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Improvement of surface quality of continuously cast steel control of cast structure and straightening temperature

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University of Wollongong

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IMPROVEMENT OF SURFACE QUALITY OF CONTINUOUSLY CAST STEEL BY CONTROL OF CAST STRUCTURE AND STRAIGHTENING TEMPERATURE

A thesis submitted in fulfilment of the requirement for

the award of the degree of

HONOURS MASTER OF ENGINEERING

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SYNOPSIS

In recent years, most world wide mass produced steels have been made by continuous casting. Despite significant advances in continuous casting technology, including advances made at PT. Krakatau Steel in Indonesia, transverse cracking remains one of the most common defects encountered during normal production of steel slabs containing 0.12-0.18% carbon.

The presence of transverse cracks has undesirable influences on the product because they can degrade quality as well as increasing the risk of failure during applications of the material in finished products. The problem of transverse cracking can be eliminated by removing the defect by scarfing the corner of the slab, but this process involves a degree material loss and associated expenditure. Therefore, decrease in the incidence of transverse cracks is demanded to satisfy the customers as well as to obtain a significant yield increment and to improve process efficiency. Consequently, examination of these defects and determination of means of controlling the incidence of them is a matter of considerable importance.

This thesis presents the results of a program of work designed to identify means by which the incidence of transverse cracking of continuously cast steel slabs can be reduced in a production machine such as that operating at PT. Krakatau Steel.

In Chapters 2 and 3, the process of continuous casting of steel is examined and the way in which solidification of the continuously cast strand takes place is described.

The effects of operational parameters such as steel composition, secondary cooling water, tundish temperature and straightening temperature, which are the principal effects in causing cracking in the cast material, are set out in Chapter 4. Particular attention is directed to transverse cracking and the operational variables causing this defect are identified.
In Chapters 6 and 7 the experimental procedure for determining the influence on cracking of secondary water rate and titanium in the cast material are described and the results of the experimental work are set out. A mathematical model for estimating the strand surface temperature is presented in Chapter 5 and used in Chapter 7 to assist in analysing the experimental results.

It is concluded that to reduce the incidence of transverse cracking, austenitic grain growth must be suppressed by titanium addition up to 0.017% and the precipitation of allotriomorphs of ferrite must be avoided by lowering the water rate from 0.756 l/kg to 0.736 l/kg.
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Chapter 1

INTRODUCTION

At the present time, steel is mainly produced by a sequence of several processing steps comprising iron and steelmaking, rolling, drawing and other primary forming operations followed, if necessary, by secondary forming.

Iron is the principal raw material for the steelmaking process, and is obtained by reducing iron ore to molten iron in the blast furnace or by direct reduction in the Hyl reactors and/or Midrex reactors. In these processes, the iron dissolves from 2.0 to 4.0% carbon, but as commercial steels contain considerably less than 1.0% carbon, the excess carbon must be removed during the conversion into steel. The excess carbon is removed by controlled oxidation of mixtures of molten iron and steel scrap in steelmaking furnaces including the basic oxygen furnace and the electric arc furnace to produce carbon steels with the required carbon content. Various elements such as manganese, silicon, titanium and vanadium may be added singly or in combination to molten steel in the treatment ladle to produce alloyed steel. When the molten steel has attained the required chemical composition, it is teemed into the moulds where it solidifies to form ingots. The ingots, after being removed from the moulds, are reheated and rolled into shapes known as blooms, slabs, or billets and referred to as semifinished steel.
However, since the early 1960s, the production of semifinished steels by ingot casting routes has decreased with increasing development of continuous casting in most steel industry worldwide. Continuous casting offers excellent cost efficiency due to a substantial improvement in yield of about 15% compared with ingot casting. The improvement is a consequence of elimination of processing stages such as blooming and slabbing which result in a certain degree of material loss. In continuous casting, feed stocks are produced directly in the form of blooms, slabs, or billets and, almost all steel plants, including PT. Krakatau Steel in Cilegon, Indonesia, in the world today use this process for commercial production of most steels.

PT. Krakatau Steel, a steel company owned by the Indonesian Government, is one of the largest steel producers in South East Asia with a crude steel production capacity of 2,500,000 tonne per year. At present, the steelmaking plant consists of the Billet Steel Plant (BSP) commissioned in 1978, Slab Steel Plant No.1 (SSP 1) commissioned in 1983 and the new and modern Slab Steel Plant No.2 (SSP2) commissioned at the end of 1994. The BSP is equipped with four 65 tonne arc furnaces and two continuous casters producing 600,000 tonne of mainly commercial steel grades for wire rods. The SSP 1, has four 125 tonne arc furnaces and two bow-type continuous casters with one straightening point, producing 1,200,000 tonne of mainly low and medium carbon steel grades for drawing sheets, high grade pipes and structural purposes. The SSP2, has two 125 tonne arc furnaces and one continuous caster with several straightening points, producing 700,000 tonne of mainly ultra-low and low
carbon steel grades for deep and normal drawing sheets for automotive parts, cans and other special purposes.

Although most steel products made by PT. Krakatau Steel generally satisfy many applications, improvements in quality as well as diversification of products have been made during the last few years. The medium carbon steel currently produced in SSP1 is one typical example of the diversified steel products from PT Krakatau Steel. It is produced to anticipate the increasing demand for high grade pipes for oil and gas transmission and also for structural proposes.

A problem encountered in the production of the medium carbon steel in PT. Krakatau Steel is the formation of transverse (and other) cracks on the surface of continuously cast slabs, particularly for steels containing 0.12 to 0.18% C. In practice, the cracks are currently removed by substantial scarfing to avoid the formation of defects on the hot rolled steel as well as on end products originating from it. Although the surface transverse cracks can be removed by this means, the practice involves a degree of material loss and other expenditure such as labor and consumable costs.

Recent developments of rolling without scarfing and of hot charge rolling of continuously cast slab are being practiced at various steel makers to save energy and improve yield. To operate this latter process therefore, it is important to avoid surface cracking and the consequential defects which would occur in the absence of scarfing.
In the present study, factors affecting the surface quality of continuously cast slab and the preventive measures which can be taken to avoid surface defects are examined. Investigations into the nature of, and mechanism of formation of, transverse cracks are described, as are the methods used to reduce the incidence of this cracking. Steel composition, secondary cooling water distribution, and slab surface temperature were found to be major factors influencing the cracking.

To appreciate the origins of the several kinds of surface defects, it is necessary to examine the process of continuous casting and to consider the ways in which solidification and subsequent cooling occurs in a caster.
Chapter 2

CONTINUOUS CASTING OF STEEL

Continuous casting is a relatively recent development in the steel industry and has had a dramatic impact on steel production throughout the world. Compared with conventional ingot casting, continuous casting has had greatest effect on improving the efficiency of production. Additionally, it has the advantage that the products of continuous casting approximate more closely to the final product dimensions than the products of ingot casting with corresponding reduction in the number of forming steps required. The conventional ingot casting route requires soaking pits for ingot heating, and primary mills for rolling the ingots into blooms, slabs, or billets. In continuous casting, all these facilities are eliminated and the feed stock is produced directly in the form of blooms, slabs, or billets. Such a process not only leads to a considerable improvement in yield, but also reduction in energy consumption. The process yield is more than 95% compared with approximately 80% for the production of semi finished products by rolling from a cast ingot[1,2].

Today, continuous steel casting is performed with a water-cooled open ended copper mould which is separated from the unit supplying the molten steel. At the commencement of the casting operation, the mould is closed at the bottom by a so-called dummy bar, then filled with the molten steel after which the dummy bar is continuously withdrawn in the casting direction. At the same time, the mould is oscillated in sinusoidal
movement to accommodate movement of the strand and to prevent sticking of the strand to the mould wall. The steel shell which forms in the mould contains a core of liquid steel which gradually solidifies as the strand moves through the machine. The main components[1,2] of the machine are shown schematically in Fig.2.1.

![Diagram of a continuous casting machine](image)

Fig. 2.1 Schematic diagram of a continuous casting machine[2]

2.1. Continuous Casting Machine Components and Functions

There are three essential requirements of the continuous casting machine: one is to guide the solidifying metal, the second is to remove heat from the steel at the required rate, and the other is to deliver a solidified strand at the strand device. A continuous casting machine (Fig.2.1) consists of a liquid metal reservoir and distribution system (a tundish), liquid metal
shrouding, a mould with water cooling system, secondary cooling zones in association with a strand containment section, bending rolls, a straightener and cutting equipment[2,3].

2.11 **The tundish** is essentially a rectangular box, but some tundishes are T shaped or L shaped made from steel plate and lined with refractory bricks, and a nozzle is located in the bottom of the tundish. The tundish is designed to provide a number of important functions including:

(a) control of metal flow patterns to enhance the stability of the metal stream entering the casting mould,

(b) provision of a metal reservoir to facilitate casting a sequence of heats, and

(c) a means to facilitate the separation of inclusions and slag entering the tundish from the ladle.

2.12 **Liquid metal shrouding** is employed to avoid the metal stream absorbing oxygen from the air, and thereby forming deleterious inclusions in the liquid steel. Shrouds are normally placed between the ladle and the tundish as well as between the tundish and the mould. A shroud is normally tubular in form and manufactured from alumina/graphite refractory. In addition, in most modern continuous casting plant, argon is introduced into the refractory tube to avoid aspiration of air through pores and joints due to the venturi effect of the moving metal stream.
2.13 **The mould** is constructed from a copper-based alloy as an open ended box structure with an inner lining which serves as the interface with the steel being cast and which determines the shape of the cast section. The primary function of the mould system is to contain and start solidification of the liquid steel. Control of heat transfer in the mould is accomplished by a forced convection cooling water system which is designed to accommodate the high heat transfer rates that result from the solidification process. In addition, mould powder is introduced into the mould as the lubricant to minimize the frictional force between the mould surface and the solidified cast steel shell, and it is an absorber of oxide inclusions and acts as a protective layer preventing reoxidation of the liquid metal.

2.14 **Secondary cooling** is designed to produce a final cast section which has the required shape, and surface quality. To accomplish these results, the solidification section leaving the mould is cooled in a series of spray zones and is contained and withdrawn by a series of roll assemblies until the solidified cast section reaches the cut-off machine. The secondary cooling system is normally divided into a series of zones to control the cooling rate as the strand progresses through the machine.

2.15 **Strand containment** is provided by a series of retaining rolls which extend across the two opposite faces of the cast section in a horizontal direction. The basic functions of the mechanical strand containment and withdrawal equipment, which form an integral part of the secondary cooling system, are:
(a) to support and guide the strand from the mould exit to the cut-off operation, and
(b) to drive the strand through the caster at a controlled speed.
In these two functions, the final objective is to minimize the mechanical stress and consequential strain imposed upon the strand.

2.16 The straightener is installed following the completion of unbending which, as the name implies, straightens the strand and completes the transition from the vertical to horizontal orientation. During straightening, the strand is "unbent" to reverse the tension and compression forces in the horizontal faces of the strand. The series of rolls which guide the strand through a prescribed arc from the vertical to the horizontal plane must be strong enough to withstand the bending or straightening reaction forces. During movement from the mould to the torch cutter, the strand is subjected to a number of loads which lead to defects in the product if the resultant stresses and the associated strains become too high to be sustained by the cast cross section and steel grade. The loading phenomena which occur are:
(a) tensile stresses resulting from the withdrawal forces,
(b) bending of the strand shell from the vertical orientation into a curve, and
(c) straightening of the strand shell or the solidified strand into the horizontal orientation.
The stresses associated with these mechanical phenomena may be exacerbated by additional thermal stresses [3].
2.2. Strand Deformation during Bending and Straightening

Strand support involves restraint of the solidifying steel shape which consists of a solid steel shell and a liquid steel core. During bending, the inner radius of the solid shell is placed in compression and the outer radius in tension. On the other hand, during straightening the inner radius is placed in tension and the outer radius is placed in compression[4]. Excessive strain may result in metal failure and strand defects (cracks). The strain $\varepsilon$, which arises during straightening from the curvature radius to straight in the horizontal direction is a function of the caster radius $R_c$, and strand thickness $t$, and can be derived from consideration of Fig.2.2.

![Diagram showing strain due to straightening in continuous casting process](image)

Fig. 2.2 Diagram showing strain due to straightening in continuous casting process[4]
From Fig. 2, the strain $\varepsilon_i$, in the interior of the slab, can be formulated:

$$
\varepsilon_i = \frac{l_c - l_i}{l_c} \quad (2.1)
$$

where, $l_c \approx R_c \cdot \tan \beta$ as $\beta \rightarrow 0$

$l_i \approx (R_c - d_i) \cdot \tan \beta$ as $\beta \rightarrow 0$

when $\beta \rightarrow 0$,

$$
\varepsilon_i = \frac{R_c \cdot \tan \beta - (R_c - d_i) \cdot \tan \beta}{R_c \cdot \tan \beta} \quad (2.2)
$$

i.e. $\varepsilon_i = \frac{d_i}{R_c} \quad (2.3)$

The strain due to straightening will be minimum for $d_i \approx 0$, in the center of slab and will be maximum for $d_i \approx t/2$, on the surface. Thus, the strain on the outer fibre $\varepsilon_s$, can be described as[4]:

$$
\varepsilon_s = \frac{t}{2R_c} \quad (2.4)
$$
In SSP1-PT. Krakatau Steel, for which the caster radius \((R_c)\) is 9700 mm and the strand thickness is 200 mm \((f)\), the strain on the outer fibre during straightening is:

\[
\varepsilon = \frac{200}{2 \times 9700} = 1.03\% \quad (2.5)
\]

Generally, strain values up to 1.5% on the outer fibre are tolerated, because the solidified steel in the outer fibre has sufficient strength to accommodate the strain. On the other hand, for the internal fibre, the value must not exceed 0.5 %, because the solidification front is very sensitive to cracking due to the lack of ductility when tensile strain arises at the solid/liquid interface [4].

