SIMULATION AND ANALYSIS OF THIN STRIP CASTING PROCESSES

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ABSTRACT

During thin strip casting of alloys such as steel and aluminum, major considerations are the commonly observed casting defects such as centerline segregation and formation of cracks due to thermo-mechanically induced stresses. To compute these stresses and the susceptibility of the material to crack due to these stresses, a two-dimensional finite element thermo-mechanical model has been developed. This model computes turbulent fluid flow profiles, heat transfer, solidification, and thermo-mechanically induced stresses in the solidifying body. The temperature field obtained by the fluid flow and heat transfer model is imposed as a thermal load to the system. A visco-plastic constitutive relation has been used to describe the behavior of solidifying steel. A temperature dependent ultimate strength is used to define the cracking index, which indicates the susceptibility of the material to crack. The fluid flow calculations and stress calculations are performed using commercial software programs FIDAP and ANSYS, respectively. This model was used to study two-roll melt drag method for thin strip casting of steel and horizontal twin-roll method for thin strip casting of aluminum. The effects of process parameters, such as roller speed, melt-roll heat transfer coefficient and melt superheat, on these thin strip casting processes were investigated.
To my parents
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# TABLE OF CONTENTS

Abstract................................................................................................................. ii

Dedication............................................................................................................. iii

Acknowledgements.............................................................................................. iv

Vita..................................................................................................................... v

List of Tables....................................................................................................... ix

List of Figures..................................................................................................... x

Nomenclature...................................................................................................... xiii

Chapter:

1. Introduction.................................................................................................... 1

2. Literature review............................................................................................ 4

   2.1 Melt drag process for thin strip casting of steel........................................ 4

   2.2 Twin-roll thin strip casting of steel............................................................. 6

   2.3 Two-roll melt drag process for thin strip casting of steel.......................... 7

   2.4 Horizontal type twin-roll thin strip casting of aluminum......................... 8

   2.5 Past studies............................................................................................... 9

      2.5.1 Fluid flow modeling........................................................................ 9

      2.5.2 Stress modeling............................................................................. 12
3. Mathematical modeling

3.1 Fluid flow model

3.1.1 Assumptions

3.1.2 Transport equations

3.1.3 Turbulence model

3.1.4 Effective viscosity model

3.1.5 Boundary conditions

3.1.5.1 Two-roll melt drag thin strip casting of steel

3.1.5.2 Horizontal type twin-roll thin strip casting of aluminum

3.1.6 Solution procedure for fluid flow equations

3.2 Stress Model

3.2.1 Assumptions

3.2.2 Governing equations

3.2.3 Special procedure for liquid regions

3.2.4 Solution procedure for mechanical equilibrium equations

3.3 Algorithm to calculate stresses in a solidifying body

4. Results and discussion

4.1 Two-roll melt drag thin strip casting of steel

4.1.1 Fluid flow model predictions

4.1.1.1 Effect of roller speed on cast strip thickness

4.1.1.2 Effect of melt-roll heat transfer coefficient on cast strip thickness

4.1.1.3 Effect of melt superheat on cast strip thickness

4.1.1.4 Conclusions

4.1.2 Stress model predictions

4.1.2.1 Conclusions
4.2 Horizontal type twin-roll thin strip casting of aluminum ........................................60
  4.2.1 Fluid flow model predictions ..................................................................69
  4.2.2 Stress model predictions .....................................................................71
  4.2.3 Conclusions ....................................................................................72

5. Summary and future work ...........................................................................73
  5.1 Conclusions ..........................................................................................73
    5.1.1 Two-roll melt drag thin strip casting of steel ........................................73
    5.1.2 Horizontal twin-roll thin strip casting of aluminum ...............................73
  5.2 Future work ..........................................................................................74

References ........................................................................................................75
# LIST OF TABLES

<table>
<thead>
<tr>
<th>Table</th>
<th>Description</th>
<th>Page</th>
</tr>
</thead>
<tbody>
<tr>
<td>3.1</td>
<td>Material parameters for Fe-2%Si</td>
<td>27</td>
</tr>
<tr>
<td>3.2</td>
<td>Material parameters for 1100 Al</td>
<td>28</td>
</tr>
<tr>
<td>4.1</td>
<td>Thermo-physical properties and base conditions used in the fluid flow model of two-roll melt drag thin strip casting of steel</td>
<td>35</td>
</tr>
<tr>
<td>4.2</td>
<td>Composition of Al-8111 alloy</td>
<td>61</td>
</tr>
<tr>
<td>4.3</td>
<td>Geometrical and thermo-physical parameters used in fluid flow model for twin-roll thin aluminum strip casting</td>
<td>62</td>
</tr>
</tbody>
</table>
LIST OF FIGURES

<table>
<thead>
<tr>
<th>Figure</th>
<th>Description</th>
<th>Page</th>
</tr>
</thead>
<tbody>
<tr>
<td>2.1</td>
<td>Schematic of the melt drag process</td>
<td>5</td>
</tr>
<tr>
<td>2.2</td>
<td>Schematic of the twin-roll process</td>
<td>6</td>
</tr>
<tr>
<td>2.3</td>
<td>Schematic of the two-roll melt drag process</td>
<td>7</td>
</tr>
<tr>
<td>2.4</td>
<td>Schematic of the horizontal twin-roll process</td>
<td>8</td>
</tr>
<tr>
<td>3.1</td>
<td>Computational domain for the two-roll melt drag process</td>
<td>21</td>
</tr>
<tr>
<td>3.2</td>
<td>Computational domain for the horizontal twin-roll casting process</td>
<td>23</td>
</tr>
<tr>
<td>4.1</td>
<td>Position of solid liquid interface for two-roll melt drag thin strip casting of steel</td>
<td>37</td>
</tr>
<tr>
<td>4.2</td>
<td>Velocity vectors in the melt pool and rolls for two-roll melt drag thin strip casting of steel</td>
<td>38</td>
</tr>
<tr>
<td>4.3</td>
<td>Turbulent kinetic energy contours in the melt pool for two-roll melt drag thin strip casting of steel</td>
<td>39</td>
</tr>
<tr>
<td>4.4</td>
<td>Rate of dissipation of turbulent kinetic energy in the melt pool for two-roll melt drag thin strip casting of steel</td>
<td>40</td>
</tr>
<tr>
<td>4.5</td>
<td>Turbulent viscosity contours in the melt pool for two-roll melt drag thin strip casting of steel</td>
<td>41</td>
</tr>
<tr>
<td>4.6</td>
<td>Velocity vectors in the melt pool and the rolls when inlet is near the top</td>
<td>42</td>
</tr>
<tr>
<td>4.7</td>
<td>Turbulent kinetic energy contours in the melt pool for two-roll melt drag thin strip casting of steel when the inlet is near the top</td>
<td>43</td>
</tr>
</tbody>
</table>
4.8 Strip thickness as a function of roller speed in two-roll melt drag thin strip casting of steel .................................................................45
4.9 Strip thickness as a function of melt-roll heat transfer coefficient in two-roll melt drag thin strip casting of steel..................................................47
4.10 Strip thickness as a function of melt superheat in two-roll melt drag thin strip casting of steel...............................................................48
4.11 Cracking index across the cast strip at the kissing point of rollers in two-roll melt drag thin strip casting of steel for different casting speed..........52
4.12 Cracking index across the strip at the kissing point for different values of melt-roll heat transfer coefficients in two-roll melt drag thin strip casting of steel ...............................................................54
4.13 Cracking index along the middle layer of the strip for different casting speeds in two-roll melt drag thin strip casting of steel..........................55
4.14 Cracking index along the middle layer of the strip for different melt-roll heat transfer coefficients in two-roll melt drag thin strip casting of steel ..................57
4.15 Cracking index along the upper surface of the strip for different melt-roll heat transfer coefficient in two-roll melt drag thin strip casting of steel ..............59
4.16 Typical temperature contours in the melt pool and rolls in horizontal type twin roll strip casting of aluminum .................................................................63
4.17 Position of the solid-liquid interface and the sump depth in horizontal twin-roll thin strip casting of aluminum ......................................................64
4.18 Sump depth as a function of roller speed in horizontal twin-roll thin strip casting of aluminum .................................................................65
4.19 Sump depth as a function of melt-roll heat transfer coefficient in horizontal twin-roll thin strip casting of aluminum..........................66
4.20 Sump depth as a function of melt superheat in horizontal twin-roll thin strip casting of aluminum......................................................67
4.21 Centerline segregation in Al-1.7%Fe alloy for various casting speeds..............68
4.22 Principal stress vector plot in the solidified metal and rollers in horizontal twin-roll thin strip casting of aluminum..........................69
4.23 Maximum principal stress across the cast strip at the kissing point in horizontal twin-roll thin strip casting of aluminum..........................71
NOMENCLATURE

\( a \) (on page 19): Turbulence intensity (dimensionless)

\( a \): Strain rate sensitivity of hardening or softening (dimensionless)

\( A \): Pre-exponential factor (s\(^{-1}\))

\( C_1, C_2, C_\mu \): Constant appearing in turbulence model (dimensionless)

\( c_p \): Specific heat (J/kg K)

\( d_{in} \): Width at the inlet for the two-roll melt drag process (m)

\( d^0 \): Effective inelastic deformation rate (s\(^{-1}\))

\( E_{ijkl}(T) \): Temperature dependent modulus of elasticity (Pa)

\( f_i \): Body forces acting on the system (kg-m/s\(^2\))

\( f_x, f_y \): Body forces in x and y direction (kg-m/s\(^2\))

\( f_{\text{decay}} \): Decay factor for turbulent viscosity (dimensionless)

\( f_t \): Tangential component of total stress vector (Pa)

\( F \): Vector representing body forces and boundary conditions

\( G \): Elastic bulk modulus for the material (Pa)

\( h \): Convective heat transfer coefficient for cooling fluid (W/m\(^2\) K)

\( h_{\text{melt-roll}} \): Convective heat transfer coefficient between strip and roller (W/m\(^2\) K)

