile does not match the desired shrinkage. The choice of location of the soft reduction region depends greatly on the shell thickness profile and the metallurgical length. If the steel is completely solid when the slab enters the soft reduction region, then the rolls experience large forces from the solid steel which could cause damage to both the slab and the rolls. The soft reduction operation performs best when the shell thickness profile and metallurgical length stay constant with time.
1.2.4 Control methods

Continuous casters do not always operate under constant conditions. Under certain circumstances, such as a drop in speed required by a sticker-breakout alarm, an upstream or downstream delay, or a schedule change, a change of casting speed would happen. Then the water spray flow rates in the secondary cooling region would need to be adjusted accordingly. Currently, one of the most common control methods used in the steel industry for the secondary cooling region is the spray table control method, which is also called speed based control. In this method, the spray flow rates are controlled according to a series of spray patterns (maps of spray flow rate in each cooling zone as a function of distance down the caster) which depend on steel grade, production dimension, casting speed, and machine design. These spray patterns, which can be stored in a simple look-up table, are determined by experience, measurement, or steady state modeling so that the slab temperatures at steady state under different casting conditions are the same. When a speed change occurs, the water flow rates in the entire caster are changed immediately according to the values indicated for the new speed in the spray table. Spray table control will be introduced in more detail in section 4.2.1.

One of the problems of spray table control is that the fast change in spray water flow rates after the change of casting speed, may cause severe change of surface temperature and lead to the formation of cracks and other defects.

Another widely used class of control methods, are called dynamic spray control in general with particular variations that have been referred to as the element life time method (ELTM) [11], or the residence time model method [11], or time constant control, or effective speed control [10, 38]. This class of control methods are actually improved versions of spray table control. Time constant control changes the flow rates according to the dwell time (the time takes for a steel slice to reach a location from the meniscus). Then the dwell time is used to calculate the average casting speeds, which are used to calculate spray flow rates from the spray table. The effective speed control method uses the effective casting speed (weighted speed of the average casting speed and the current casting speed) to calculate the spray flow rates from the spray patterns. These control methods cause smoothing of the actual casting speed, especially in the spray zones that are further down in the caster. As a result, the effective speed control smoothes out the surface
temperature during the transient speed fluctuations in relative to that under the spray table control method [10].

The third control method is proportional integral control. The difference between the pre-determined temperature setpoint and the predicted average temperature from the model in each spray zone is used to calculate the spray water flow rate commands based on the proportional–integral controller (see chapter 4 for details).

All three control methods introduced above focus on maintaining the surface temperature during speed changes to minimize the formation of surface defects. The steel grade of most interest in this work is more sensitive to centerline defects like centerline segregation than to surface defects. As described in section 1.2.3, the soft reduction operation is used to reduce centerline segregation, and the location of the soft reduction zone depends greatly on the metallurgical length profile. After the speed change, either the location of the soft reduction zone need to be adjusted according to the metallurgical length or the metallurgical length need to be controlled to maintain within this region during the transition with fixed soft reduction zone.

Bang-bang control method is a control method explored in this work to maintain the metallurgical length during speed changes. This method, which is introduced in detail in chapter 5, switches suddenly between predetermined flow rates at predetermined switching times [12].

1.3 Objective of the Current Work

After the solidifying steel strand enters the spray cooling region, it is cooled by spray cooling water injected from the nozzles installed between the supporting rolls. If the strand is undercooled or is subjected to excessive temperature variations or non-optimal surface temperature profile, then defects like whales might occur; if the strand is overcooled, then defects like transverse surface cracks could be created by mechanical strain during bending and straightening [13]. Because the ductility of steel varies with temperature, a common practice for preventing surface cracks is to ensure that the temperature of the strand surface in the bender or straightener, where tensile stresses are greatest, stays within a range that avoids the low-ductility temperature zones for that steel grade.
In the secondary cooling region, the amount of cooling water received by the strand surface per unit area per minute would affect the shell growth and strand surface temperature. The more cooling water hits the strand surface, the more heat will be extracted from the strand which will result in lower strand surface temperature and faster shell growth. Because the strand surface has direct contact with the cooling water, the surface temperature will change as soon as the spray flow rates change. But the effect of water flow rate changes on the shell growth is delayed, since the solidified shell will inhibit heat transfer. The strand temperature history, including both surface and internal temperature, is vital to steel product quality, because the mechanical properties of steel are highly related to temperature and the mechanical properties have considerable influence on crack formation.

The objective of the current work is to investigate the effects of different control methods on the control objectives of maintaining either the surface temperature or the metallurgical length profiles, and to explore the potential of minimizing their fluctuations during speed drops for a typical thick-slab caster.

1.4 Previous Work of Modeling on Continuous Casting Process and Thesis Overview

Careful control of the strand cooling and the shell growth, which have considerable influence on the formation of cracks and other defects in the cast material, is of central importance in the continuous casting process [14]. Steel temperature, including both surface temperature and internal temperature, is important for the final product quality. However, the measurement of temperature is difficult and unreliable. In most casters, strand surface temperature measurements are available by using optical pyrometer or similar temperature measurement devices; but due to the complex nature in the secondary cooling region, these measurements are not reliable. Even if reliable surface temperature measurements could be made, the internal temperature is impossible to measure directly.

Therefore, computational heat-transfer models for continuous casting have been developed and used both to understand the process [15, 16, 17, 18] and to control the process [19, 20, 21, 22, 4]. These models mainly focus on
steady casting conditions (when the casting conditions are constant). They give the strand temperature profile as a function of casting parameters, such as casting speed, steel grade, strand geometry, superheat, mold heat flux, and spray water flow rate. The numerical method used for these models is often finite difference or finite element method, although many online models are based on simple analytical solutions. A lot of knowledge has been gained from these models since steady casting is the most common and desired state in the continuous casting process. However, transient events might happen, for example, the casting speed might change during start up, change of heat, or change of steel grade. Plant engineers need to understand how these events will affect the temperature, and researchers have continued to develop real-time dynamic models that are valid under transient casting conditions to help understanding the behaviors.

There has been a variety of work in recent years on modeling tools specifically designed to be used in real time on operating continuous casters. With the help of real-time dynamic models, several open-loop model-based control systems have been developed to control the spray water cooling in the secondary cooling region under transient conditions for conventional thick-slab casters. These systems use on-line computational models to ensure that each part of the shell experiences the same cooling conditions. The earliest work by Louhenkilpi and co-workers [14], solved a 2-D longitudinal slice through the center of the caster using finite elements analysis and implicit time stepping. The model calculates the strand temperature and the shell thickness profile along the caster as a function of the actual casting variables, strand geometry, and steel grade. A control model based on real-time model called DYNCOOL, which was developed by Jauhola [19], has been used to control spray cooling at Rautaruukki Oy Raahe Steel Works. Dittenberger et al [20] controlled the spray cooling water in a thick-slab caster using a 1-D finite difference model which updates every minute. DYN3D [21], a 3-D heat flow model for on-line control of spray cooling, uses steel properties and solid fraction/temperature relationships based on multicomponent phase diagram computations. The DYSCOS model of Hardin et al [22] modeled a 2-D longitudinal domain, using finite volumes method and implicit sweeping. Slab temperature and solidification are computed by the model as a function of time-varying casting speed, secondary spray cooling water flow rate, slab thickness, steel chemistry, pouring temperature, and ambient tem-
perature. Finally, the CONONLINE model [4] models a 2-D longitudinal
domain through interpolation of multiple 1-D moving slices. CONONLINE
is the first on-line control system that works on thin-slab casters, which tend
to have faster casting speed and thus require faster model updates. Petrus
and co-workers [23] validated the model’s accuracy of predicting metallur-
gical length variations during speed changes with plant measurements. A
simple PI controller with anti-windup was used in the CONONLINE model
to control the surface temperature.

In this work, the off-line version of CONONLINE model - CONOFFLINE
- was used to investigate the dynamic thermal behavior of continuous steel
slab casters during speed changes under different secondary cooling control
methods. In chapter 2, a brief overview of the CONONLINE model is given.
In chapter 3, CONOFFLINE is used to perform numerical studies of differ-
ent speed change scenarios under constant secondary cooling to study the
dynamic thermal behavior alone. In chapter 4, four different control meth-
ods (constant spray cooling, spray table control, time-constant control and
PI control) are used to explore their performance on maintaining surface tem-
perature during speed change. Most of the previous work on control of the
continuous casting process focused on controlling the surface temperature.
Few attempts have been made to control the metallurgical length during
changes of casting conditions. In Chapter 5, the CONOFFLINE model is
used to study control of the metallurgical length during a speed drop under
three different control methods (spray table control, time-constant control
and bang-bang control).
Figure 1.1: Schematic diagram of continuous steel caster [24].
Figure 1.2: Schematic of spray cooling zone [3].

Figure 1.3: Schematic drawing of strand cast section showing different types of cracks [24].
Figure 1.4: Dendritic structure of columnar grain (left) and equiaxed grain (right) [7].

Figure 1.5: Typical zones found in a cast showing chill, columnar and equiaxed zone [7].
Figure 1.6: Effect of deformation on a casting material with macro-segregation [25].

Figure 1.7: Illustration of formation of whale.
Figure 1.8: Soft reduction operation to reduce centerline defects.
Figure 1.9: Sulfur print of a longitudinal section of a 250 x 320-mm bloom of a 0.50C, 0.80Mn, 0.055S, 0.13 V steel showing mini-ingotism [26].
CHAPTER 2
MODEL DESCRIPTION

2.1 Heat Transfer Model: CON1D

CON1D is a simple but comprehensive model of heat transfer and solidification of continuous casting of steel slabs. A brief overview of the model will be given in this section, but the reader may refer to [3] for more details.

The CON1D model includes phenomena in both the mold and the secondary spray cooling region. The simulation domain is a transverse slice through the strand thickness. CON1D computes the entire temperature distribution within the solidifying slice from the meniscus to the end of containment.

For a solidifying material with heat transferred by conduction and advection, conservation of energy satisfies the following partial differential equation (PDE):

\[
\rho c^* \left( \frac{\partial T}{\partial t} + \vec{v} \cdot \nabla T \right) = \nabla \cdot (k \nabla T)
\]

(2.1)

where \(T(x, y, z, t)\) is the temperature at a given point \((x, y, z)\) in the cast material and \(\vec{v} = (v_x, v_y, v_z)\) is the velocity of the material at that point. For this thesis, \(z\) denotes the casting direction, \(x\) denotes the narrow face cross-sectional dimension and \(y\) denotes the wide face cross-sectional dimension. The origin of \(x\)– and \(y\)–axis are at the center of the strand and the origin of \(z\)–axis is at the meniscus. The density \(\rho\), the thermal conductivity \(k\), and the effective specific heat \(c^*_p\) are the properties of the cast material. The effective specific heat includes the latent heat:

\[
c^*_p = c_p + L_f \frac{df_s}{dT}
\]

(2.2)

where \(c_p\) is the usual specific heat, \(L_f\) is the latent heat, and \(f_s\) is the solid
fraction of the steel. The governing equation in CON1D is 1-D transient heat-conduction equation:

\[
\rho c_p \frac{\partial T}{\partial t} = k \frac{\partial^2 T}{\partial x^2} + \frac{\partial k}{\partial T} \left( \frac{\partial T}{\partial x} \right)^2
\]  

(2.3)

The CON1D model takes the Lagrangian reference frame and by taking advantage of large Peclet number of the continuous casting process, the model assumes that the heat conduction in the casting direction is negligible. The detailed scaling analysis of the JFE thick-slab caster is shown in the following section. The solution method CON1D uses is an explicit central finite difference method with a post-step correction to maintain an accurate balance on the latent heat [3].

The casting conditions simulated in this work are based on three different casters: the thin-slab caster at Nucor Steel in Decatur, Alabama; the thick-slab caster at ArcelorMittal Steel in Burns Harbor, Indiana; and the thick-slab caster at JFE Steel, Japan. Some of the plant details have been changed in the thesis in order to more easily illustrate the fundamental concepts of interest in this work. If it is not mentioned, then the results refer to the default caster in Table 4.2 is used.

2.1.1 Scaling analysis and material reference frame

In most casters, the material only moves in the \(z\)–direction at casting speed \(v_c\), and the conduction in the \(y\)–direction only matters near the corners of the slab. With these simplifications, equation (2.1) simplifies to the following:

\[
\rho c_p \left( \frac{\partial T}{\partial t} + v_c \frac{\partial T}{\partial z} \right) = \frac{\partial}{\partial x} \left( k \frac{\partial T}{\partial x} \right) + \frac{\partial}{\partial z} \left( k \frac{\partial T}{\partial z} \right)
\]  

(2.4)

The relative size of the remaining terms are compared through scaling analysis. The Peclet number,

\[
P_e = \frac{v_c L_z \rho c_p}{k}
\]  

(2.5)

is the ratio of advection to conduction heat transfer rate in the \(z\)–direction, where \(L_z\) is the characteristic length in the casting direction. If \(L_z\) is taken to be the length of the whole caster (30 m for the ArcelorMittal caster), then
\[ Pe = 0.025 \times 30 \times 7400 \times 670/30 \approx 2 \times 10^5 \] (2.6)

The conduction in the \(z\)-direction is negligibly small compared to the advection in the \(z\)-direction. However, the characteristic length in the \(x\)-direction, \(L_x\)-in this case half thickness of the slab (0.1295\text{m} for the ArcelorMittal caster)-is much smaller than \(L_z\). This means that the conduction in the \(z\)-direction is safe to neglect, but not in the \(x\)-direction. The remaining terms in the equation (2.4) are:

\[ \rho_c s \left( \frac{\partial T}{\partial t} + v_c \frac{\partial T}{\partial z} \right) = \frac{\partial}{\partial x} \left( k \frac{\partial T}{\partial x} \right) \] (2.7)

However, if \(L_z\) is chosen to be the distance across a roll contact (about 0.01 \text{m}), then the \(z\)-conduction term is no longer negligible relative to the \(z\)-advection term. Therefore, the average temperatures over long sections of the caster are expected to be accurate, but the local temperatures where heat flux changes rapidly, for example directly under a roll or spray, are expected be less accurate using this simplification.

Furthermore, CON1D takes the Lagrangian reference frame for its simulation domain: a slice through the slab thickness, which moves with the steel in the \(z\)-direction at the casting speed. Mathematically, instead of calculating \(T(x,z,t)\), the model calculates \(T(x,v_c t,t)\). Then, equation (2.7) becomes:

\[ \rho c_p \frac{\partial T}{\partial t} = \frac{\partial}{\partial x} \left( k \frac{\partial T}{\partial x} \right) \] (2.8)

2.1.2 Boundary conditions

The boundary conditions (BC) for equation (2.8) are quite complex. The initial temperature of the steel at the meniscus (\(z = 0\)) is approximated as the pouring temperature. At the strand surface (\(x = \pm S/2\)), heat is removed through the slag-formed mold gap between the solidifying steel shell and the mold wall while in the primary cooling region, and by natural convection, radiation, spray nozzle cooling, and conduction through contact with the containment rolls while in the secondary cooling region. All are discussed in detail in [4].

Heat flux in the mold depends on many complicated phenomena. In this
thesis, CON1D takes the measurement of the average mold heat flux - typically from the measurement of the temperature change of the mold cooling water - and fits it to a simplified local heat flux profile down the mold that has been calibrated to match measurements at previous casters. These are given in equations (2)-(7) in [4].

The boundary conditions in the secondary cooling region deserve special mention here since they highly influence the product quality and process safety. In CON1D, the heat transfer in the secondary cooling region is simplified to the following four main mechanisms: spray water cooling ($h_{\text{spray}}$), radiation($h_{\text{rad-spray}}$), air convection ($h_{\text{conv}}$) and conduction to the supporting rolls($h_{\text{roll}}$), as shown in Figure 2.1.

The heat extraction due to the spray cooling water is a function of water flow [27], of the following form:

$$h_{\text{spray}} = A \times Q_{\text{water}} \times (1 - b \times T_{\text{spray}})$$ (2.9)

where $Q_{\text{water}}$ ($l/m^2s$) is water flux in each spray zone, $T_{\text{spray}}$ is the temperature of the cooling water spray. Based on Nozaki’s empirical correlation [27], $A = 0.3925$, $c = 0.55$, and $b = 0.0075$, which has been used successfully by other modelers [11, 28].

Radiation is calculated by:

$$h_{\text{rad-spray}} = \delta \times \varepsilon_{\text{steel}} (T_{sK} \times T_{\text{ambK}}) (T_{sK}^2 \times T_{\text{ambK}}^2)$$ (2.10)

where $T_{\text{amb}}$ is the ambient temperature, $T_s$ is the strand surface temperature, and $T_{sK}$ and $T_{\text{ambK}}$ are $T_s$ and $T_{\text{amb}}$ expressed in Kelvin, $\delta$ is the Stefan-Boltzman constant ($5.67 \times 10^{-8}W/m^2K^4$), and $\varepsilon_{\text{steel}}$ is the emissivity of the strand surface (0.8). Because for water cooling only, air convection is not very important, $h_{\text{conv}}$ is treated as a constant ($8.7W/m^2K$).

The heat extraction into rolls is calculated based on the fraction of the total heat extraction to rolls, $f_{\text{roll}}$, which is calibrated as the following for each spray zone:

$$h_{\text{roll}} = \frac{f_{\text{roll}}}{L_{\text{roll contact}}(1 - f_{\text{roll}})} ((h_{\text{rad-spray}} + h_{\text{conv}} + h_{\text{spray}}) L_{\text{spray}} + (h_{\text{rad-spray}} + h_{\text{conv}}) (L_{\text{spray pitch}} - L_{\text{spray}} - L_{\text{roll contact}}))$$ (2.11)
A typical \( f_{\text{roll}} \) value of 0.05 produces local temperature drops beneath the rolls of about 100°C [3]. Increasing \( f_{\text{roll}} \) will increase the heat extracted into the rolls and will result in more local temperature drop under the rolls. Beyond the spray cooling region, heat transfer simplifies to radiation and natural convection.

2.2 Real-time Control System: CONONLINE

A brief overview of the CONONLINE model, which was developed by Petrus et al. [4, 6], will be given in this section. CONONLINE is a real time control system consisting of several programs running at the same time. The old CONONLINE runs in two connected powerful workstations: the “Model” workstation, which runs software sensor - CONSENSOR, and the “Controller” workstation which runs the controller - CONCONTROLER [4]. Now the new CONONLINE only needs a single workstation which runs both CONSENSOR and CONCONTROLER. The various programs communicate through ‘shared memory’, which is a block of memory that is accessible by any program and is updated every second.