To minimize the development of cast strand defects, particularly surface defects due to excessive strain, and which may be exacerbated by additional thermal stresses, it is necessary to control both the temperature levels and thermal gradients in the strand over the time of the casting process. In practice, this aim can be achieved by controlling the cooling of the solidifying strand. Solidification and the cooling process that occur in the strand are described in Chapter 3.
Chapter 3

SOLIDIFICATION AND COOLING PROCESS IN CONTINUOUS CASTING

The continuous casting process is essentially a continuous solidification process, with the molten metal travelling through a water-cooled mould and a series of rollers and cooling sprays. Steel is poured into the water cooled copper mould having a predetermined shape and solidifies under forced cooling conditions in the mould and progressively in the spray cooling zone. During solidification, 15% of the heat is extracted in the mould, 30 to 40% in the water spraying zone, and 10% in other parts of the machine. This leaves 35 to 45% of the total heat still in the product at exit and then sensible heat may be lost to the atmosphere[5].

The phase transformations involved during the solidification process can be determined from consideration of the Fe-C constitutional diagram (see Fig. 3.1). For alloys containing 0.12 to 0.18% carbon, cooling from an initial temperature higher than the liquidus to a temperature slightly below it (A), results in nucleation of primary α-dendrites in the liquid until recalescence occurs due to the heat released from the growing nuclei. During recalescence, nucleation ceases and existing nuclei grow rapidly into dendritic grains which impinge on each other at the end of recalescence. Growth of α-dendritic grains is then succeeded by coarsening of the dendritic arms until solidification is complete (B). When the temperature decreases further, austenite precipitates from the delta phase until all α-
phase is transformed. The austenite phase cools through the austenite zone until the temperature reaches $\text{Ar}_3$ at which ferrite begins to precipitate (C). Finally, at or below $\text{Ar}_3$, the remaining austenite transforms to pearlite (D) by nucleation at interfaces and coupled growth of ferrite and cementite phases[6].

Fig. 3.1 Iron-carbon constitutional diagram[6].

During solidification, transformation of the liquid involves the extraction of two kinds of the heat i.e. sensible-heat and latent-heat. Sensible-heat, is the heat component which results from the difference in the actual liquid metal temperature and the transformation temperatures. Latent-heat, is the heat which is evolved during solidification and phase changes in the solid state during cooling as the cast strand leaves the caster. Dissipation of these heat components takes place by radiation, conduction and
convection in the three cooling zone (Fig. 3.2), which are the primary cooling zone (in the mould), the secondary cooling zone (spray cooling including the roll cooling system) and the zone in which heat is exclusively transferred by radiation to the environment[7].

![Diagram of heat transfer zones in continuous casting](image)

**Fig. 3.2** Schematic representation of the three heat transfer zones in continuous casting[7].

### 3.1. Cooling Process in the Mould

Solidification of liquid steel in the mould begins by the formation of the strand shell in contact with the walls of the mould just below the liquid meniscus (Fig. 3.3). This phase of solidification continues progressively and is characterized by the presence of oscillation marks that form periodically at the meniscus due to mould reciprocation. The movement of the mould prevents the newly formed strand shell from sticking to the mould wall[8,9].
The formation of oscillation marks has been studied [10] in relation to the solidification in the meniscus region. The study suggested that a partially solidified meniscus could play a significant role in the formation of oscillation marks during slab casting. There is a close relationship between the distance between the oscillation marks \( d \), the oscillation frequency \( f \), and the casting speed \( V_c \). This relationship can be expressed [10]:

\[
\frac{V_c}{d} = \frac{1}{f}
\]

The formation of oscillation marks is largely the result of the interaction between the mould flux and the mould movement, and has been related previously to the stroke \( S \), frequency of mould oscillation, and more generally to the negative-strip time \( t_N \), which is the time interval in each
cycle of mould oscillation in which the downward velocity of the mould \( (V_m) \) exceeds the withdrawal rate of the strand. The negative-strip time can be expressed as [10]:

\[
t_N = \frac{1}{\pi f} \arccos \left( \frac{V_c}{\pi f S} \right) \tag{3.2}
\]

There are two types of oscillation marks based on the presence or absence of a small hook in the sub surface structure adjacent to each oscillation mark. Figure 3.3 shows a schematic representation of the formation of the two types of oscillation marks i.e., with and without adjacent subsurface hooks.

As shown in Fig. 3.3, during the negative-strip time (stages 1-3), the meniscus is pushed by the positive pressure, generated in the mould flux, away from the mould wall. Then in the ensuing positive period (stages 4-7), the meniscus is drawn back toward the mould wall. It is most likely that the meniscus is drawn back uniformly, however, because the upper part of the meniscus skin is farthest from the cooling influence of the mould wall, it therefore should be the hottest and weakest. As a result, the upper part of the skin is expected to be drawn back more by the negative flux pressure and inertial force of the surging liquid steel. The difference
between the two types of oscillation marks then arises because of differences in the mechanical strength of the meniscus skin. In the case of oscillation marks with subsurface hooks, the skin is relatively strong, owing to the greater thickness (low superheat or minimal bath movement). Thus the top of the skin resists being bent back fully toward the mould wall, and liquid steel overflows it (stage 4, Fig. 3.3a) to form a subsurface hook. On the other hand, with oscillation marks having no subsurface hooks, the skin is weak and behaves more like a liquid. Thus, at beginning of positive strip, the top of the skin is easily pulled back with the liquid toward the mould so that overflow does not occur (stage 4, Fig. 3.3b). In addition, formation of pronounced hooks may cause weakening of the subsurface cast structure, due to the presence of non-metallic inclusions and/or segregated structures in the vicinity of the hook region.

During solidification in the mould, heat transfer from the strand shell surface to the mould is probably the least understood and most complex of the heat transfer steps [11]. The salient feature of this heat transfer is the liquid-solid shrinkage and the resulting tendency for an air gap to form between the strand shell and mould surface. As the air gap is formed, heat transfer changes from mainly conduction to radiation across the gap with a resulting decrease in heat flux. This effect can be reduced by designing
the mould space with a taper of the narrow faces of the mould to follow
the shrinkage in the cross section, while the broad faces are set parallel to
each other. In addition, the complexity of heat transfer at the mould inner
surface may be exacerbated by introducing mould powder into the mould
and also by the formation of oscillation marks at the strand surface.

Although the mechanism of heat transfer in the mould is complex, for
practical purposes, the average heat transfer from the mould can be
calculated from a heat balance applied to the cooling water which
accommodates the high heat transfer rates resulting from the solidification
process. The average heat transfer $q_m$ (heat flux), due to strand shell
formation and cooling process in the mould, can be described as[11]:

$$q_m = \frac{M_w C_w}{L \cdot F} (T_o - T_i) \quad (3.3)$$

thus,

$$q_m = \frac{\rho_w Q_w C_w}{L \cdot F} (T_o - T_i) \quad (3.4)$$
where, \( q_m \): heat flux of mould cooling (W/m\(^2\))
\( M_w \): mass-rate of water (kg/sec.)
\( \rho_w \): density of water (kg/m\(^3\))
\( Q_w \): flow rate of water (m\(^3\)/sec.)
\( C_w \): specific heat of water (J/kg/K)
\( L \): mould length (m)
\( F \): mould width (m)
\( T_i, T_o \): water temperature in, out (K)

After the strand shell is formed and primary cooling occurs in the mould, additional heat is removed in the secondary cooling zones below the mould where solidification is completed.

3.2. Secondary Cooling

Secondary cooling of the strand has a decisive influence on the degree to which the strand surface and center remain crack free[11]. The purpose of secondary cooling is to produce and maintain a strand shell having sufficient strength to withstand bulging, bending and straightening below the mould, and is provided in cooling zones consisting of a series of water sprays and water cooled support roll units (see Fig. 3.4). The main heat transfer functions of the water system in the secondary cooling zone are to provide:

(a) the required amount of water to complete the solidification process,
(b) the capability to regulate the thermal conditions of the strand from below the mould to the cut-off operation, and
(c) cooling of the containment rolls.
It is necessary to control both the temperature levels and thermal gradients in the strand to avoid the occurrence of surface and internal defects such as improper shape and cracks[11]. At high temperature, the strength properties of the steel shell are critical to the ability of the shell to withstand the external and internal forces that are imposed by the casting operation. In particular, the ductility of steel close to the solidus temperature and to the temperature at which the $\gamma/\alpha$ transformation occurs, is low and at these temperatures, the shell is therefore susceptible to crack formation. It is also important to control temperature gradients because consequential thermal strains can exceed the strength of the steel resulting in cracks.
Excessive thermal strains may result from changes in the heat extraction rate by over cooling. Thus, in the design of a secondary cooling system, the thermal conditions along the strand must be established to satisfy the required product integrity and quality. For these purposes, the surface temperatures along the strand are generally specified to be above 900 °C, particularly for avoiding the formation of surface defects such as transverse cracks which can occur due to lack of ductility in relation to the γ/α transformation. Based on this information[11], the cooling rate along the strand may be determined from heat transfer equations. Important parameters in these calculations include the convection heat transfer coefficient of the water spray and the water flux which is the amount of water provided per unit area of surface contact per unit time. In addition, changes to the water flux can be made to compensate for changes in casting conditions, such as casting speed, strand surface temperature and steel grade.

Many investigations[12,13] have been concerned with determination of the heat transfer coefficients under virtual production conditions. The results of these studies show that the quantity of heat extracted is largely dependent on the water flux $V_s$ (l/m²/sec), of spray water. The values of heat extracted $q_s$ (W/m²), by spray water cooling can be described by the empirical relationship between $q_s$ and $V_s$:

$$q_s = 160.000 \cdot (V_s)^{0.75}$$ (3.5)
Recently, the spray intensity of secondary cooling water has been considerably reduced in most continuous casting operations to obtain the required quality of cast steel slab, particularly surface quality[14]. As a result, there is an increase in the radiation component of the total heat extracted due to effective replacement of a portion of the spray cooling with the radiant cooling.

The quantity of heat extracted due to radiant cooling \( q_r \) (W/m\(^2\)), is largely dependent on the surface temperature \( T_s \) (K), of the strand, and may be expressed[15]:

\[
q_r = \varepsilon\sigma (T_s^4 - T_e^4)
\]  \hspace{1cm} (3.6)

where, \( \varepsilon \) : emissivity  
\( \sigma \) : Stefan-Boltzmann constant \((5.67\times10^{-8} \text{ W/m}^2\text{K}^4)\)  
\( T_e \) : surrounding temperature (K)

In addition, it is necessary to consider the heat extracted by a series of water cooled support rolls to determine the strand surface temperature. Practically, the heat transfer coefficient of the contact between the strand surface and the rolls in this case can be expected to be in the region 300 - 1200 W/m\(^2\).K[16].
3.3. Processes at the Solidification Front

During continuous casting, solidification commences with nucleation of dendrites of δ-phase. Crystals become attached to the gradually forming solidification front and have compositions dictated by the solidus line of the phase diagram (see Fig. 3.1). Owing to the increase in concentration of alloying elements such as carbon, manganese and silicon in the vicinity of the solidification front, it is here that the liquid temperature is lowest. However, the temperature in the liquid phase is higher than the temperature of the solid phase so that heat will flow from the liquid phase to the solidification front and if the temperature of the molten steel decreases to below the liquidus temperature, constitutional supercooling occurs and solidification commences within the liquid pool. Once the superheat has been removed, usually in the region 1 to 3 m below the meniscus, a two phase layer of liquid plus δ-phase crystals, which may be up to 5 mm thick, forms on the solidification front[17].

In the region where superheat is still present, formation of the fine-grained edge zone is followed preferentially by dendritic crystallization. After removal of the superheat from the liquid phase, equiaxed crystals form in the vicinity of the solidification front, and as they have higher density than the remaining liquid metal, they sink and sedimentate. Convection
currents in the liquid core of the strand exert some influence on this process. The rate of solidification and the temperature gradient in and at the solidification front are the main factors affecting dendritic crystallization but the factors which cause change in the solidification morphology from dendritic to equiaxed crystallization have yet to be accurately defined. Nevertheless, low temperatures in the tundish (low level of superheat) can be expected to result in larger regions of equiaxed crystallization [17,18].

The thermal behavior and microstructural evolution in continuously cast steel have been investigated in numerous studies. At the high temperature at which continuous casting is carried out, defects such as cracks associated with phase transformation and microstructural changes tend to form during the process. Normally, these defects are not acceptable in the final products and therefore, to prevent such defect formation, it is absolutely necessary to understand the mechanism of genesis of these defects. The defects that commonly occur in continuous casting and relevant preventive measures are described in Chapter 4.
Chapter 4
DEFECTS IN CONTINUOUSLY CAST STEEL SLAB

Continuous casting-direct rolling techniques have been adopted increasingly in recent years to improve both efficiency and yield productivity in modern integrated steel production plant. However, to operate these processes effectively, it is necessary to prevent the cracking that often occurs on the slab surfaces.

Surface cracking on continuously cast slab can occur in various forms of which longitudinal cracks, star cracks and transverse cracks are significantly important[19]. All these surface defects have undesirable influences on the product because they can degrade quality as well as increase the risk of failure during application of the material in finished products. Figure 4.1 shows the appearance of surface defects in cast slab.

Fig. 4.1 Schematic representation of surface cracks in cast slab
Although almost all surface defects can be removed by scarfing the slab, this practice may in fact increase defect probability in finished products, particularly if the scarfing shells are not removed completely and still remain in the slab surface. Additionally, scarfing involves a degree of material loss and other expenditure including labor costs. Therefore, preventing surface defects is most important to assure the consistency of steel product quality as well as to achieve efficiency of the process.

4.1. Longitudinal Surface Cracks

A longitudinal surface crack is a crack which occurs, on the surface of slab, parallel to the casting direction and usually originates in the mould[19,20]. This defect occurs as a result of weakening of the grain boundaries during formation of the strand shell. It has been suggested that fluctuation of temperature due to non-uniformity of heat transmission below the mould meniscus probably contributes to formation of the defect. This phenomenon might occur if any defect such as scratch or a non-uniformity inflow of mould powder occurs in the mould thereby resulting in diminished contact between the strand shell and the mould. As the heat transmission resistance between the strand surface and the inner face of the mould decreases in the small area just below the meniscus, heat conducted from the interior liquid metal will reheat the solidified shell. At the same time,
thermal shrinkage and ferrostatic pressure applied to this area results in
crosectional stresses in the casting direction leading to weakening of the
grain boundaries.