\( h_{\text{effective}} \): Effective heat transfer coefficient (W/m\(^2\)K)

\( h_0 \): Hardening/softening constant (Pa)
H: Heat generation rate per unit volume (J/m$^3$s)  

K : Global system matrix  

$K_0$: Molecular thermal conductivity (W/mK)  

$K_t$: Turbulent thermal conductivity (W/mK)  

$K_{eff}$: Effective thermal conductivity (W/mK)  

k : Kinetic energy of turbulence (m$^2$/s$^2$)  

m : Strain rate sensitivity of stress (dimensionless)  

n : Strain rate sensitivity of saturation value (dimensionless)  

$n_i$ : Normal vector at the strip-roll boundary  

P: Pressure (Pa)  

$q_s$: Heat generation rate due to applied sources or sinks (J/m$^3$s)  

$q_d$: Heat generation rate due to viscous dissipation (J/m$^3$s)  

$q_r$: Heat generation rate due to chemical reactions (J/m$^3$s)  

$q_i$: Heat generation rate due to electrical heating (J/m$^3$s)  

Q : Activation energy (J/mole)  

R : Universal gas constant (J/mole-K)  

$R_i$: Inner radius of roller (m)  

$R_o$: Outer radius of roller (m)  

$R_r$: Turbulent Reynolds number (dimensionless)  

s : Deformation resistance (Pa)  

$s^*$ : Saturation value of deformation resistance (Pa)  

$s'$ : Time derivative of deformation resistance (Pa/s)  

$s_0$: Initial value of deformation resistance (Pa)
\( s^\wedge \): Coefficient for deformation resistance saturation value (Pa)

\( t \): Roll separation (m)

\( t_i \): Tangential vector at the strip-roll boundary

\( T \): Temperature (K)

\( \Delta T \): Temperature difference (K)

\( T_{in} \): Inlet temperature of molten alloy (K)

\( T_{liq} \): Liquidus temperature of the alloy (K)

\( T_{ref} \): Reference (strain free) temperature (K)

\( T_{sol} \): Solidus temperature of the alloy (K)

\( u \): Global vectors of unknown variables

\( u^* \): Approximation to the exact solution \( u \)

\( U \): \( x \) (horizontal) component of velocity (m/s)

\( U_{in} \): Inlet velocity (m/s)

\( U_n \): Normal component of velocity (m/s)

\( U_t \): Tangential component of velocity (m/s)

\( U_{wall} \): \( x \) component of the velocity at the walls (m/s)

\( V \): \( y \) (vertical) component of velocity (m/s)

\( V_{cast} \): Casting speed (m/s)

\( V_{roll} \): Roller speed (m/s)

\( V_{wall} \): \( y \) component of the velocity at the walls (m/s)

\( u_t \): Velocity of the boundary (m/s)

\( u_t^h \): Velocity of the boundary for which tangential stress is zero (m/s)

\( x \): Cartesian coordinate in horizontal direction
y: Cartesian coordinate in vertical direction

\( \alpha \): Slip coefficient (dimensionless)

\( \alpha_x, \alpha_y \): Thermal coefficient of expansion in x and y direction (K\(^{-1}\))

\( \delta \): Characteristic width of the shear layer (m)

\( \beta \): Acceleration or scaling factor (dimensionless)

\( \varepsilon_{ij} \) (on page 17): Shear rate (deformation) tensor

\( \varepsilon_{ij} \): Total strain tensor

\( \varepsilon_{ij}^e \): elastic component of total strain

\( \varepsilon_{ij}^p \): Plastic component of total strain

\( \varepsilon_{ij}^{Th} \): Thermal component of total strain

\( \varepsilon \): Dissipation rate of kinetic energy (m\(^2\)/s\(^3\))

\( \mu_o \): Molecular viscosity (Pa.s)

\( \mu_t \): Turbulent viscosity (Pa.s)

\( \mu_{eff} \): Effective viscosity (Pa.s)

\( \rho \): Density (kg/m\(^3\))

\( \lambda \): Fraction of solid

\( \sigma_k \) and \( \sigma_e \): Constants appearing in turbulence model

\( \sigma_{ij} \): Stress tensor

\( \tau_{ij} \): Deviatoric part of the stress tensor

\( \xi \): Multiplier of stress (dimensionless)
CHAPTER 1

INTRODUCTION

Thin strip casting processes for casting molten metal directly into sheets of desired thickness and width are relatively new, and are currently being developed around the world. When developed reliably, they will have the following advantages over the conventional continuous casting processes:

1) Energy savings in heating and deformation in hot and cold rolling.
2) Reduced production time, which increases the efficiency of the process.
3) Reduced segregation due to decreased solidification time.
4) Refined microstructure due to faster cooling.

The major process problems, which thin strip casting industries are facing, are listed below:

1) Cracking and surface defects due to thermally induced stresses.
2) Difficulties in casting alloys with long solidification time.
3) High roll wear.
4) Homogenous flow problem due to free surface fluctuations.
5) Low productivity and yield due to edge trimming and reoxidation problem.
In the present study, strip casting processes for casting thin strips (of thickness between 2 and 20 mm) of stainless steel and aluminum are investigated. An overview of single roll melt drag process, twin-roll process, and two-roll melt drag process for strip casting of steel is presented. Two-roll melt drag process for strip casting of steel is studied in detail. For industrial production of thin strips of aluminum, horizontal type twin-roll casters have proven to be the most economical and efficient machines and hence it has been studied in detail.

A two-dimensional finite element based mathematical model was developed to simulate turbulent fluid flow, heat transfer, solidification, and thermally induced stresses in two-roll melt drag thin strip casting of steel and horizontal type twin-roll thin strip casting of aluminum. Fluid flow and heat transfer calculations were performed using commercially available software, FIDAP. In thin strip casting processes, a major consideration is the formation of cracks due to thermally induced stresses in the solidifying material. To compute these stresses and the susceptibility of the material to crack due to these stresses, the temperature field obtained from fluid flow calculations is imposed as a thermal load to the mechanical system. Stress field calculations are performed using commercially available software ANSYS. A visco-plastic constitutive relation has been used to describe the behavior of solidifying material. A temperature dependent ultimate strength is used to define the cracking index, which indicates the susceptibility of the material to crack.

The last decade has seen a tremendous activity in the area of thin strip casting and metallic ribbon formation. A brief review of some of the important past studies in thin strip casting of steel and aluminum is presented in Chapter 2. Chapter 3 presents the formulation of mathematical model. This integrated mathematical model consists of a
two-dimensional turbulent fluid flow, heat transfer, solidification and stress model. The first section of this chapter deals with the formulation of fluid flow and heat transfer model and the next section deals with the formulation of stress model. Assumptions, appropriate boundary conditions and methodology to solve the governing equations are also presented in this chapter. Chapter 4 discusses the results obtained from the simulation of fluid flow, heat transfer, solidification and stresses for two-roll melt drag thin strip casting of steel and horizontal type twin-roll thin strip casting of aluminum. Chapter 5 summarizes the results and presents suggestions for future study.
CHAPTER 2

LITERATURE REVIEW

In the past, a number of processes were considered as possible candidates for thin strip production. However, as discussed in recent literature [1-4] on the thin strip casting, only three processes have shown the potential for industrial production of thin strips of steel. These are:

1) Melt drag process
2) Twin-roll process
3) Two-roll melt drag method.

For thin strip casting of aluminum, horizontal twin-roll casters have been proved to be the most efficient and economical machines.

2.1 Melt Drag Process for Thin Strip Casting of Steel:

The melt drag process is shown schematically in Fig. 2.1. Austenitic stainless steel is cast successfully by Allegheny Ludlum using this process [5]. In this process, a chilled rotating drum is placed adjacent to a tundish, which is filled with molten metal to a constant depth to maintain a constant melt flow rate. The rotating drum then drags metal out of the tundish in the form of a thin solidified strip. In this process, only one side of
the strip solidifies on the roller while the other side solidifies in the open atmosphere and thus results in poor surface finish.

Figure 2.1: Schematic of the melt drag process.
2.2 Twin-roll Thin Strip Casting of Steel:

To get better surface finish on both sides, the twin-roller caster is used. A schematic of this process is shown in Fig. 2.2. Nippon Steel and Mitsubishi Heavy Industries are using this process to cast austenitic stainless steel [5]. In this process, molten metal is fed between two water-cooled counter-rotating rollers. Solidification starts at the point of first metal-roller contact, and shell binding takes place at the kissing point where the rollers are closest to each other. This process requires a very strict control and thus leaves a very small window for operational variables. Moreover, turbulence in the metal pool caused by the momentum of the incoming stream can affect the product quality.

Submerged entry nozzle

![Schematic of the twin-roll thin strip casting process](image)

Solidified Strip (thickness ~ 2-7 mm)

Figure 2.2: Schematic of the twin-roll thin strip casting process
2.3 Two-roll Melt Drag Process for Thin Strip Casting of Steel:

In this process, as shown in Fig. 2.3, a roller of smaller or equal diameter is placed on top of the free surface which improves the quality of the top cast surface. This development combines the features of the single and twin-roll casting processes. The main roll generates the strip using the melt drag technique and the presence of second roll provides the second surface contact. A horizontal feed system provides less turbulent feed supply and better control of the process. The rolls are oriented such that the contact length of rolls with the molten metal is same for both rolls. This process is most suitable for casting strips up to 2 mm thick, while the two-roller melt drag and the twin-roller strip casting processes are preferable for thicker strips up to 7 mm [1].

Figure 2.3: Schematic of the two-roller melt drag process
2.4 Horizontal Type Twin-roll Thin Strip Casting of Aluminum:

For industrial production of thin strips of aluminum, twin roll casters have proven to be the most economical and efficient machines. In a horizontal type twin roll caster, molten aluminum is fed from a refractory feed tip into the gap between two counter rotating, water-cooled cylindrical steel rolls. The schematic of this caster is shown in Fig. 2.4. Unlike thin strip casting of steel, the solidification gets over well before the kissing point during casting of aluminum sheets and the material undergoes a considerable amount of rolling before it leaves the rolls [6]. Also, there is a relative velocity between the cast strip and the rolls which results in shear stresses at the metal-roll interface.