CONSENSOR, the software sensor, predicts the temperature profile through strand thickness for the entire caster in real time, by using CON1D as a subroutine. Then CONCONTROLER reads the temperature distribution and computes the spray water flow rates needed for each spray zone to maintain the surface temperature. The control algorithm used by CONCONTROLER is simple PI controller with classic anti-windup. In this work, the PI control method is not the only control method applied to CONONLINE system. When other control methods are applied, CONCONTROLER is turned off, the control algorithms hard coded in CONSENSOR are used to calculate the spray water flow rates.

CONONLINE actually has two different versions: on-line version (CONONLINE) and off-line version (CONOFFLINE). Currently the only difference between these two versions is how the model gets the casting parameters, such as casting speed, steel chemistry, and superheat. CONONLINE gets the casting parameters directly from the active caster that CONONLINE is controlling, and CONOFFLINE uses recorded or invented casting parameters. In this thesis, all the simulations are simulated off-line with CONOF-
FLINE, but since the models are actually the same, it will still be referred to as CONONLINE in the remaining chapters of this thesis.

2.2.1 Software sensor: CONSENSOR

CONSENSOR is designed to produce the temperature profile along the entire caster \((z)\) and through its thickness \((x)\) in real time, by exploiting CON1D as a subroutine. It does this by managing the simulation of \(N\) different CON1D slices, each starting at the meniscus at different time to achieve a fixed \(z\)–direction spacing between the slices. Equation (2.8) could be solved by CON1D faster than real time, but it only gives the temperature estimation at the locations of the moving reference frame, which in return depends on the casting speed history.

CONCONTROLLER requires CONSENSOR to update every \(\Delta t\) seconds. During each time interval, the \(N\) different CON1D simulations track the temperature evolution of each slice over this time interval, given the previous calculated and stored temperature of that slice at the start of the interval. The computation time required is approximately the same as that for just one complete CON1D steady-state simulation of the entire caster. On the Scientific Linux workstation, it takes about 0.67 seconds for 200 slices when casting at 1.5 \(m/min\).

Currently, CONSENSOR always manages exactly 200 slices, which corresponds to a uniform spatial interval of 0.255\(m\) along the entire simulation domain for the illustrative example case based on the JFE thick-slab caster, \(z_{\text{total}} = 51m\). After the first slice was created at the meniscus, whenever the most recent slice moves downward 0.255 \(m\), a new slice will be generated at the meniscus and start moving downward. If an old slice moves out of the containment, a new slice will start from the meniscus. By using this method, there will always be 200 slices in the whole caster after start up.

For the \(i^{th}\) slice started at the meniscus at time \(t_i^0\), \(T_i(x,t)\) can be calculated from equation (2.8). The location of the \(i^{th}\) slice in the caster is:

\[
z_i(t) = \int_{t_i^0}^{t} \nu_c dt
\]  

(2.12)

Then the exact temperature prediction at location \(z_i\) can be calculated by the following equation:
\[ T(x, z_i(t), t) = T_i(x, t) \quad (2.13) \]

CONSENSOR uses delay interpolation method to get the temperature estimation for locations between slices \((z_{i-1} < z < z_i)\). It searches for the most recent exact temperature estimation of that location, i.e. the temperature of the most recent slice that passes through that location.

Mathematically, this means:

\[ \hat{T}(x, z, t) = T_i(x, t_i(z)) \quad (2.14) \]

where \(t_i(z)\) can be found by solving the inverse of equation (2.12), i.e.

\[ z = z_i(t_i(z)) = \int_{t_i(z)}^{t_i(z) + v_c \Delta t} v_c dt \quad (2.15) \]

In practice, CONONLINE simply stores the entire temperature history of all active slices, and searches through them to find the most recent temperature, rather than solving equation (2.12).

The delay interpolation method is illustrated in Figure 2.2 with only two slices moving at constant casting speed \(v_c\). In this case, the temperature at a specific time \(t^*\) and distance \(z^*\) from the meniscus needs to be estimated. Slice 1 and slice 2 started at the meniscus at \(t_{10}\) and \(t_{20}\), so at time \(t^*\) slice 1 and slice 2 are at location \(z_1(t^*) = v_c(t^* - t_{10})\) and \(z_2(t^*) = v_c(t^* - t_{20})\). Therefore, the exact temperature estimations at this two location for time \(t^*\) are available. Location \(z^*\) is between these two locations \((z_2(t^*) < z^* < z_1(t^*))\), in order to get the temperature estimation of location \(z^*\) CONSENSOR looks backward in time for the most recent exact estimations at that locations. In this case, as the figure shows, the temperature comes from the temperature estimation of slice 1 when it was at that location. Since the casting speed is constant, equation (2.12) can be solved directly to get:

\[ \hat{T}(x, z^*, t^*) = T_1 \left(x, t = t_{10} + \frac{z^*}{v_c}\right) \quad (2.16) \]

The delay interpolation temperature error introduced at location \(z^*\) in Figure 2.2 is the temperature change from time \(t_1(z^*)\) to \(t^*\), which is a function of the extent of transient effects in the laboratory frame, and slice spacing. This error will be largest when the casting speed is small, increasing
the time slices take to travel, or the casting conditions change drastically in a small amount of time.

2.2.2 Control methods for different objectives: CONCONTROLLER

Control of the secondary cooling region is very important to the continuous casting process, since it decides the steel quality and the production rate. This thesis explores the performance of the following different control methods on controlling the metallurgical length or the surface temperature:

(1) constant spray cooling
(2) spray table control
(3) time-constant control
(4) bang-bang control
(5) PI (proportional - integral) control

The first four methods above calculate the spray flow rate commands based on the casting speed, and the PI control method determines the spray flow rate commands based on the tracking error between the CONSENSOR estimation and the setpoints. Constant spray cooling means during speed changes, the spray flow rates in the secondary cooling region are kept constant. This control method is used as a reference to compare the performance of other control methods. The Spray table control method, time-constant control method, and PI control method will be introduced in detail in Chapter 4, and the bang-bang control method will be introduced in detail in Chapter 5.

Currently only the PI control method is coded in CONCONTROLLER. The constant spray cooling method, spray table control method, time-constant control method, and bang-bang control method are coded in CONSENSOR; when these four methods are applied, CONCONTROLLER is turned off. Here a brief introduction to CONCONTROLLER (the PI control method) would be given.

CONSENSOR will provide the temperature estimation \( \hat{T}(z,t) \) every second. Then CONCONTROLLER calculates the average temperature error for each spray zone using the following equation:
\[ \Delta T_i (t) = \frac{\int_{z_{\text{start}(i)}}^{z_{\text{end}(i)}} (\hat{T} (z, t) - T_{\text{set}}^i (z, t)) \, dz}{L_i} \]  

(2.17)

where \( L_i \) is the total length and \( T_{\text{set}}^i \) is the temperature set point, \( z_{\text{start}(i)} \) and \( z_{\text{end}(i)} \) are the beginning and the end of spray zone \( i \), \( \Delta T_i \) is the racking error for the spray zone \( i \). Then the spray flow rate commands are updated every second using the following equation:

\[ u_i (t + \Delta t) = k_{iP}^i \Delta T_i (t) + k_{iI}^i \Delta T_i (t) \Delta t, \quad i = 1, \ldots, n_{\text{zone}} \]  

(2.18)

where \( k_{iP}^i \) and \( k_{iI}^i \) is the PI controller gain for the spray zone \( i \). The simulation results show that the real time control system, CONONLINE, has better temperature control performance than conventional systems [4]. For more details about CONCONTROLLER please refer to [4, 6].

2.3 Model Validation and Calibration

2.3.1 Steady-state validation of CON1D

CON1D has been validated with plant measurements in the spray zones on several operating slab casters [3, 4, 29, 30] and has been applied to a wide range of practical problems in continuous casters. However, since pyrometer measurements in the spray cooling region are not reliable, other methods of calibration are needed. Petrus et al. [23] developed a new method to more accurately measure the metallurgical length (ML) and the measurements matched the metallurgical length predicted by CONSENSOR.

In this work, CON1D is further verified by validation with a simple test problem with an analytical solution: a solid bar cooling symmetrically with no solidification.

A completely solid steel strand is moving down the caster at constant casting speed \( v_c \), with initial temperature, \( T_{\text{solid}} \). The simulation domain is a slice through the slab thickness, as shown in Figure 2.3. The heat transfer boundary condition on each side of the slab is controlled to be uniformly constant \( h_{\text{test}} \), which is calculated by taking the weighted average heat flux of the four main heat transfer mechanisms in the spray cooling region:
\[ h_{\text{test}} = \frac{h_{\text{spray}}L_{\text{sptotal}} + h_{\text{roll}}L_{\text{rolltotal}} + h_{\text{conv}}(L_c - L_{\text{sptotal}} - L_{\text{rolltotal}})}{L_c} \]  \hspace{1cm} (2.19)

where \( L_c \) is the length of the whole caster, \( L_{\text{sptotal}} \) is the total length of strand surface length that contacts with spray water, and \( L_{\text{rolltotal}} \) is the total contact length with rolls. The detailed scaling and analytical analysis is shown in Appendix A.

The parameters used for validation problem are listed in Table 2.1. The analytical solution used for comparison is equation (A.21) listed in Appendix A.

**Table 2.1: Parameters used in CON1D validation case**

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
<th>Unit</th>
</tr>
</thead>
<tbody>
<tr>
<td>Solidus Temperature, ( T_{\text{solid}} )</td>
<td>1509</td>
<td>°C</td>
</tr>
<tr>
<td>Ambient Temperature, ( T_{\infty} )</td>
<td>35</td>
<td>°C</td>
</tr>
<tr>
<td>Specific heat of solid steel, ( c_p )</td>
<td>670</td>
<td>J/kg · K</td>
</tr>
<tr>
<td>Thermal conductivity of solid steel, ( k )</td>
<td>30</td>
<td>W/mK</td>
</tr>
<tr>
<td>Density of solid steel, ( \rho )</td>
<td>7400</td>
<td>kg/m³</td>
</tr>
<tr>
<td>Thermal diffusivity of solid steel, ( \alpha )</td>
<td>( 6.0508 \times 10^{-6} )</td>
<td>/</td>
</tr>
<tr>
<td>Latent heat of fusion, ( L_f )</td>
<td>0</td>
<td>kJ/kg</td>
</tr>
<tr>
<td>Total spray zone length, ( L_z )</td>
<td>29000</td>
<td>mm</td>
</tr>
<tr>
<td>Slab thickness, ( L_x )</td>
<td>230</td>
<td>mm</td>
</tr>
<tr>
<td>Slab width, ( L_y )</td>
<td>1500</td>
<td>mm</td>
</tr>
<tr>
<td>Casting speed, ( v )</td>
<td>1.1</td>
<td>m/min</td>
</tr>
<tr>
<td>Heat transfer coefficient, ( h_{\text{test}} )</td>
<td>180</td>
<td>W/m²K</td>
</tr>
<tr>
<td>Biot number, ( Bi )</td>
<td>0.69</td>
<td>/</td>
</tr>
<tr>
<td>Time constant, ( t_c )</td>
<td>( 2.19 \times 10^3 )</td>
<td>sec</td>
</tr>
</tbody>
</table>

Figures 2.4, 2.5, and 2.6 show the results of numerical solution from CON1D, the analytical result, and their comparison respectively. In Figure 2.6, data from three parts (6 locations) of the caster are chosen, the upper part (\( z=2000 \ mm, 6000 \ mm \)), middle part (\( z=12000 \ mm, 18000 \ mm \)) and lower part (\( z=24000 \ mm, 28000 \ mm \)).

The results show that the numerical result matches with the analytical result with a root mean square (RMS) error of 0.36%:
\[
\varepsilon_{RMS} = \sqrt{\frac{1}{N} \sum_{n=1}^{N} (\theta_{\text{numerical}}(t, x) - \theta_{\text{analytical}}(t, x))} = 0.0036
\]  

(2.20)

where \( \theta_{\text{numerical}} \) and \( \theta_{\text{analytical}} \) are scaling variable from Appendix A. This validates the accuracy of the solution method for the transient heat conduction problem with solidification that is contained in CON1D, CONONLINE, and CONOFFLINE.

2.3.2 Transient validation of CONONLINE

The CONONLINE model was recently validated by Petrus [6] for transient conditions. Specifically, the validation is done by comparing the simulation results of transient changes in the metallurgical length during casting speed changes with the plant measurements. The following gives a brief overview. CONSENSOR was applied to a ArcelorMittal 260 mm thick-slab caster at Burns Harbor where measurements during transient conditions were recently reported [31]. During the trial, strain gauges were installed on some of the support rolls to measure the changing forces exerted on those rolls by the strand.

The first two plots in Figure 2.7 are the reproduction of the measurements from [31] and the last three plots are the prediction results from CONSENSOR. Comparison between the measurements in the second plot and the model prediction in the third plot shows good qualitative matches between the measured roll forces and the predicted TLE (Thermal linear expansion) at the rolls. In particular, the figure shows quantitative match between the timing of changes in roll force and the timing of changes in the thermal linear expansion predicted by CONSENSOR. Readers may refer to [6] for more detailed information.
2.4 Hysteresis Effect

2.4.1 Leidenforst effect

The heat extraction by cooling water is governed by water boiling phenomena [32], which greatly depends on the temperature. As shown in Figure 2.8, the heat transfer phenomena at the steel surface in the spray cooling regions can be divided into four main phases when cooling water comes in contact with the hot metal surface [2].

(1) Forced convection when temperature is lower than 100 °C. In this region, heat transfer occurs via natural convection and the heat transfer coefficient is very small.

(2) Nucleate boiling between 100 °C and the burnout temperature. The burnout temperature is the temperature at which the heat transfer coefficient reaches its maximum value during the nucleate boiling phase. As the temperature increases, the water starts to vaporize; bubbles of water form at the metal surface, break off and rise in the water film and finally escape from the free surface. The intensity of bubbles formation and breakaway continues to increase as the temperature rises. This effect would increase the heat transfer coefficient until it reaches a maximum value (referred to as the burnout point). The burnout temperature for steel is between 500 °C and 700 °C.

(3) Transition boiling between burnout and the Leidenfrost temperatures. The Leidenfrost temperature is the temperature at which the heat transfer coefficient reaches its minimum value during the transition boiling phase. At temperatures above the burnout temperature, the water (steam) bubbles start sticking to the metal surface, and a vapor layer, which decreases the heat transfer, begins to form. When the metal surface is completely covered by the vapor layer, the heat transfer coefficient reaches its minimum (referred to as the Leidenfrost point). The Leidenfrost temperature for steel is about 700 °C to 1000 °C.

(4) Film boiling at high temperature (larger than the Leidenfrost temperature). In this region, heat is transferred by conduction through the stable vapor layer. The heat transfer coefficient does not change much as the temperature increases.

In the secondary cooling region, cooling water droplets are impinged onto the hot steel surface and the droplets vaporize immediately to form a sta-
ble steam layer. The steam layer prevents the water droplets from coming in contact with the steel strand surface and decreases the heat removal. The Leidenfrost effect is any variation in the heat removal (usually increase) not accounted by the spray cooling water heat transfer coefficient equation (equation (2.9)). To account for the changes of the heat transfer coefficient of spray nozzle cooling \(h_{\text{spray}}\) due to the boiling heat transfer effect, a set of h-multipliers \(h_{\text{multi}}\) can be input to CONONLINE \([33]\), such as follows:

**Table 2.2: Example of the Leidenfrost effect h-multipliers**

<table>
<thead>
<tr>
<th>Temperature (^{\circ}\text{C})</th>
<th>500</th>
<th>600</th>
<th>700</th>
<th>800</th>
<th>900</th>
</tr>
</thead>
<tbody>
<tr>
<td>h-multiplier</td>
<td>1.0</td>
<td>2.5</td>
<td>1.8</td>
<td>1.3</td>
<td>1.0</td>
</tr>
</tbody>
</table>

If the h-multipliers in Table 2.2 are used, when the strand surface temperature \(T\) is at 800 \(^{\circ}\text{C}\), then the actual heat exaction due to spray nozzle cooling \(h'_{\text{spray}}\) is:

\[
h'_{\text{spray}} = h_{\text{spray}} \times h_{\text{multi}}(800{^{\circ}\text{C}}) = h_{\text{spray}} \times 1.3 \tag{2.21}
\]

where \(h_{\text{spray}}\) is from equation (2.9). For temperatures between those listed in Table 2.2, linear interpolation method is used to get the h-multiplier. The Leidenfrost effect is an optional feature in CONONLINE; the user can either choose h-multipliers to be 1 at all temperature or to choose different values at different temperatures. The Leidenfrost effect should only affect the heat extraction according to the spray water cooling, the heat transfer according to the other three main mechanisms: radiation \((h_{\text{rad-spray}})\), air convection \((h_{\text{conv}})\), and conduction to the supporting rolls \((h_{\text{roll}})\) should not be affected. Therefore, the h-multipliers are only used to adjust the \(h_{\text{spray}}\).

A rigorous experiment was done by Hernandez-Bocanegra et al. to determine the boiling curve from 200 \(^{\circ}\text{C}\) to 1200 \(^{\circ}\text{C}\) and then back to 200 \(^{\circ}\text{C}\) for a Pt-specimen under air-mist spray nozzles \([34]\). The curves for both temperature processing histories show that there is strong boiling hysteresis during nucleate and transition boiling regimes. However, the hysteresis is almost absent in the stable film boiling regime. The existence of hysteresis, in a certain temperature interval, points out the significance of considering the thermal history of actual cooling processes when simulating them in the laboratory \([34]\).
2.4.2 Methodology

The existence of different transition boiling curves for heating and cooling paths [34] indicates that to better calculate the boiling heat transfer coefficient with sprays \( h'_{spray} \), CONSENSOR should have two boiling curves. Therefore, two sets of h-multipliers, which can be determined and changed by operators, are introduced into CONSENSOR. One set of h-multipliers is used when the strand surface temperature is increasing and the other set is used when the temperature is decreasing, such as the ones given in the following table:

<table>
<thead>
<tr>
<th>Temperature (^\circ C)</th>
<th>500</th>
<th>600</th>
<th>700</th>
<th>800</th>
<th>900</th>
</tr>
</thead>
<tbody>
<tr>
<td>h-multiplier-heating</td>
<td>1.0</td>
<td>2.5</td>
<td>1.8</td>
<td>1.3</td>
<td>1.0</td>
</tr>
<tr>
<td>h-multiplier-cooling</td>
<td>1.0</td>
<td>0.6</td>
<td>0.5</td>
<td>0.7</td>
<td>1.0</td>
</tr>
</tbody>
</table>

The following simulations of CONONLINE model with hysteresis effect feature are based on the casting parameters of the JFE thick-slab caster. Currently, CONONLINE can store at most 3000 temperature data points. Therefore, the surface temperature history are available at the locations:

\[
z = n \times \Delta z, \quad n \in [0, 3000], \quad n \in N
\]  

(2.22)

where \( \Delta z \) is the printout frequency distance interval. For the JFE thick-slab caster, \( \Delta z \) is chosen to be 17 mm.