To prevent longitudinal surface cracking, it is necessary to control the
uniformity of the heat transmission[20,21] by maintaining the mould surface
regularity as well as controlling the uniformity of mould powder inflow.
Control of these factors limits the incidence of longitudinal cracking to a
few percent only of slabs.

4.2. Star Cracks

Star cracks are probably caused by the phenomenon of hot shortness in
which, for example, a low melting phase penetrates the casting through the grain boundaries[23]. In view of the high strand shell temperature, the
penetrating phase remains liquid and causes weakening of the inter-
granular bond and in the continuous casting context, hot shortness is usually attributed to copper released through abrasion of the mould plates.
In most cases, segregated copper has been found in and near the star
crack in the cast slab confirming the proposal that embrittlement of the cast slab at a high temperature results from action of copper derived from the mould copper plate.
In early practice, the inner face of the mould was chromium plated to prevent release of copper by mould abrasion. The plating, however, detached from the lower part of mould surface after long-time operation, resulting in occurrence of star cracking\cite{23,24,25}. Therefore, regular control of coating thickness over the mould surface could be effective in preventing star cracks. Generally, as with longitudinal cracks, these defects occur in only a few percent of continuously cast steel slabs.

Although these and other surface defects do occur in continuously cast slab, transverse cracks are probably the most common and therefore have attracted numerous studies and still continue to be studied.

**4.3. Transverse Cracks**

At PT. Krakatau Steel, transverse cracks occur in approximately 68\% of the total production of continuous cast steel slabs, mainly in 0.12-0.18 \%C steels. Insignificant cracking occurs in steel containing less than 0.09 \%C. The incidence of these defects is most easily observed after the oxidized surface layer on the narrow side of the slabs is removed by scarfing. To obtain the better understanding of effective preventive measures for transverse crack prevention it is necessary to understand the mechanism of genesis of the defect.
4.3.1. Appearance of Transverse Cracks

In continuous slab casting, transverse surface cracks usually occur on the corners of the slabs (see Fig.4.1), perpendicular to the casting direction, and usually along the valley of the oscillation marks. In general, these cracks penetrate from a few mm to about 10 mm into the surface, and occur only on the top face of cast slab, on which a tensile stress is generated during straightening [26,27]. In addition, microscopical study indicates that the cracks are mostly intergranular. These observations suggest that the occurrence of a transverse crack is a consequence of interaction between the straightening strain and the proof stress of the slab material. Based on this consideration, models have been developed to explain the mechanism of transverse cracking in relation to the effects of grain boundary embrittlement.

4.3.2. Mechanism of Transverse Cracking

In this section, the mechanism of genesis and means of prevention of transverse cracking is described using models which emphasise the factors that significantly affect the formation of cracks and the relationship with limitations of practical processes.
By combining plant experience and fundamental knowledge, as discussed by Brimacombe and Sorimachi[28], the models of cracking are based on relationships between cracking, the formation of the coarse austenite grains during solidification, and subsequent precipitation of carbides and/or nitrides at the austenitic grain boundaries.

4.3.2.1. Effect of Coarse Austenitic Grain Structure

The surface cracking susceptibility of low alloy steel slabs during continuous casting processes depends largely on the carbon content, and is a maximum in the range 0.1-0.15% (referred to as a medium range). This phenomenon has been studied[29] in relation to the peritectic reaction during solidification in the mould. In this study it was confirmed that the primary solidified shell for 0.1-0.15% C becomes uneven in the mould due to shrinkage associated with transformation of δ-ferrite to austenite. The shrinkage results in a marked reduction of heat extraction owing to lack of close contact between the metal and the mould. Consequently, the temperature gradient between the liquid steel and the thinner parts of the unevenly solidified shell is quite small, the thin metal is close to the solidus temperature, and it has low strength and/or ductility. Therefore, it will be susceptible to cracking. In addition, a local delay of cooling in the thinner parts of the unevenly solidified shell result in larger grains of austenite in the
surface region of the cast strand. This phenomenon is also to be noted as the second factor which causes high transverse cracking susceptibility.

The combined effects of uneven solidification and metallographic changes with carbon content are shown in Fig.4.2

![Figure 4.2](image_url)

**Fig. 4.2** Schematic representation of the difference of $\gamma$ grain size between (a) low carbon steels and (b) the medium carbon steels, owing to uneven solidification in the mould [29].

Figure 4.2 shows that if the contact between the solidified shell and the mould is poor due to thermal shrinkage, as occurs in medium carbon steels, a coarse grain structure will be produced in the thin solidified shell.
On the other hand, if the contact is good, as is known to occur for low carbon steels, the grain structure will be fine.

Although the carbon content corresponding with the maximum austenite grain size agrees well with the carbon content for maximum surface cracking susceptibility (Fig. 4.3), it is considerably less than the 0.18 %C peritectic point in the Fe-C constitutional system. This can be explained in terms of the effects of alloying elements such as Mn, Si, Ni and etc. which reduce the carbon content at the peritectic point. The effect of these elements can be represented as the carbon equivalent (CE), and described using the empirical relationship [29]:

\[ CE (\%) = \%C + \%Mn/6 + \%Si/24 + \%Ni/40 + \%Cr + \%Mo/4 + \%V/14 \]  

(4.1)

The carbon dependence of austenitic grain size also corresponds to that for ductility loss and is in good agreement with the variation of austenite formation temperature in the Fe-C constitutional diagram, as shown in Fig. 4.3.
Fig. 4.3 Diagram showing the dependence on carbon content of (a) surface cracking frequency, (b) ductility (RA) and γ grain size ($D_\gamma$) of as cast steel and (c) their relation to the peritectic transformation [28].

4.3.2.2. Effect of $\gamma/\alpha$ Transformation

Practically, transverse surface cracking of continuously cast slabs occurs mainly at low strain rates in the straightening operation[30]. The total strain at the slab surface is estimated to be fairly small, probably less than a few percent (see Sec. 2.2). The strain to which the strand is subjected becomes particularly critical as it passes through the $\text{Ar}_3$ temperature (850-750°C), at
which the $\gamma/\alpha$ transformation occurs. It has been suggested[31,32] that transverse cracking occurs at the $Ar_3$ temperature and is related to intergranular embrittlement caused by allotriomorphs of ferrite precipitating along the austenitic grain boundaries. The embrittlement phenomenon occurring at $Ar_3$ has been studied[33,34] by hot tensile testing (Fig. 4.4).

Fig. 4.4 Diagram showing the influence of test temperature on hot ductility[34].

Figure 4.4 shows hot ductility as a function of test temperature for low alloy steels, and indicates that ductility decreases to a minimum value at about 800 °C. This temperature approximates to the beginning of $\gamma/\alpha$ transformation that occurs at the austenitic boundaries of the coarse-grained cast material. Additionally, there is a sharp decrease in the values for reduction of area below 600 °C.
The tensile stresses generated during straightening will concentrate strains preferentially within the soft ferrite seams, and therefore, plastic deformation will occur predominantly in this ferrite [34,35]. Plastic deformation will then be followed by (austenite) grain boundary sliding causing wedge type cracks at the grain boundary triple points. Final fracture will occur as the result of micro-void coalescence. From these consideration, a mechanism of intergranular ductile fracture of austenite with dynamic precipitation of carbide and/or nitride particles has been postulated as shown in Fig.4.5.

![Fig.4.5 Schematic diagrams showing (a) the dynamic precipitation of carbides and/or nitrides on austenitic grain boundaries, (b) nucleation of allotriomorphs of ferrite and strain concentration within soft ferrite seams along austenitic grain boundaries in the initial stage of deformation, (c) microvoid formation by decohesion of precipitate/matrix interfaces and (d) coalescence of microvoids resulting in ductile intergranular fracture of austenite [35].](image-url)
4.3.3. Prevention of Surface Transverse Cracking

It should be emphasised again that the transverse surface cracking which occurs in continuously cast steel slabs depends largely on the austenitic structure and/or the presence of ferrite at the austenitic grain boundaries. Therefore, control of structure and suppression of precipitation should be the first priority in preventing this defect.

4.3.3.1. Control of $\gamma$ Structure

As discussed in Section 4.3.2.1, the grain size of the austenitic structure significantly affects transverse cracking susceptibility. The relationship between the final grain size, $D_\gamma$, and cooling rate, $dT/dt$, has been studied [36] using an empirical relationship developed from the nucleation law of solidification of cast iron, and it was found that the final grain size can be described by the equation:

$$D_\gamma = 0.336 (dT/dt)^{-1/2}$$ \hspace{1cm} (4.2)

Equation (4.2) shows that a fine-grain structure can be achieved by increasing the cooling rate. Therefore, there is theoretical justification for reduction of transverse cracking susceptibility of continuously cast steel
slab (associated with the coarsening of the structure), by increasing the rate of cooling of the slab.

Although increasing the cooling rate can suppress austenitic grain growth, in practice it is not feasible to achieve sufficiently rapid cooling before the end of grain growth at temperatures above 1300 °C in the limited mould cooling. Consequently, under the limited cooling condition in practical continuous casting, the austenitic grain size after solidification depends largely on the concentrations of alloying elements, including carbon. Therefore, it is possible that effective prevention of surface cracking might be possible through addition of small amounts of micro-alloying elements such as titanium, boron and zirconium which can refine the austenitic grain structure. Addition of titanium can result in precipitation of TiN during solidification[37], thereby increasing the number of nuclei resulting in grain refinement. Moreover, at lower temperatures, the precipitation of TiC on austenitic grain boundaries will suppress austenitic grain growth and result in a grain refinement effect. It is unlikely that the precipitates of TiC will promote formation of allotriomorphs of proeectoid ferrite as they may retard the γ/α transformation (see Sec. 4.3.2.2). Both boron and zirconium additions have similar effects in the grain refinement of austenite, but these elements are seldom used for economical reasons.
The Influence of titanium, boron and/or zirconium in reducing transverse cracking susceptibility associated with the grain refinement, is shown in Fig. 4.6.

![Graph showing hot ductility at low strain rate for low alloy steels containing microalloying additions.](image)

**Fig. 4.6** Hot ductility at low strain rate for low alloy steels containing microalloying additions [37]

Figure 4.6 shows hot ductility determined at low strain rate such as occurs in continuous casting of low alloy steels containing micro-alloying additions. Clearly, the addition of a small amounts of titanium, boron or zirconium can significantly improve ductility of steels in the critical temperature range of 850-750 °C.

### 4.3.3.2. Control of Precipitation

The second factor controlling transverse cracking is dynamic precipitation of carbides and/or nitrides at the austenitic grain boundaries. Suppression
of precipitation should be effective in preventing transverse surface cracking, since precipitates such as niobium and vanadium carbides and/or aluminium and vanadium nitrides which are formed at temperatures close to $\text{Ar}_3$, will promote the formation of allotriomorphs of proeutectoid ferrite and cause embrittlement at the grain boundaries[38].

If the secondary cooling pattern below the mould can be controlled to avoid dynamic precipitation, the formation of proeutectoid ferrite might be avoided thereby preventing embrittlement at the boundaries. However, as it is difficult to control the secondary cooling due to the limitations of the production process, this means of control is not practicable. Consequently, in practice, the precipitation of carbides and/or nitrides does frequently occur in the grain boundary regions. However, addition of titanium has a strong effect in the refinement of austenitic structures during solidification, and may also retard the $\gamma/\alpha$ transformation resulting in reduction of transverse cracking susceptibility by suppressing the formation of allotriomorphic ferrite on the grain boundaries [38].

An alternative method for prevention of transverse cracking caused by intergranular embrittlement associated with $\gamma/\alpha$ transformation, is control of
during straightening to above $\text{Ar}_3$, precipitation of allotriomorphs of ferrite at the austenitic grain boundaries can be avoided altogether thereby removing the embrittlement effect of strain in the straightening operation. In practical processing, therefore, the surface edge temperature during straightening must be controlled, if possible to higher than $900 \, ^\circ\text{C}$ [39]. Control of aluminium and nitrogen content and their solubility product $\text{Al}_x\text{N}$ is also feasible in a production environment, as are taper, mould powder, spray design, spray arrangement, spray condition and machine alignment.

The opportunity for extensive experimentation in the continuous casting plant is very limited. Additions of small amounts of titanium and control of surface edge temperature of the strand to above $900 \, ^\circ\text{C}$ are practicably the only sensible preventive measures that can be exercised.

Measurement of the surface temperature as the strand progresses from the tundish to the straightener is essential to control of cracking, but very difficult to achieve directly. On the other hand, it is possible to calculate these temperatures using a mathematical model of the cooling process as described in Chapter 5.
Chapter 5

MATHEMATICAL HEAT TRANSFER MODEL FOR SOLIDIFICATION OF CONTINUOUSLY CAST STEEL SLABS

As has been discussed in Chapter 4, the quality of continuously cast steel slab depends largely on the operational conditions, particularly the cooling processes from commencement in the mould to completion of solidification in the secondary cooling zones. Operational experience has shown that appropriate control of the cooling processes, particularly in spray cooling, is essential for achieving the proper quality of the cast products [39]. Overcooling can lead to intergranular embrittlement and associated transverse crack defects due to transformation which occurs below $\text{Ar}_3$. To minimize that cracking, the strand surface must be maintained in the austenitic range or, in practice, above 900 °C, until after the straightening operation (refer to Sec. 4.3.2.2). With this limitation in mind, and due to lack of appropriate operating plant and experimental facilities, direct measurements of temperature distributions and solidification rates on moving castings are very difficult to make. Consequently, it has become necessary to adopt an alternative approach to obtain thermal and kinetic data. The most appropriate alternate
approach is to mathematically simulate the heat transfer in a continuously cast section, and then calculate the temperature distributions as a function of the controllable variables of the process.

Simulation of heat transfer during solidification requires that a nonlinear mathematical problem be solved. As pointed out by Ruddle [40], there are two mathematical approaches to the problem, the analytical approach and the numerical approach. While the analytical approach is certainly the more elegant of the two, it does require a number of inexact assumptions because of the complexity of the problem. Simplifying one or more of these assumptions, such as invoking invariant thermophysical properties, constant heat-transfer coefficients, and linear temperature profiles in the shell, can introduce considerable uncertainty in the validity of the results. Thus, this approach is not favoured. On the other hand, numerical solutions, which are considerably more versatile, appear to be better suited for solving solidification problems. Complex variations in the boundary conditions and variable thermophysical properties can be handled readily with this technique. Fortunately, numerical computations to the problem can be obtained quite readily with use of the digital computer.
In this present study, a model of unidimensional heat transfer in continuously cast slab is presented. The method of solution using a digital computer is also included. Calculated temperature distributions during secondary cooling along with attempts to verify the model are discussed.