![Diagram of horizontal twin-roll caster](image)

Roll Diameter = 900 mm  
Roll thickness = 50 mm  
Tip set back = 85 mm  
Cast strip (thickness = 10 mm)

Figure 2.4: Schematic of the horizontal twin-roll caster.
2.5 Past Studies:

2.5.1 Fluid Flow Modeling:

A significant amount of work has been done to mathematically model fluid flow and heat transfer in single roll melt drag process. Takeshita and Shingu [7] analyzed the ribbon formation process by the single roller rapid solidification technique by considering the thermal and momentum analyses separately. Kawasa et. al [8] found that the undercooling effect was significant for 304 stainless steel for a shell thickness of 0.5 mm or less. Granasy [9] took the heat transfer, momentum transfer, and temperature dependence of viscosity into account for the analysis of ribbon formation by the single roller process and concluded that the final ribbon thickness is effected mostly by the momentum transport and not the heat transport. Mehrotra and co-workers [10-14] started with a simple overall enthalpy model [10] and came up with a rigorous mathematical model based on segmentwise heat balance model [11]. Later [12], they presented a fluid flow model, in which the effects of all the parameters were lumped in a single empirical coefficient referred to as ‘solidification growth parameter’. In their heat balance model [11], they assumed a constant temperature of liquid metal and ignored the fluid flow in the tundish and the liquid pool region. Also, they did not consider heat transfer in the fluid mechanics model [12]. In another work, [13], they modeled the process by considering both fluid flow and heat transfer with the assumption of one-dimensional coquette flow, which is actually a three-dimensional turbulent flow. Their most recent work [14] was based on two-dimensional heat transfer and fluid flow, and predicted the variation of strip thickness as a function of superheat, wheel speed, heat transfer
coefficient, contact length, etc. However, no turbulence in the liquid pool was considered in the their work.

Various models have been suggested in literature to predict fluid flow, heat transfer and solidification in twin roll casters [15-30]. Fujita et. al [15] performed casting experiments using paraffin wax to study solidification and roll binding characteristics in twin roll casting process. They found that the roll gap had a large influence on the casting behavior. For a narrow roll gap, partially solidified layer on the shell squeezed out into the molten metal pool and resulted in negative segregation and lowering of pool temperature, whereas a large roll gap resulted in breakout of the strip. Stable casting operation can only be obtained if solidification completes in the close vicinity of kissing point. Miyazawa and Szekely [16] developed a model to predict heat flow and solidification in a twin-roll system. They concluded that there exists only a narrow range of process variables that gives a stable mode of operation. Their calculations were carried out in one dimension, for a single system, for a fixed geometry, and for fixed roll properties. The mushy zone and two-phase region were neglected.

Hwang et.al [28] considered mushy zone in their transient fluid flow and heat transfer analysis, however, they didn’t consider turbulent flow in the melt pool. Murukami et. al [21] developed a mathematical model of coupled turbulent flow, heat transfer, and solidification in a wedge-shaped pool of a vertical twin-roll process. They concluded that both the flow and temperature profiles were strongly affected by the zone of solid liquid coexistence. The effect of solidification would therefore seem to be very important in the design of molten alloy delivery to the twin-roll casters.
A significant amount of work has been done by Gupta and Sahai [1], to mathematically model thin strip casting processes for steel. In their work, they simulated melt drag, twin roll and two roll melt drag thin strip casting processes. The mathematical models developed by them simulate the coupled turbulent flow, heat transfer, and solidification of liquid steel in the melt pool. Their results for the melt drag and twin roll casting methods matched well with the pilot plant data. In the case of two roll melt drag method, the design of caster in work by Gupta [1] was such that the kissing point of rolls lies above the molten metal level in the melt pool. This is undesirable for smooth flow of liquid metal in the melt pool because then the rolls have to pull the metal against the gravitational force.

Thin strip casting of aluminum is a relatively new process, which utilizes rapid solidification technology. Horizontal twin roll casters have proven to be the most economical and efficient machines. This decade has seen a tremendous development in the art of thin strip casting of aluminum. The three major thin-gauge high-speed casters operating in the United States and Europe are [31]:

1. The FATA Hunter 2180 mm wide “Speed Caster”
2. The Kvaerner Davy 1800 mm wide “Fastcast”
3. The Pechiney “Jumbo 3CM” 2020 mm wide caster

Norandal USA and FATA Hunter have been trying to develop a thin-gauge roll casting technology. It was found that the productivity of the caster could be doubled by reducing the casting gauge to about 1 mm. At full width, this caster can produce 6.5 tonnes per hour.
Eurofoil S.A. makes aluminum foil and sheets for a host of applications at any gauge between 0.350 mm to 0.006 mm and at any width from 1640 mm down. The new caster developed by them is a twin-roll four high machine—the only one of its kind in the world.

The Jumbo 3CM caster developed by Pechiney Aluminum Engineering [32] can cast strips in the region of 2mm gauge. These casters feature an improved tip design and a capacitance sensor to accurately control the head of molten metal in the tip.

The major problems in the industry today are the various defects in the cast product. Various casting defects like centerline segregation [33], heat line formation [34], and sticking problems occur during strip casting. Centerline segregation [35], which appears to be the major limitation in the industrial production of aluminum strip, is caused by roll force induced inter-dendritic liquid flow and becomes more severe when an alloy with a wide freezing range is cast. Heat lines are formed when the material leaves the roll bite in the partially molten state. These are semi-continuous longitudinal defects, a few centimeters in width. Sticking occurs because of a high, steep pressure distribution along the arc of roll-strip contact, which is caused by the friction developed between the rolls and the strip.

2.5.2 Stress Modeling

The modeling of stresses in thin strip casting processes has received little attention of the researchers in past. Jarry et. al [36] proposed a thermo-mechanical model for 3C roll casting of alloys. They considered the coupling between the thermokinetics of solidification and mechanics of deformation due to the rolling operation. Some work has also been done to compute thermal stresses in the conventional continuous casting processes. Li and Ruan [37] presented a transient coupled thermal, fluid flow and stress
model to predict thermal stresses which evolve in a solidifying ingot during the initial phase of continuous casting of aluminum. They used a hypoelastic-viscoplastic constitutive relation to describe the deformations in the solidifying material. Aboutalebi et. al [38] developed a three-dimensional model to analyze heat transfer and solidification in continuous casting of arbitrary sections. From the temperature field predicted by a heat flow model, they carried out a quasi-non-linear stress analysis within the solidifying shell of the beam blank section. Song et. al [39] developed a coupled finite element model to simulate heat transfer, phase change and stress build up in the material. They used a viscoplastic constitutive relation to predict the evolution of plastification. Zabaras et. al [40] used a front tracking finite element method to calculate temperature and stress field in a solidifying pure metal. They employed a rate dependent viscoplastic-hypoelastic constitutive model to solve the equilibrium equations.

From the above discussion, it is evident that a number of studies have been published to model fluid flow and heat transfer in thin strip casting processes but no work has been done to develop a comprehensive mathematical model capable of simulating turbulent fluid flow, heat transfer, solidification, and thermally induced stresses in thin strip casting of steel and aluminum.
CHAPTER 3

MATHEMATICAL MODELING

3.1 Fluid Flow Model

A general two-dimensional mathematical model to simulate fluid flow, heat transfer, and solidification is described here. This model is applicable for all thin strip casting processes for steel and aluminum, with some differences, such as velocity and load boundary conditions.

The following assumptions were made in the development of the mathematical model.

3.1.1 Assumptions

The assumptions made in the development of the mathematical model for two roll melt drag process are as follows:

1) The process is at steady state. After a small initial transient period, process parameters do not change with time.

2) By taking into account the fact that the width/thickness ratio is very large and ignoring the end effects, the geometry of the process can be approximated as two-dimensional.

3) Liquid metal is incompressible and is a Newtonian fluid.

4) Material properties (except viscosity and specific heat) are temperature independent.
5) There is no segregation.

6) Temperature may be used as the criterion for determination of the strip thickness.

7) Process is assumed to be growth controlled.

8) There is no entrainment of gas.

### 3.1.2 Transport equations

Under the above assumptions, the following transport equations were solved in the calculation domain:

(i) Continuity equation

The continuity equation, which represents conservation of mass, can be expressed, for a steady state and incompressible fluid flow, in the following two-dimensional form,

\[
\frac{\partial U}{\partial x} + \frac{\partial V}{\partial y} = 0
\]  

(3.1)

(ii) Turbulent Navier–Stokes equation

The Navier–Stokes equation, which is a momentum balance equation, can be expressed, for a steady state, two-dimensional incompressible fluid flow, in the following form,

\[
\begin{align*}
\left( \frac{U}{\partial x} + V \frac{\partial U}{\partial y} \right) \rho = & - \frac{\partial P}{\partial x} + \rho F_x + 2 \frac{\partial}{\partial x} \left( \mu_{\text{eff}} \frac{\partial U}{\partial x} \right) + \frac{\partial}{\partial y} \left( \mu_{\text{eff}} \left( \frac{\partial U}{\partial y} + \frac{\partial V}{\partial x} \right) \right) \\
\left( U \frac{\partial V}{\partial x} + V \frac{\partial V}{\partial y} \right) \rho = & - \frac{\partial P}{\partial y} + \rho F_y + 2 \frac{\partial}{\partial y} \left( \mu_{\text{eff}} \frac{\partial V}{\partial y} \right) + \frac{\partial}{\partial x} \left( \mu_{\text{eff}} \left( \frac{\partial U}{\partial y} + \frac{\partial V}{\partial x} \right) \right)
\end{align*}
\]  

(3.2)