Before calculating the current temperature at location \( z \), CONONLINE needs to decide which h-multiplier to use. As introduced in the previous section, CONSENSOR updates the temperature estimation every second. Therefore, the previous temperatures of 1 second ago \( T_{pre}(z) \) and 2 seconds ago \( T_{prepre}(z) \) are stored. When CONSENSOR calculates the \( i^{th} \) slice’s current temperature, it first compares the previous temperatures \( T_{pre}(n_i) \) and \( T_{prepre}(n_i) \), where \( n_i \) is the smallest number satisfy:

\[
n_i \times \Delta z \geq z_i
\]  

(2.23)

where \( z_i \) is the current location of the \( i^{th} \) slice.

If \( T_{pre}(n_i) \geq T_{prepre}(n_i) \), then the strand is heating and h-multiplier-heating set is used; otherwise h-multiplier-cooling set is used. Figure 2.10
shows the flow chart of the procedure that CONONLINE uses to calculate the boiling heat transfer coefficient with sprays.

The CONONLINE model with the hysteresis effect uses the temperature history of specific locations in the caster instead of the temperature history of slices. For a slice, the temperature is always changing and will not reach a steady state while it is in the caster; but for specific locations in the caster, the temperature at those locations can reach steady states. As shown in the flow chart, when the temperature is at steady state \( T_{\text{pre}}(n_i) = T_{\text{prepre}}(n_i) \), heating set of h-multipliers are used. Thus, at steady states, temperature profile with the hysteresis effect (two boiling curves) will be the same as the temperature with the Leidenfrost effect (one boiling curve).

2.4.3 Test Simulation

All the following simulations are based roughly on the thick-slab (221 mm-thick) caster in JFE Steel, Japan. Some of the plant details have been changed in the thesis in order to more easily illustrate the fundamental concepts of interest in this work. The speed drop scenario simulated is a sudden speed drop from 1.7 m/min to 1.5 m/min. The spray flow rates in the secondary spray cooling region are kept constant after the speed drop, the spray pattern used is ‘1.7-orig’ (Table 4.6).

Figure 2.11 shows that the temperature profiles with these two effects at steady state are identical. Figures 2.12 through 2.14 compare the transient behavior of the average temperature of zone 4, zone 8, and zone 12, which represent the upper, middle, and lower parts of the caster, during the speed drop with the hysteresis effect or the Leidenfrost effect. Because the secondary cooling sprays are left constant after the speed drop, the surface temperature of the strand is decreasing and cooling set of h-multipliers are used for the hysteresis effect case. Table 2.3 shows that the h-multipiers of the cooling set are smaller than those of the heating set, and the h-multipiers of the heating set are same as the ones used in the Leidenfrost effect. Therefore, the average temperature with the hysteresis effect should be higher than the temperature with the Leidenfrost effect during the transition. The temperatures with the hysteresis effect have bigger vibration than those with the Leidenfrost effect, this is because for the case with hysteresis effect the
h-multipliers switch between the cooling set and the heating set when the temperature vibrates.

Figures 2.15 and 2.16 show the temperature profiles along the caster 10 and 20 minutes after the speed drop. Figures 2.17 through 2.19 are the amplification of Figure 2.15 around zone 4, zone 8, and zone 12. Figures 2.20 and 2.21 are the amplification of Figure 2.16 for zone 8 and zone 12. Figure 2.12 shows that the temperature has already reached steady state for both cases (the hysteresis effect and the Leidenfrost effect) for zone 4. Therefore the temperature profiles of these two cases are identical in Figure 2.17.

In Figures 2.18 through 2.21, the temperatures for zone 8 and zone 12 haven’t reached steady state yet. The figures show that the temperature with the hysteresis effect is higher than that with the Leidenfrost effect. The figures also show that the temperature profiles under the sprays are quite different for these two effects, which is because two effects use different sets of h-multipliers when the temperature is decreasing leading to different heat transfer coefficients of spray cooling water. The heat transfer coefficients of roll contact are not affected by these two effects, the temperature differences at roll contacts are caused by the temperature differences under the sprays.
Figure 2.1: Schematic of the spray cooling region [4].
Figure 2.2: Illustration of delay interpolation method, with two slices and constant casting speed [4].
Figure 2.3: Simulation domain in the shell.
Figure 2.4: Simulation result of the temperature profile along the $x-$axis at different part of the caster.

Figure 2.5: Analytical result of the temperature profile along the $x-$axis at different part of the caster.
Figure 2.6: Comparison of simulation and analytical results of the temperature profile along the $x$–axis at different part of the caster.
Figure 2.7: Predictions of dynamic temperature, solidification, and thermal shrinkage model during series of speed change in Burns Harbor caster, compared to measured roll loads from [6].
Figure 2.8: Generic boiling curve for water cooling indicating the different heat transfer regions [32].

Figure 2.9: Boiling curves for a thermal loop with temperature between 200$^\circ$C and 1200$^\circ$C [34].
Figure 2.10: Flow chart of the CONONLINE model with the hyteresis effect calculating the spray water heat transfer coefficient.

Figure 2.11: Strand surface temperature along the whole caster under steady state of two casting speeds.
Figure 2.12: CONONLINE prediction of the average temperature of zone 4 with hysteresis effect feature during the speed drop.

Figure 2.13: CONONLINE prediction of the average temperature of zone 8 with hysteresis effect feature during the speed drop.
Figure 2.14: CONONLINE prediction of the average temperature of zone 12 with hysteresis effect feature during the speed drop.

Figure 2.15: Snapshot of the surface temperature along the caster 10 min after the speed drop.
Figure 2.16: Snapshot of the surface temperature along the caster 20 min after the speed drop.

Figure 2.17: Snapshot of the surface temperature in zone 4 10 min after the speed drop (amplification of Figure 2.15).
Figure 2.18: Snapshot of the surface temperature in zone 8 10 min after the speed drop (amplification of Figure 2.15).
Figure 2.19: Snapshot of the surface temperature in zone 12 10 min after the speed drop (amplification of Figure 2.15).
Figure 2.20: Snapshot of the surface temperature in zone 8 20 min after the speed drop (amplification of Figure 2.16).

Figure 2.21: Snapshot of the surface temperature in zone 12 20 min after the speed drop (amplification of Figure 2.16).
CHAPTER 3

STUDY OF DYNAMIC THERMAL BEHAVIOR OF A THICK-SLAB CASTER

Computational models, while difficult to design, program, and validate, have at least two benefits over experiments on actual systems. First, experiments can be performed on models that would be expensive, dangerous, or impossible to be performed on actual systems. The conditions can be designed systematically and controlled precisely. Second, models can produce outputs that cannot be measured practically. In this chapter, both benefits of a computational model – CONONLINE, which has been validated through measurements from real plants, are utilized to explore the dynamic thermal behavior of a thick-slab caster.

3.1 Casting Conditions

A great deal of information (like slab geometry, pouring temperature, and steel grade) about a caster need to be known to model the solidification and heat transfer. The following simulations are based on the thick-slab (259.5 mm-thick) caster at ArcelorMittal Steel in Burns Harbor, Indiana. Some of the casting conditions, including caster geometry, roll pitches, and casting speeds, were taken from the text and figure in [31]. Other parameters, such as steel grade, pour temperature, and heat flux in the mold and secondary cooling zone were based on best available information from previous experience [3, 4], and calibrated to match the reported metallurgical lengths.

The steel simulated is low Carbon (0.05 weight-% Carbon) steel with properties given in Table 3.1. For simplicity, boundary heat flux is assumed to be the same on both sides of the strand. Based on the equation characterized in [35] from extensive plant measurements for thin-slab casters, the average heat flux in the mold was controlled to be:
Table 3.1: Steel properties in simulation

<table>
<thead>
<tr>
<th>Property</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Liquidus temperature</td>
<td>1532.1 °C</td>
</tr>
<tr>
<td>Solidus temperature</td>
<td>1515.3 °C</td>
</tr>
<tr>
<td>Latent heat of solidification</td>
<td>271 kJ/kg</td>
</tr>
</tbody>
</table>

\[ Q_m \left[ MW/m^2 \right] = 0.9535 \left( v_c \right)^{0.5} \quad (3.1) \]

The exponent was chosen to be 0.5, which is the theoretical value that gives constant surface temperature in the mold [36], and is close to those reported in [33, 35, 37]. The coefficient was chosen together with the spray water flow rates in order to match the reported metallurgical lengths in [31]: 28 m at the casting speed of 1.1 m/min and 23 m at the casting speed of 0.9 m/min. This average mold heat flux was converted into a heat flux profile using Equations (2)-(7) in [4]. The pouring temperature is 1550 °C.

The heat flux in the secondary cooling region was assumed to be uniform throughout the caster; the spray flow rates were kept constant during speed changes, to investigate the dynamic thermal behavior of the heat transfer and solidification alone.

3.2 Constant Secondary Cooling

Currently, only cases of sudden speed drops are considered in this work. The maximum casting speed reported in [31] (1.14 m/min) is chosen as the initial speed and casting speeds of 0.95 m/min, 0.76 m/min and 0.57 m/min were chosen to be the final speeds in the following simulations. The reason for choosing 0.76 m/min is that it is the minimum casting speed reported in [31], as for 0.95 m/min and 0.57 m/min, they were chosen to form an arithmetic casting speed sequence to study the effects of different speed drops. At time \( t = 0 \) seconds, the casting speed suddenly drops.

In this work, the settling time for the metallurgical length is defined as the time from the beginning of speed change to the time when it reaches steady state, and the settling time for the surface temperature is defined as the time from the beginning of the speed drop to the time when the temperature reaches within 10 °C of its final value.
Figures 3.1 through 3.6 are model prediction results of the metallurgical length and the surface temperature for the speed drops of 0.57 m/min, 0.38 m/min, and 0.19 m/min. The results show that after the speed drop, the metallurgical lengths gradually decrease for all three cases. The surface temperatures at mold exit first increase and then decrease to a lower steady state, the average surface temperatures for other three segments (spray zones) gradually decrease to lower values. The settling times for the metallurgical length under different speed changes are approximately the same. But for the surface temperature, it takes longer for larger speed drops to reach steady state.

Figure 3.7 compares the transient behavior of the metallurgical length for the three different speed drops. All three lines appear to be linear after the speed drop. For the thick-slab caster studied, this linear relation works for the whole transient progress, but as found in [6] for a thin-slab caster, it only works for initial stage before the “pinch off” happens. Figure 3.8 compares the transient behavior of the metallurgical length for four different speed drops of a thin-slab caster [6]. In the case of the large speed drop (3 m/min), there is a sudden drop in the metallurgical length near the end of the transition, due to the pinching off of the liquid core. This is because for thin-slab casters, when the speed drop is too large, it is possible that the steel strand in the upper region of the caster is already fully solid while there is still liquid steel further down in the caster. The sudden drop in the metallurgical length is due to a phenomenon called “pinch off”, and is likely responsible for causing centerline segregation and other centerline defects during rapid speed drops. Pinch off is less likely to happen in thick-slab casters.

Table 3.2: Results of linear regression on ML data in Figure 3.7

<table>
<thead>
<tr>
<th></th>
<th>Case 1</th>
<th>Case 2</th>
<th>Case 3</th>
</tr>
</thead>
<tbody>
<tr>
<td>Final casting speed (m/min)</td>
<td>0.95</td>
<td>0.76</td>
<td>0.57</td>
</tr>
<tr>
<td>Casting speed drop (m/min)</td>
<td>0.19</td>
<td>0.38</td>
<td>0.57</td>
</tr>
<tr>
<td>Slope of ML (m/min)</td>
<td>-0.193</td>
<td>-0.382</td>
<td>-0.567</td>
</tr>
<tr>
<td>Settling time (sec)</td>
<td>1400</td>
<td>1406</td>
<td>1417</td>
</tr>
</tbody>
</table>

The results in Table 3.2 show that the absolute value of the slopes are almost equal to the corresponding speed drops. And this match can be predicted by the K-factor model introduced in [6].

The simplest model to predict shell thickness $s$ at distance $z$ from the
meniscus is approximately

\[ s(t) = K \sqrt{t(z)} \] (3.2)

where \( t(z) \) is the time for steel to travel from the meniscus to the point \( z \) in the caster. When the casting speed is constant, equation (3.2) is simply

\[ s(z) = s(t(z)) = K \sqrt{\frac{z}{v_c}} \] (3.3)

where, \( v_c \) is the casting speed. Due to the symmetry of strand and the assumption that heat flux boundary conditions at both sides are the same, the metallurgical length is the distance from the meniscus to the location \( z_{ML} \) at which the shell thickness is half the thickness of the stand, i.e. \( s(z_{ML}) = S/2 \). Then from equation (3.2), the metallurgical length can be calculated by the following:

\[ \frac{S}{2} = K \sqrt{t(z_{ML})} \] (3.4)

When the steel is casting at constant casting speed \( v_c \), equation (3.4) can be solved to get the predicted metallurgical length by the following equation:

\[ z_{ML} = \frac{S^2}{4K^2v_c} \] (3.5)

For the case of sudden speed change from \( v_{c1} \) to \( v_{c2} \) at time \( t = 0 \). The time it takes for steel to travel from the meniscus to any location \( z \) in the caster at any time \( t \) can be directly calculated by the following equation:

\[
t(z) = \begin{cases} 
\frac{z}{v_{c1}}, & t < 0 \\
\frac{z + (v_{c1} - v_{c2})t}{v_{c1}}, & 0 \leq t \leq \frac{z}{v_{c2}} \\
\frac{z}{v_{c2}}, & \frac{z}{v_{c2}} < t
\end{cases}
\] (3.6)

The following equation is derived by combining equations (3.6) and (3.4):

\[
z_{ML}(t) = \begin{cases} 
\frac{S^2}{4K^2v_{c1}}, & t < 0 \\
\frac{S^2}{4K^2v_{c1}} - t(v_{c1} - v_{c2}), & 0 \leq t \leq \frac{S^2}{4K^2} \\
\frac{S^2}{4K^2v_{c2}}, & \frac{S^2}{4K^2} < t
\end{cases}
\] (3.7)
The $K$-factor model derived above predicts two things. First, the change of metallurgical length after sudden speed drop is proportional to the difference of two casting speeds. As illustrated in Figure 3.7, the slopes are equal to the corresponding speed drops. Second, the transient behavior of the metallurgical length takes the same amount of time to stabilize for different speed drops, which means that the settling time for metallurgical length does not depend on the casting speed and can be estimated by the following simple equation:

$$t_s = \frac{S^2}{4K^2}$$  \hspace{1cm} (3.8)

Table 3.2 shows that the metallurgical length settling times for different speed drops are very close. In Table 3.3, the actual $K$-factors were calculated for each of the steady conditions in these simulations, based on half slab thickness of 129.75 mm (5.1 in). Each speed has a slightly different $K$-factor associated with it, but all the $K$-factors are very close to the typical text-book value of 1 in/min$^2$. For casting speed 0.57 m/min, the metallurgical length is 13.58 m. Considering the caster is 30 m long, this is a very low casting speed. Also the maximum casting speed that ArcelorMittal Burns Harbor caster can get is around 1.2 m/min, otherwise the metallurgical length will exceed the total length of the caster. 1.14 m/min is already very close to it.

Table 3.3: Calculation of k-factor

<table>
<thead>
<tr>
<th>Casting speed (m/min)</th>
<th>Steady-state ML (m)</th>
<th>Time from meniscus to ML (min)</th>
<th>$K$ (in/min$^{0.5}$)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1.143</td>
<td>26.92</td>
<td>23.55</td>
<td>1.051</td>
</tr>
<tr>
<td>0.95</td>
<td>22.42</td>
<td>23.6</td>
<td>1.050</td>
</tr>
<tr>
<td>0.76</td>
<td>17.99</td>
<td>23.67</td>
<td>1.048</td>
</tr>
<tr>
<td>0.57</td>
<td>13.58</td>
<td>23.82</td>
<td>1.045</td>
</tr>
</tbody>
</table>

Figures 3.2, 3.4 and 3.6 show that the average temperatures gradually decrease. It takes longer for temperature to change in segment (spray zone) 12 and segment 16, because these segments, being further away from the meniscus, have longer dwell time. In Figure 3.4, the surface temperature for segment 16 has not reached steady state value yet, therefore the settling time for segment 16 is not included in Figure 3.9.

After sudden speed changes, the surface temperature at location $z$ reaches steady state after the slice, which was generated at the meniscus at the
time of the speed change, travels to location $z$. To calculate the settling
time for average temperature of each segment, $z$ was chosen to be the end
of each segment. The estimations and actual settling times calculated with
the CONONLINE model are shown in Figure 3.9. Based on the previous
discussion, the dwell time in each zone will have an upper bound. Denoting
this upper bound estimate of the settling time as $t_{max}$, the settling time after
a speed change can be estimated in any zone by solving the inverse of the
following equation:

$$z = \int_0^{t_{max}} v_c dt$$

(3.9)

where $z$ is the distance of particular location in the caster below the menis-
cus. Since in these simulations, the speed is constant after the initial change,
equation (3.9) can be easily solved:

$$t_{max} = \frac{z}{v_{c,final}}$$

(3.10)

where $v_{c,final}$ is the final casting speed. The predicted settling times of
surface temperature and the simulation results are shown in Figure 3.9, and
they match quite well.
Figure 3.1: Model prediction of metallurgical length during casting speed change from 1.14 $m/min$ to 0.57 $m/min$ under constant secondary cooling spray.
Figure 3.2: Model prediction of average surface temperature for different segments during casting speed change from 1.14 m/min to 0.57 m/min under constant secondary cooling spray.