5.1. Mathematical Model

As has been discussed in Chapter 3, the solidification of a slab during continuous casting occurs as the slab passes through three distinct zones of cooling. Accordingly, the mathematical model developed for simulating the process consists of three parts: solidification in the mould zone, solidification in the spray cooling zone and solidification in the radiant cooling zone.

A mathematical model of the cooling process can be developed from a heat balance on a horizontal slice of cast slab over the time period required for the slice to proceed from the liquid meniscus in the mould to the cut off station.[41]. As the slice moves downward, heat is conducted from the center line to the surface of the slab at a rate governed by the surface boundary conditions and thermophysical properties of the metal in the slice. As a consequence of heat loss from the surface, a temperature profile exists in the slab and as the temperature decreases
progressively, so that profile will change. The model enables the profile and the change in profile to be estimated in terms of the imposed boundary conditions. The model is based on a heat balance derived from the partial differential equation describing the unsteady state conduction of heat in a medium moving at velocity \( u \) in direction \( z \) (casting direction) that can be described[41]:

\[
\frac{\partial T}{\partial t} = \frac{\partial}{\partial z} \left[ \frac{\partial}{\partial z} \left( \frac{1}{\rho c} \frac{\partial T}{\partial z} \right) \right] + \frac{\partial}{\partial x} \left( \frac{1}{\rho c} \frac{\partial T}{\partial x} \right) + \frac{\partial}{\partial y} \left( \frac{1}{\rho c} \frac{\partial T}{\partial y} \right) = 0 \quad (5.1)
\]

where, \( \rho \): density of steel (kg/m\(^3\))
\( c \): specific heat of steel (J/kg.K)
\( k \): thermal conductivity of steel (W/m.K)
\( T \): temperature (K)
\( x,y \): transverse directions.

As pointed out by Hills[41], conduction of heat in the transverse (\( x \)) direction is very much greater than conduction of heat in the withdrawal (\( z \)) direction. Also, conduction in the other transverse (\( y \)) direction can be justifiably ignored. As the horizontal slice moves downward at the velocity of the casting, the relative velocity of the slice is zero and the bulk heat transfer term therefore vanishes. Consequently:
\[
\frac{\partial T}{\partial z} (k - ) = 0, \quad (5.2)
\]

\[
\frac{\partial T}{\partial y} (k - ) = 0, \text{ and} \quad (5.3)
\]

\[
\frac{\partial T}{\partial z} \mu c. v = 0 \quad (5.4)
\]

Substituting Eqs. (5.2), (5.3) and, (5.4) into Eq. (5.1) leads to:

\[
\frac{\partial T}{\partial t} - \frac{\partial}{\partial x} (k - ) = 0 \quad (5.5)
\]

Variation of thermal conductivity with temperature can be taken into account by expansion of the second term of Eq. (5.5) giving:

\[
\rho c. v - k \frac{\partial^2 T}{\partial x^2} - b ( - )^2 = 0 \quad (5.6)
\]

where \( b \) is the rate of change of thermal conductivity with temperature.

To enable digital programming of the differential heat transfer expression, it is first necessary to reduce it to algebraic equations. The finite difference
technique, which is one of many numerical schemes available, is suitable for this purpose and is described in Section 5.2.

5.2. Numerical Solution

To adapt the model described in Eq. (5.6) for numerical analysis solution, the horizontal slice is subdivided into a number of small elements each of which is represented by a node located at the center of the element. The nodes are respectively identified as 1 at the center, 2, 3, ..., n-1 and n the surface element.

Figure 5.1 shows that the half slab slice passes through three distinct zones of cooling with respective extracted heats. These extracted heats are $q_m$ due to cooling in the mould, $q_s$ spray cooling, and $q_r$ due to radiant cooling. In addition, the containment rolls are involved in the extraction of heat $q_{rc}$ by conduction during contact with the strand surface. At time $t = 0$, the temperature of each node is fixed by the time boundary condition that the temperature profile of the slice at the meniscus is equal to the incoming metal temperature. At the next time step $\Delta t$, the temperatures of nodes 2 through (n-1) are calculated using the finite difference form of the conduction equation and the known temperatures for nodes at time $t = 0$. The temperatures at nodes n are then calculated.
using the surface boundary conditions and the temperatures of the remaining nodes as calculated at the next time step $\Delta t$.

Fig. 5.1 Node arrangement and boundary conditions of half slab slice

Under limitations of the assumptions of the mathematical modelling, the calculation is repeated with the appropriate thermophysical properties of the elements until the completion time is reached[41].
5.2.1 Determination of the Temperatures at Nodes 2 through (n-1)

Development of the numerical analysis solution of the model for calculating the temperatures of nodes 2 through (n-1) at time \( t \) from known node temperatures calculated at time \((t-\Delta t)\), is derived from the Eq. (5.6), expressed in the form:

\[
\frac{\partial T}{\partial t} = \frac{1}{\rho c \Delta x^2} \left[ k \frac{\partial^2 T}{\partial x^2} + b \left( \frac{\partial T}{\partial x} \right)^2 \right]
\]  

(5.7)

Consider a slice which is to be divided into equal elements in the \( x \) direction (refer to Fig. 5.1). The nodal points are designated as shown by the \( J \) locations indicating the \( x \) increment. Then over the next time step, \( \Delta t \), the temperature is calculated using the finite difference approximation of Eq. (5.7), where[41]:

\[
\frac{\partial T}{\partial t} = \frac{T_{j+1} - T_j}{\Delta t}
\]  

(5.8)

\[
\frac{\partial^2 T}{\partial x^2} = \frac{(T_{j+1} - T_j)/\Delta x - (T_j - T_{j-1})/\Delta x}{\Delta x^2} = T_{j+1} - T_{j-1} - 2T_j, \text{ and}
\]  

(5.9)

\[
\left( \frac{\partial T}{\partial x} \right)^2 = \left( \frac{T_{j+1} - T_{j-1}}{2 \Delta x} \right)^2 = T_{j+1}^2 - 2T_{j+1}T_j + T_j^2
\]  

(5.10)

Substituting Eqs. 5.8, 5.9 and 5.10 into Eq. 5.7 leads to:
\[
\frac{T_j' - T_j}{\Delta t} = \frac{1}{\Delta x^2 \rho c} \left[ K(T_{j+1} - T_{j-1} - 2T_j) + \frac{b}{4}(T_{j+1}^2 - 2T_{j+1}T_{j-1} + T_{j-1}^2) \right] \quad (5.11)
\]

where, \( T_j \): temperature of \( J \) node calculated at time \( t \), and.
\( T_j' \): temperature of \( J \) node calculated at time \( t + \Delta t \).

Thus, the explicit form of the finite difference approximation Eq. (5.11) for calculating the interior node temperatures is

\[
T_j' = T_j + \frac{\Delta t}{\Delta x^2 \rho c} \left[ K(T_{j+1} - T_{j-1} - 2T_j) + \frac{b}{4}(T_{j+1}^2 - 2T_{j+1}T_{j-1} + T_{j-1}^2) \right] \quad (5.12)
\]

### 5.2.2. Determination of Strand Surface Temperature

The temperature of node \( n \), at the strand surface, can be calculated using an equation which is derived from the heat balance for the heat-in, \( q_{in} \), heat-out, \( q_{out} \), and accumulated heat on a half element \( n \) over the time \( \Delta t \), as shown in Fig. 5.2.

![Fig. 5.2 Heat balance on a half element.](image-url)
As shown in Fig. 5.2, the heat balance on a half element can be described as

\[ \text{[Accumulated heat]} = \text{[Heat-in]} - \text{[Heat-out]} \]  \hspace{1cm} (5.13)

For the heat which flows in direction, \( x \), the following differential form of Eq. (5.13) is appropriate:

\[
\frac{\partial T}{\partial t} \left. \right|_{x=x} \frac{\partial q}{\partial x} \Delta y \Delta z - \left. q_{\text{out}} \right|_{x=x+\Delta x} \frac{\partial T}{\partial x} \Delta y \Delta z = \left. q_{\text{in}} \right|_{x=0} - \left. q_{\text{out}} \right|_{x=0+\Delta x} \]
\[
\rho c \frac{\partial T}{\partial t} = \frac{\Delta x \Delta y \Delta z}{\Delta x} \frac{\partial q}{\partial x} \frac{\partial T}{\partial x} - \frac{\Delta x}{\Delta x} \] \hspace{1cm} (5.14)

Equation (5.14) may be expressed in differential equation form:

\[
\frac{\partial q}{\partial x} = \frac{\partial T}{\partial t} \frac{\partial q}{\partial x} \] \hspace{1cm} (5.15)

and for \( x = \Delta x/2 \):

\[
\rho c \frac{\partial T}{\partial t} = \frac{\partial q}{\partial x} \left|_{(\Delta x/2)} \right. \]
\hspace{1cm} (5.16)

Since \( \partial q \) can be expressed as \( k(\partial T/\partial x) - q_{\text{out}} \), Eq. (5.16) becomes

\[
\rho c \frac{\partial T}{\partial t} = \frac{k(\partial T/\partial x) - q_{\text{out}}}{\Delta x/2} \] \hspace{1cm} (5.17)
thus,

\[
\frac{\partial T}{\partial t} = \frac{2}{\Delta x, \rho, c} \left[ k \left( \frac{\partial T}{\partial x} \right) - q_{\text{out}} \right]
\]  \hspace{1cm} (5.18)

The strand surface temperature can now be calculated using Eq. (5.19) derived from Eq. (5.18) using respective boundary conditions of the heats extracted in each cooling zone as a function of time. For surface nodes \( n \), this equation, expressed as a finite difference, is:

\[
T_n' = T_n + \frac{2 \Delta t}{\Delta x^2, \rho, c} \left[ k \left( T_{n-1} - T_n \right) - \Delta x, q_{\text{out}} \right]
\]  \hspace{1cm} (5.19)

where, \( T_n, T_n' \): strand surface temperature calculated at times \( t \) and \( t + \Delta t \).

5.3. Application of the Model to Continuous Casting of Slabs

As explained in Chapter 4, it is necessary to control the strand surface temperature at above 900 °C to avoid transverse surface cracking associated with intergranular embrittlement associated with the \( \gamma/\alpha \) transformation below the \( Ar_3 \) temperature. Therefore, control of spray cooling during continuous casting is very important to minimize the cracking.
With the mathematical model that has been developed in this present study, control of the strand surface temperature during continuous casting can be simulated for a given set of casting parameters. The model may be used to assist in designing the cooling process, particularly if the spray cooling is to be changed to improve the cooling process.

Development of the computer solution of the model is described in Appendix A. The mathematical model was coded to the Q-Basic language for solution using Compal 486 computers. To run the mathematical model, the following input data were required:

(a) section size,
(b) casting temperature, speed and time,
(c) liquidus temperature of the steel,
(d) water flux of spray cooling in each cooling zone, and
(e) thermophysical constants and operational conditions.

This study was limited to a casting 200 mm thick, 1200 mm wide, of 0.16-0.18% C steel for which the casting machine is used at PT. Krakatau Steel and for which the transverse cracking problem is most prevalent. The parameters used in the study are described in Chapter 6.
Chapter 6

EXPERIMENTAL

Despite recent significant advances in continuous casting technology, transverse surface cracking remains one of the most common defects encountered during the production of continuously cast steel slab in most world wide plant. At PT. Krakatau Steel, the incidence of these defects is mainly restricted to steel containing 0.12-0.18% of carbon, and is insignificant for steel containing less than 0.09% C.

As discussed in Chapter 4, the presence of transverse cracks has undesirable influences on the product because they can degrade quality as well as increasing the risk of failure during applications of the material in finished product. At present, the problem of transverse cracking is attacked by removing the defect by scarfing the corners of the slab, which involves a degree of material loss and associated expenditure. Therefore, decrease in the incidence of transverse cracks is demanded to obtain a significant yield increment and to improve process efficiency. Consequently, examination of these defects and determination of means
of controlling the incidence of them is a matter of considerable importance.

Theoretically, the opportunity to meet requirements of a stable quality of slab greatly depends on the capability of the continuous casting machine as well as the operational conditions of the process. These conditions comprise the radius of the cooling strand, strand size, steel grade, casting speed, distribution of cooling water, tundish temperature and strand surface temperature. However, as the opportunity for extensive experimentation in the continuous casting plant is very limited, the only variables that could be examined in this study of transverse cracking were operational process parameters which can be controlled in practice, and that can be studied without interfering with production schedules. Therefore, experimentation was carried out under this limitation. The two parameters which were investigated were believed to strongly influence the formation of transverse cracking. These parameters were:

(a) composition of steel, and

(b) water rate, with limited variation possible only for control of the strand surface temperature.
As is believed, addition of small amounts of titanium to the cast steel might be effective in preventing the incidence of transverse cracking associated with austenitic grain growth. Accordingly, to examine the effect of titanium additions, the steels chosen for metallurgical study of transverse cracking had four different titanium contents with similar carbon, manganese, and aluminium contents, and other undesirable elements such as phosphorus, sulphur and nitrogen which remain in steel in limited concentration. The specified titanium contents of steel for this purpose were:

(A) without titanium
(B) 0.005 - 0.010% Ti,
(C) 0.015 - 0.020% Ti, and
(D) 0.025 - 0.030% Ti

Compositions of the steels are set out in Tables 6.1, 6.2, 6.5 and 6.6.

For the purpose of controlling the strand surface temperature during continuous casting, the rate of spray cooling water for strand cooling is controlled through a flow-meter panel, located in the control room, and designed to distribute water in the six secondary cooling zones comprising the spray-ring, the zones-1A and 1B, the zone-2, the zone-3 and the zone-4, as shown in Fig. 6.1.
Fig. 6.1 Schematic diagram showing distribution of water cooling in the secondary cooling[43]

The transient heat transfer model developed in Chapter 5 to simulate the strand cooling process was used to determine how the strand surface temperature responds to variation of the secondary cooling water rate during the continuous casting process.
6.1. Methods

The hypothesis that both steel composition and straightening temperature are the most important factors influencing the susceptibility to transverse cracking dictated the experimental methodology.