(3.3)
Equation for conservation of thermal energy

Conservation of thermal energy can be written as,

\[
\left( U \frac{\partial T}{\partial x} + V \frac{\partial T}{\partial y} \right) \rho c_p = \frac{\partial}{\partial x} \left( K_{\text{eff}} \frac{\partial T}{\partial x} \right) + \frac{\partial}{\partial y} \left( K_{\text{eff}} \frac{\partial T}{\partial y} \right) + H \tag{3.4}
\]

where \( K_{\text{eff}} \) is the sum of molecular conductivity and turbulent conductivity and \( H \) is the heat generation term. This term is typically a combination of several factors, and it may take the form:

\[
H = q_s + q_d + q_r + q_i
\]

where,

\( q_s \) = the heat generation rate due to applied sources or sinks per unit volume

\( q_d \) = the heat generation rate due to viscous dissipation (viscous shearing), and has the representation

\[
q_d = \tau_{ij} \varepsilon_{ij} = 2 \mu_{\text{eff}} \varepsilon_{ij} \varepsilon_{ij}
\]

\( q_r \) = the heat generation rate due to chemical reactions

\( q_i \) = the heat generation rate due to electrical heating

### 3.1.3 Turbulence model

The two equation \( k-\varepsilon \) model of turbulence of Launder and Spalding [41] was used for the turbulence considerations. Governing transport equations for turbulent kinetic energy, \( k \), and its dissipation rate, \( \varepsilon \), can be written as,
For turbulent kinetic energy,

\[
\left( \frac{U}{\rho} \frac{\partial k}{\partial x} + \frac{V}{\rho} \frac{\partial k}{\partial y} \right) = -\rho \varepsilon + \mu_t \varphi + \frac{\partial}{\partial x} \left( \frac{\mu_t}{\sigma_k} \frac{\partial k}{\partial x} \right) + \frac{\partial}{\partial y} \left( \frac{\mu_t}{\sigma_k} \frac{\partial k}{\partial y} \right)
\] (3.5)

For dissipation rate of turbulence,

\[
\left( \frac{U}{\rho} \frac{\partial \varepsilon}{\partial x} + \frac{V}{\rho} \frac{\partial \varepsilon}{\partial y} \right) = -C_\varepsilon \frac{\varepsilon^2}{k} + C_\varepsilon \mu_t \frac{\varepsilon}{k} + \frac{\partial}{\partial x} \left( \frac{\mu_t}{\sigma_\varepsilon} \frac{\partial \varepsilon}{\partial x} \right) + \frac{\partial}{\partial y} \left( \frac{\mu_t}{\sigma_\varepsilon} \frac{\partial \varepsilon}{\partial y} \right)
\] (3.6)

where,

\[
\mu_{\text{eff}} = \mu_o + \mu_t
\] (3.7)

\[
\mu_t = C_\mu \rho k^2 / \varepsilon
\] (3.8)

\[
K_{\text{eff}} = K_o + K_t
\] (3.9)

\[
K_t = C_p \mu_t / Pr
\] (3.10)

Where, Pr is the turbulent Prandtl number. In the present simulation, a value of 1 is taken for Pr.
\[ \phi = 2 \left( \frac{\partial U}{\partial x} \right)^2 + 2 \left( \frac{\partial V}{\partial y} \right)^2 + \left( \frac{\partial V}{\partial x} + \frac{\partial U}{\partial y} \right)^2 \]  

(3.11)

According to Launder and Spalding [41], the five constants appearing in equations (3.5) and (3.6) take the following values,

\[ C_1 = 1.44, \ C_2 = 1.92, \ C_\mu = 0.09, \ \sigma_k = 1.0, \ \sigma_\varepsilon = 1.3 \]

3.1.4 Effective viscosity model

The effective viscosity of the liquid metal will be the sum of turbulent and laminar viscosity. In the solid phase, the viscosity is very high (>10^{12} poise) and hence the flow can be considered as laminar. A model was used to reduce turbulent viscosity to zero in the solid phase, i.e. decay it to zero in some transition zone [42].

\[ \mu_t = \mu_t * f_{\text{decay}} \]  

(3.12)

\[ f_{\text{decay}} = \lambda^{1.5} \exp\left[(-3.4/(1+R_t/50)^2)\right] \]  

(3.13)

\[ R_t = \rho \kappa^2 / (\mu_0 \varepsilon) \]  

(3.14)
\[
\lambda = \left[\frac{T - T_{liq}}{(T_{sol} - T_{liq})}\right]^{\text{power}}
\] (3.15)

The value of the ‘power’ can be set for the desired slope of viscosity in the transition zone to represent the mushy zone. In the present simulation ‘power’ is taken as 1, which represents a linear variation of fraction of solid with temperature.

3.1.5 Boundary conditions

3.1.5.1 Two-roll Melt Drag Thin Strip Casting of Steel

Figure 3.1 shows a simple schematic of computational domain for the two-roll melt drag process. The computational domain includes both rollers, melt pool and a part of the solidified strip. The governing equations were subject to boundary conditions on every surface of the computational domain. These boundary conditions are,

**Inlet:**

Horizontal component of velocity:

\[U = U_{in}\]

Vertical component of velocity, \(V = 0\).

\[T_{in} = T_{liq} + T_{sup}\]

\[k = a*U_{in}^2 \quad \text{where} \quad 0 < a < 0.001\]

\[\varepsilon = k^{1.5} * 10/\delta, \quad \text{where} \quad \delta \quad \text{is the characteristic thickness of shear width. In the present work,} \quad \delta \quad \text{is taken as the length of the inlet.}\]
Refractory walls of tundish

At all refractory walls the zero slip condition was applied. All refractory walls were assumed to be thermally insulated. Kinetic energy and dissipation were also specified to be zero at the wall.

\[ U_{wall} = 0, \quad V_{wall} = 0, \quad \text{Heat flux}_{wall} = 0, \quad k_{wall} = 0, \quad \varepsilon_{wall} = 0. \]

Outer surface of rolls

Tangential and normal velocities were specified on this surface.

\[ U_t = V_{cast}, \quad U_n = 0. \]

An effective heat transfer coefficient due to convective and radiative heat transfer to the atmosphere from this surface was specified as 50 W/m\(^2\)K [1], and reference temperature was specified as 300 K.

Inner surface of rolls

At this surface, normal velocity was specified as zero and the tangential velocity was calculated using casting velocity.

\[ U_t = V_{cast} \times \frac{R_t}{R_o}, \quad U_n = 0. \]

Convective heat transfer coefficient was specified as 10 kW/m\(^2\)K [1] and reference temperature was specified as 313 K.

Strip/Roll interface

The heat transfer coefficient to represent the heat transfer from melt poll to rolls is found to vary over a wide range of values as reported in the literature [43-46]. In the present work, the value of this heat transfer coefficient was taken as 11 kW/m\(^2\)K.
3.1.5.2 Horizontal Type Twin-roll Thin Strip Casting of Aluminum

Figure 3.2 shows a simple schematic of computational domain for the horizontal type twin-roll caster. The computational domain includes both rollers, melt pool, feed tip, and a part of solidified strip. The governing equations were subject to boundary conditions on every surface of the computational domain. These boundary conditions are,

Feed tip

At the feed tip, $U = U_{in}$

Outer surface of rolls

Tangential and normal velocities were specified on this surface.
$U_t = V_{roll}, \ U_n = 0.$ 

An effective heat transfer coefficient due to convective and radiative heat transfer to the atmosphere from this surface was specified as $50\text{W/m}^2\text{K}$, and reference temperature was specified as $300\ \text{K.}$

**Inner surface of rolls**

At this surface, normal velocity was specified as zero and the tangential velocity was specified as:

$U_t = V_{roll}*R_i/R_o.$

Convective heat transfer coefficient was specified as $10,000\text{W/m}^2\text{K}$ and reference temperature was specified as $313\ \text{K.}$

**Strip/roll interface**

To represent the heat transfer between the melt pool and rolls, a heat transfer coefficient is taken as $7,000\ \text{W/m}^2\text{K}$ [33].

Unlike two-roll melt drag method for steel, the cast strip in horizontal type twin-roll caster for aluminum is pulled at a speed which is 10-15% higher than the roll speed. Due to this relative speed between roller and cast strip, a shear stress boundary condition is applied at roll/strip interface. This shear stress is proportional to the relative tangential velocity at the boundary; i.e.,

$$f_i = \sigma_{ij}n_it_i = (1/\alpha)(u_i - u_i^b)t_i$$ \hspace{1cm} (3.16)

$f_i$ is the tangential component of the total stress vector at the boundary, $\sigma_{ij}$ is the stress tensor, $n_i$ and $t_i$ are components of the normal and tangential vectors at the boundary, $\alpha$ is the slip coefficient (constant of proportionality), $u_i$ is the tangential velocity and $u_i^b$ is the
tangential velocity for which the tangential stress at the boundary is zero [42]. \( \alpha \) is taken as 2 in the present model [36].

Figure 3.2: Computational domain for the horizontal type twin-roll casting process.

3.1.6 Solution Procedure for Fluid Flow Equations

The Galerkin finite element procedure is applied to the transport equations. This results in a set of nonlinear algebraic equations that may be represented by the following matrix form:

\[
K(u) \ u = F 
\]  

(3.17)

Where \( K \) is the global system matrix, \( u \) is the global vectors of unknowns, such as velocity field, pressure field and temperature field, and \( F \) is a vector which includes the effects of body forces and boundary conditions. The above nonlinear algebraic equation
system is solved by a fixed point iteration procedure known as Successive Substitution (S.S). It can be written as:

\[ K(u_i)u_{i+1} = F \]  

(3.18)

The non-linearity was evaluated at the known iterate \( u_i \), and a non-symmetric system was formed and solved at each iteration. This method was found to converge for a wide range of Reynolds number. The rate of convergence of S.S. can be improved by the use of an acceleration or scaling factor \( \beta \) as described below:

\[ K(u_i)u^* = F \]  

(3.19)

\[ u_{i+1} = \beta u_i + (1-\beta)u^* ; \quad 0<\alpha<1 \]  

(3.20)

There are no general rules to choose a suitable value of \( \beta \) but a commonly used value is 0.5 and the same value was used in the present simulation.

3.2 Stress Model

3.2.1 Assumptions

The mathematical model involves the following assumptions:

1) The material is assumed to be isotropic in nature.