Figure 3.3: Model prediction of metallurgical length during casting speed change from 1.14 m/min to 0.76 m/min under constant secondary cooling spray.
Figure 3.4: Model prediction of average surface temperature for different segments during casting speed change from 1.14 m/min to 0.76 m/min under constant secondary cooling spray.

Figure 3.5: Model prediction of metallurgical length during casting speed change from 1.14 m/min to 0.95 m/min under constant secondary cooling spray.
Figure 3.6: Model prediction of average surface temperature for different segments during casting speed change from $1.14 \text{ m/min}$ to $0.95 \text{ m/min}$ under constant secondary cooling spray.

Figure 3.7: Metallurgical length during sudden speed drops under constant secondary cooling spray.
Figure 3.8: Metallurgical length during sudden speed drops for thin-slab caster [6].

Figure 3.9: Settling times for average surface temperature.
Strand temperature, both the surface and the internal temperature, is vital to steel product quality. Because the mechanical properties of steel are highly related to the temperature, and the mechanical properties have considerable influences on the formation of defects like cracks. Therefore, it is important to maintain the surface temperature during speed changes, especially for those steel grades which are sensitive to surface defects. This chapter explores the performance of different control methods on maintaining the surface temperature during small speed drops for illustrative example case based on the JFE thick-slab caster.

4.1 Casting Conditions

Realistic caster and casting conditions are chosen for the following simulations. The simulations in this chapter are based on the thick-slab (221 mm-thick) caster at JFE Steel used for the test simulation in chapter 2. The steel grade studied in this chapter has the properties given in Table 4.1.

Based on an empirical correlation for a thin-slab caster proposed by Duvvuri [35], the average heat flux in the mold was controlled to be:

\[ Q_m \ [MW/m^2] = 1.2154 (v_c)^{0.47} \]

The coefficient and exponent are chosen to match the reported data from

<table>
<thead>
<tr>
<th>Table 4.1: Steel properties in simulation</th>
</tr>
</thead>
<tbody>
<tr>
<td>Liquidus temperature</td>
</tr>
<tr>
<td>Solidus temperature</td>
</tr>
<tr>
<td>Peritectic temperature</td>
</tr>
<tr>
<td>Latent heat of solidification</td>
</tr>
</tbody>
</table>
JEF Steel. More casting conditions are listed in Table 4.2.

For the heat flux of the spray cooling water in the secondary cooling region, Nozaki’s empirical correlation \([27]\) was used:

\[
h_{\text{spray}} = 0.3925 \times Q_{\text{water}}^{0.55} \times (1 - 0.0075 \times T_{\text{spray}}) \tag{4.2}
\]

where \(Q_{\text{water}} (L/m^2 \cdot s)\) is water flux in the spray zones, \(T_{\text{spray}}\) is the temperature of the cooling water spray. The heat transfer in the secondary cooling region is a subject of ongoing research, and other relations are available and used at different casters (including JFE).

Table 4.2: Casting conditions of the JFE caster simulations

<table>
<thead>
<tr>
<th>Value</th>
<th>Unit</th>
</tr>
</thead>
<tbody>
<tr>
<td>Density of solid steel, (\rho)</td>
<td>7400 kg/m(^3)</td>
</tr>
<tr>
<td>Steel emissivity, (\varepsilon_{\text{steel}})</td>
<td>0.8 /</td>
</tr>
<tr>
<td>Fraction solid for shell thickness location, (f_s)</td>
<td>0.3 /</td>
</tr>
<tr>
<td>Specific heat of solid steel, (c_p)</td>
<td>670 J/kg \cdot K</td>
</tr>
<tr>
<td>Thermal conductivity of solid steel, (k)</td>
<td>30 W/mK</td>
</tr>
<tr>
<td>Thermal diffusivity of solid steel, (\alpha)</td>
<td>(6.0508 \times 10^{-6}) /</td>
</tr>
<tr>
<td>Initial cooling water temperature (T_{\text{water}})</td>
<td>29.67 (^\circ)C</td>
</tr>
<tr>
<td>Slab thickness, (L_x)</td>
<td>221 mm</td>
</tr>
<tr>
<td>Slab width, (L_y)</td>
<td>2095 mm</td>
</tr>
<tr>
<td>Casting speed, (v)</td>
<td>1.5 1.7 m/min</td>
</tr>
<tr>
<td>Ambient temperature, (T_{\infty})</td>
<td>35 (^\circ)C</td>
</tr>
<tr>
<td>Pouring temperature, (T_{\text{pour}})</td>
<td>1545 (^\circ)C</td>
</tr>
<tr>
<td>Mold conductivity (WF/NF), (k_{\text{mold}})</td>
<td>418.7/355 W/mK</td>
</tr>
<tr>
<td>Time step, (dt)</td>
<td>0.01 s</td>
</tr>
<tr>
<td>Mesh size, (dx)</td>
<td>0.55 mm</td>
</tr>
</tbody>
</table>

The speed change scenario simulated in this work is a sudden speed drop from 1.7 m/min to 1.5 m/min. However, in the real caster in JFE Steel, the casting speed first drops from 1.7 m/min to 1.6 m/min, stays at 1.6 m/min for 4.2 seconds and then drops to 1.5 m/min. This is because there are limits of sudden speed drops that can be applied during the continuous casting process. If a sudden speed drop is too big, it might be unable to maintain constant steel flow in the caster and cause problems like breakout, especially for thick-slab casters. Therefore, when a large speed drop is needed during operation, the speed drop is made within certain time interval (usually seconds), i.e. the casting speed decreases gradually.
If the actual speed change scenario (1.7 m/min to 1.6 m/min then to 1.5 m/min) was used in the simulations, extra transient behavior might be introduced into the results. Thus, an instantaneous speed drop from 1.7 m/min to 1.5 m/min was used for the following simulations.

The speed drop of 0.2 m/min is small. The results in section 4.2.1 show that under this small speed drop, maintaining constant surface temperature under steady state of these two casting speeds is achievable by applying feasible water flow rates. The limitations of spray flow rates for each spray zone, i.e. the maximum \(Q_{i,sw\max}\) and minimum \(Q_{i,sw\min}\) flow rates allowed, are listed in Table 4.3. At each spray zone, there are a series of rows of spray nozzles; the spray flow rates of spray zone \(i\), denoted as \(Q_{i,sw}\) throughout this thesis, can be calculated by:

\[
Q_{i,sw}^i = q_{i,sw}^i / m_r
\]  

(4.3)

where \(q_{i,sw}[L/min]\) is the total amount of water applied to spray zone \(i\), \(m_r\) is the number of rows of spray nozzles in zone \(i\), and \(Q_{i,sw}^i[L/min/row]\) is the water flow rate for each row of spray nozzles in spray zone \(i\).

Table 4.3: Maximum and minimum flow rates allowed

<table>
<thead>
<tr>
<th>Zone</th>
<th>(Q_{i,sw\max}^i) (L/min/row)</th>
<th>(Q_{i,sw\min}^i) (L/min/row)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>250</td>
<td>75.0</td>
</tr>
<tr>
<td>2</td>
<td>286.67</td>
<td>28.67</td>
</tr>
<tr>
<td>3</td>
<td>360</td>
<td>36</td>
</tr>
<tr>
<td>4</td>
<td>200</td>
<td>10</td>
</tr>
<tr>
<td>5</td>
<td>200</td>
<td>10</td>
</tr>
<tr>
<td>6</td>
<td>90</td>
<td>4.5</td>
</tr>
<tr>
<td>7</td>
<td>90</td>
<td>4.5</td>
</tr>
<tr>
<td>8</td>
<td>60</td>
<td>3</td>
</tr>
<tr>
<td>9</td>
<td>30.59</td>
<td>1.53</td>
</tr>
<tr>
<td>10</td>
<td>15</td>
<td>1.0</td>
</tr>
<tr>
<td>11</td>
<td>15</td>
<td>1.0</td>
</tr>
<tr>
<td>12</td>
<td>15</td>
<td>1.0</td>
</tr>
</tbody>
</table>

In order to investigate the dynamic thermal behavior of the steel strand during changes between two specific speeds (1.5 m/min and 1.7 m/min), the original spray table was modified to make a hypothetical, but still realistic, example of a part of a spray table. Specifically, the water flow rates used in the plant at a casting speed of 1.5 m/min were changed in the different
spray zones to match the surface temperature profile of 1.7 \textit{m/min}, using the mold heat-flux equation (4.1), and Nozoki heat flux / water flow rate relation given earlier in equation (4.2).

For the secondary cooling region, four different control methods were applied to explore their performance on maintaining the surface temperature: constant spray cooling, spray table control, time-constant control, and PI control of surface temperature.

Before running simulations of different control methods, CONONLINE model was first calibrated according to provided casting conditions. Figure 4.1 shows the shell thickness profile under steady state for two casting speeds, spray flow patterns used in this simulation are listed in Table 4.5. The predicted metallurgical lengths of both speeds match the measured data. Figures 4.2 and 4.3 show that the predicated surface temperatures agree with the measurements at steady state at these two casting speeds. After calibration, the CONONLINE model was ready to be used for further simulations.

4.2 Constant Spray Cooling (No Spray Control)

With this control “method”, after the speed drop, the spray flow rates in the secondary cooling region are kept constant, i.e. there are no spray control of the secondary cooling region. The specific spray flow rates used are from Table 4.5 for casting speed of 1.7 \textit{m/min} (‘1.7-orig’).

Figures 4.4 through 4.6 illustrate the transient behavior of the surface temperature after the speed drop. For spray zone 1-2, the temperature first increases and then decreases; while for the rest spray zones, the temperature gradually decreases. The new steady state temperature of all spray zones are lower than the initial values, which agree with the findings in Chapter 3.

4.3 Spray Table Control

4.3.1 General spray table control

For spray table control, the spray flow rates in spray zones, or spray patterns, that produce good quality steel are determined by experience, plant trial and
error, and steady state modeling. Higher casting speed requires higher water flow rates to maintain same cooling conditions. These spray patterns depend on steel grade, production dimension, casting speed, and machine design. During the continuous casting process, if the casting speed changes, plant operators or an automatic level 2 control system will instantly change spray water flow rates according to the spray patterns defined in the table for that speed.

A typical spray table is shown in Table 4.4, where $Q_{sw}^i(v_{cj})$ represents the spray flow rate of spray zone $i$ at casting speed $v_{cj}$. For an arbitrary casting speed $v_c$, define $v_{c\text{upper}}$ as the minimum casting speed $v_{cj}$ in the spray table that is bigger than $v_c$, and $v_{c\text{lower}}$ as the maximum casting speed in spray table that is smaller than $v_c$. For the current casting speed $v_c(t)$, CONONLINE searches through the casting speeds $v_{cj}$ in the spray table to find the corresponding $v_{c\text{lower}}$ and $v_{c\text{upper}}$. Then the spray flow rate of spray zone $i$ at casting speed $v_c$ can be calculated by the following equation:

$$Q_{sw}^j(v_c) = Q_{sw}^j(v_{c\text{lower}}) \frac{v_{c\text{upper}} - v_c}{v_{c\text{upper}} - v_{c\text{lower}}} + Q_{sw}^j(v_{c\text{upper}}) \frac{v_c - v_{c\text{lower}}}{v_{c\text{upper}} - v_{c\text{lower}}} \quad (4.4)$$

<table>
<thead>
<tr>
<th>Spray zone</th>
<th>$Q_{sw}$ at $v_{c1}$ $(L/min/row)$</th>
<th>$Q_{sw}$ at $v_{c2}$ $(L/min/row)$</th>
<th>$Q_{sw}$ at $v_{c3}$ $(L/min/row)$</th>
<th>...</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>$Q_{sw}^1(v_{c1})$</td>
<td>$Q_{sw}^1(v_{c2})$</td>
<td>$Q_{sw}^1(v_{c3})$</td>
<td>...</td>
</tr>
<tr>
<td>2</td>
<td>$Q_{sw}^2(v_{c1})$</td>
<td>$Q_{sw}^2(v_{c2})$</td>
<td>$Q_{sw}^2(v_{c3})$</td>
<td>...</td>
</tr>
<tr>
<td>3</td>
<td>$Q_{sw}^3(v_{c1})$</td>
<td>$Q_{sw}^3(v_{c2})$</td>
<td>$Q_{sw}^3(v_{c3})$</td>
<td>...</td>
</tr>
<tr>
<td>4</td>
<td>$Q_{sw}^4(v_{c1})$</td>
<td>$Q_{sw}^4(v_{c2})$</td>
<td>$Q_{sw}^4(v_{c3})$</td>
<td>...</td>
</tr>
<tr>
<td>...</td>
<td>...</td>
<td>...</td>
<td>...</td>
<td>...</td>
</tr>
</tbody>
</table>

For example, consider the spray table shown in Table 4.4, if $v_{c1} < v_c < v_{c2}$, then $v_{c\text{upper}} = v_{c2}$ and $v_{c\text{lower}} = v_{c1}$; if $v_{c2} < v_c < v_{c3}$, then $v_{c\text{upper}} = v_{c3}$ and $v_{c\text{lower}} = v_{c2}$.

Suppose the caster starts casting at $t = 0$, then the spray flow rate of spray zone $i$ at time $t$ can be calculated by:

$$Q_{sw}^i(t) = Q_{sw}^i(v_c(t)) \quad (4.5)$$
4.3.2 Implementation for thick-slab caster

The goal of spray table control is to maintain the same surface temperature after a speed change. Therefore, two hypothetical but realistic spray patterns at 1.5 m/min and 1.7 m/min, which give same average surface temperature at steady state, are listed in Table 4.5. These two spray patterns are referred to as ‘1.7-orig’ and ‘1.5-sameT’ in the following sections. Table 4.5 is actually part of a spray table, and since the casting speed change scenario in this study is from 1.7 m/min to 1.5 m/min, it is also called a “spray table” in this thesis. The flow rate of spray zone $i$ at time $t$ after the start up can be calculated by:

$$Q_{sw}^i(t) = Q_{sw}^i(v_c(t)) = Q_{sw}^i(1.5\text{-sameT}) \frac{1.7 - v_c}{0.2} + Q_{sw}^i(1.7\text{-orig}) \frac{v_c - 1.5}{0.2}$$

where $Q_{sw}^i(1.5\text{-sameT})$ and $Q_{sw}^i(1.7\text{-orig})$ are the spray flow rates shown in Table 4.5. The evolution of water flow rates with time for all spray zones under spray table control are shown in Figure 4.8. The average surface temperatures produced from spray patterns of ‘1.7-orig’ and ‘1.5-sameT’ are listed in Table 4.6.

Table 4.5: Hypothetic but realistic spray patterns that produces the same average surface temperature at two casting speeds of 1.7 and 1.5 m/min

<table>
<thead>
<tr>
<th>Zone</th>
<th>Spray pattern</th>
<th>1.7-orig ($L/min/row$)</th>
<th>1.5-sameT ($L/min/row$)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td></td>
<td>90.2</td>
<td>75.0</td>
</tr>
<tr>
<td>2</td>
<td></td>
<td>61.9</td>
<td>54.0</td>
</tr>
<tr>
<td>3</td>
<td></td>
<td>98.2</td>
<td>85.0</td>
</tr>
<tr>
<td>4</td>
<td></td>
<td>127.9</td>
<td>111.0</td>
</tr>
<tr>
<td>5</td>
<td></td>
<td>111.0</td>
<td>98.0</td>
</tr>
<tr>
<td>6</td>
<td></td>
<td>70.9</td>
<td>62.0</td>
</tr>
<tr>
<td>7</td>
<td></td>
<td>51.0</td>
<td>44.0</td>
</tr>
<tr>
<td>8</td>
<td></td>
<td>19.1</td>
<td>16.5</td>
</tr>
<tr>
<td>9</td>
<td></td>
<td>6.0</td>
<td>5.0</td>
</tr>
<tr>
<td>10</td>
<td></td>
<td>4.1</td>
<td>2.8</td>
</tr>
<tr>
<td>11</td>
<td></td>
<td>3.6</td>
<td>1.4</td>
</tr>
<tr>
<td>12</td>
<td></td>
<td>6.5</td>
<td>3.6</td>
</tr>
</tbody>
</table>

Figure 4.7 shows the CON1D model prediction of surface temperature profile along the caster under these two spray patterns – ‘1.7-orig’ and ‘1.5-
Table 4.6: Average surface temperature for each spray zone with spray pattern of ‘1.7-orig’ and ‘1.5-sameT’ from CON1D

<table>
<thead>
<tr>
<th>Zone</th>
<th>Average temperature '1.7-orig' °C</th>
<th>Average temperature '1.5-sameT' °C</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>1205</td>
<td>1202</td>
</tr>
<tr>
<td>2</td>
<td>1114</td>
<td>1116</td>
</tr>
<tr>
<td>3</td>
<td>1065</td>
<td>1065</td>
</tr>
<tr>
<td>4</td>
<td>927.1</td>
<td>928</td>
</tr>
<tr>
<td>5</td>
<td>834.9</td>
<td>835.2</td>
</tr>
<tr>
<td>6</td>
<td>791.4</td>
<td>791.2</td>
</tr>
<tr>
<td>7</td>
<td>768.5</td>
<td>769.1</td>
</tr>
<tr>
<td>8</td>
<td>778.4</td>
<td>777.6</td>
</tr>
<tr>
<td>9</td>
<td>820.1</td>
<td>818.7</td>
</tr>
<tr>
<td>10</td>
<td>813.3</td>
<td>812.4</td>
</tr>
<tr>
<td>11</td>
<td>788.7</td>
<td>787.6</td>
</tr>
<tr>
<td>12</td>
<td>722.7</td>
<td>721.2</td>
</tr>
</tbody>
</table>

sameT’, and the temperature profiles are almost identical. This figure indicates again that the surface temperature varies a lot within a small distance due to the complex cooling conditions in the spray cooling region. Figure 4.9 shows the surface temperatures at five different locations: zone 7-beneath roll 36 (10.56 m down from meniscus), zone 7-between roll 36 and roll 37 (10.59 m), zone 7-beneath spray(10.71 m), caster exit (49 m), and beneath pyrometer after caster exit (49.13 m). The local variation of surface temperature including changes between roll pitches in each spray zone along the caster actually is indicated by the surface temperature profile along the caster (Figure 4.7). Therefore, in order to evaluate the controller’s performance on maintaining surface temperature, the average surface temperature of each spray zone instead of the surface temperature at specific locations is chosen to be studied.