Initially, a statistical survey of accumulated production data was made to identify susceptible alloys, and to determine the critical straightening temperatures which are associated with poor ductility of the strand. For this, the data were classified into three steel grades with compositions:

(a) $C \leq 0.06\%$
(b) $0.07 \leq C \leq 0.09\%$
(c) $0.12 \leq C \leq 0.18\%$

The incidence of transverse cracking in these steels had been routinely determined by removal of the oxidized surface layer on the corner region of the slabs followed by visual assessment. Results of the statistical surveys are shown in Figs. 7.1 and 7.2 in Chapter 7. The survey clearly established that the susceptibility of transverse cracking of the cast steel slabs (c) containing 0.12-0.18% C is very high, whereas the susceptibility of the steels (a) and (b) is (negligibly) low. Therefore, the relationship between straightening temperature and transverse cracking was examined only for steel grade (c).
The steel used for this study was a grade frequently produced at PT. Krakatau Steel, cast with the specified casting conditions:

(i) casting (tundish) temperatures of 1530-1550 °C

(ii) casting speed of 0.9 m/min., and

(iii) strand size of 200 mm thick, 1200 mm width

and having composition shown in Table 6.1.

<table>
<thead>
<tr>
<th>Elements</th>
<th>wt. %</th>
</tr>
</thead>
<tbody>
<tr>
<td>C</td>
<td>0.16 - 0.18</td>
</tr>
<tr>
<td>Mn</td>
<td>0.80 - 1.00</td>
</tr>
<tr>
<td>Al</td>
<td>0.030 - 0.060</td>
</tr>
<tr>
<td>P</td>
<td>0.025 max.</td>
</tr>
<tr>
<td>S</td>
<td>0.020 max.</td>
</tr>
<tr>
<td>N</td>
<td>0.0070 max.</td>
</tr>
</tbody>
</table>

6.1.1. Effect of Steel Composition

As discussed in Section 4.3.3.1, suppression of austenitic grain growth during solidification, by addition of small amounts of titanium to refine the grain structure during solidification, could be effective in preventing transverse cracking associated with coarsening of the cast structure.
To examine possible beneficial effects of titanium, four steels A, B, C and D with compositions shown in Table 6.2, were examined with a secondary cooling water rate of 0.756 l/kg, as specified for normal production operation as shown in Table 6.3.

Table 6.2 Composition of the steels cast with water rate 0.756 l/kg

<table>
<thead>
<tr>
<th>Steels</th>
<th>Ti</th>
<th>C</th>
<th>Mn</th>
<th>Al</th>
<th>P</th>
<th>S</th>
<th>N</th>
<th>Nr. of slabs</th>
</tr>
</thead>
<tbody>
<tr>
<td>A1</td>
<td>-</td>
<td>0.17</td>
<td>0.78</td>
<td>0.034</td>
<td>0.006</td>
<td>0.009</td>
<td>0.0046</td>
<td></td>
</tr>
<tr>
<td>A2</td>
<td>-</td>
<td>0.18</td>
<td>0.85</td>
<td>0.048</td>
<td>0.006</td>
<td>0.007</td>
<td>0.0054</td>
<td>17</td>
</tr>
<tr>
<td>A3</td>
<td>-</td>
<td>0.16</td>
<td>0.85</td>
<td>0.047</td>
<td>0.007</td>
<td>0.006</td>
<td>0.0036</td>
<td></td>
</tr>
<tr>
<td>B1</td>
<td>0.007</td>
<td>0.17</td>
<td>0.89</td>
<td>0.043</td>
<td>0.008</td>
<td>0.006</td>
<td>0.0048</td>
<td>17</td>
</tr>
<tr>
<td>B2</td>
<td>0.009</td>
<td>0.17</td>
<td>0.87</td>
<td>0.052</td>
<td>0.008</td>
<td>0.007</td>
<td>0.0041</td>
<td></td>
</tr>
<tr>
<td>B3</td>
<td>0.006</td>
<td>0.16</td>
<td>0.87</td>
<td>0.052</td>
<td>0.007</td>
<td>0.009</td>
<td>0.0039</td>
<td></td>
</tr>
<tr>
<td>C1</td>
<td>0.017</td>
<td>0.17</td>
<td>0.86</td>
<td>0.043</td>
<td>0.013</td>
<td>0.008</td>
<td>0.0053</td>
<td>17</td>
</tr>
<tr>
<td>C2</td>
<td>0.017</td>
<td>0.17</td>
<td>0.85</td>
<td>0.050</td>
<td>0.011</td>
<td>0.009</td>
<td>0.0037</td>
<td></td>
</tr>
<tr>
<td>C3</td>
<td>0.018</td>
<td>0.18</td>
<td>0.82</td>
<td>0.049</td>
<td>0.006</td>
<td>0.007</td>
<td>0.0054</td>
<td></td>
</tr>
<tr>
<td>D1</td>
<td>0.026</td>
<td>0.17</td>
<td>0.86</td>
<td>0.044</td>
<td>0.008</td>
<td>0.007</td>
<td>0.0056</td>
<td>16</td>
</tr>
<tr>
<td>D2</td>
<td>0.028</td>
<td>0.17</td>
<td>0.90</td>
<td>0.048</td>
<td>0.008</td>
<td>0.008</td>
<td>0.0040</td>
<td></td>
</tr>
<tr>
<td>D3</td>
<td>0.025</td>
<td>0.18</td>
<td>0.84</td>
<td>0.041</td>
<td>0.007</td>
<td>0.008</td>
<td>0.0049</td>
<td></td>
</tr>
</tbody>
</table>
It should be noted that water rate $W_r$ is indicated by the litres (l) of water used per kilogram (Kg) of steel, determined from the relationship:

$$ W_r = \frac{V_s}{V_c \cdot r \cdot F \cdot \rho} $$  \hspace{1cm} (6.1)

Where,
- $V_s$ : water spray (l/min.)
- $V_c$ : casting speed (m/min.)
- $r$ : slab thickness (m)
- $F$ : slab width (m)
- $\rho$ : density of steel (kg/m³)

Table 6.3 The normal rate of spray cooling water

<table>
<thead>
<tr>
<th>Cooling zones</th>
<th>Utilized water rate (for cast. speed = 0.9 m/min.)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>$V_s$ (l/min.)</td>
</tr>
<tr>
<td>Spray-ring</td>
<td>89</td>
</tr>
<tr>
<td>Zone-1A</td>
<td>360</td>
</tr>
<tr>
<td>Zone-1B</td>
<td>206</td>
</tr>
<tr>
<td>Zone-2</td>
<td>284</td>
</tr>
<tr>
<td>Zone-3</td>
<td>206</td>
</tr>
<tr>
<td>Zone-4</td>
<td>129</td>
</tr>
<tr>
<td>Total</td>
<td></td>
</tr>
</tbody>
</table>
The incidence of transverse cracks on the upper surface edge of slabs of the four steels was then determined as a function of the titanium content of the steel, observed by removing the oxidized layer by a slight scarfing. The grain size of the subsurface cast structure was measured.

6.1.2. Effect of Straightening Temperature

As discussed in Section 4.3.3.2, suppression of precipitation of the allotriomorphs of ferrite during straightening by maintaining the strand temperature at above 900 °C might be affective in preventing the incidence of the transverse cracking. Accordingly, the possibility of controlling the straightening temperature using an empirical relationship (see Fig. 7.3) between the water rate and straightening temperature was examined. It was possible to vary the water rate only slightly by an overall reduction of the water rate in zones-3 and 4 to achieve reducing the water rate from 0.756 l/kg as specified for normal production operation, to 0.736 l/kg, without significant interference with production practice, and with an expectation of obtaining a straightening temperature higher than 900 °C. The distribution of the reduced water rate is shown in Table 6.4.
To examine possible beneficial effects of straightening temperature, steel A was examined with the two different secondary cooling water rates of 0.756 l/kg and 0.736 l/kg. For the water rate of 0.756 l/kg, the experimental data were obtained using steel with compositions A1, A2 and A3, shown in Table 6.2. For the water rate of 0.736 l/kg, three slightly different compositions of steel A were used as shown in Table 6.5.
Table 6.5 Composition of the steels cast with water rate 0.736 l/kg

<table>
<thead>
<tr>
<th>Steels</th>
<th>Chemical composition (wt. %)</th>
<th>Nr. of Slabs</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Ti</td>
<td>C</td>
</tr>
<tr>
<td>A4</td>
<td>-</td>
<td>0.18</td>
</tr>
<tr>
<td>A5</td>
<td>-</td>
<td>0.18</td>
</tr>
<tr>
<td>A6</td>
<td>-</td>
<td>0.17</td>
</tr>
</tbody>
</table>

During straightening, the surface edge temperature of the slabs was measured using an optical pyrometer. Subsequently, the incidence of transverse cracks on the upper surface edge of slabs cooled with two water rates was determined as a function straightening temperature, and the grain size of the subsurface cast structure was determined.

6.1.3. Combined Effects of Titanium and Straightening Temperature

As discussed in Sections 4.3.3.1 and 4.3.3.2, suppression of austenitic grain growth and/or precipitation of allotriomorphs of ferrite can reduce the susceptibility of transverse cracking. Accordingly, combined control of both effects by addition of small amounts of titanium to the steel and by maintaining the strand surface temperature to above \( A_r_3 \) might be especially effective in preventing cracking.
To identify the optimum combination of the limited variables, the steels A, B, C and D shown in Table 6.6 were cast using the water rate 0.736 l/kg (see Table 6.4).

Table 6.6. Composition of the steels cast with water rate 0.736 l/kg

<table>
<thead>
<tr>
<th>Steels</th>
<th>Chemical composition (wt. %)</th>
<th>Nr. of slabs</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Ti</td>
<td>C</td>
</tr>
<tr>
<td>A4</td>
<td>-</td>
<td>0.18</td>
</tr>
<tr>
<td>A5</td>
<td>-</td>
<td>0.18</td>
</tr>
<tr>
<td>A6</td>
<td>-</td>
<td>0.17</td>
</tr>
<tr>
<td>B4</td>
<td>0.006</td>
<td>0.16</td>
</tr>
<tr>
<td>B5</td>
<td>0.006</td>
<td>0.17</td>
</tr>
<tr>
<td>B6</td>
<td>0.009</td>
<td>0.16</td>
</tr>
<tr>
<td>C4</td>
<td>0.018</td>
<td>0.18</td>
</tr>
<tr>
<td>C5</td>
<td>0.016</td>
<td>0.18</td>
</tr>
<tr>
<td>C6</td>
<td>0.017</td>
<td>0.16</td>
</tr>
<tr>
<td>D4</td>
<td>0.026</td>
<td>0.16</td>
</tr>
<tr>
<td>D5</td>
<td>0.028</td>
<td>0.17</td>
</tr>
<tr>
<td>D6</td>
<td>0.029</td>
<td>0.17</td>
</tr>
</tbody>
</table>
During straightening, the surface edge temperatures of the slabs were measured using an optical pyrometer and the incidence of transverse cracks on the upper surface edge of the slabs was used to identify the optimum combination of titanium content and straightening temperature.

6.2. Metallography

To obtain a better understanding of the mechanism of transverse crack formation, especially in relation to grain boundary embrittlement, the subsurface structure of the cast steel was examined metallographically to determine:

(a) the appearance of transverse cracks, and
(b) the grain size of the cast structure.

Test pieces 200x200x50 mm (see Fig. 6.2) were oxy-acetylene cut from the slabs of steels A, B, C and D, which have different titanium contents, as shown in Tables 6.2 and 6.6 for water rates of 0.756 and 0.736 l/kg. Four specimens measuring 20x20x50 mm were cut from each piece using a machine saw equipped with a cooling system. The specimens were then prepared by polishing and etching for metallographic investigation using a Leitz Wetzlar-Metalux II microscope.
6.3. Application of the Mathematical Model

Careful control of spray cooling conditions is essential for successful operation of the continuous casting process. Under-cooling can result in retention of liquid metal in the core of the strand with consequential sensitivity to cracking due to the lack of ductility when tensile strain arises at the solid/liquid interface. On the other hand, over cooling of the strand surface to below $Ar_3$ may lead to surface transverse cracking. Therefore, control of spray cooling is essential and normally, the strand surface should be maintained at above $900\, ^\circ \text{C}$. 

Fig. 6.2 Schematic diagram showing location of metallographic specimens.
The temperature of liquid metal in the tundish can be measured using a thermocouple, and in the straightening region with an optical pyrometer. However, it is very difficult to determine temperature between these two stations. With this limitation in mind, secondary cooling was investigated using the model described in Chapter 5 to simulate the strand surface temperature during continuous casting under a given set of casting parameters. The model may be used to assist in designing the cooling process, particularly if the spray cooling is to be changed to improve the cooling process. Accordingly, the mathematical model was used to calculate the surface temperature for spray cooling conditions of 0.756 l/kg and 0.736 l/kg. The surface temperature of the strand was calculated as a function of time under limitations of the assumptions made during development of the model and which are discussed in Chapter 7, and with the input data shown in Table 6.7.
Table 6.7 Input data for running the mathematical model

<table>
<thead>
<tr>
<th>Casting parameters</th>
<th>Normal water rate</th>
<th>Red. water rate</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>(0.756 l/kg)</td>
<td>(0.736 l/kg)</td>
</tr>
<tr>
<td>(a) Slab width (mm)</td>
<td>1200</td>
<td>1200</td>
</tr>
<tr>
<td>(b) Tundish temperature (°C)</td>
<td>1544</td>
<td>1544</td>
</tr>
<tr>
<td>(c) Liquidus temperature (°C)</td>
<td>1516</td>
<td>1516</td>
</tr>
<tr>
<td>(d) Casting speed (m/min..)</td>
<td>0.9</td>
<td>0.9</td>
</tr>
<tr>
<td>(e) Casting time (min.)</td>
<td>16.9</td>
<td>16.9</td>
</tr>
<tr>
<td>(f) Water rate (l/min.):</td>
<td></td>
<td></td>
</tr>
<tr>
<td>spray-ring</td>
<td>89</td>
<td>89</td>
</tr>
<tr>
<td>zone-1A</td>
<td>360</td>
<td>360</td>
</tr>
<tr>
<td>zone-1B</td>
<td>206</td>
<td>206</td>
</tr>
<tr>
<td>zone-2</td>
<td>284</td>
<td>284</td>
</tr>
<tr>
<td>zone-3</td>
<td>206</td>
<td>183</td>
</tr>
<tr>
<td>zone-4</td>
<td>129</td>
<td>119</td>
</tr>
</tbody>
</table>
RESULTS AND DISCUSSION

The work carried out in this study of surface quality of continuously cast steel was concerned with preventing, or at least reducing, transverse cracking by measures that are practicably sensible in routine production. Experimental work was carried out as described in Chapter 6 to:

(a) identify susceptible alloys and the important factors influencing susceptibility to transverse cracking, and

(b) investigate the possible beneficial effects of controlling steel composition and/or straightening temperature to minimise the occurrence of transverse cracking.