2) The elastic strains are small relative to the plastic strains.

3) The plastic flow of material is assumed to be isochoric (i.e. volume preserving).

4) Rolling of solidified steel is not considered.

5) There are no stresses due to friction between roll and the cast strip.

3.2.2 Governing equations

The mechanical equilibrium equation for the solidifying body can be written as:
\[
\sigma_{ij}(x,y,z) + f_i = 0, \quad i, j = 1,2,3 \text{ (summation on } i) \tag{3.21}
\]

where, \(\sigma_{ij}\) are the Cartesian components of the Cauchy stress tensor in the body and \(f_i\) represents the body force acting on the system.

The total strain tensor, \(\{\varepsilon_{ij}\}\) can be decomposed in the following way:

\[
\varepsilon_{ij} = \varepsilon_{ij}^e + \varepsilon_{ij}^p + \varepsilon_{ij}^{Th} \tag{3.22}
\]

where, \(\varepsilon_{ij}^e\) is the elastic, \(\varepsilon_{ij}^p\) is the plastic and \(\varepsilon_{ij}^{Th}\) is the thermal part respectively.

According to the constitutive law for isotropic linear elasticity, the stress is related to strains by:

\[
\sigma_{ij} = E_{ijkl}(T) \varepsilon_{kl}^e, \quad i, j, k, l = 1,2,3 \tag{3.23}
\]

where, \(E_{ijkl}(T)\) is the temperature dependent modulus of elasticity.

Thermal strain vector is given by:

\[
\{\varepsilon_{ij}^{Th}\} = \Delta T \begin{bmatrix} \alpha_x & \alpha_y & 0 & 0 & 0 \end{bmatrix}^T \tag{3.24}
\]

where:

\(\alpha_x\): thermal coefficient of expansion in the x direction

\(\Delta T\): \(T - T_{ref}\)

\(T\) is the current temperature at the point and \(T_{ref}\) is the reference (strain-free) temperature at that point.

Inelastic deformation, \(\varepsilon_{ij}^p\), is calculated according to a rate dependent viscoplastic constitutive model. The following general form [40] is assumed here,

\[
\varepsilon_{ij}^p = f_{ij}(\sigma_{ij}, q_{ij}^K, T), \quad i, j = 1, 2, 3 \tag{3.25}
\]

where \(q_{ij}^K\) denote properly defined state variables for which evolution equations of the following form are given:
Anand’s model [47] for viscoplasticity is employed in the present work. There are two basic features in Anand’s model applicable to isotropic rate-dependent constitutive model for metals. First, there is no explicit yield surface, rather the instantaneous response of the material is dependent on its current state. Secondly, a single scalar internal variable ‘s’, called the deformation resistance, is used to represent the isotropic resistance to inelastic flow of the material. The specifics of this constitutive equation are the flow equation:

\[ \frac{dp}{dt} = A e^{Q/R\theta} \left[ \sinh(\xi \sigma/s) \right]^{1/m} \] (3.27)

and the evolution equation:

\[ s' = \{ h_o(\|B\|^2B/\|B\|) \} dp \] (3.28)

where,

\[ B = 1 - s/s^* \] (3.29)

with

\[ s^* = s^* [ (dp/A)e^{Q/R\theta} ]^n \] (3.30)

Where:

\[ dp = \text{effective inelastic deformation rate} \]

\[ \sigma = \text{effective cauchy stress} \]

\[ s = \text{deformation resistance} \]

\[ s^* = \text{saturation value of deformation resistance} \]

\[ s' = \text{time derivative of deformation resistance} \]

\[ \theta = \text{absolute temperature} \]

Material parameters for viscoplasticity model are given in Table 3.1 and 3.2.
<table>
<thead>
<tr>
<th>Material Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>( A )</td>
<td>( 6.346 \times 10^{11} \text{ s}^{-1} )</td>
</tr>
<tr>
<td>( Q )</td>
<td>312.35 kJ/mole</td>
</tr>
<tr>
<td>( \xi )</td>
<td>3.25</td>
</tr>
<tr>
<td>( s^\wedge )</td>
<td>125.1 MPa</td>
</tr>
<tr>
<td>( n )</td>
<td>0.06869</td>
</tr>
<tr>
<td>( h_0 )</td>
<td>3093.1 MPa</td>
</tr>
<tr>
<td>( a )</td>
<td>1.5</td>
</tr>
</tbody>
</table>

Table 3.1: Material parameters for Fe-2\%Si as given in [47].
<table>
<thead>
<tr>
<th>Material Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>A</td>
<td>$1.91 \times 10^7$ s$^{-1}$</td>
</tr>
<tr>
<td>Q</td>
<td>175.3 kJ/mole</td>
</tr>
<tr>
<td>$\xi$</td>
<td>7.00</td>
</tr>
<tr>
<td>$s^\nu$</td>
<td>18.9 MPa</td>
</tr>
<tr>
<td>n</td>
<td>0.07049</td>
</tr>
<tr>
<td>$h_0$</td>
<td>1115.6 MPa</td>
</tr>
<tr>
<td>a</td>
<td>1.5</td>
</tr>
</tbody>
</table>

Table 3.2: Material parameters for 1100 Al as given in [47].

### 3.2.3 Special procedure for liquid regions

Since both liquid and solid regions are part of the calculation domain, a special procedure is employed to handle the liquid region [48]. A value of Poisson’s ratio very close to 0.5 is assigned at the nodes where temperature is above the coherence (or zero-strain) temperature. This makes the liquid phase close to being incompressible for mechanical loading. In order to avoid singularity in forming the stiffness matrix, the Young’s
modulus is set to a very small number, instead of exactly zero, at the nodes above the coherence temperature.

For a non linear elastic solid,

\[ \sigma_{ij} = \sigma'_{ij} + p \delta_{ij} \]

where,

\[ \sigma'_{ij} = 2G \varepsilon_{ij} \]

\[ p = \lambda \varepsilon_v - \kappa (T - T_0) \]

\[ G = E/2(1+v), \lambda = E\nu/(1+\nu)(1-2\nu), \kappa = E\nu/3(1-2\nu) \]

Therefore, when the Poisson’s ratio \( \nu \) is set close to 0.5 and Young’s modulus is a very small number, the deviatoric stress \( \sigma'_{ij} \) can be suppressed whilst keeping the hydrostatic pressure finite.

### 3.2.4 Solution Procedure for Mechanical Equilibrium Equations

\[
\begin{bmatrix} K \end{bmatrix} \{ u \} = \{ F^a \}
\]

(3.31)

The finite element discretization process yields a set of simultaneous equations:

Where: \( [K] \) = coefficient matrix

\( \{ u \} \) = displacement vector

\( \{ F^a \} \) = vector of applied loads.

In the present work, the Newton-Raphson iterative method [49] is employed to solve this highly non-linear problem. This method can be written as:
\[ [K_i^T](\Delta u_i) = \{ F^a \} - \{ F_{i}^{nr} \} \] (3.32)

\[ \{ u_{i+1} \} = \{ u_i \} + \{ \Delta u_i \} \] (3.33)

Where:  
\[ [K_i^T] \] = Jacobian matrix (tangent matrix)  
\[ i \] = subscript representing the current equilibrium iteration  
\[ \{ F_{i}^{nr} \} \] = vector of restoring loads corresponding to the element internal loads.

Both \[ [K_i^T] \] and \[ \{ F_{i}^{nr} \} \] are evaluated based on the values given by \( \{ u_i \} \).

The Newton-Raphson restoring force is given by

\[ \{ F^{nr} \} = \int_{vol} [B]^T \{ \sigma \} d(vol) \] (3.34)

Where,  
\[ [B] \] = strain-displacement matrix  
\[ \{ \sigma \} \] = Cauchy stress.

Cauchy stress can be decomposed into deviatoric part and the pressure part.

\[ \{ \sigma \} = \{ \sigma' \} - \{ q \} P \] (3.35)

Where,  
\[ \{ \sigma' \} \] = Cauchy stress deviator  
\[ \{ q \} = [1 1 1 0 0 0] \]

\[ P = \text{hydrostatic stress} = -(\sigma_x + \sigma_y + \sigma_z)/3 \]

The restoring force can now be written as:

\[ \{ F^{nr} \} = \int_{vol} [B]^T \{ \sigma' \} d(vol) - \int_{vol} [B]^T \{ q \} P d(vol) \] (3.36)
The incompressibility constraint during plastic flow is enforced through the augmentation of the momentum equations with the additional equation:

\[
\int_{\text{vol}} \left[ N^p \right]^T \left( \Delta J - \hat{J} \Delta P \right) \text{d}(\text{vol}) = 0 \quad (3.37)
\]

Where,

\[ N^p \] = shape function associated with the independently interpolated pressure  
\[ \Delta J \] = determinant of the relative deformation gradient (the relative volume change)

\[ \hat{J} \] = constitutively prescribed function expressing the pressure - volume change relationship

\[ \Delta J = \exp \left( -\frac{\Delta P}{G} \right) \quad (3.38) \]

where,  
\[ G \] = elastic bulk modulus for the material  
\[ E \] = Young’s modulus  
\[ \nu \] = Poisson’s ratio

The total Cauchy stress is calculated by finding the deviatoric part from the constitutive equations using the strains calculated from nodal displacements and subtracting the separately interpolated pressure, i.e.

\[ \{\bar{\sigma}\} = \{\sigma\} - \{q\} P_0 \quad (3.39) \]