Figures 4.10 through 4.12 show the average surface temperature profile of all spray zones under spray table control. The average surface temperature of each spray zone immediately increases after the speed drop, then gradually decreases to steady state. After the speed drop, the spray water flow rates suddenly drop on the slice partly finished the process, so the slice gets less water than it is should. As a result, its surface temperature is expected to increase (overshoot) before returning to the steady state value, which is expected to match the surface temperature before the speed drop if the spray
table is good. The overshoot is larger further down in the caster, because the underspraying occurs on a slice that is almost finished which leaves no time for the surface temperature overshoot to get corrected by further time in the caster at the new lower casting speed. Specifically, the overshoot increases from around 10-12 °C in spray zones 1-10 up to around 20-25 °C in zones 11-12. Based on the discussion in chapter 3, the time it takes for the surface temperature to get back to steady state should equal the time needed for the steel to move from the meniscus to the end of that zone. Thus, lower in the caster, it takes longer for surface temperature to get to steady state.

Figure 4.13 shows the metallurgical length profile under spray table control. After the speed drop, the metallurgical length gradually decreases to a lower steady state value. Because the spray flow patterns are designed to maintain the surface temperature, the steady state metallurgical length would not match.

4.4 Time-constant Control

4.4.1 General time-constant control

As discussed in the previous section, the spray table control method simply gives different flow rates for different casting speed. Sudden speed changes will lead to sudden flow rate changes; This will result in sudden fluctuations of the surface temperature, which is not good for steel surface quality. Therefore, a classic type of control algorithm, which is often called “dynamic spray cooling”, has been developed and widely used in the steel industry to improve on simple spray table control. These methods all include the concept of cooling time [11]. The original method has been called the “residence time”, “element lifetime”, or “effective lifetime” method [11], and is referred to as “time constant” control method in this work. A more general dynamic spray cooling method, which combines the spray table cooling velocity and cooling with time concepts together with an empirical mixture constant, and is called the effective speed control method [10, 38].

Under conditions of constant casting speed, the strand surface temperature at a given location z along the caster is constant and depend on the time it takes for the slice to reach that point from the meniscus, i.e. the “dwell time”
of a slice, and the cooling conditions encountered. An increase in the casting speed will cause the dwell time to decrease while the opposite will occur with a decrease in the casting speed. The idea of time constant control method is to change the flow rates according to the dwell time. After calculating the dwell time, the average casting speed \( \bar{v}_c \) for a slice to reach location \( z \) from the meniscus can be calculated. Then \( \bar{v}_c \) of all the slices in the spray zone \( i \) are averaged to get the average casting speed \( \bar{v}_{ci} \) of the spray zone \( i \). Finally, the average casting speed \( \bar{v}_{ci} \) of the spray zone \( i \) is used to determine the spray flow rates from the spray tables.

The effective speed control method is an combination of spray table control and time constant control. The effective casting speed calculated from the following equation is used to calculate flow rates from spray tables:

\[
v_{ei}(t) = \epsilon_i \bar{v}_{ci}(t) + (1 - \epsilon_i)v_c(t)
\]  

(4.7)

where \( \epsilon_i \) is a weighting coefficient, which is between 0 and 1, and depends on the distance from the center of the spray zone \( i \) to the meniscus. The water flow rate command for spray zone \( i \) under the effective speed control method can be calculated by:

\[
Q_{sw}^i(t) = Q_{sw}(v_{ei}(t)) = \epsilon_i Q_{sw}(\bar{v}_{ci}(t)) + (1 - \epsilon_i)Q_{sw}(v_c(t))
\]  

(4.8)

If \( \epsilon_i = 1 \), then equation (4.8) becomes equation (4.12), the effective speed control method is the same as time constant control; If \( \epsilon_i = 0 \), then equation (4.8) becomes equation (4.5), the effective speed control method is the same as spray table control.

The effective casting speed causes smoothing of the real casting speed, especially in spray zones that are further down in the caster [38]. As a result, the effective speed control smooths out the surface temperature during the transient speed fluctuations in comparison to that under the spray table control [10].

In this work, \( \epsilon_i = 1 \) were chosen for all spray zones, to investigate the extreme case of time-constant control only (considering that we already have spray table control). The average casting speed \( \bar{v}_{ci} \) of the spray zone \( i \) is used to determine the spray flow rates.

Consider the following general case: at wall-clock time \( t = 0 \), the caster starts casting and the steel is casting at time-varying casting speed \( v_c(t) \), z
is an arbitrary location in the caster. The dwell time \( \tau(z, t) \) is defined as the
time it takes for the steel to travel from the meniscus to location \( z \) at time
\( t \), and it can be found by solving the inverse of the following equation:

\[
z = \int_{t-\tau(z,t)}^{t} v_c(s)ds \tag{4.9}
\]

Then the average casting speed for the steel to move from the meniscus to
location \( z \) at time \( t \) from the start up of the casting process can be calculated
from equation (4.10):.

\[
\bar{v}_c(z, t) = \frac{z}{\tau(z, t)} \tag{4.10}
\]

In the continuous casting process, one spray zone usually can only have one
spray flow rate, so instead of the average casting speed \( \bar{v}_c(z, t) \) for location
\( z \), the average casting speed \( \bar{v}_{ci}(t) \) for the spray zone \( i \) at time \( t \) needs to be
calculated. Then \( \bar{v}_{ci}(t) \) can be substituted into equation (4.4) as \( v_c \) to get
the spray flow rate for the spray zone \( i \).

In the time-constant control method, the following equation is used to
calculate \( \bar{v}_{ci}(t) \):

\[
v_{ci}(t) = \frac{z_{mid}(i)}{\tau(z_{end}(i), t)} \tag{4.11}
\]

where, \( \tau(z_{end}(i), t) \) is the solution to the inverse of equation (4.9), \( z_{end}(i) \)
is the distance from the meniscus to the end of spray zone \( i \) and \( z_{mid}(i) \) is the
distance from the meniscus to the middle of spray zone \( i \).

Then the spray flow rate for spray zone \( i \) can be calculated by:

\[
Q_{sw}^i(t) = Q_{sw}^i(v_{ci}(t)) = Q_{sw}^i(v_{c}^{lower}) \frac{v_{c}^{upper} - v_{ci}}{v_{c}^{upper} - v_{c}^{lower}} + Q_{sw}^i(v_{c}^{upper}) \frac{v_{ci} - v_{c}^{lower}}{v_{c}^{upper} - v_{c}^{lower}} \tag{4.12}
\]

4.4.2 Implementation of time-constant method for thick-slab
caster

In practice, it is hard to solve the inverse of equation (4.9) to find \( \tau(z_{end}(i), t) \).
The CONONLINE model stores the casting speed history for every slice and
updates each slice’s speed every $\Delta t$ seconds, currently $\Delta t = 1\sec$ is used. Therefore, CONONLINE uses a discrete (integer) version of wall-clock time $t$ (which approximates the real continuous function). So the approximate dwell time $\tau'(z_{end}(i), t)$ that satisfies the following inequality are obtained first:

$$
\sum_{s=t-\tau'(z_{end}(i), t)}^{t-1} v_c(s) < z_{end}(i) < \sum_{s=t-\tau'(z_{end}(i), t)}^{t} v_c(s) \tag{4.13}
$$

where $\tau'(z_{end}(i), t)$ and $t$ are both integers. The $\tau'(z_{end}(i), t)$ obtained by solving inequality (4.13) is called the approximate dwell time because error is introduced into the solution by forcing the dwell time to be integer. To get the dwell time for the model $\tau(z_{end}(i), t)$, $\tau'(z_{end}(i), t)$ is substituted into the following equation:

$$
\tau(z_{end}(i), t) = \tau'(z_{end}(i), t) - \frac{\sum_{s=t-\tau'(z_{end}(i), t)}^{t} v_c(s) - z_{end}(i)}{v_c(t)} \tag{4.14}
$$

The average casting speed for each zone can be calculated by inserting $\tau(z_{end}(i), t)$ into equation (4.11). Then $\bar{v}_{ci}(t)$ from the above equation is used to calculate the spray flow rates based on the spray patterns of ‘1.7-orig’ and ‘1.5-sameT’. The flow rate of each spray zone can be calculated by the following question:

$$
Q^{i}_{sw}(t) = Q^{i}_{sw}(\bar{v}_{ci}(t)) = Q^{i}_{sw}(1.5\text{-sameT})\frac{1.7 - \bar{v}_{ci}}{0.2} + Q^{i}_{sw}(1.7\text{-orig})\frac{\bar{v}_{ci} - 1.5}{0.2} \tag{4.15}
$$

Figure 4.14 shows the flow rates history of all spray zones under time-constant control, the flow rates gradually decrease from the values of ‘1.7-orig’ to the values of ‘1.5-sameT’. Figure 4.15 shows the metallurgical length profile under time-constant control. After the speed change, the metallurgical length gradually decreases. The metallurgical length deviation increases from 2.42 $m$ to 2.43 $m$ compared with spray table control.

Figures 4.16 through 4.18 show the average surface temperatures of different zones under the time-constant control method. The average surface
temperature profile of zone 1 has overshoot of 5 °C; the profiles of zone 2-6 and 11-12 have undershoot about 3-6 °C; the profiles for zone 7-9 first have overshoot about 1 °C and then have undershoot about 2 °C. The temperature deviations are clearly much smaller compared with spray table control.

The results show that if the spray table is good at steady state, i.e. the average surface temperature of every spray zone is the same under steady state at different casting speeds, then the time-constant control method will have good performance on maintaining surface temperature, allowing virtually constant surface temperature during speed changes.

4.5 PI Control of Surface Temperature

The PI (proportional-integral) control is a kind of feedback control methods. Figure 4.19 shows an example of the block diagram of a feedback control system. The system consists of a plant (spray zone i in this work), a controller and a sensor (software sensor-CONSENSOR in this work), first, the plant operators will decide the desired temperature \( T_{set}^i \) (or the reference input temperature), then the reference temperature is compared with the measured temperature \( T_i \) (or the measured output) to get the measured error \( e_i(t) \).

\[
e_i(t) = T_{set}^i - T_i(t) \tag{4.16}
\]

The control signal \( u_i(t) \) is then determined based on the measured temperature error. The proportional component of the control means that this component of the control signal is proportional to the error; and integral component of the control means that this component of the control signal is proportional to the time-integral of the error. The I controller is necessary for maintaining the surface temperature with no steady state error under a constant setpoint and rejecting disturbances. Derivative control, which is normally introduced to increase damping and stability margin, is not used because the system itself is well damped, owing to the high thermal inertia of the solidifying steel strand. Therefore, the control signal of the PI controller can be calculated by:

\[
u_i(t) = k_i^P e_i(t) + k_i^I \int_0^t e_i(s)ds + u_i(0) \tag{4.17}
\]
where $k_i^P$ and $k_i^I$ are called the P-controller gain and the I-controller gains, $u_i(0)$ are the initial control signals at $t = 0$. In the case of the speed drop, since the caster casing at constant casting speed before speed drop, $u_i(0)$ is the control signal before the speed drop. In practice, it is hard to solve the integral in equation (4.17) since the system is discrete time, equation (2.18) is then used [4]. The temperature setpoints for different spray zones are chosen to be the steady state average surface temperature under the spray pattern ‘1.7-orig’ and are listed in Table 4.7.

Table 4.7: Fixed temperature setpoints for PI control method

<table>
<thead>
<tr>
<th>Zone</th>
<th>Temperature setpoints °C</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>1205</td>
</tr>
<tr>
<td>2</td>
<td>1114</td>
</tr>
<tr>
<td>3</td>
<td>1065</td>
</tr>
<tr>
<td>4</td>
<td>927.1</td>
</tr>
<tr>
<td>5</td>
<td>834.9</td>
</tr>
<tr>
<td>6</td>
<td>791.4</td>
</tr>
<tr>
<td>7</td>
<td>768.5</td>
</tr>
<tr>
<td>8</td>
<td>778.4</td>
</tr>
<tr>
<td>9</td>
<td>820.1</td>
</tr>
<tr>
<td>10</td>
<td>813.3</td>
</tr>
<tr>
<td>11</td>
<td>788.7</td>
</tr>
<tr>
<td>12</td>
<td>722.7</td>
</tr>
</tbody>
</table>

The initial control signal of spray zone $i$ before the speed change is the flow rate at the initial casting speed of 1.7 m/min, i.e. $u_i(0) = Q_{sw}^i(1.7$-orig). Therefore, under PI control, the spray flow rate of spray zone $i$ can be calculated by the following equation:

$$Q_{sw}^i(t) = k_i^P \Delta T_i(t) + k_i^I \int_0^t \Delta T_i(s)ds + Q_{sw}^i(1.7$-orig) \quad (4.18)$$

where

$$\Delta T_i = \frac{\int_{zonei}[T(z,t) - T_{set}]}{L_i} \quad (4.19)$$

where $L_i$ is the total length of zone $i$. Since CONCONTROLLER updates the control commands every $\Delta t$ seconds, the spray flow rate of spray zone $i$ is:
\[ Q_{sw}^i(t + \Delta t) = u_i^P(t + \Delta t) + u_i^I(t + \Delta t) \]  \hspace{1cm} (4.20)

where the proportional \((u_i^P)\) and integral \((u_i^I)\) components are defined as follows:

\[ u_i^P(t + \Delta t) = k_i^P \Delta T_i(t) \]  \hspace{1cm} (4.21)

\[ u_i^I(t + \Delta t) = u_i^I(t) + k_i^I \Delta T_i(t) \Delta t \]  \hspace{1cm} (4.22)

Because of the physical limitations of the spray cooling system at the caster, it is possible that the spray flow flow rate requested by PI control logic \(u_i(t)\) is infeasible, so the actual flow rate is different than the requested one. Even when the requested flow rate is feasible, the requested and actual flow rates \(u_i'(t)\) also may be different because of dynamics such as actuator interactions with the header piping system. For the simulations in this study, no actual plant is involved, so the requested and actual flow rates always match when the requested flow rates is feasible. The differences between the requested and actual flow rates tend to cause controller instability, known as “windup”. Therefore, classic anti-windup is adopted to avoid integrator windup:

\[ u_i^I(t + \Delta t) = u_i^I(t) + k_i^I \Delta T_i(t) \Delta t + k_i^{aw} (u_i'(t) - u_i(t)) \]  \hspace{1cm} (4.23)

where \(k_i^{aw}\) are tuning parameters that can be used to relax the rate of windup. Here, all \(k_i^{aw}\) are set to 1.

The P-controller gain \(k_i^P\) and the I-controller gain \(k_i^I\) in equation (2.18) for each spray zone \(i\) are listed in Table 4.8. The P gains are tuned based on the total flow rates change at two steady states; bigger P gain is used for zones with larger flow rate change. The I gains are tuned based on the P gain value. PI controller’s performance vitally depends on the choice of PI controller gains, it needs a set of good gains to have good performance.

The flow rates under the PI control method are shown in Figure 4.20. The flow rates gradually decrease, similar to time-constant control. Figures 4.21 through 4.23 show the model prediction of surface temperature during speed drop, the average surface temperatures first decrease and then increase to steady state values, which match the temperature setpoints.
Table 4.8: PI controller gains

<table>
<thead>
<tr>
<th>Zone</th>
<th>$k^P$</th>
<th>$k^i$</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>0.9</td>
<td>0.12</td>
</tr>
<tr>
<td>2</td>
<td>0.81</td>
<td>0.11</td>
</tr>
<tr>
<td>3</td>
<td>0.9</td>
<td>0.12</td>
</tr>
<tr>
<td>4</td>
<td>1.15</td>
<td>0.15</td>
</tr>
<tr>
<td>5</td>
<td>0.9</td>
<td>0.12</td>
</tr>
<tr>
<td>6</td>
<td>0.6</td>
<td>0.08</td>
</tr>
<tr>
<td>7</td>
<td>0.5</td>
<td>0.06</td>
</tr>
<tr>
<td>8</td>
<td>0.18</td>
<td>0.02</td>
</tr>
<tr>
<td>9</td>
<td>0.07</td>
<td>0.01</td>
</tr>
<tr>
<td>10</td>
<td>0.09</td>
<td>0.01</td>
</tr>
<tr>
<td>11</td>
<td>0.15</td>
<td>0.02</td>
</tr>
<tr>
<td>12</td>
<td>0.2</td>
<td>0.03</td>
</tr>
</tbody>
</table>

The PI controller’s performance can be improved by carefully tuning the controller gains. The PI gains can be tuned through trial and error, increasing the $P$ gain will shorten the response time and will decrease the undershoot, but if the $P$ gain is too big, overshoot problem might occur. The I-controller basically iterates the tracking error, so the tuning of the I gain should start from a small number then carefully increase the I gain until a satisfying response are found.

Figure 4.24 shows the metallurgical length profile under the PI control method, the deviation of the metallurgical length is $2.63 \text{ m}$. Because this PI controller is designed for maintaining the surface temperature, its performance on maintaining the metallurgical length is not very good.

4.6 Comparison of three control methods

The above three control methods’ performance on maintaining the surface temperature are compared together in Figures 4.25 and 4.26, which show the average surface temperature of zone 5 and zone 9 respectively.

After the speed drop, the average temperature of all spray zones will have overshoot in spray table control and undershoot in PI control. For time constant control, it is possible to have undershoot only (as shown in Figure 4.25 for example) or to first have overshoot then undershoot (as shown in Figure 4.26 for example) for average surface temperature.
Both figures show that the spray table control, which has largest temperature fluctuation, has the worst performance on maintaining surface temperature. The problems with this method become worse for those slices of the strand that are furthest from the meniscus (larger zone number) when the speed change occurred. The PI control method has the best performance on maintaining surface temperature, allowing virtually constant surface temperature during the speed drop.