For purpose (a), a statistical survey of accumulated production data was made and for purpose (b) a program of experimental work on transverse cracking prevention was carried out. The recorded data are stored on the computer and available on request from PT. Krakatau Steel.
7.1. Statistical Survey

PT. Krakatau Steel records relating to production from the No. 1 casting machines for the period July 1992 to July 1993 were searched to obtain information about:

(a) susceptibility to transverse cracking of various slab grades, Fig. 7.1,
(b) the relationship between straightening temperature and transverse cracking probability, Fig. 7.2, and
(c) the relationship between water rate and straightening temperature, Fig. 7.3, including:
   (i) the statistical variation of actual water rate for nominal standard practice of 0.756 l/kg Fig. 7.4, and
   (ii) the consequential variation of straightening temperatures for the nominal standard practice of 0.756 l/kg, Fig. 7.5.

Figure 7.1 is a histogram which shows the percentage of slabs which contained transverse cracks for steels classified into three grades with compositions:

(a) $C \leq 0.06\%$,
(b) $0.07 \leq C \leq 0.09\%$, and
(c) $0.12 \leq C \leq 0.18\%$
Fig. 7.1 Histogram showing the effect of carbon content on transverse cracking susceptibility of cast steel.

For grade (a) 0.5% of 787 slabs, for which information was available, were cracked; for grade (b) 7% of 1201 slabs were cracked and for grade (c) 62.8% of 508 slabs were cracked. These data clearly establish that the susceptibility of cast steel slabs (c) containing $0.12 \leq C \leq 0.18\%$ is excessively high with 62.8% cracking incidence, whereas the susceptibility of the steels (a) is negligible and of the steels (b) is acceptably low. Consequently, attention was directed in this study to steels with carbon content in the range 0.12-0.18%.

As discussed in Section 4.3.2.1, the high incidence of cracking for steels containing 0.12-0.18% C might be explained in terms of microstructural
changes by peritectic reaction during solidification in the mould. These
changes result in a coarse austenitic grain structure with consequential
increased sensitivity to cracking (see Sec. 7.6, Metallography). The
dependence of austenitic grain size on carbon content corresponds to
the range in which ductility loss occurs (Fig. 4.3) and is in good agreement
with the variation of austenite formation temperature by the peritectic
transformation as shown by the Fe-C constitutional diagram (see Fig. 3.1).

With respect to the relationship between straightening temperature and
transverse cracking, the survey established that for steels containing $0.12 \leq C \leq 0.18\%$, the sensitivity was maximum at about $800 \pm 25 \, ^{\circ}C$, Fig. 7.2. This
result indicates that the incidence of transverse cracking conforms with
the hot tensile testing results carried out using a Gleeble machine (see Fig.
4.4). Those test data showed that the ductility, as indicated by
percentage reduction in area, had a minimum value at about $800 \, ^{\circ}C$
corresponding to:

(a) the temperature for which cracking was maximum (83.7\%, Fig. 7.2), and
(b) the temperature at which significant precipitation of ferrite from
austenite is expected.

Furthermore, Fig. 7.2 shows that there is a significant decrease of cracking
to 31.2\% for straightening temperatures of about $900 \, ^{\circ}C$. Since the $Ar_3$
temperature at which precipitation of ferrite from austenite commences during cooling is about 900 °C for very low carbon steels, it would appear that the decrease in cracking relates to microstructure at the straightening station. The decrease to 56.2% for straightening temperatures of 750 ± 25 °C was a consequence of increase in the amount of ferrite in the cast structures, resulting in improvement of ductility as shown in Fig. 4.4.

![Graph showing the effect of straightening temperature on transverse cracking susceptibility of continuously cast 0.12-0.18% C steels. The number N refer to the statistical data of slabs examined during normal production operation, with a secondary cooling water rate of 0.756 l/kg, and ± 25 is 3 standard deviation.]

As discussed in Section 4.3.2.2, the occurrence of transverse cracking in the temperature range 775-824 °C is apparently related to intergranular
embrittlement (see Sec. 7.6, Metallography) caused by allotriomorphs of ferrite precipitated at the austenitic grain boundaries in combination with the tensile stresses generated on the upper slab surface during straightening. As postulated in Fig. 4.5 (page 31), inter-granular embrittlement arises because the tensile stresses concentrate strains preferentially within the soft ferrite seams with the consequence that plastic deformation occurs predominantly in the ferrite and is then followed by (austenitic) grain boundary sliding causing wedge type cracks at the grain boundary triple points. Final fracture occurs as a result of microvoid coalescence, resulting in transverse cracks.

The advantageous possibility of controlling secondary cooling to raise the straightening temperature to about 900 °C is evident in the empirical relationship between the water rate and straightening temperature shown in Fig. 7.3. The data shown in this Figure clearly establishes that, as the secondary cooling water is reduced from 0.771-0.790 l/kg (of steel) to 0.731-0.750 l/kg, the temperature at the straightening station increases from about 755 °C to about 880 °C. The nominal water rate designated for normal operation of the caster is 0.756 l/kg, for which the straightening temperature is 800-850 °C. Figure 7.3. shows that reducing the water rate by 0.01 l/kg increases the straightening temperature by approximately
30 °C, so that a temperature of about 900 °C should result from reduction of the water rate from 0.756 l/kg to about 0.736 l/kg. The effects of water rate on straightening temperature and cracking are described in Section 7.2.

Fig. 7.3 Diagram showing an empirical relationship between water rate and straightening temperature.

7.2. Water Rate.

The objective of controlling the water rate is to obtain a straightening temperature in excess 900 °C. In normal operation of the continuous caster, the nominal water rate is 0.756 l/kg which was established from empirical work with particular concern only for the requirement that
solidification of the strand must be completed before straightening. This requirement was imposed to avoid the incidence of internal cracking which would be caused by liquid metal remaining in the core of the strand under the tensile strain arising at the solid/liquid interface during straightening (see Sec. 2.2). However, the opportunity exists to lower the water rate slightly to 0.736 l/kg, as this practice had been examined previously and was known not to impair the internal quality of slabs, or interfere with production schedules.

There is no doubt that variations in water pressure, increases or decreases in casting speed, changes in the tundish temperature and inconsistencies in other operational parameters result in a considerable range of straightening temperatures for standard operating conditions, Fig. 7.4 and 7.5. Clearly, these statistical data show that for the nominal rate of 0.756 l/kg under normal production conditions of 159 slabs, the straightening temperature varied from about 700 °C to over 900 °C with ~ 46% of the slabs having a straightening temperature of 850 ± 25 °C. Under this condition, it is expected that significant precipitation of ferrite at the austenitic grain boundaries will occur before straightening with the consequence that many slabs will contain transverse cracks.
Variation in straightening temperature for normal production conditions, shown in Fig. 7.5, might be attained by varying the actual water rate used for the secondary cooling process. Figure 7.4 shows that for a nominal rate of 0.756 l/kg, the actual utilized water rate ranges from about 0.730 l/kg to about 0.790 l/kg.

Fig. 7.4 Histogram showing range of the actual water rate for normal production operation with nominal water rate of 0.756 l/kg. The number N refers to the statistical data of slabs, as examined in Fig. 7.2.
Fig. 7.5 Histogram showing range of straightening temperature for cooling conditions operation with nominal water rate of 0.756 l/kg.

To minimize the consequences of normal production variations in water rate, the rate was very carefully controlled at the nominal value of 0.756 l/kg during the production of 67 slabs, as described in Section 6.1.1, Table 6.2. Even with this careful control the straightening temperatures for the slabs, shown in Fig 7.6, were in the range 775-925 °C with about 60% in the 850 ± 25 °C range.

It is obvious from this experimentation that careful control of the cooling water rate at 0.756 l/kg, would reduce the spread of the range of straightening temperatures. Equally obvious, the majority of slabs have a straightening temperature of about 850 °C and would be expected to be
cracked, despite the careful control. Thus for production proposes, it is evident that careful control of the water rate at 0.756 l/kg will not significantly reduce the incidence of cracking and it is further evident that a slightly lower water rate, could be used to raise the straightening temperature. Consequently, 66 slabs as described in Section 6.1.3, Table 6.6 were cast using a water rate of 0.736 l/kg to determine whether this measure could reduce the incidence of cracking. The results of the experimentation, shown in Fig. 7.7, indicate that the reduced water rate did in fact result in a straightening temperature of ~ 900 °C as expected from the relationship shown in Fig. 7.3.

![Graph showing straightening temperature](image)

**Fig. 7.6** Histogram showing the range of straightening temperatures for trial operation with the water rate controlled to exactly 0.756 l/kg.
Fig. 7.7 Histogram showing the range of straightening temperatures for trial operation with the water rate controlled to 0.736 l/kg.

The incidence of cracking in the slabs, which were cast using the two different water rates, Figs. 7.6 and 7.7, without and with titanium additions are shown respectively in Figs. 7.8 and 7.11 (see Sec. 7.5).

For steel A, Fig. 7.8 shows the incidence of cracking of 17 slabs and 16 slabs which were cast respectively using 0.756 l/kg and 0.736 l/kg of secondary cooling water. Clearly, the effect of reduced water rate was significant in decreasing the incidence of transverse cracking from about 60% of slabs to about 30% of slabs. The beneficial effect of control of straightening temperature probably resulted from prevention of
precipitation of allotriomorphs of ferrite by raising the temperature of the austenite at straightening as indicated by comparison of Figs. 7.6 and 7.7. The tensile stresses generated during straightening, as discussed in Sec 4.3.2.2, were therefore accommodated without failure in the grain boundary regions of the austenite.

Fig. 7.8 Histogram showing the effect of the two rates of secondary cooling water on transverse cracking susceptibility of continuously cast titanium free 0.16-0.18% C steels.

Attainment of a straightening temperature of ~ 900 °C is a consequence of the complete cooling process that occurs between the tundish and the straightening station. During secondary cooling the surface temperature of the strand is progressively reduced from the known tundish temperature
to the temperature which can be measured at the straightening station. It is not possible to obtain measures of the surface temperature of the strand during this cooling despite the importance of knowledge of the way the temperature changes during progress of the strand through the caster. Consequently the mathematical model, established in Chapter 5, was used to calculate these temperatures and thereby to assist in understanding the effect of secondary water rate on straightening temperature.

7.3. Mathematical Model

For a particular tundish temperature, the straightening temperature will be determined principally by:

(i) the dimensions of the strand,
(ii) the casting speed, and
(iii) the secondary cooling, including the strand containments and the rolls cooling system.

A strand surface edge of above 900 °C is demanded at straightening to prevent precipitation of ferrite and consequential transverse cracking. To attain a straightening temperature of (say) 900 °C it is essential to have information about the progressive decrease in temperature from the tundish to a straightening station. However, it is very difficult to measure
the temperature of the strand directly in the continuous casting machine, and so the most profitable approach to determination of the temperature profile of the strand surface is by calculation using the mathematical model. For this purpose, the strand is represented as a thin slice which progresses from the tundish to the straightening station, and which undergoes cooling that can be calculated using the model set up in Chapter 5.

The model is a finite element analysis by which the temperature of the node of the surface element is ultimately calculated. This node is located in the center of the wide face of the strand. However, the transverse cracking occurs, not at the face center but, at the face edge. Consequently, the critical condition which should govern whether or not cracking occurs is the temperature of the strand edge at straightening. The mathematical processes for determining the edge temperature from the calculated face center temperature are complex and were not pursued in this work. Alternatively, a simple empirical relationship between the two temperatures was obtained from direct measurement, near the straightening station, for the specified casting conditions as described in Sec. 6.1. This relationship was found to be:
\[ T_{\text{edge}} = (T_{\text{face}} - 90) \, ^{\circ}\text{C} \] \hspace{1cm} (7.1)

and was applied to the calculated results to obtain the required edge temperatures.

It should be noted that the correction temperature of 90 \(^{\circ}\text{C}\) is valid for the conditions and the machine involved and should not be applied to the other situations indiscriminantly.

The input data required to run the mathematical model are shown in Table 6.6, and the results of the calculations are shown in Fig. 7.9 and in Appendix A. From Fig. 7.9 it is generally evident that the temperature of edge of the slice decreases sharply from the tundish/mould region to about the end of zone-1B. The high rate of decrease in this region is a consequence of high cooling intensity due to the high ratio of water rate to sprayed surface area (see Fig. 6.1 and Table 6.3 or 6.4). In these zones, the total surface area of the top of the strand is approximately 1.34 m \times 1.2 m and was cooled, as designated in Table 6.3 or 6.4, with about 650 l/m\(^2\)/min of water. This rate may be compared with the rate of about 52 l/m\(^2\)/min in zone-2, for which the surface area is approximately 4.39 m \times 1.2 m.
Fig. 7.9 Diagram showing the calculated edge temperature as the strand moves through the continuous caster for water rates 0.756 l/kg and 0.736 l/kg.
As the slice enters zone-2, the surface is reheated by conduction from within the slab until the temperature gradient between surface and interior of the slice becomes relatively small due to reduced cooling from spray water (as discussed above). The temperature of the slice then decreases again through zones-3 and 4, mainly due to spray cooling. When the slice leaves zone 4, the cooling conditions change from forced convection cooling to radiant cooling and consequentially, the surface temperature increases due to conduction of heat from interior of the slice.