Where, \( P_0 \) = interpolated from the pressure field
The stiffness matrix is constructed by evaluating the exact Jacobian of the discretized system. This yields an equation of the form:

\[
\begin{bmatrix}
K_{uu} & K_{up} \\
K_{pu} & K_{pp}
\end{bmatrix} \begin{bmatrix}
\Delta u \\
\Delta P
\end{bmatrix} = \begin{bmatrix}
\{F\} \\
\{0\}
\end{bmatrix} - \begin{bmatrix}
\{F_u\} \\
\{F_p\}
\end{bmatrix}
\]  \quad (3.40)

where,

\[
\{F\} = \text{external node forces}
\]

\[
\{\Delta u\}, \{\Delta P\} = \text{increments of displacement and pressure, respectively}
\]

\[
\{F_u\} = \int_{\text{vol}} [B]^T \{\tilde{\sigma}\} \text{d(vol)}
\]  \quad (3.41)

\[
\{F_p\} = \int_{\text{vol}} [N^p]^T (\Delta J - \Delta \hat{J}(\Delta P)) \text{d(vol)}
\]  \quad (3.42)

\[
[K_{uu}] = \frac{\partial}{\partial u} \left[ \int_{\text{vol}} [B]^T \{\tilde{\sigma}\} \text{d(vol)} \right]
\]  \quad (3.43)

\[
[K_{up}] = [K_{pu}]^T = \frac{\partial}{\partial p} \left[ \int_{\text{vol}} [B]^T \{\tilde{\sigma}\} \text{d(vol)} \right]
\]  \quad (3.44)

\[
[K_{pp}] = \frac{\partial}{\partial p} \left[ \int_{\text{vol}} [N^p](\Delta - \Delta \hat{J}(\Delta P)) \text{d(vol)} \right]
\]  \quad (3.45)

3.3 Algorithm to Calculate Stresses in a Solidifying Body

The algorithm to calculate stresses in the solidifying body can be summarized in the following steps:
Fluid flow and thermal field calculation

1. Identify the calculation domain.

2. Create a finite element mesh in the calculation domain.

3. Specify material properties.

4. Specify temperature and velocity boundary conditions.

5. Solve governing equations for temperature and velocity at each node of the mesh.

Stress field calculations

6. Create the same mesh as in step 2.

7. Specify mechanical properties of the material.

8. Specify displacement boundary condition at the walls.

9. Apply temperature field solution (obtained from step 5) as a thermal load to the mechanical system.

10. Impose a value of 0.499 for Poisson’s ratio and a value very close to zero for Young’s modulus \(10^{-11}\) Pa at nodes where the temperature is higher than the coherence temperature (liquidus temperature).

11. Specify the reference temperature for the system.

12. Solve governing equations for stress and strain at each node of the mesh.
CHAPTER 4

RESULTS AND DISCUSSION

Results of the two-dimensional finite element model, described in the previous chapter, are presented here. The finite element software, FIDAP generates the mesh for the computational domain. Fluid flow and heat transfer equations are solved, using FIDAP, for pressure, velocity, and temperature field in the computational domain. To calculate the thermal stresses in the solidifying body, the finite element mesh is imported from FIDAP to a commercial software ANSYS. Equations for mechanical equilibrium are solved, using visco-plastic constitutive relationship to describe the material behavior at elevated temperature, for the stress field. Results obtained for two-roll melt drag process for thin strip casting of steel and horizontal type twin-roll casting process for thin strip casting of aluminum are presented in this chapter.

4.1 Two-roll Melt Drag Thin Strip Casting of Steel

4.1.1 Fluid flow Model Predictions

The two-roll melt drag thin strip casting process combines the features of single roll melt drag and twin-roll thin strip casting processes. Computational domain for this process is shown in Fig. 3.1. Geometrical parameters and thermo-physical parameters used in this simulation are listed in Table 4.1.
<table>
<thead>
<tr>
<th>Property</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Specific heat of solidified steel</td>
<td>680 J/kg K</td>
</tr>
<tr>
<td>Viscosity of liquid steel</td>
<td>0.0050 Pa.s</td>
</tr>
<tr>
<td>Latent heat of fusion of steel</td>
<td>272,000 J/kg</td>
</tr>
<tr>
<td>Thermal conductivity of solidified steel</td>
<td>28 W/m K</td>
</tr>
<tr>
<td>Density of solidified steel</td>
<td>7,200 kg/m³</td>
</tr>
<tr>
<td>Degree of superheat in steel</td>
<td>60 K</td>
</tr>
<tr>
<td>Atmosphere temperature</td>
<td>300 K</td>
</tr>
<tr>
<td>Cooling water temperature</td>
<td>313 K</td>
</tr>
<tr>
<td>Thermal conductivity of copper</td>
<td>390 W/m K</td>
</tr>
<tr>
<td>Thickness of both the rollers</td>
<td>5 mm</td>
</tr>
<tr>
<td>Melt-roll heat transfer coefficient</td>
<td>11.0 kW/m² K</td>
</tr>
<tr>
<td>Heat transfer coefficient for cooling fluid</td>
<td>10 kW/m² K</td>
</tr>
<tr>
<td>Roller diameter</td>
<td>300 mm</td>
</tr>
<tr>
<td>Contact angle</td>
<td>40 °</td>
</tr>
<tr>
<td>Liquidus temperature of steel</td>
<td>1,733 K</td>
</tr>
<tr>
<td>Solidus temperature of steel</td>
<td>1,673 K</td>
</tr>
<tr>
<td>Roller material</td>
<td>Copper</td>
</tr>
</tbody>
</table>

Table 4.1: Thermo-physical properties and base conditions used in the fluid flow model of two-roll melt drag thin strip casting of steel.
The melt flow in the melt pool and near the roller in this process is not laminar, hence turbulence is considered in the present study.

Figures 4.1 to 4.5 show the solid-liquid interface, velocity vectors in the melt pool and rollers, turbulent kinetic energy contours in the melt pool, rate of dissipation of turbulent kinetic energy contours, and turbulent viscosity contours in the melt pool, respectively.

The solid-liquid interface in the melt pool shows the initiation of solidification of liquid metal as soon as it comes in contact with rollers and it gets over at the kissing point of two rolls.

The velocity vector plot in the melt pool shows the metal, in the vicinity of the rolls, being dragged along the rolls at the same velocity. The excess metal, which couldn’t be solidified, flows back with a relatively lesser velocity. Two recirculation loops in the flow of liquid metal can also be noticed from this vector plot.

The turbulent kinetic energy contour plot shows that the flow is more turbulent at the corners where liquid metal and rolls are in contact. It can be noticed that the turbulent kinetic energy is more near the upper roller.

The rate of dissipation of turbulent kinetic energy contour plot shows that the dissipation of turbulence is highest at the corners where liquid metal and rolls are in contact.

The turbulent viscosity contour plot shows the highest turbulent viscosity observed in the melt pool is 35.62 Pa-s, which is more than 7,000 times of the molecular viscosity.
Figure 4.1: Position of solid liquid interface for two-roll melt drag thin strip casting of steel
Figure 4.2: Velocity vectors in the melt pool and rolls for two-roll melt drag thin strip casting of steel
Figure 4.3: Turbulent kinetic energy contours in the melt pool for two-roll melt drag thin strip casting of steel
Figure 4.4: Rate of dissipation of turbulent kinetic energy contours in the melt pool for two-roll melt drag thin strip casting of steel.
Figure 4.5: Turbulent viscosity contours in the melt pool for two-roll melt drag thin strip casting of steel
Effect of the position of the inlet

The position of inlet was moved from the bottom to near the top surface to see its effect on the turbulent flow profile. Figures 4.6 and 4.7 show the velocity vector plot and the turbulent kinetic energy contour plot for the new position of inlet, respectively. It can be noticed from these plots, that this new position of the inlet results in a more symmetric flow. In this case, the maximum turbulent kinetic energy contour lies in between the two rolls, whereas in the earlier case, the maximum turbulent kinetic energy contour was near the upper roll.

Figure 4.6: Velocity vectors in the melt pool and rollers when inlet is near the top
Figure 4.7: Turbulent kinetic energy contours in the melt pool when the inlet is near the top
Process variables that affect the thickness of cast strip significantly were identified as:

(i) Roller speed
(ii) Melt-roll heat transfer coefficient
(iii) Melt superheat

4.1.1.1 Effect of roller speed on cast strip thickness

Fig. 4.8 shows the variation in strip thickness with the roller speed for different inlet velocities. It can be seen that as the inlet velocity increases the thickness of the solidified shell on the rolls also increases. Strip thickness is plotted as a function of roller speed for three values of inlet velocities. An inlet velocity of 0.05 m/s results in the thinnest strips whereas a inlet velocity of 0.15 m/s results in the thickest strips.

The roll separation ‘t’ is calculated according to the equation:

\[ U_{in} \times d_{in} = V_{roll} \times t \]

As the inlet velocity increase, the roll separation is increased accordingly to satisfy above equation. As roll separation increases, the depth of melt pool also increases to ensure that the contact length remains same for both the rolls. This results in a higher contact length of molten metal and rolls, which results in a thicker solidified shell on the rolls.