The time-constant control method and the spray table control method use the same spray patterns, thus the temperature at steady state were arranged to the same. For these two control methods, a good spray table is required to have same surface temperature at steady states. For the PI control method, there is no need for a good spray table, the only parameters needed are temperature setpoints and two sets of gains – $k_p$ and $k_i$ – in addition to accurate knowledge of the fundamental relation between spray water flow rate, and heat extraction. When the PI gains are properly tuned, the temperature will always return to the setpoints. The spray flow rates in the spray table for zone 5 were properly tuned, so the steady state average surface temperature after the speed drop is the same as the temperature before the speed drop. However, the average temperature at steady state for the two casting speeds (1.5 m/min and 1.7 m/min) in zone 9 are slightly different, which is because ‘1.7-orig’ and ‘1.5-sameT’ do not give exactly the same temperature along the caster.

The advantage of PI control is that the surface temperatures always return to the setpoints. With properly tuned PI gains and accurate heat transfer coefficients, it is possible to have a constant temperature profile during speed changes. But the tuning of PI gains requires great deal of efforts using computational models or real plant experiments. Also, the effect on temperature variations at individual locations in the caster has not been evaluated quantitatively, and deserves further consideration. The advantage of the time constant control method is that no tuning of PI gains is needed; only a good spray table is required.
Figure 4.1: Shell thickness at steady state.
Figure 4.2: Model prediction and measured surface temperature at steady state under casting speed of 1.7 m/min.

Figure 4.3: Model prediction and measured surface temperature at steady state under casting speed of 1.5 m/min.
Figure 4.4: Model prediction of average surface temperature of spray zone 1-4 under constant spray cooling.

Figure 4.5: Model prediction of average surface temperature of spray zone 5-8 under constant spray cooling.
Figure 4.6: Model prediction of average surface temperature of spray zone 9-12 under constant spray cooling.

Figure 4.7: Model prediction of surface temperature along caster under spray pattern of ‘1.7-orig’ and ‘1.5-sameT’.
Figure 4.8: Spray flow rate histories under spray table control.

Figure 4.9: Model prediction of surface temperature at 5 different locations: beneath roll, between rolls, beneath spray, beneath pyrometer and at caster exit.
Figure 4.10: Model prediction of surface temperature under spray table control of zone 1-4.

Figure 4.11: Model prediction of surface temperature under spray table control of zone 5-8.
Figure 4.12: Model prediction of surface temperature under spray table control of zone 9-12.

Figure 4.13: Model prediction of ML under spray table control of ‘1.7-orig’ to ‘1.5-sameT’.
Figure 4.14: Spray flow rate histories under time-constant control method.

Figure 4.15: Model prediction of ML under time-constant control under spray pattern of ‘1.7-conv’ to ‘1.5-sametemp’.
Figure 4.16: Average surface temperature of time-constant control of zone 1-4.

Figure 4.17: Average surface temperature of time-constant control of zone 5-8.
Figure 4.18: Average surface temperature of time-constant control of zone 9-12.
Figure 4.19: System block diagram of PI controller.

Figure 4.20: Spray flow rate histories under PI control.
Figure 4.21: Model prediction of average surface temperature under PI control for zone 1-4.

Figure 4.22: Model prediction of average surface temperature under PI control for zone 5-8.
Figure 4.23: Model prediction of average surface temperature under PI control for zone 9-12.

Figure 4.24: Model prediction of metallurgical length under PI control during speed drop.
Figure 4.25: Comparison of three control method on maintaining average surface temperature of zone 5.

Figure 4.26: Comparison of three control method on maintaining average surface temperature of zone 9.
CHAPTER 5

STUDY OF CONTROL METHODS TO MAINTAIN METALLURGICAL LENGTH DURING SPEED CHANGE

Maintaining the metallurgical length within a narrow range during casting speed changes is helpful for many operations, for example, unbending to prevent cracks, staying inside the roll support zone to prevent whales, and soft reduction to prevent centerline segregation. This chapter explores the potential to minimize metallurgical length variations during a small casting speed drop for the JFE thick-slab caster example case using different control methods.

5.1 Casting Conditions

Most of the casting conditions for the simulations in this chapter are the same as listed in Table 4.2 in the previous chapter. The difference is that in this chapter the control objective is not maintaining constant surface temperature; instead, minimizing the metallurgical length variations during speed drops is the new control objective. Therefore, new spray patterns are given in Table 5.1, which achieve same metallurgical length under steady state conditions at two different casting speeds. The control of the transition between these two conditions is investigated in this chapter.

The spray pattern ‘1.5-sameML’ was determined by steady state simulations using the CONID model, the flow rates were chosen to match the metallurgical length under the spray pattern of ‘1.7-orig’ at steady state. Figures 5.1 and 5.2 show the model predictions of the surface temperature histories and the shell thickness profiles under three different spray patterns: ‘1.7-orig’, ‘1.5-sameT’, and ‘1.5-sameML’. Figure 5.2 shows that the shell thickness profile under the spray pattern of ‘1.5-sameML’ is almost identical to the shell thickness profile under the spray pattern of ‘1.7-orig’. Therefore, the metallurgical lengths are the same under these two spray patterns.
Table 5.1: Spray patterns that gives same metallurgical length at steady state of two casting speed

<table>
<thead>
<tr>
<th>Zone</th>
<th>1.7-orig (L/min/row)</th>
<th>1.5-sameML (L/min/row)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>90.2</td>
<td>75.0</td>
</tr>
<tr>
<td>2</td>
<td>61.9</td>
<td>55.0</td>
</tr>
<tr>
<td>3</td>
<td>98.2</td>
<td>56.6</td>
</tr>
<tr>
<td>4</td>
<td>127.9</td>
<td>40.0</td>
</tr>
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<td>111.0</td>
<td>30.0</td>
</tr>
<tr>
<td>6</td>
<td>70.9</td>
<td>20.0</td>
</tr>
<tr>
<td>7</td>
<td>51.0</td>
<td>14.4</td>
</tr>
<tr>
<td>8</td>
<td>19.1</td>
<td>5.0</td>
</tr>
<tr>
<td>9</td>
<td>6.0</td>
<td>3.9</td>
</tr>
<tr>
<td>10</td>
<td>4.1</td>
<td>3.4</td>
</tr>
<tr>
<td>11</td>
<td>3.6</td>
<td>2.8</td>
</tr>
<tr>
<td>12</td>
<td>6.5</td>
<td>5.6</td>
</tr>
</tbody>
</table>

at different casting speeds. Figure 5.1 indicates a large temperature difference between two steady state temperatures under the spray patterns of ‘1.5-sameML’ and ‘1.7-orig’; however, under ‘1.7-orig’ and ‘1.5-sameT’ spray patterns, the surface temperatures are very close.

For those steel grades, which are sensitive to surface cracks, it is best to maintain the surface temperature during speed changes to minimize the creation of surface defects. However, the steel grade studied in this work is more sensitive to centerline defects than to surface defects. Soft reduction is an operation designed to reduce centerline segregation (one kind of centerline defects), and minimizing the metallurgical length during speed changes is important for this operation. Therefore, maintaining the metallurgical length during speed changes is the main control objective of this chapter. Also, it is not possible to maintain both surface temperature and metallurgical length to be constant during a speed change. So severe changes of the surface temperature during the speed changes are not the main concern in this chapter.

The results in Figure 5.2 show that under the small speed drop of 0.2 m/min studied in this chapter, constant metallurgical lengths at two casting speeds are achievable by applying feasible water flow rates under steady state. This was the largest feasible speed drop for this caster. Four different control methods were used to investigate their performance: constant
spray cooling, spray table control, time-constant control, and the bang-bang control methods. The metallurgical length deviation, defined as the difference (undershoot and/or overshoot) between the metallurgical length at 1.7 m/min and the minimum metallurgical length after the speed drop, is used to compare their performance.

5.2 Constant Spray Cooling (No Spray Control)

In order to compare the performance of different control methods, a ‘no control’ case was simulated. ‘No control’ of the secondary cooling region means that after the speed drop, the spray flow rates in the secondary cooling region are kept constant. The specific spray flow rates used are the values from the spray pattern of ‘1.7-orig’.

The surface temperature histories of this case are shown in section 4.2. The model prediction of the metallurgical length after the speed drop is shown in Figure 5.3. After the speed drop, the metallurgical length decreases linearly. By 776 seconds after the speed drop, the steel is fully solid around 19.57 m and the maximum metallurgical length deviation is a decrease of 2.72 m.

As discussed in section 1.2, controlling the metallurgical length profile is very important for preventing whale and centerline segregation. After the speed drop, the metallurgical length decreased by 2.72 m and is even smaller than before, so the constraint of preventing the whale problem is satisfied. The steel grade studied is sensitive to centerline defects, so it is very important to prevent centerline segregation. Soft reduction operation is used to reduce the centerline segregation, the choice of location of the soft reduction region depends greatly on the metallurgical length profile. If the steel is completely solid when the slab enters the soft reduction region, then the rolls that would squeeze upon the solid steel would generate extremely large forces, which would likely cause damage to both the slab and the rolls. The metallurgical length decreased 2.72 m after the speed drop, so the concern here is that the steel may already be fully solid when entering the soft reduction zone.

This thesis explores three actual control methods to reduce the metallurgical length deviation. The results in the following sections show that the best control method studied in this thesis reduces the metallurgical length
deviation during the transition from 2.72m to 0.8m.

5.3 Spray Table Control

Applying spray table control to the secondary cooling region using the spray patterns given in Table 5.1 gives the following water flow rates equation:

\[ Q_{sw}^i(t) = Q_{sw}^i(v_c(t)) = Q_{sw}^i(1.5\text{-sameML}) \frac{1.7 - v_c}{0.2} + Q_{sw}^i(1.7\text{-orig}) \frac{v_c - 1.5}{0.2} \]  

(5.1)

Figure 5.4 shows the spray flow rates calculated by the above equation during the speed drop. Figure 5.5 shows the casting speed profile and the model prediction of the metallurgical length profile. After the speed drop, the metallurgical length gradually decreases, then increases, and finally reaches steady state after a small overshoot. The metallurgical lengths before and after the speed drop under steady state are almost the same; the deviation of the metallurgical length for spray table control under the spray patterns of ‘1.7-orig’ and ‘1.5-sameML’ is an undershoot of 0.92 m followed by an overshoot of 0.18 m, which compared with spray table control under the spray patterns of ‘1.7-orig’ and ‘1.5-sameT’ (2.31 m), is a dramatic improvement in performance. The metallurgical length deviation is reduced by 66.1% compared with the constant spray cooling case. The overshoot would be a potential concern for whale formation, but is small, within the deviation remaining at steady-state for this set of conditions.

Figures 5.6 through 5.8 show the model predictions of the average surface temperature histories for all of the spray zones under spray table control with the spray patterns ‘1.7-orig’ and ‘1.5-sameML’. Because the water flow rates in zone 1 and zone 2 remain the same as spray table control based on the spray patterns given in Table 4.5, the transient behaviors of zone 1 and zone 2 remain the same as the spray table control results. For zone 3 – zone 8, the trend is the same but with around 120-150 °C larger deviation. And for zone 9 – zone 12, although the water flow rates remain the same as in Table 4.5, the change of temperature in previous zones also affect these zones.
5.4 Time-constant Control

Time-constant control has been introduced in the previous chapter. The average casting speed for each zone can be calculated by equation (4.11), \( v_{ci}(t) \) from the above equation is then used to calculate the spray flow rates based on the spray patterns - ‘1.7-orig’ and ‘1.5-sameML’:

\[
Q^i_{sw}(t) = Q^i_{sw}(v_{ci}(t)) = Q^i_{sw}(1.5\text{-sameML}) \frac{1.7 - v_{ci}}{0.2} + Q^i_{sw}(1.7\text{-orig}) \frac{v_{ci} - 1.5}{0.2} \quad (5.2)
\]

Figure 5.9 shows the metallurgical length profile under the time-constant control method; the deviation of the metallurgical length is an undershoot of 1.6 m with no overshoot. Compared with the constant spray cooling case, the metallurgical length deviation is reduced by 41.2%. Compared with Figure 5.5, the metallurgical length deviation of time-constant control is larger than the result under spray table control. This is because the flow rates calculated by the time-constant method change gradually, thus during the transition, the steel receives more water; as a result more heat is extracted by spray cooling water, and leads to smaller minimum metallurgical length and bigger metallurgical length deviation.

Figures 5.10 through 5.12 show the average surface temperature of different zones under the time-constant control method. Except for zone 1 and zone 2, all of the other zones have much smoother temperature transient behavior compared with spray table control. The reason why zone 1 and zone 2 have almost the same transient behavior for both control methods is that this two zones are very short, and the dwell times in these two zones are very small, so the time-constant control method has almost the same effect as the spray table control method.

Overall, the time-constant control method’s performance on maintaining the metallurgical length is worse than the spray table control method.

5.5 PI Control of Metallurgical Length

PI control can also be applied to maintain the metallurgical length during speed change by controlling the shell thickness instead of the surface...
temperature. But PI control of the metallurgical length faces the following challenges: 1) The shell thickness will not respond to spray flow rate changes immediately due to the solidifying shell reducing the heat transfer. 2) If the speed change is too big, the flow rates calculated by the PI controller may not be feasible due to the limitation of the spray equipment. In order to have good performance on maintaining the metallurgical length, extremely large gains may be needed, and this should be explored in the future.

5.6 Bang-bang Control

In control theory, the bang-bang optimal controller, also known as the hysteresis controller, is a controller that switches abruptly between two or more states [12]. Consider a simple model of a car moving on a horizontal line. The driver wants to spend minimum time to move to a destination that is far away from the starting point. The best solution is to apply maximum acceleration until a unique switching point and then apply maximum deceleration, which makes the car stop exactly at the destination. The above is an example of a two-step bang-bang controller that switches abruptly between two states: maximum acceleration and maximum deceleration.

In this section, optimal control refers to the following optimization problem:

$$\min_{Q_{sw}} \Delta z_{ML}(Q_{sw}) = z_{ML}^{v_{c1}} - \min(z_{ML}(t))$$

subject to $Q_{sw} \in (Q_{swmin}, Q_{swmax})$ (5.3)

where $z_{ML}^{v_{c1}}$ is the metallurgical length at steady state of the casting speed $v_{c1}$ (1.7 m/min), $\min(z_{ML}(t))$ is the minimum metallurgical length during speed changes and $\Delta z_{ML}(u)$ represents the metallurgical length deviation during speed changes. $Q_{sw} = (Q_{sw1}, Q_{sw2}, ..., Q_{swn})$ are the spray flow rates for $n$ (12 for this caster) different spray zones, $Q_{swmin}$ and $Q_{swmax}$ denote the minimum and maximum flow rates allowed for each spray zone of this caster.

The bang-bang control method is a form of optimal control that solves the above optimization problem. Three different particular types of bang-bang control sequences were investigated here: single-step bang-bang, two-step bang-bang, and three-step bang-bang control sequences. Single-step bang-
bang control means that the flow rates only switch once, after the speed drop, the flow rate at each spray zone immediately switches from the value in the spray pattern at ‘1.7-orig’ to the minimum flow rate allowed. Under single-step bang-bang control, the flow rate of spray zone $i$ can be calculated by the following equation:

$$ Q_{sw}^i(t) = \begin{cases} 
Q_{sw}^i(1.7-\text{orig}) & \text{if } t < 0 \\
Q_{sw}^i_{\text{min}} & \text{if } t \geq 0 
\end{cases} \tag{5.4} $$

For two-step bang-bang control, the spray flow rate for each spray zone switches twice: after the speed drop, the flow rate first immediately switches to the minimum flow rate allowed, and then at a predetermined switching time, the flow rate switches to the value from another spray pattern, denoted as $Q_{sw}^i(1.5-\text{Twostep})$. Under two-step bang-bang control, the flow rate of the spray zone $i$ can be calculated by the following equation:

$$ Q_{sw}^i(t) = \begin{cases} 
Q_{sw}^i(1.7-\text{orig}) & \text{if } t < 0 \\
Q_{sw}^i_{\text{min}} & \text{if } 0 \leq t \leq t_{2b}^i \\
Q_{sw}^i(1.5-\text{twostep}) & \text{if } t \geq t_{2b}^i 
\end{cases} \tag{5.5} $$

where $t_{2b}^i$ is the switching time and $Q_{sw}^i(1.5-\text{Twostep})$ is the final value of flow rate that need to be determined for spray zone $i$.

For three-step bang-bang control, the flow rate equation for spray zone $i$ is shown below:

$$ Q_{sw}^i(t) = \begin{cases} 
Q_{sw}^i(1.7-\text{orig}) & \text{if } t < 0 \\
Q_{sw}^i_{\text{min}} & \text{if } 0 \leq t \leq t_{3b1}^i \\
Q_{sw}^i_{3b} & \text{if } t_{3b1}^i \leq t \leq t_{3b2}^i \\
Q_{sw}^i(1.5-\text{threestep}) & \text{if } t \geq t_{3b2}^i 
\end{cases} \tag{5.6} $$

where $t_{3b1}^i$ and $t_{3b2}^i$ are the switching times for the second and third switches (also referred to as steps), and $Q_{sw}^i_{3b}$ are the switching water flow rates. All of the above three parameters can be controlled and need to be determined before applying three-step bang-bang control. Under three-step bang-bang control, after the speed drop, the spray flow rate at the spray zone $i$ first drops to the minimum flow rate allowed $Q_{sw}^i_{\text{min}}$, then at time $t_{3b1}^i$, the flow rate switches to $Q_{sw}^i_{3b}$, and at time $t_{3b2}^i$, the flow rate switches to the final value $Q_{sw}^i(1.5-\text{threestep})$. 

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In this work, the parameters \((t_{2b}, t_{3b1}, t_{3b3}, Q_{sw3b}, Q_{sw}^{i}(1.5\text{-threestep})\) for the above bang-bang control sequences were tuned based on the CONONLINE model prediction of the metallurgical length under corresponding bang-bang control sequences.

### 5.6.1 Single-step bang-bang control

For all three bang-bang control sequences, the first switch is the same, i.e. after the speed drop, the flow rates immediately drop to the minimum flow rates allowed for all spray zones. It is vital to study the effect of the first switch, because it will affect the selection of the parameters for two-step and three-step bang-bang control sequences.

The flow rates for different spray zones under single-step bang-bang control are described in equation (5.4), and are shown in Figure 5.14. Figure 5.15 shows the CONONLINE model prediction of the average shell thickness in zone 1-9 for single-step bang-bang control, zone 10-12 is neglected because the steel strand is already fully solid before entering zone 10.