If due to severity of cyclic heating/cooling, the temperature gradients during secondary cooling are too high, it will result in the strand surface temperature to fall below 800 °C, as shown in Fig. 7.9. This can increase the susceptibility to transverse cracking related to the intergranular embrittlement caused by allotriomorphs of ferrite precipitated at the austenitic grain boundaries in combination with the tensile stresses generated due to the withdrawal forces, strand bulging, roll eccentricity, and poor alignment between the individual strand guide elements and segments. Consequently, transverse cracks would be expected to occur in continuously cast steel slabs, despite the reducing of water rate to 0.0736 l/kg, which was expected to give the straightening temperature of
above 900 °C to avoid the formation of allotriomorphs of ferrite during straightening.

Figure 7.9 shows the edge temperature variations for secondary cooling water rates of 0.756 l/kg and 0.736 l/kg. The effect of reduced water rate is evident and results in a calculated increase in edge surface temperature of about 10 °C in zone-3 and-4, and in the straightening region. It should be noted that the reduced water rate is effective only in zones 3 and 4 (see Table 6.3, 6.4) so that there is no change to the temperature variation from the mould to zone 2. Calculated straightening temperatures of 848 °C and 856°C are to be compared with the respective measured temperatures at the straightening station of 850°C and 900°C. It is obvious from this comparison that the mathematical model under-estimates the straightening temperatures, particularly for the lower water rate. Nevertheless, the model provides much useful information about the temperature variation during cooling and will be pursued further to determine how it can be modified to provide more reliable predictions of the exit temperature.
7.4. Effect of Titanium

The work described in Section 6.1.3 establishes that addition of titanium to cast steel has beneficial effects in reducing the incidence of cracking. The effect is probably due to suppression of austenitic grain growth by precipitates of titanium compounds. Consequently, the effects of titanium additions on cracking were examined.

As discussed in the experimental work (see Chapter 6), titanium additions were made to a normal 0.16-0.18\%C steel to provide four base alloys with titanium contents in the ranges (Table 6.2):

(A) 0\% Ti,
(B) 0.005 - 0.010\% Ti,
(C) 0.015 - 0.020\% Ti, and
(D) 0.025 - 0.030\% Ti

The additions were made so that TiN and/or TiC would precipitate at the austenitic grain boundaries and thereby pin those boundaries and refine the grain structure. The effect of the additions on the surface quality of slabs of the four alloys which were cast using a water rate of 0.756 l/kg, with careful control of the process, are shown in Fig. 7.10 (see Appendix B). Clearly the effect of titanium is significant up to approximately 0.017\% and
results in a decrease in the incidence of transverse cracking from approximately 60% of slabs to approximately 40%. Increasing the titanium content to ~ 0.028% seems to have had no additional influence on the surface quality. The effects of titanium additions are probably due to limited precipitation of TiC and/or TiN at the austenitic grain boundaries to refine the grains during cooling in the mould in practical continuous casting as discussed in Section 4.3.3.1. Furthermore, the negligible effect of increasing the titanium content from 0.017% to 0.028% is most likely associated with the grain sizes of the cast structure for these two compositions. Figure 7.17, which is discussed in Section 7.6.2, shows that the distribution of the ASTM grain sizes for the steel containing 0.028% Ti was very similar to the distribution in the steel containing 0.017% Ti. Consequently, it is apparent that the increase in titanium content did not alter the grain size distribution and so the cracking propensity of the steels would be expected to be the same, resulting in similar surface quality levels.
7.5. Combined Effect Water Rate and Titanium

The experiment work described in Sections 6.1.1 and 6.1.2 established that reduction of water rate and addition of titanium to the cast steel each have beneficial effects in reducing the incidence of cracking. The effects are probably due to suppression of austenitetic grain growth and suppression of precipitation of allotriomorphs of ferrite by titanium additions,
and raising of the straightening temperature by lowering the water rate. The results indicate that the beneficial effect of titanium addition and reduced water rate might be combined advantageously. Consequently, the effects of reduced water rate on the cracking propensity of steels containing several levels of titanium were examined as described in section 6.1.3. For this purpose, the titanium contents of steels were designed (Table 6.5):

(A) 0%Ti,
(B) 0.005 - 0.010 %Ti,
(C) 0.015 - 0.020 %Ti, and
(D) 0.025 - 0.030 %Ti.

Slabs of the four steels were cast using both the normal water rate of 0.756 l/kg and the reduced rate of 0.736 l/kg and the incidence of cracking in these slabs is shown in Fig. 7.11 (see Appendix B).
Fig. 7.11 Diagram showing the effect of titanium on transverse cracking susceptibility of continuously cast steel slabs for water rate 0.736 l/kg and 0.756 l/kg. The number N refer to the experimentation data of slabs of the steels A, B, C and D with 95% confidence limit, composition shown in Tables 6.2 and 6.6.

Clearly, the effect of titanium and reduced water rate combined was significant in decreasing the incidence of transverse cracking up to approximately 0.017 %Ti. As noted in Section 7.4, the relationships shown in Fig. 7.11 indicate that addition of 0.017 %Ti reduced the incidence of cracking from about 60% of slabs to about 40% of slabs for the standard water rate. Additionally, reduced secondary cooling reduced the number of cracked slabs by about 50% at each level of titanium. Further addition
of titanium to 0.028 % had negligible influence on the surface quality. Clearly, the beneficial effect of combined control of titanium content and straightening temperature can result in suppression of austenitic grain growth and suppression of precipitation of allotriomorphs of ferrite by retardation of the \(\gamma/\alpha\) transformation. The tensile stresses generated during straightening, as discussed in Sections 4.3.2.1 and 4.3.2.2, can therefore be accommodated without failure in the grain boundary regions of the austenite. Therefore, it can be concluded that, by control of the titanium content to approximately 0.017% and straightening temperature to above 900 °C by lowering the water rate to 0.736 l/kg, it is possible to obtain optimum measures to minimize transverse cracking. Both of these measures can be applied to the production casting machine without interference with the production schedule.

### 7.6. Metallography

As has been discussed in Section 3, the surface of continuously cast steel slab contains oscillation marks formed perpendicular to the casting direction, as shown in Fig. 7.12. This observation is considered to be important with respect to the location of transverse cracks which frequently occurred at the bottom of oscillation marks, on the upper surface edge of the slabs, and perpendicular to the casting direction as
shown in Fig. 7.13. Normally, transverse cracks can be clearly observed by removing the oxidized layer on the corner of slabs, by a slight scarfing. Defects revealed in this way are shown in Fig. 7.14.

**Fig. 7.12** Typical appearance of oscillation marks on the surface of continuously cast steel slabs shown in cm scale.

**Fig. 7.13** Photomacrograph showing occurrence of transverse cracks at the bottom of oscillation marks.
7.6.1. Subsurface Structures in the Vicinity of Transverse Cracks

Figures 7.15 and 7.16 show two different, but typical, transverse cracks found at the bottom of oscillation marks in two specimens cut from different slabs containing 0% Ti, and cast using a secondary cooling water
of 0.756 l/kg. Both subsurface structures consist of pearlite and ferrite as idiomorphs, allotriomorphs and some Widmanstatten plates. These subsurface characteristics provide good sites for transverse crack formation at the bottom of oscillation marks on the upper surface of slabs, at the straightening point.

Figure 7.15 shows the high volume fraction of allotriomorphs of ferrites in the subsurface structure beneath the oscillation marks. The photomicrograph also shows that the cracks initiated from that surface, then propagated, mostly intergranularly.
Fig. 7.15 Photomicrograph showing typical appearance of a transverse crack, with no solidified hook formation, in the subsurface structure below an oscillation mark, magnification of 100 x, etched in nital.
The crack shown in Fig. 7.15 might be associated with the allotriomorph of ferrites formed by phase transformation at and below the $Ar_3$ as a consequence of casting with the water rate of 0.756 l/kg. The presence of the allotromorphs resulted in intergranular embrittlement when tensile strain arose during straightening. Consequently, it appears evident that the incidence of transverse cracks in the slabs containing 0% Ti and cast using the water rate of 0.756 l/kg would be too high. As shown in Fig. 7.8, the incidence was, in fact, about 60%.

On the other hand, the crack shown in Fig. 7.16 apparently initiated from the segregation line between the fine and coarse grains consequent upon the formation of a solidified hook (refer to section 3.1 and Fig. 3.3), then propagated mostly intergranularly in the coarse grained region below the hook. Additionally, it can be seen that the cracks propagated through regions of fine ferrite which are likely to be allotriomorphs formed at the grain boundaries of a coarse grained austenite.
Fig. 7.16  Photomicrograph showing typical appearance of a transverse crack, with solidified hook formation, in the subsurface structure below an oscillation mark, magnification of 75 x, etched in nital.
Clearly, transverse cracks, such as shown in Fig. 7.16, are likely to propagate in the most brittle zone, owing to the effects of segregation and/or discontinuities in the solidified shell due to the formation of a solidified hook. The effect results in coarse austenite grains which in turn can result in increasing susceptibility to transverse cracking. Consequently, transverse cracks would be expected to occur in continuously cast steel slabs in which solidification hooks occur, despite addition of titanium to refine the grain structure and/or controlling the straightening temperature to avoid the formation of allotriomorphs of ferrite. As these two measures cannot control the formation of solidified hooks this factor has an independent influence on the susceptibility to transverse cracking and clearly needs to be investigated.

7.6.2. Grain size of the Subsurface Cast Structure

Grain sizes of the cast structures of steels A, B, C and D, which have different titanium contents, shown in Tables 6.2 and 6.6 for water rates of 0.756 and 0.736 l/kg, were measured using standard ASTM grain size charts and were classified according to ASTM grain size number.

It was, in fact, quite difficult to obtain a measure of the grain sizes of the cast structures, as they normally consisted of mixtures of small, medium
and large grains. However, the grains which were measured, were representative of the grain size distributions.

The results of the measurements are shown in Figs. 7.17 and 7.18 (See Appendix B). Clearly, for both water rates (0.756 l/kg, 0.736 l/kg), the grain size was sensitive to titanium content up to about 0.017%. Further increase in the titanium content to 0.028% had little additional effect on the grain size distribution. As the titanium concentration was increased from 0 to 0.017% the fraction of large grains (ASTM 6) decreased, the fraction of small grains (ASTM 9) remained about the same, and the fractions of the intermediate sized grains (ASTM 7, 8) changed in opposing ways. As a consequence of these changes, the grain structures become finer as the titanium content increased, as expected.

The effect of water rate on the grain structure is indicated by comparison of Figs. 7.17 and 7.18. For both rates, the average grain size for the steel containing 0 %Ti was ASTM 7.65 and for 0.017 %Ti was ASTM 8.02. The reduction of average grain size consequent upon addition of titanium is almost certainly responsible for the reduced cracking incidence shown in Fig. 7.10 and 7.11 because by reducing the grain size with addition of small amounts of titanium significant improvement in the ductility of the
steel in the critical temperature range of 850 - 750°C occurred, as discussed in Section 4.3.3.1 (see Fig. 4.6)

Fig. 7.17 Diagram showing the effect of titanium on grain size of subsurface structure of the cast slabs for water rate of 0.756 l/kg.

Fig. 7.18 Diagram showing the effect of titanium on grain size of subsurface structure of the cast slabs for water rate of 0.736 l/kg.
Chapter 8

CONCLUSIONS

Practical measures for reducing the incidence of transverse cracking associated with austenitic grain growth and precipitation of allotriomorphs of ferrite in continuously cast steel slabs have been studied. The conclusions reached from a statistical survey of PT. Krakatau Steel production data and experimental work were as follows.

1. The defect propensity in continuously cast steel slabs containing $0.12 \leq C < 0.18\%$ is very high, for $0.07 \leq C \leq 0.09\%$ it is acceptably low and for $C \leq 0.06\%$ it is negligible.

2. The critical straightening temperature for which transverse cracking susceptibility maximum was $800 \pm 25^\circ C$.

3. Under normal production conditions, slabs were cast with nominal water rate of 0.756 l/kg, resulting in straightening temperatures from about $700^\circ C$ to over $900^\circ C$ with $\sim 46\%$ of the slabs having a straightening temperature of $850 \pm 25^\circ C$. 
4. The range of straightening temperatures for a water rate of 0.756 l/kg could be reduced by careful control of the process, to the range 775 °C to 925 °C with ~ 60% of the slabs having a straightening temperature of 850 ± 25 °C.

5. For a straightening temperature below about 900 °C, allotriomorphs of ferrite, precipitated at the austenitic grain boundaries, cracked under tensile stress generated on the top surface during straightening of the slab.

6. The incidence of transverse cracking associated with precipitation of allotriomorphs of ferrite was reduced significantly from about 60% of slabs to about 30% of slabs, by raising the straightening temperature to above 900 °C by lowering the water rate to about 0.736 l/kg.

7. Straightening temperatures calculated as a function of water rate made with a finite element mathematical model were under-estimated compared with direct measurement. Nevertheless, the model provides much useful information about the temperature changes in the strand surface during cooling; these data are very difficult to measure directly.
8. The incidence of transverse cracking associated with coarsening of the austenitic grain structure can be reduced significantly from about 60% of slabs to about 40% of slabs, by addition of titanium up to 0.017%.

9. Under limited cooling conditions, the grain size of the cast slab was not sensitive to water rate, but was sensitive to addition of titanium up to about 0.017%.

10. The negligible effect of increasing the titanium content from 0.017% to 0.028% was most likely associated with the similar grain size distributions in the structures for these two compositions.

11. The fraction of large grains (ASTM 6) decreased, the fraction of small grains (ASTM 9) remained about the same and the fraction of the intermediate size grains (ASTM 7,8) changed in opposing ways with increase in the concentration of titanium from 0% to 0.017%, and remained essentially the same for further addition of titanium to 0.028%.

12. The average grain size for the steel containing 0% Ti was ASTM 7.65 and for the steel containing 0.017% Ti was ASTM 8.02.
13. The highest incidence of transverse cracking occurred at the bottom of oscillation marks and occurred intergranularly.