It can be seen that the strip thickness decreases with increase in roller speed. This is because, at higher casting speed contact time of melt with roller is less, hence the time provided for solidification of liquid metal is also less, which results in thinner strips. From Fig. 4.8, it also evident that the growth rate for low roller speed is higher than that for high roller speeds.
Figure 4.8: Strip thickness as a function of roller speed in two-roll melt drag thin strip casting of steel for different casting speeds ($T_{\text{sup}} = 60 \, \text{K}$, $H_{\text{melt-roll}} = 11,000 \, \text{W/m}^2\text{K}$).
4.1.1.2 Effect of melt-roll heat transfer coefficient on cast strip thickness

Melt-roll heat transfer coefficient governs the rate of heat transfer from melt pool to roller. Fig. 4.9 shows the variation in strip thickness with melt-roll heat transfer coefficient. It can be seen from the figure that as the value of this heat transfer coefficient increases the thickness of cast strip increases. In the present investigation, melt-roll heat transfer coefficient was varied from 9,000 W/m²K to 20,000 W/m²K to see its effect on strip thickness. Strip thickness increased from 0.86 mm to 1.2 mm. In this range, strip thickness can be represented by the following equation:

\[ \text{Strip thickness (mm)} = 0.0228 \times (\text{Melt-roll heat transfer coeff. (W/m}^2\text{K}))^{0.4004} \]

4.1.1.3 Effect of melt superheat on cast strip thickness

Fig. 4.10 shows the variation in strip thickness with the melt superheat. Larger amount of heat is to be removed through the solidifying strip in case of liquid steel with higher superheat. This, in turn reduces the solidification rate. It can be seen in Fig. 4.10 that, increase in superheat of liquid steel reduces the solidified thickness of the shell. In the present investigation melt superheat was varied from 20K to 80K to see the sensitivity of strip thickness with respect to melt superheat. Strip thickness reduced from 1.06 mm to 0.93 mm. The effect of superheat of strip thickness does not seem to be significant because the major part of heat is coming from the release of latent heat of fusion and not from superheat. Sensible heat due to superheat is very less compared to latent heat of fusion. In this range, strip thickness can be represented by the following equation:

\[ \text{Strip thickness (mm)} = -0.0022 \times (\text{Melt superheat (K))} + 1.1 \]
Figure 4.9: Strip thickness as a function of melt-roll heat transfer coefficient in two-roll melt drag thin strip casting of steel ($T_{\text{sup}} = 60$ K, $V_{\text{roll}} = 0.77$ m/s, $U_{\text{in}} = 0.05$ m/s).
Figure 4.10: Strip thickness as a function of melt superheat in two-roll melt drag thin strip casting of steel ($V_{\text{roll}} = 0.77 \text{ m/s}$, $h_{\text{melt-roll}} = 11,000 \text{W/m}^2\text{K}$, $U_{\text{in}} = 0.05 \text{ m/s}$)
4.1.1.4 Conclusion

A two-dimensional mathematical model was developed to simulate turbulent fluid flow, heat transfer and solidification for the two-roll melt drag process. There was no pilot plant study available in the open literature for this process hence the results could not be validated. Relationships between various operating parameters like roller speed, melt-roll heat transfer coefficient and melt superheat and cast strip thickness were obtained from this model. Roller speed and melt-roll heat transfer coefficient were found to have a significant effect on the thickness of cast strip, whereas melt superheat was found to have a little effect on the cast strip thickness.

4.1.2 Stress Model Predictions

The stress model can be used to evaluate conditions under which cracks may appear in the solidifying body. In this study, a cracking criterion proposed by Ramacciotti [50] has been employed in which a temperature dependent ultimate strength is used as a reference for cracking criterion:

\[ \sigma_R = C(T_{sol} - T)^{0.5} \text{ (MPa)} \]  

where, \( C = 1.2^{-0.5} \text{ (MPa)K}^{-0.5} \).

A cracking index was defined based on the principal stress as \( \text{C.I.} = (\sigma_p/\sigma_R)_{\text{max}} \).

Where, \( \sigma_p \) is the principal stress.

The temperature dependence of Young’s modulus of steel is given by the following equation [50]:

\[ Y = \frac{(T_{sol} - T)^{2.14875}}{14.752} \text{ MPa} \]  

(4.2)
A positive value of the cracking index at a point means that the material is subjected to tensile stresses at that point whereas a negative cracking index would mean that the material is subjected to compressive stresses. Cracks can appear at points with C.I. greater than one, i.e. the points, which are subjected to tensile stresses more than the ultimate strength, while cracks will not appear at the points under the compressive stresses.

Thermal stresses arise primarily due to thermal gradients in the material. The thin strip casting process results in very high stresses in the material before the kissing point and relatively lower stresses in the exiting cast strip. It is understandable that before the kissing point liquid steel comes directly into contact with chilled roller surface, which provides rapid cooling resulting in a very high thermal gradient in the material. Due to this thermal gradient, material experiences different amount of shrinkage at different locations in the melt pool and hence large thermal stresses develop in the material. For the exiting cast strip, even though the temperature drop in material is more, the fact that the strip is very thin (2mm in this case) and exposed to open atmosphere on both the surfaces results in small thermal gradient and hence comparatively low thermal stresses are generated.

The magnitude of these stresses depends on the process parameters such as casting speed and melt-roller heat transfer coefficient. To investigate the effect of process parameters on thermal stresses, cracking index is plotted at different locations of cast strip in two-roll melt drag method for various casting conditions.
For a given roll gap and melt-roll heat transfer coefficient, cracking index is plotted for three values of casting speed. Similarly, for a constant value of roll gap and casting speed, melt-roll heat transfer coefficient is varied to see its effect on the stress profile in the solidified metal.

Fig. 4.11 shows the variation in the cracking index across the cast strip at the kissing point for three casting speeds. In the finite element mesh, there are 45 nodes along the width of the strip. Point 1 represents the node at the point of contact between the upper roll and the strip, similarly point 45 represents the node at the point of contact between the lower roll and the strip. It is evident from this figure that in the vicinity of the rolls, the material is most susceptible to crack for high casting speeds. At high casting speeds, high thermal gradients exist in the vicinity of the rolls because of very less time provided for homogenization of temperature. These gradients result in high thermal stresses in the solidified shell. At low casting speeds, the stresses are low at the points near the rollers but at points away from the rolls the stresses are high. At points located away from the rolls, the material is more susceptible to crack for low casting speeds. This is because at low casting speeds, the thickness of solidified shell on the rolls is much higher than that for high casting speeds. This solidified shell reduces the rate of heat transfer from the melt pool to the rolls, which results in high thermal gradients and hence high thermal stresses in the material.
Figure 4.11: Cracking index across the cast strip at the kissing point of rollers in two-roll melt drag thin strip casting of steel (\(h_{\text{melt-roller}} = 11,000 \text{ W/m}^2\text{K}, U_{\text{in}} = 0.05 \text{ m/s}, T_{\text{sup}} = 60\text{K})


Fig. 4.12 shows the variation in the cracking index across the width of the cast strip at the kissing point for three values of melt-roll heat transfer coefficient. It can be seen from this figure that as the value of this heat transfer coefficient increases, the cracking index in the strip also increases. A higher value of melt-roll heat transfer coefficient implies a higher rate of heat transfer from the melt pool to the rolls. As the rate of heat transfer increases, the thermal stresses developing in the material also increase due to the higher cooling of the material. It can be noticed that the stresses in the material in the vicinity of upper roll are higher than the material in the vicinity of lower roll. This may be due to the fact that the thickness of the solidified shell on the rolls is more for the upper roll.
Figure 4.12: Cracking index across the strip at the kissing point for different values of melt-roll heat transfer coefficients in two-roll melt drag thin strip casting of steel ($V_{\text{roll}} = 0.30 \text{ m/s}$, $T_{\text{sup}} = 60 \text{ K}$, $U_{\text{in}} = 0.05 \text{ m/s}$)
Fig. 4.13 shows the variation in the cracking index along the middle layer of the cast strip for different casting speeds. Cracking index is plotted at 15 nodes along the middle layer of the strip. Points 1 to 3 are before the kissing point and 5 to 15 are after the kissing point. For points 1 to 3 no stresses are present in the material because the material is still in the liquid state at these points. For a casting speed of 0.45 m/s, there are no stresses in the middle layer of the cast strip because the core of the strip is still liquid. As the casting speed decreases, the cracking index increases because of increasing thermal load in the system.

Figure 4.13: Cracking index along the middle layer of the strip for different casting speeds in two-roll melt drag thin strip casting of steel ($h_{\text{melt-roll}} = 11,000 \text{ W/m}^2\text{K, } U_\text{in} = 0.05 \text{ m/s, } T_{\text{sup}} = 60 \text{ K}$)
Fig. 4.14 shows the cracking index along the middle layer of the strip for different values of the melt-roll heat transfer coefficient. As the value of this heat transfer coefficient increases, the rate of heat transfer from molten metal to the rolls increases and the cracking index in the middle of the strip also increases. For a value of 8,000 W/m² K, the rate of heat transfer is not enough for complete solidification of molten metal and hence no stresses are generated in the material. For higher values of heat transfer coefficient, the thermal load to the system is also high and hence a higher susceptibility of material to crack can be noticed.
Figure 4.14: Cracking index along the middle layer of the cast strip for different melt-roll heat transfer coefficients in two-roll melt drag thin strip casting of steel \((V_\text{roll} = 0.30 \text{ m/s}, \quad U_{\text{in}} = 0.05 \text{ m/s, } T_{\text{sup}} = 60\text{K})\)

Fig. 4.15 shows the variation in the cracking index along the surface of the cast strip in contact with the upper roll for three different values of melt-roll heat transfer coefficient. The cracking index is plotted at 16 points along the surface of the solidified shell. Points 1 to 5 are located on the nodes at metal/roll contact and 6 to 16 are along the upper surface of the cast strip. As the liquid metal comes into contact with chilled rolls very
high stresses develop in the material. These stresses gradually decrease as the solidification progresses.

It is evident from Fig. 4.15 that the susceptibility of material to fail due to thermal stresses is highest at locations before the kissing point of two rolls and is lowest at locations after the kissing point. At locations before the kissing point, these stresses are high for high melt-roll heat transfer coefficient and are low for low heat transfer coefficients.
Figure 4.15: Cracking index along the upper surface of the strip for different melt-roll heat transfer coefficient in two-roll melt drag thin strip casting of steel ($V_{\text{roll}} = 0.30$ m/s, $U_{\text{in}} = 0.05$ m/s, $T_{\text{sup}} = 60$ K).

4.1.2.1 Conclusions

The susceptibility of material to fail due to thermal stresses is maximum at locations before the kissing point of two rolls and is minimum at locations after the kissing point. Since cracking index is significantly less along the middle layer of the strip as compared
to that at the upper and lower surfaces, the upper and lower surfaces of the cast strip are
more prone to cracking than the inside of the strip. Very rapid cooling should be avoided
as it results in very high thermal stresses in the solidified material. Less cooling rates
should also be avoided as they may result in breakouts if the solidification is not over
before the strip leaves the rolls.