Figure 5.16 shows the metallurgical length profile under single-step bang-bang control, the metallurgical length has the opposite transient behavior as the behavior of the average shell thickness shown in Figure 5.15. The figure indicates that the minimum undershoot of the metallurgical length possible is 0.8 m, since the flow rates have already been switched to the minimum flow rates allowed in all spray zones immediately after the speed drop. By applying minimum flow rates in the spray cooling region, the heat removal is reduced to minimum as well, which means that the shell growth rate is minimized. However, there is a large overshoot (2.68 m) in the metallurgical length profile, and the metallurgical lengths at steady states of two casting speeds are different. In order to keep metallurgical lengths under steady state the same, two-step bang-bang control is applied.
5.6.2 Two-step bang-bang control

5.6.2.1 Classic two-step bang-bang control

Bang-bang optimal control usually switches between extreme control states, like the example of spending minimum time to drive a car from one location to another, the driver first apply the maximum acceleration and then apply the maximum deceleration. To apply extreme control states in the steel casting process means that after the speed drop, the flow rate at each spray zone immediately switches to minimum flow rate allowed, and then at predetermined time \( t_{2b}^i \) switches to the maximum flow rate allowed. Then equation (5.5) becomes the following:

\[
Q_{sw}^i(t) = \begin{cases} 
Q_{sw}(1.7\text{-orig}) & \text{if } t < 0 \\
Q_{swmin}^i & \text{if } 0 \leq t \leq t_{2b}^i \\
Q_{swmax}^i & \text{if } t \geq t_{2b}^i 
\end{cases} 
\] (5.7)

The transient behavior of the first switch alone has already been studied in the previous section. The average shell thickness profiles in zone 1–3 increase after the speed drop; for zone 4–9, the average shell thickness profiles first increase and then decrease. After the flow rates switch from \( Q_{swmin} \) to \( Q_{swmax} \), the average shell thickness of all spray zones will increase again. For zone 1–3, the effects of both the first switch and the second switch are increasing the average shell thickness, so \( t_{2b}^1, t_{2b}^2, t_{2b}^3 \) were all chosen to be 0, i.e. only one switch is applied. For zone 4-9, \( t_{2b}^i, i = 4, ..., 9 \) were chosen to be the time from the beginning of the speed drop to the time at which the average shell thicknesses reach the maximum values. In order to use the increase caused by the second switch to partially cancels the decrease of the average shell thickness caused by the first switch. The values of \( t_{2b}^i \) are shown in Table 5.2.

Equation (5.7) is then modified to the following:

\[
\begin{align*}
\text{for } i = 1, 2, 3 : & \quad Q_{sw}^i(t) = \begin{cases} 
Q_{sw}(1.7\text{-orig}) & \text{if } t < 0 \\
Q_{swmin}^i & \text{if } 0 \leq t \leq t_{2b}^i \\
Q_{swmax}^i & \text{if } t \geq t_{2b}^i 
\end{cases} \\
\text{for } i = 4, ..., 9 : & \quad Q_{sw}^i(t) = \begin{cases} 
Q_{sw}(1.7\text{-orig}) & \text{if } t < 0 \\
Q_{swmin}^i & \text{if } 0 \leq t \leq t_{2b}^i \\
Q_{swmax}^i & \text{if } t \geq t_{2b}^i 
\end{cases}
\end{align*}
\] (5.8) (5.9)
Table 5.2: Switching time of the classic two-step bang-bang control sequence

<table>
<thead>
<tr>
<th>Zone ($i$)</th>
<th>$t^i_{2b}$ (sec)</th>
</tr>
</thead>
<tbody>
<tr>
<td>4</td>
<td>30</td>
</tr>
<tr>
<td>5</td>
<td>40</td>
</tr>
<tr>
<td>6</td>
<td>60</td>
</tr>
<tr>
<td>7</td>
<td>130</td>
</tr>
<tr>
<td>8</td>
<td>200</td>
</tr>
<tr>
<td>9</td>
<td>270</td>
</tr>
</tbody>
</table>

for $i = 10, 11, 12$ : $Q^i_{sw}(t) = \begin{cases} 
Q^i_{sw}(1.7\text{-orig}) & \text{if } t < 0 \\
Q^i_{sw}(1.5\text{-sameML}) & \text{if } t \geq 0 
\end{cases}$  \hspace{1cm} (5.10)

The flow rates are shown in Figure 5.17. Figure 5.18 illustrates the model prediction of the metallurgical length during the speed drop. The metallurgical lengths at two steady states are different, the maximum metallurgical length deviation is 3.93m. Thus, the flow rate of the second switch need to be adjusted to give the same metallurgical length under steady state at different casting speeds.

5.6.2.2 Modified two-step bang-bang control

In order to match the metallurgical lengths at steady state for the two casting speeds, the second step’s flow rates are chosen to be the flow rates from the spray pattern ‘1.5-sameML’, which achieves same metallurgical length as ‘1.7-orig’. Therefore, from equation (5.5), the parameters that need to be chosen are switching times $t^i_{2b}$ for all spray zones. The switching times can vary for different spray zones.

The relation between the shell thickness and the metallurgical length is clear: the metallurgical length is determined by the shell thickness profile of the spray zone in which the steel strand becomes fully solid, i.e. zone 9 in this case. However, the shell thickness behavior in the upper zones will affect the behavior in zone 9. Therefore, the switching times of the second switch $t^i_{2b}$ were tuned sequentially and separately for every spray zone based on the average shell thickness profile of the corresponding zone.
In order to investigate the effects of the second switch, large $t^{i}_{2b}$s, which leaves enough time for the transient behavior of the first switch to stabilize at corresponding spray zone, are chosen. It is clear that if the second switch is applied after the average shell thickness reaches steady state, the shell thickness will increase due to the increase of the spray flow rates. From Figure 5.15, the average shell thicknesses in zone 1-3 continue to increase, this is because after the speed drop, the heat removal in the mold increases, the shell thickness at the mold exit increases as well. It is impossible to reduce the shell thickness by controlling spray cooling unless negative spray flow rates are applied. For zone 1-3, $t^{1}_{2b}, t^{2}_{2b}, t^{3}_{2b}$ were all chosen to be 0. For zone 4-9, $t^{i}_{2b}, i = 4, ..., 9$ were first intentionally chosen to be 1000 sec.

Figure 5.19 shows the average shell thickness for all spray zones for the above case. For zone 4-9, the average shell thicknesses’ transient behavior after the first switch is the same as the results shown in Figure 5.15, and after the second switch the average shell thicknesses at all zones increase. Figure 5.20 is the amplification of zone 4 and 5 of Figure 5.19. Spray zone 4-9 have the similar transient behavior. After the first switch, the average shell thickness first increases, then decreases, and increases again to reach steady state; after the second switch is applied, the shell thickness increases again and reaches steady state. Now consider zone 4, by choosing switching time of the second switch to be smaller than 1000 sec, the third increase in Figure 5.20 will move to the left and partially cancels out the first decrease shown in the figure. But switching time of the second switch should not be smaller than the time it takes for the average shell thickness to reach its maximum value after the first increase. Otherwise, the maximum value of average shell thickness will increase and leads to bigger metallurgical length deviation. Because while shell thickness increases, the minimum metallurgical length decreases. Therefore, $t^{4}_{2b}$ is chosen to be 30 sec.

The model prediction of the average shell thickness for the new case with zone 4 has the switching time of the second switch equals to 30 sec is shown in Figure 5.21. The figure shows that the first decrease appeared in Figure 5.20 is partially canceled by the second increase, now the average shell thickness in zone 4 appears to be two rises. Although it looks like there is no increase in the shell thickness for zone 5 after the second switch, it is because the increase is too small (0.06 mm) to be seen in the current scale. Then using the similar method as mentioned above, $t^{i}_{2b}$ can be tuned in sequence from...
zone 5 to zone 9.

The equation (5.5) is modified to the following:

\[
\text{for } i = 1, 2, 3 : \quad Q_{sw}^{i}(t) = \begin{cases} 
Q_{sw}^{i}(1.7-\text{orig}) & \text{if } t < 0 \\
Q_{sw}^{i}(1.5-\text{sameML})^i & \text{if } t \geq 0 
\end{cases} \tag{5.11}
\]

\[
\text{for } i = 4, ..., 9 : \quad Q_{sw}^{i}(t) = \begin{cases} 
Q_{sw}^{i}(1.7-\text{orig}) & \text{if } t < 0 \\
Q_{swmin}^i & \text{if } 0 \leq t \leq t^i_{2b} \\
Q_{sw}^{i}(1.5-\text{sameML}) & \text{if } t \geq t^i_{2b} 
\end{cases} \tag{5.12}
\]

\[
\text{for } i = 10, 11, 12 : \quad Q_{sw}^{i}(t) = \begin{cases} 
Q_{sw}^{i}(1.7-\text{orig}) & \text{if } t < 0 \\
Q_{sw}^{i}(1.5-\text{sameML}) & \text{if } t \geq 0 
\end{cases} \tag{5.13}
\]

where the switching times for zone 4-9 are shown in Table 5.3.

Table 5.3: Switching time of the modified two-step bang-bang control sequence

<table>
<thead>
<tr>
<th>Zone (i)</th>
<th>(t^i_{2b} ) (sec)</th>
</tr>
</thead>
<tbody>
<tr>
<td>4</td>
<td>30</td>
</tr>
<tr>
<td>5</td>
<td>48</td>
</tr>
<tr>
<td>6</td>
<td>80</td>
</tr>
<tr>
<td>7</td>
<td>140</td>
</tr>
<tr>
<td>8</td>
<td>180</td>
</tr>
<tr>
<td>9</td>
<td>296</td>
</tr>
</tbody>
</table>

The flow rates under the two-step bang-bang control sequence are shown in Figure 5.22. Figures 5.24 through 5.26 show the average surface temperatures for all spray zones. For spray zone 1-3 and 10-12, since only single-step bang-bang control is applied, the transient behavior of average temperatures of these zones are similar to those under spray table control. For spray zone 4-9, the temperatures first increase, have an overshoot, and decreases to higher steady state values.

The model prediction of the metallurgical length profile is shown in Figure 5.23, with maximum metallurgical length deviation of 0.8 m. Compare with the constant spray cooling case, the metallurgical length deviation is reduced by 70.6%. However there is a small overshoot (0.37 m) before reaching steady state, that is unable to be removed with two-step bang-bang control sequence.
while still maintaining the 0.8 m maximum metallurgical length deviation. The reason is that, in order to remove the overshoot, for the slice which have the maximum metallurgical length – slice (2.54) (2.54 means the slice is 2.54 m away from the meniscus when speed drop happens), to receive more cooling water. Figure 5.27 shows the flow rate history of slice (2.54), the results show that the flow rates in zone 5-9 have already reached the values from the spray pattern of ‘1.5-sameML’ when slice (2.54) enters the spray zones. If more cooling water is needed for slice (2.54), the flow rate in zone 4 need to be increased, i.e. decrease $t_{2b}$, but this might increase the metallurgical length deviation according to the previous discussion.

To better straighten out the response (decrease the overshoot), three-step bang-bang control is considered: full step down, a bit earlier strong step up and then small step down. In the other parts of this thesis, the two-step bang-bang control method refers to the modified two-step bang-bang control method.

5.6.3 Three-step bang-bang control

To reduce the overshoot shown in the metallurgical length profile in Figure 5.23, another bang-bang control sequence, which added a third sudden step (switch) to the two-step bang-bang control sequence, is applied to the model. In three-step bang-bang control, there are three sets of parameters that need to be determined: $t_{3b1}, t_{3b2}$ and $Q_{sw3b}$.

From the discussion in the previous section, to reduce the overshoot, the amount of spray cooling water received by slice (2.54) in spray zone 4 needs to be increased. Therefore, three-step bang-bang control is first only applied in zone 4, and two-step bang-bang control sequence is applied in the rest zones. $Q_{sw3b}$ is chosen to be 98.2 $L/min/row$, $t_{3b1}, t_{3b2}$ are chosen to be 30 sec and 60 sec, this set of parameters give roughly the same amount of cooling water for slice (2.54) in zone 4 as in ‘1.5-sameML’. The metallurgical length transient behavior of the above case is shown in Figure 5.28. The result shows that the slice that has maximum metallurgical length is now slice (3.74). The metallurgical length for slice (2.54) and the slices near it were reduced. Although the spray flow rate in the spray zone 4 was tuned specially for slice (2.54), the spray flow rate in the whole zone is changed.
and affected the other slices in zone 4. Now repeat the same method for slice (3.74), by this tuning method, the set of parameters listed in Table 5.4 are finally chosen.

Table 5.4: Parameters of three-step bang-bang controller

<table>
<thead>
<tr>
<th>Zone</th>
<th>$t_{3b1}^i$ (sec)</th>
<th>$t_{3b2}^i$ (sec)</th>
<th>$Q_{sw3b}^i$ (L/min/row)</th>
</tr>
</thead>
<tbody>
<tr>
<td>4</td>
<td>30</td>
<td>60</td>
<td>98.2</td>
</tr>
<tr>
<td>5</td>
<td>40</td>
<td>110</td>
<td>55</td>
</tr>
<tr>
<td>6</td>
<td>80</td>
<td>130</td>
<td>30</td>
</tr>
<tr>
<td>7</td>
<td>110</td>
<td>140</td>
<td>20</td>
</tr>
</tbody>
</table>

The equation (5.6) is then modified to the following:

for $i = 1, 2, 3$: $Q_{sw}^i(t) = \begin{cases} Q_{sw}^i(1.7\text{-orig}) & \text{if } t < 0 \\ Q_{sw}^i(1.5\text{-sameML}) & \text{if } t \geq 0 \end{cases}$ (5.14)

for $i = 4, 5, 6, 7$: $Q_{sw}^i(t) = \begin{cases} Q_{sw}^i(1.7\text{-orig}) & \text{if } t < 0 \\ Q_{sw_{min}}^i & \text{if } 0 \leq t \leq t_{3b1}^i \\ Q_{sw3b}^i & \text{if } t_{3b1}^i \leq t \leq t_{3b2}^i \\ Q_{sw}^i(1.5\text{-sameML}) & \text{if } t \geq t_{3b2}^i \end{cases}$ (5.15)

for $i = 8, 9$: $Q_{sw}^i(t) = \begin{cases} Q_{sw}^i(1.7\text{-orig}) & \text{if } t < 0 \\ Q_{sw_{min}}^i & \text{if } 0 \leq t \leq t_{2b}^i \\ Q_{sw}^i(1.5\text{-sameML}) & \text{if } t \geq t_{2b}^i \end{cases}$ (5.16)

for $i = 10, 11, 12$: $Q_{sw}^i(t) = \begin{cases} Q_{sw}^i(1.7\text{-orig}) & \text{if } t < 0 \\ Q_{sw}^i(1.5\text{-sameML}) & \text{if } t \geq 0 \end{cases}$ (5.17)

The flow rates history of all spray zones are shown in Figure 5.29. The final result of the metallurgical length profile under three-step bang-bang control sequence is shown in Figure 5.30. Same as two-step bang-bang control sequence, the maximum deviation of metallurgical length is 0.8 m. Compare with the constant spray cooling case, the metallurgical length deviation is reduced by 70.6%. There is still a small overshoot (0.1 m) in the metallurgical length profile, but the slice (0.8), which has maximum metallurgical
length in the overshoot region, is still in the mold when the speed change happens. When it enters each spray zones, the spray flow rates are already in steady state of ‘1.5-sameML’, which means the overshoot is not caused by the secondary cooling but the primary cooling.

Figures 5.31 through 5.33 show the average surface temperature of all spray zones under three-step bang-bang control. The average temperatures have smaller overshoot but are more damped compared to the temperatures under two-step bang-bang control.

5.6.4 Comparison of two-step and three-step bang-bang control sequences

The comparison of the performance on controlling the metallurgical length for two-step and three-step bang-bang control sequences is shown in Figure 5.34. These two bang-bang control sequences are compared through maximum metallurgical length deviation, response time (the time from the beginning of the speed drop to the time at which the metallurgical length reaches steady state), and overshoot. The values are listed in Table 5.5. Two control sequences have the same maximum metallurgical length deviation and response time. The metallurgical length under three-step bang-bang control sequence has smaller overshoot than the one under two-step bang-bang control sequence. Therefore, three-step bang-bang control sequence has better performance on maintaining the metallurgical length.

Table 5.5: Comparison of performance on the metallurgical length for two-step and three-step bang-bang control sequences

<table>
<thead>
<tr>
<th>method</th>
<th>ML deviation ((m))</th>
<th>response time ((sec))</th>
<th>overshoot ((m))</th>
</tr>
</thead>
<tbody>
<tr>
<td>Two-step</td>
<td>0.8</td>
<td>880</td>
<td>0.37</td>
</tr>
<tr>
<td>Three-step</td>
<td>0.8</td>
<td>879</td>
<td>0.1</td>
</tr>
</tbody>
</table>

Figures 5.35 through 5.38 compare the average surface temperatures of the two control sequences for 12 different spray zones. For zone 1-3, the two sequences have the same response. For zone 4-9, the performance is compared through response time (the time from the beginning of the speed drop to the time at which the surface temperature stays with in 10\(°\)C of its
steady state value), overshoot, and damping. The results are listed in Table 5.6.

Table 5.6: Comparison of performance on the temperature for two-step and three-step bang-bang control sequences

<table>
<thead>
<tr>
<th>Spray zone</th>
<th>response time (sec)</th>
<th>Overshoot (°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Two-step</td>
<td>Three-step</td>
</tr>
<tr>
<td>Zone 4</td>
<td>77</td>
<td>90</td>
</tr>
<tr>
<td>Zone 5</td>
<td>115</td>
<td>140</td>
</tr>
<tr>
<td>Zone 6</td>
<td>120</td>
<td>150</td>
</tr>
<tr>
<td>Zone 7</td>
<td>168</td>
<td>152</td>
</tr>
<tr>
<td>Zone 8</td>
<td>115</td>
<td>115</td>
</tr>
<tr>
<td>Zone 9</td>
<td>306</td>
<td>306</td>
</tr>
</tbody>
</table>

The results show that three-step bang-bang control has smaller overshoot, and two-step bang-bang control has smaller response time. For zone 4–9, the responses of three-step bang-bang control sequence are more damped. For zone 10-12, the responses of three-step bang-bang control are almost the same to the responses of two-step bang-bang control, but with smaller overshoot. Overall, the performance on temperature control is similar between these two bang-bang control sequences, two-step bang-bang control sequence has slightly better performance from the response time aspect, and three-step bang-bang control sequence has slightly better performance from the overshoot aspect. However, since the temperature profiles under three-step bang-bang control sequence is more damped than the profiles under two-step bang-bang control sequence, two-step bang-bang control sequence has slightly better performance on controlling the surface temperature.
Figure 5.1: CON1D model prediction of surface temperature profile under different spray pattern.
Figure 5.2: CON1D model prediction of shell thickness profile under different spray pattern.

Figure 5.3: Model prediction of metallurgical length during the speed drop under constant secondary spray cooling.
Figure 5.4: Flow rates under spray table control.

Figure 5.5: Model prediction of ML under spray table control.
Figure 5.6: Model prediction of average surface temperature for zone 1-4 under spray table control.

Figure 5.7: Model prediction of average surface temperature for zone 5-8 under spray table control.

Figure 5.8: Model prediction of average surface temperature for zone 9-12 under spray table control.
Figure 5.9: Model prediction of metallurgical length under time-constant control.

Figure 5.10: Model prediction of average surface temperature for zone 1-4 of time-constant control method.
Figure 5.11: Model prediction of average surface temperature for zone 5-8 of time-constant control method.

Figure 5.12: Model prediction of average surface temperature for zone 9-12 of time-constant control method.

Figure 5.13: Comparison of average surface temperature of spray table control and time-constant control method.
Figure 5.14: Flow rates of single-step bang-bang control.
Figure 5.15: Model prediction of average shell thickness in all spray zones of single-step bang-bang control.

Figure 5.16: Model prediction of metallurgical length of single-step bang-bang control.
Figure 5.17: Spray flow rate after speed change for classic two-step bang-bang control sequence.

Figure 5.18: Model prediction of metallurgical length for classic two-step bang-bang control sequence.
Figure 5.19: Model prediction of average shell thickness in all spray zones: zone 1-3 single-step and zone 4-9 with switching time for second step equals to 1000 sec.

Figure 5.20: Average shell thickness of zone 4 and 5 from Figure 5.19
Figure 5.21: Model prediction of average shell thickness in all spray zones: zone 1-3 single-step, zone 4 first switch followed by the second switch after 30 sec and zone 5-9 with switching time for the second step equals to 1000 sec.

Figure 5.22: Spray flow rate after speed change for modified two-step bang-bang control sequence.
Figure 5.23: Model prediction of metallurgical length for modified two-step bang-bang control sequence.

Figure 5.24: Model prediction of average surface temperature of zone 1-4 for two-step bang-bang control sequence.
Figure 5.25: Model prediction of average surface temperature of zone 5-8 for two-step bang-bang control sequence.

Figure 5.26: Model prediction of average surface temperature of zone 9-12 for two-step bang-bang control sequence.
Figure 5.27: Flow rate history for slice (2.54) in Figure 5.23.

Figure 5.28: Model prediction of metallurgical length for two-step bang-bang control sequence and three-step bang-bang control sequences only applied in zone 4.
Figure 5.29: Spray flow rate after speed change for three-step bang-bang control sequence.

Figure 5.30: Model prediction of metallurgical length for three-step bang-bang control sequence.
Figure 5.31: Model prediction of average surface temperature of zone 1-4 for three-step bang-bang control sequence.

Figure 5.32: Model prediction of average surface temperature of zone 5-8 for three-step bang-bang control sequence.
Figure 5.33: Model prediction of average surface temperature of zone 9-12 for three-step bang-bang control sequence.

Figure 5.34: Comparison of metallurgical length for two different bang-bang control sequences.
Figure 5.35: Comparison of average surface temperature for zone 1-3 for two different bang-bang control sequences.

Figure 5.36: Comparison of average surface temperature for zone 4-6 for two different bang-bang control sequences.
Figure 5.37: Comparison of average surface temperature for zone 7-9 for two different bang-bang control sequences.
Figure 5.38: Comparison of average surface temperature for zone 7-9 for two different bang-bang control sequences.
CHAPTER 6

CONCLUSIONS

By using the heat conduction model - CON1D and the real-time dynamic model – CONONLINE (and its offline version, CONOFFLINE), the behavior of the metallurgical length and the surface temperature histories during a sudden drop in casting speed were investigated with four different types of control methods in this thesis. The models were first validated with both analytical solutions, and comparison with plant measurements.

The hysteresis effect feature was included in the CONONLINE model based on the Leidenfrost effect feature of the previous version (two-workstations version) of CONONLINE. Although this feature of the model needs further work, especially via calibration with measurements, simple tests illustrates that the hysteresis effect works.

Under constant secondary spray cooling conditions, the settling time for surface temperature (the time from the beginning of the speed drop to the time when the temperature reaches within 10 °C of its final value) can be estimated by equation (3.2); an analytical equation (3.8) was derived to estimate the settling time for metallurgical length.

The spray table and time constant control methods were implemented quantitatively with equations into the CONOFFLINE control model. Instead of the simple speed-based relation that comprises the spray-table method, the time constant control method changes spray water flow rates according to the time each slice of steel has resided in the caster. With this control method, the flow rates change gradually even while there are sudden speed changes. Finally, PI control and Bang-Bang control were investigated for surface-temperature control and metallurgical length control respectively.

For the surface temperature control objective, the spray table control has the worst performance, as expected. The spray table has a set of predetermined spray patterns for different casting conditions and a good spray table gives the same surface temperature at different casting conditions. Time-
constant control has relatively better performance than spray table control. There is still variance in surface temperature during a speed change, but the temperature deviation is small.

For properly tuned PI controller of surface temperature, there is a perfectly flat temperature profile during the speed drop. But for non-perfectly tuned PI controller, there is always undershoot of the surface temperature. The performance might be worse than that of the time-constant control method with a good spray table. Therefore, for PI control to be successful, it is important to have good sets of PI controller gains. Thus, large efforts should be invested into tuning of the gains and finding accurate data to relate spray flow rates and heat transfer coefficients. When a good spray table is available, time-constant control has the advantage that no tuning is needed. Thus, for many caster plant operations (which already have a good spray table, but are not sure about heat transfer coefficients) time-constant control is the best method studied for the surface-temperature control objective.

For the metallurgical length control objective, it is feasible to achieve constant metallurgical length for a speed drop of 0.2 mm/min from 1.7 to 1.5 m/min, for a typical thick slab caster. Spray table control based on spray patterns of ‘1.7-orig’ and ‘1.5-sameML’ reduces the maximum deviation of the metallurgical length by 66.1%, decreasing the undershoot from 2.72 m (with no control) to 0.9 m. The time-constant control method based on the same spray patterns reduces the maximum deviation of metallurgical length by only 41.2 %, so is not as good.

For the same spray patterns, spray table control has better performance at maintaining the metallurgical length while time-constant control has better performance at maintaining the surface temperature.

Two-step and three-step bang-bang control sequences have maximum deviation of metallurgical length of 0.8 m, reducing the metallurgical length undershoot by 70.6 %. The bang-bang control sequences have better performance at maintaining the metallurgical length. Moreover, three-step bang-bang control has no overshoot of the metallurgical length profile. Overall, the three-step bang-bang control sequence has the best performance at controlling the metallurgical length during the speed drop among all the methods studied. However, this method causes sudden changes in the surface temperature profile which are very likely to cause cracks. So, this method is likely not the most optimal control method overall. Further work is needed.
to evaluate the different control methodologies for different control objectives together.
CHAPTER 7

FUTURE WORK

The problems this thesis attempts to deal with have not been definitively solved. There are problems, both in the application of the results and the development of the control theory, that are waiting to be solved.

Tuning of the PI controller gains for the surface temperature control. As discussed in chapter 4, the PI controller currently applied is able to maintain the same surface temperature at steady state after the speed change. But there are still small temperature plural during the transition. The performance may be improved by spending more time to get a better set of PI gains. The PI gains can be tuned through trial and error by using the CONONLINE model. Increase the P gain will shorten the response time but will also increase the overshoot. The I controller iterates the measured error, so the tuning of the I gain should start from a relatively small number, and then gradually increase the I gain until reached a satisfying response.

Explore the PI control method for the metallurgical length control. To control the metallurgical length during the speed changes using the PI control method, first, the control reference must be chosen, and second, the gains must be tuned. The metallurgical length can be chosen directly as the control variable; or the shell thickness profiles can be chosen as the control variables to control the metallurgical length indirectly. Because the solidifying shell will reduce the heat removal, extremely large PI gains might be needed. The method of tuning the PI gains is similar as the PI control of surface temperature.

Derive the theory for bang-bang control of the PDE model of the continuous casting process. The bang-bang control methods applied in this thesis has only finite many switches (single-step, two-step and three-step). The water flow rates and the switching time applied is derived from simulations of the CONONLINE model. It might be able to derive these control variables mathematically based on the partial differential equation model of the continuous
casting process.

Investigate other methods for control of the metallurgical length. This thesis only explored three different control methods. There are other control methods available that may have a good performance on controlling the metallurgical length, which also should be explored in the future.

This work has shown that the ability to control the metallurgical length is very limited compared to the ability to control surface temperature even with a small speed drop for thick-slab casters. When there is no danger of a whale formation (which may cause serious problem like breakout), the steel companies are typically more concerned with avoiding surface cracks to improve the steel quality, which requires surface temperature control. The ultimate goal of designing control methods for the continuous casting process should consider both profiles (surface temperature and metallurgical length), and decide the control objective itself based on the steel grade that are casting: (1) when the steel grade is very sensitive to surface defects, the control method should focus on controlling the surface temperature; (2) when the steel grade is less sensitive to surface defects but more sensitive to center-line defects, the control method should focus on controlling the metallurgical length; (3) For some steel grades, the controller should consider controlling both the surface temperature and the metallurgical length. The results in this work shows that it is impossible to have same surface temperature or same metallurgical length after the speed change when the speed drop is small, so the control method may sacrifice some controllability on both profiles to achieve a overall good performance. Petrus [13] developed a control law that guarantees asymptotic convergence of both the temperature field and the solidification front position to a desired reference under steady state by controlling the thermodynamic energy of the cast material-enthalpy. The possibility of applying the method to transient process should also be explored in the future.
APPENDIX A

SCALING AND ANALYTICAL SOLUTION FOR STEADY STATE VALIDATION OF CON1D

Figure (A.1) shows simplified schematic of the steel strand. Here the curved strand and spray zone are simplified into a straight line. The strand is continuously moving down at a constant velocity (casting speed), and spray water is impinging onto the slab wide face.

For the analytical solution for the test problem, I ignored the solidification phenomenon in the process, because I am more interested in the heat conduction and convection aspect. Also I assumed constant spray cooling heat transfer coefficient on both sides of wide face.

The governing equation for the process are:

\[
\rho \frac{\partial h}{\partial t} + \rho \vec{v} \cdot \nabla h = k \Delta T \tag{A.1}
\]

where

\[
h = c_p T \tag{A.2}
\]

Note: In this validation case there is no \( h_{\text{spray}} \), \( h_{\text{rad-spray}} \), \( h_{\text{roll}} \) and \( h_{\text{conv}} \) introduced in chapter 2, only one unify heat flux boundary condition of secondary cooling zone, denoted as \( h \).

Therefore equation (A.1) becomes

\[
\rho c_p^* \left( \frac{\partial T}{\partial t} + v_x \frac{\partial T}{\partial x} + v_y \frac{\partial T}{\partial y} + v_z \frac{\partial T}{\partial z} \right) = k \frac{\partial^2 T}{\partial x^2} + k \frac{\partial^2 T}{\partial y^2} + k \frac{\partial^2 T}{\partial z^2} \tag{A.3}
\]

Here, assume that the steel moves only along the casting direction. so \( v_y = 0 \) and \( v_x = 0 \).

\[
\rho c_p^* \left( \frac{\partial T}{\partial t} + v_z \frac{\partial T}{\partial z} \right) = k \frac{\partial^2 T}{\partial x^2} + k \frac{\partial^2 T}{\partial y^2} + k \frac{\partial^2 T}{\partial z^2} \tag{A.4}
\]

In order to perform the scaling calculation, first, a group of dimensionless
variables need to be chosen:

\[
\begin{align*}
\theta &= \frac{T - T_\infty}{T_0 - T_\infty} \\
\frac{x^*}{L} &= \frac{L_x}{\theta} \\
\frac{y^*}{L} &= \frac{L_y}{\theta} \\
\frac{z^*}{L} &= \frac{L_z}{\theta} \\
\frac{t^*}{L} &= \frac{L_t}{\theta}
\end{align*}
\]  
(A.5)

Substituting equation (A.5) into equation (A.4) and simplifying the latter yields:

\[
\frac{L_z}{v_z t_c \partial t^*} + \frac{\partial \theta}{\partial z^*} = \frac{k L_z}{\rho c_p v_z L_x^2} \frac{\partial^2 \theta}{\partial x^* \partial z^*} + \frac{k L_z}{\rho c_p v_z L_y^2} \frac{\partial^2 \theta}{\partial y^* \partial z^*} + \frac{k}{\rho c_p v_z L_z} \frac{\partial^2 \theta}{\partial z^* \partial z^*}
\]  
(A.6)

The following equation is derived by choosing \(t_c = \frac{L_z}{v_z}\) and substituting the values in Table (2.1) into equation (A.6):

\[
\frac{\partial \theta}{\partial t^*} + \frac{\partial \theta}{\partial z^*} = 0.5663 \frac{\partial^2 \theta}{\partial x^* \partial z^*} + 0.0043 \frac{\partial^2 \theta}{\partial y^* \partial z^*} + 1.14 \times 10^{-5} \frac{\partial^2 \theta}{\partial z^* \partial z^*}
\]  
(A.7)

Equation (A.7) shows that it is reasonable to drop \(\frac{\partial^2 \theta}{\partial y^* \partial z^*}\) and \(\frac{\partial^2 \theta}{\partial z^* \partial z^*}\), then equation (A.7) becomes:

\[
\frac{\partial \theta}{\partial t^*} + \frac{\partial \theta}{\partial z^*} = 0.5663 \frac{\partial^2 \theta}{\partial x^* \partial z^*}
\]  
(A.8)

which means that the heat conduction along \(y\)- and \(z\)- axis is negligible. Thus, equation (A.4) is simplified to:

\[
\rho c_p^* \left( \frac{\partial T}{\partial t} + v_z \frac{\partial T}{\partial z} \right) = k \frac{\partial^2 T}{\partial x^2}
\]  
(A.9)

Initial conditions:

\[
T(t = 0) = T_0
\]  
(A.10)

Boundary conditions:
\[
q = \frac{\partial T}{\partial x}\bigg|_{x=0} = 0 \quad (A.11)
\]
\[
q = \frac{\partial T}{\partial x}\bigg|_{x=H} = h (T - T_\infty) \quad (A.12)
\]

By taking Lagrange reference frame, the simulation domain moves with the cast material.

\[
\frac{\partial T'}{\partial t} = \frac{\partial T}{\partial t} + v \frac{\partial T'}{\partial t'} , \frac{\partial^2 T'}{\partial x'^2} = \frac{\partial^2 T}{\partial x^2} \quad (A.14)
\]

Equation (A.9) becomes

\[
\rho c_p^* \frac{\partial T}{\partial t} = k \frac{\partial^2 T}{\partial x^2} \quad (A.15)
\]

Then choose the same dimensionless group

\[
\begin{align*}
\theta &= \frac{T - T_\infty}{T_0 - T_\infty} \\
x^* &= \frac{x}{L_x} \\
t^* &= \frac{t}{L_t}
\end{align*} \quad (A.16)
\]

where \( t_c = \frac{\rho c_p H^2}{k} \). After same scaling process, equation (A.15) is simplified to:

\[
\frac{\partial \theta}{\partial t^*} = \frac{\partial^2 \theta}{\partial x^{*2}} \quad (A.17)
\]

where \( H \) is half thickness. \( H = \frac{1}{2} = 115\,mm \)

Initial conditions:

\[
\theta(t = 0) = 1 \quad (A.18)
\]

Boundary conditions:
\[ q = \frac{\partial \theta}{\partial x} \bigg|_{x=0} = 0 \quad \text{(A.19)} \]

\[ q = \frac{\partial \theta}{\partial x} \bigg|_{x=H} = -\frac{hH}{k} \theta = -Bi\theta \quad \text{(A.20)} \]

The analytical solution for above problem is shown below [36]:

\[ \theta = 2 \sum_{n}^{\infty} \frac{\sin(u_n)}{u_n + \sin(u_n) \cos(u_n)} \exp(-u^2t^*) \cos(u_n x^*) \quad \text{(A.21)} \]

where \( u_n = \text{Bicot}(u_n) \)

General analytical solution code (MATLAB):

```matlab
t1 = 0:109:1526;
tc = 2.19*10^3;
t=t1/tc;
z = 0:0.5:115;
l=115;
x=z/l;
u1 = fsolve(@(x) x-0.69*cot(x),(1/2)*pi);
Tnew=zeros(length(t),length(x));
Tpre=zeros(length(t),length(x));
for ii = 1:length(t)
ttemp=t(ii);
    for jj = 1:length(x)
        xtemp=x(jj);
        Tnew(ii,jj) = 2*(sin(u1))/(u1+sin(u1))...
            *cos(u1)) * exp(-u1^2*ttemp) * cos(u1*xtemp);
        Tpre(ii,jj) = 0;
n=1;
    while abs(Tnew-Tpre) > 0.00001
        u= fsolve(@(x) x-0.69*cot(x),n*pi);
        T=2*(sin(u))/(u+sin(u)*cos(u))...
            * exp(-u1^2*ttemp) * cos(u1*xtemp);
        Tpre(ii,jj)=Tnew(ii,jj);
        Tnew(ii,jj)=Tpre(ii,jj)+T;
    
end
```

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n=n+1;
end
end
end

Note: for the general solution code of equation (A.21), the code truncates the infinity series to $N = 85$. $N$ is the smallest integer $n$ satisfying the following inequality:

$$\frac{\sin (u_n)}{u_n + \sin (u_n) \cos (u_n)} \exp (-u^2 t^*) \cos (u_n x^*) \leq 0.00001$$
Figure A.1: Schematic of strand
REFERENCES