4.2 Horizontal Type Twin-roll Thin Strip Casting of Aluminum

4.2.1 Fluid Flow Model Predictions

The cause of centerline segregation is the interdendritic fluid motion through the partly
solid region, which arises because of the pressure gradient in the roll gap. The intensity of
segregation or the liquid motion between the dendritic arms is influenced by the local
solidification time, which is defined as the time at a given location in a casting between
initiation and completion of solidification. The length of solidification interval or the
sump depth increases as solidification time increases. Sump depth is a function of alloy
freezing range, casting speed, melt-roll heat transfer coefficient and inlet temperature of
liquid metal. In the present study Al-8111 alloy is studied. The composition of this alloy
is given in Table 4.2. Geometrical parameters and thermo-physical parameters are given
in Table 4.3.
Table 4.2: Composition of Al-8111 alloy.

<table>
<thead>
<tr>
<th>Element</th>
<th>Wt. %</th>
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<tr>
<td>Si</td>
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<tr>
<td>Fe</td>
<td>0.40-1.0</td>
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<td>Cu</td>
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<td>Mn</td>
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<td>V</td>
<td>-</td>
</tr>
<tr>
<td>Ti</td>
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<td>Parameter</td>
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<td>----------------------------------------------------</td>
<td>-------------</td>
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<tr>
<td>Roller diameter</td>
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<td>Roll material</td>
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<tr>
<td>Roll thickness</td>
<td>50 mm</td>
</tr>
<tr>
<td>Tip set back</td>
<td>85 mm</td>
</tr>
<tr>
<td>Strip thickness</td>
<td>10 mm</td>
</tr>
<tr>
<td>Diameter of metal delivery tip</td>
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</tr>
<tr>
<td>Specific heat of Al alloy</td>
<td>900 J/kgK</td>
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<tr>
<td>Latent heat of fusion of Al alloy</td>
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<tr>
<td>Thermal conductivity of Al alloy</td>
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<td>Atmosphere temperature</td>
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<td>Cooling water temperature</td>
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<td>Thermal conductivity of roll material</td>
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<tr>
<td>Specific heat of roll material</td>
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</tr>
<tr>
<td>Heat transfer coefficient for cooling fluid</td>
<td>34 KW/sq.mK</td>
</tr>
</tbody>
</table>

Table 4.3: Geometrical and thermo-physical parameters used in fluid flow model for horizontal twin-roll aluminum thin strip casting.
The Fig. 4.16 shows the typical temperature contours in the melt pool and the rollers. The temperature contours vary from 320°C to 668°C.

Figure 4.16: Typical temperature contours in the melt pool and rolls in horizontal type twin-roll strip casting of aluminum.

The liquidus and solidus temperatures of the alloy are 660°C and 640°C respectively. It is assumed that the alloy behaves as solid at temperatures below the mean of liquidus and solidus temperature, which is 650°C, and as liquid at temperatures above 650°C.
The position of the solid/liquid interface is shown in Fig. 4.17. The distance between the nozzle and the completion of solidification is defined as the sump depth.

Figure 4.17: Position of the solid-liquid interface and the sump depth in horizontal twin-roll thin strip casting of aluminum.

Fig. 4.18, 4.19, and 4.20 show the effect of casting speed, melt-roll heat transfer coefficient, and melt superheat, respectively.

Of these parameters, variations in casting speed have the largest influence on the process. Higher casting speed gives less time for solidification and hence allows greater motion of
liquid between the dendritic arms. From Fig. 4.18 it can be seen that for casting speed of 5mm/sec, the sump depth is around 18 mm and this becomes 35 mm for casting speed of 25 mm/sec. This effect of casting speed seriously limits the strip casting process to relatively short freezing range alloys and relatively low production rates.

![Graph showing sump depth as a function of roller speed](image)

**Figure 4.18**: Sump depth as a function of roller speed in horizontal twin-roll thin strip casting of aluminum ($T_{\text{sup}} = 30$ K, $h_{\text{melt-roll}} = 7,000$ W/m²K).

The melt-roller heat transfer coefficient controls the removal of heat from the aluminum strip into the steel roll. Fig. 4.19 shows that below approximately 7,000 W/m² K, changes in the heat transfer coefficient, due, for example, to non-uniformity in the roll surface,
can have significant effects on the sump depth. This could be responsible for the periodic variation in the amount of centerline segregation, which is often seen in the cast strip.

![Graph showing sump depth as a function of metal-roll heat transfer coefficient](image)

**Figure 4.19:** Sump depth as a function of metal-roll heat transfer coefficient in horizontal twin-roll thin strip casting of aluminum ($V_{\text{roll}} = 18\text{mm/s}$, $T_{\text{sup}} = 30\text{ K}$).

Fig. 4.20 shows the increase in sump depth with increases in melt superheat. The sump depth increases with superheat because the required time for molten metal to solidify increases with the superheat in the metal. But this effect is not very significant, as the
heat released from solidification is much higher than the heat due to superheat in the metal.

Figure 4.20 Sump depth as a function of melt superheat in horizontal twin-roll thin strip casting of aluminum \( V_{\text{roll}} = 18 \text{mm/s}, h_{\text{melt-roll}} = 7,000 \text{ W/m}^2\text{K} \).
It is evident that a long solidification interval and hence strong segregation can be expected for higher casting speed and low heat transfer rate and high melt superheat. This fact is supported by an experimental study on centerline segregation in Al-Mg and Al-Fe alloys by Jin et.al. [35]. They showed, as in Fig. 4.21, the centerline segregates in Al-1.7%Fe alloy increases with casting speed during twin roll strip casting process.

Figure 4.21: Centerline segregation in Al-1.7%Fe alloy for various casting speeds, as shown in [35].
4.2.2 Stress model predictions

During the course of thin strip casting, stresses develop in the solidified material primarily due to the thermal gradient which exist in the solid metal and the pressure exerted by the rolls. A typical principal stress vector plot is shown in Fig. 4.22. Fig. 4.22 together with Fig. 4.17 show the stress free liquid region in the melt pool.

Figure 4.22: Principal stress vector plot in the solidified metal and rollers in horizontal twin-roll thin strip casting of aluminum.
Fig. 4.23 shows the maximum principal stress across the cast strip, for different casting speeds, at the point where strip leaves the rolls. In the finite element mesh, there are 45 nodes along the width of the strip. Point 1 represents the node at the point of contact between the upper roll and the strip, similarly point 45 represents the node at the point of contact between the lower roll and the strip. The surface of the strip experiences maximum stress due to the roll force and chilling action of rolls. It can be seen from Fig. 4.23 that as the casting speed increases, the magnitude of the stresses in the solidified material also increases. This is due to the fact that at higher casting speeds, contact time of metal with rolls is less hence it gets less time for homogenization of temperature. Consequently, high temperature gradients exist in the material when the casting speed is high, resulting in higher stresses in the material.
Figure 4.23: Maximum principal stress across the cast strip at the kissing point in horizontal twin-roll thin strip casting of aluminum.

4.2.3 Conclusions

The extent of centerline segregation and thermally induced stresses increases with process parameters like casting speed and melt superheat and decreases for higher heat transfer rate across slab/roll interface. Hence a low casting speed would be desirable for smooth casting operation but would reduce the productivity. High productivity can be achieved by increasing the casting speed and offsetting the effect of higher casting speed by insuring a higher rate of heat withdrawal from the melt pool through the rollers.
CHAPTER 5

SUMMARY AND FUTURE WORK

Two-dimensional finite element based mathematical models were developed to simulate fluid flow, heat transfer, solidification and stress field in thin strip casting processes for steel and aluminum. A finite element mesh was generated using commercial software, FIDAP, and fluid flow equations were solved for temperature and velocity field. This mesh and the temperature profile were exported to another commercial software, ANSYS. Mechanical equilibrium equations were solved, using ANSYS, to obtain the stress field in the solidifying body. A visco-plastic constitutive equation was used to describe the material behavior at elevated temperatures. The principal stresses in the solidifying body were used to obtain a cracking index parameter, which denotes the susceptibility of the material to crack due to the stresses. The model was used to investigate the effect of process parameters on the cast strip thickness, fluid flow profile, and stress profile.
5.1 Conclusions

5.1.1 Two-roll Melt Drag Thin Strip Casting of Steel

Following conclusions can be drawn from the present investigation of this process:

1. For two-roll melt drag thin strip casting of steel, roller speed, metal-roll heat transfer coefficient, and melt superheat were identified as parameters affecting the process.
2. Roller speed and metal-roll heat transfer coefficient were found to be the main parameters affecting the cast strip thickness while melt superheat showed little effect.
3. Strip thickness decreases with increase in roller speed and melt superheat while it increases with increase in metal-roll heat transfer coefficient.
4. Position of inlet was changed to investigate its effect on the turbulent flow profile.
5. It was found that when the inlet is close to upper surface of the melt pool it gives a more symmetric turbulent flow.
6. Stress profiles were obtained in solidifying metal as well as the rolls.
7. Principal stresses in the solidifying body and a temperature dependent ultimate strength was used to define a parameter called cracking index which denotes the susceptibility of the material to crack.
8. Cracking index was plotted at various locations in the solidifying body for different casting conditions to investigate the effect of process parameters on the susceptibility of the material to crack due to thermally induced stresses.

5.1.2 Horizontal Twin-roll Thin Strip Casting of Aluminum

Following conclusions can be drawn from the present investigation of this process:

1. For this process, roller speed, metal-roll heat transfer coefficient and melt superheat was identified as parameters, which affect the process.
2. The fluid flow model was used to see the effect of these parameters on the length of the solidification interval, or the sump depth, which affects the extent of centerline segregation in the cast product.

3. It was found that the sump depth increases with roller speed and melt superheat and decreases with metal-roll heat transfer coefficient.

4. Stress profiles were generated in the solidifying alloy as well as the rollers.

5. Maximum principal stress across the cast strip at the kissing point of rollers was plotted for different casting speeds and it was found that the magnitude of these stresses increase with casting speed.

5.2 Future Work

1. The mathematical model can be extended to three-dimensions to study the side and dam effects.

2. Stress model can be used to predict the roll deformation and roll life.

3. Effects of phase transformations on the thermo-mechanical properties can be incorporated into the model.

4. Multicomponent flows for the segregation analysis can be included.
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4) Steel Times, July (1993), 299.


75


77