14. The effect of combined control of titanium content to approximately 0.017% and straightening temperature to above 900 °C by lowering the water rate to 0.736 l/kg was most beneficial in minimizing transverse cracking and can be applied to the production casting machine at PT Krakatau Steel without interference to the production schedule.
REFERENCES


42. Slab Caster Manual, PT. Krakatau Steel, 1983.
APPENDIX A

COMPUTER PROGRAM FOR CALCULATING THE TEMPERATURE OF STRAND DURING CONTINUOUS CASTING

Start

INPUT DATA, INITIAL CONDITION

J = 2

I = 2

T(1, J) = ...
I = I + 1

I > (N - 1)

DETERMINING THE CONDUCTIVITY VALUES

T(1, J) = ....

DETERMINING THE EXTRACTED HEAT

J = J + 1

J > M

PRINTING THE TEMPERATURE PROFILE

END
'Appendix A

'COMPUTER PROGRAM FOR CALCULATING THE SURFACE TEMPERATURE OF STRAND DURING CONTINUOUS CASTING

'DEFINING THE ELEMENTS

DIM B AS DOUBLE, E AS DOUBLE, G AS DOUBLE, c AS DOUBLE, Cw AS DOUBLE
DIM k AS DOUBLE, p AS DOUBLE, Rw AS DOUBLE, Vw AS DOUBLE, Rc AS DOUBLE
DIM Trs AS DOUBLE, Tsm AS DOUBLE, Tout AS DOUBLE, Ta AS DOUBLE
DIM I AS INTEGER, Lcu AS DOUBLE, L1 AS DOUBLE, L2 AS DOUBLE, L3 AS DOUBLE
DIM L4 AS DOUBLE, L5 AS DOUBLE, L6 AS DOUBLE, L7 AS DOUBLE, L8 AS DOUBLE
DIM Nrz1B AS INTEGER, Nrz2 AS INTEGER, Nrz3 AS INTEGER, Nrz4 AS INTEGER
DIM Nrw AS INTEGER, N AS INTEGER, M AS INTEGER, i AS DOUBLE, ts AS DOUBLE
DIM A AS INTEGER, Tb AS INTEGER, Rz1B AS DOUBLE, Rz2 AS DOUBLE, Rz3 AS DOUBLE
DIM R5 AS DOUBLE, Rp1B AS DOUBLE, Rp2 AS DOUBLE, Rp3 AS DOUBLE
DIM R4 AS DOUBLE, Rp4 AS DOUBLE, Rpw AS DOUBLE, hcz1B AS DOUBLE, hcz2 AS DOUBLE
DIM hcz3 AS DOUBLE, hcz4 AS DOUBLE, hcw AS DOUBLE

'THERMOPHYSICAL CONSTANTS

E = -.03  ' (the rate of change thermal conductivity with temperature, in W/m.K^2)
E = .85   ' (emissivity)
G = 5.669E-08  ' (Stefan-Boltzman constant, in W/m^2.K^4)
c = 747   ' (heat capacity of steel, in J/kg.K)
Cw = 4178 ' (heat capacity of water, in J/kg.K)
k = 29    ' (heat conductivity of steel, in W/m.K)
p = 7854  ' (density of steel, in kg/m^3)
Rw = 999  ' (density of water, in kg/m^3)
Vw = .0367 ' (water rate of mould cooling, in m^3/sec.)
Rc = .004 ' (contact line between rolls and the strand surface, in m)
hs = 160000 ' (coefficient of spray cooling heat transfer)
Trs = 573 ' (surface temperature of the rolls, in K)
Tin = 310.5 ' (temperature of the mould water inlet, in K)
Tout = 316 ' (temperature of the mould water outlet, in K)
Ta = 313 ' (surrounding temperature, in K)
L1 = .6 ' (distance between meniscus and the mould bottom, in m)
L2 = .755 ' (distance between meniscus and the end of spray-ring, in m)
L3 = 1.329 ' (distance between meniscus and the end of zone-1A, in m)
L4 = 1.935 ' (distance between meniscus and the end of zone-1B, in m)
L5 = 4.81 ' (distance between meniscus and the end of zone-2, in m)
L6 = 9.195 ' (distance between meniscus and the end of zone-3, in m)
L7 = 12.075 ' (distance between meniscus and the end of zone-4, in m)
L8 = 15.237 ' (distance between meniscus and the straightener, in m)
hcz1B = 300 ' (heat trans. coef. between rolls zone-1B and strand surf., in W/m^2.K)
hcz2 = 300 ' (heat trans. coef. between rolls zone-2 and strand surf., in W/m^2.K)
hcz3 = 600 ' (heat trans. coef. between rolls zone-3 and strand surf., in W/m^2.K)
hcz4 = 900 ' (heat trans. coef. between rolls zone-4 and strand surf., in W/m^2.K)
hcw = 1200 ' (heat trans. coef. between withdrawal rolls and strand surf., in W/m^2.K)
Appendix A

\[ N_{rlB} = 4 \] (number of rolls zone-1B).
\[ N_{r2} = 10 \] (number of rolls at zone-2).
\[ N_{r3} = 12 \] (number of rolls at zone-3).
\[ N_{r4} = 8 \] (number of rolls at zone-4).
\[ N_{rd} = 8 \] (number of rolls at withdrawal region).
\[ R_{zlB} = 0.18 \] (diameter of rolls zone-1B, in \( m \)).
\[ R_{z2} = 0.245 \] (diameter of rolls zone-2, in \( m \)).
\[ R_{z3} = 0.31 \] (diameter of rolls zone-3, in \( m \)).
\[ R_{z4} = 0.31 \] (diameter of rolls zone-4, in \( m \)).
\[ R_{wd} = 0.39 \] (diameter of rods withdrawal, in \( m \)).
\[ R_{pzlB} = 0.215 \] (roll pitch zone-1B, in \( m \)).
\[ R_{pz2} = 0.291 \] (roll pitch zone-2, in \( m \)).
\[ R_{pz3} = 0.363 \] (roll pitch zone-3, in \( m \)).
\[ R_{pz4} = 0.363 \] (roll pitch zone-4, in \( m \)).
\[ R_{pzd} = 0.45 \] (roll pitch withdrawal, in \( m \)).

'INPUT DATA FOR RUNNING THE MATHEMATICAL MODEL OF STRAND COOLING
'---------------------------------------------
INPUT 'SLAB WIDTH (mm)' = *; Sw
INPUT 'TUNDISH TEMPERATURE (C)' = *; Tt
\[ Tt = Tt + 273 \]
INPUT 'LIQUIDUS TEMPERATURE (C)' = *; TL
\[ TL = TL + 273 \]
INPUT 'CASTING SPEED (m/min.)' = *; Vc
\[ Vc = Vc / 60 \]
INPUT 'CASTING TIME (min.)' = *; tc
INPUT 'SPRAY-RING (1/min.)' = *; Vsr
INPUT 'ZONE-1A (1/min.)' = *; Vz1A
INPUT 'ZONE-1B (1/min.)' = *; Vz1B
INPUT 'ZONE-2 (1/min.)' = *; Vz2
INPUT 'ZONE-3 (1/min.)' = *; Vz3
INPUT 'ZONE-4 (1/min.)' = *; Vz4

' NODES (x) AND TIMES INTERVAL(ts)
'------------------------------
\[ N = 10 \]
\[ M = 300 \]
\[ x = 0.2 / (2 * N) \]
\[ ts = tc * 60 / M \]
DIM T(N, M)
\[ Sw = Sw / 1000 \]
Appendix A

'INITIAL AND BOUNDARY CONDITIONS
'=================================
A = 1
DO
  T(A, 1) = Tt
  A = A + 1
  IF A > N THEN
    EXIT DO
  END IF
END DO

'CALCULATING THE STRAND TEMPERATURE
'=================================
J = 2
DO
  Tb = (J - 1) * ts
  I = 2
  DO
    P1 = T(I, J - 1)
    P2 = ts / (x * 2 * p * c)
    P3 = T(I + 1, J - 1)
    P4 = T(I - 1, J - 1)
    T(I, J) = P1 + P2 * (k * (P3 + P4 - 2 * P1) + b / 4 * (P3 ^ 2 - 2 * P3 * P4 + P4 ^ 2))
    I = I + 1
    IF I > (N - 1) THEN
      EXIT DO
    END IF
  END DO
END DO

IF T(I, J - 1) > TL THEN
  c = 747
ELSEIF 1674 < T(I, J - 1) AND T(I, J - 1) <= TL THEN
  c = 784.5
ELSEIF 1181 < T(I, J - 1) AND T(I, J - 1) <= 1674 THEN
  c = 137.47 + 348 * (T(I, J - 1) - 3 * T(I, J - 1))
ELSEIF 1181 < T(I, J - 1) AND T(I, J - 1) <= 1674 THEN
  c = 672.4
ELSEIF 273 < T(I, J - 1) AND T(I, J - 1) <= 1033 THEN
  c = 312 + 442.3 * (T(I, J - 1) - 3 * T(I, J - 1))
END IF

T(I, J) = T(I, J - 1) + ts / (x * 2 * p * c) * ((k * (2 * T(2, J - 1) - 2 * T(I, J - 1))))
IF Tb < (L1 / Vc) THEN
  Qo = Rw * Cw * Vw * (Tout - Tin) / (Sw * L1)
ELSEIF (L1 / Vc) < Tb AND Tb < (L2 / Vc) THEN
  Qo = hs * (Vsr / (2 * 60 * Sw * (L2 - L1))) ^ .75
ELSEIF (L2 / Vc) < Tb AND Tb < (L3 / Vc) THEN
  Qo = hs * (VzlA / (2 * 60 * Sw * (L3 - L2))) ^ .75
ELSEIF \( \frac{L3}{Vc} \leq Tb \text{ AND } Tb < \frac{L4}{Vc} \) THEN  
\[ Q_{sp} = h_s \times \left( \frac{Vz1B}{2 \times 60 \times Sw \times (L4 - L3)} \right) \times 0.75 \]
\[ Q_{rc} = Nrz1B \times R_c \times hcz1B \times \left( T(N, J - 1) - Trs \right) \]
\[ Q_{rad} = (Nrz1B \times 2 \times (Rpz1B - Rz1B) / (L3 - L4)) \times G \times E \times \left( T(N, J - 1) - 4 - Ta \right) \]
\[ Q_o = Q_{sp} + Q_{rc} + Q_{rad} \]
ELSEIF \( \frac{L4}{Vc} \leq Tb \text{ AND } Tb < \frac{L5}{Vc} \) THEN  
\[ Q_{sp} = h_s \times \left( \frac{Vz2}{2 \times 60 \times Sw \times (L5 - L4)} \right) \times 0.75 \]
\[ Q_{rc} = Nrz2 \times R_c \times hcz2 \times \left( T(N, J - 1) - Trs \right) / (L5 - L4) \]
\[ Q_{rad} = (Nrz2 \times 2 \times (Rpz2 - Rz2) / (L5 - L4)) \times G \times E \times \left( T(N, J - 1) - 4 - Ta \right) \]
\[ Q_o = Q_{sp} + Q_{rc} + Q_{rad} \]
ELSEIF \( \frac{L5}{Vc} \leq Tb \text{ AND } Tb < \frac{L6}{Vc} \) THEN  
\[ Q_{sp} = h_s \times \left( \frac{Vz3}{60 \times Sw \times (L6 - L5)} \right) \times 0.75 \]
\[ Q_{rc} = Nrz3 \times R_c \times hcz3 \times \left( T(N, J - 1) - Trs \right) / (L6 - L5) \]
\[ Q_{rad} = (Nrz3 \times 2 \times (Rpz3 - Rz3) / (L6 - L5)) \times G \times E \times \left( T(N, J - 1) - 4 - Ta \right) \]
\[ Q_o = Q_{sp} + Q_{rc} + Q_{rad} \]
ELSEIF \( \frac{L6}{Vc} \leq Tb \text{ AND } Tb < \frac{L7}{Vc} \) THEN  
\[ Q_{sp} = h_s \times \left( \frac{Vz4}{60 \times Sw \times (L7 - L6)} \right) \times 0.75 \]
\[ Q_{rc} = Nrz4 \times R_c \times hcz4 \times \left( T(N, J - 1) - Trs \right) / (L7 - L6) \]
\[ Q_{rad} = (Nrz4 \times 2 \times (Rpz4 - Rz4) / (L7 - L6)) \times G \times E \times \left( T(N, J - 1) - 4 - Ta \right) \]
\[ Q_o = Q_{sp} + Q_{rc} + Q_{rad} \]
ELSEIF \( \frac{L7}{Vc} \leq Tb \text{ AND } Tb < \frac{L8}{Vc} \) THEN  
\[ Q_{rc} = Nrwd \times R_c \times hwd \times \left( T(N, J - 1) - Trs \right) / (L8 - L7) \]
\[ Q_{rad} = (Nrwd \times 2 \times (Rpwd - Rw) / (L8 - L7)) \times G \times E \times \left( T(N, J - 1) - 4 - Ta \right) \]
\[ Q_o = Q_{rc} + Q_{rad} \]
END IF  
\[ T(N, J) = T(N, J - 1) + 2 \times ts / (x \times 2 \times p \times c) \times (k \times (T(N - 1, J - 1) - T(N, J - 1)) - x \times Qo) \]
\[ J = J + 1 \]
IF J > M THEN  
EXIT DO  
END IF  
LOOP

LPRINT "RESULTS OF THE PROGRAM"  
LPRINT "================================"  
J = 2  
LPRINT  
LPRINT "CASTING PARAMETERS"  
LPRINT "----------------------"  
LPRINT "1. SLAB WIDTH ="; Sw; "m"  
LPRINT "2. LIQUIDUS TEMPERATURE ="; TL; "K"  
LPRINT "3. TUNDISH TEMPERATURE ="; Tt; "K"  
LPRINT "4. CASTING SPEED ="; Vc; "m/second"  
LPRINT "5. CASTING TIME ="; tc; "min."  
LPRINT "6. WATER RATE:"  
LPRINT "(a) SPRAY RING ="; Vsr; "l/min."  
LPRINT "(b) ZONE-1A ="; Vz1A; "l/min."
Appendix A

LPRINT * (c) IONE-1B  "%; Ve1B; "1/min."
LPRINT * (d) IONE-2  "%; Ve2; "1/min."
LPRINT * (e) IONE-3  "%; Ve3; "1/min."
LPRINT * (f) IONE-4  "%; Ve4; "1/min."
LPRINT
LPRINT
LPRINT
LPRINT *

"RODE TEMPERATURES (CALCULATED AS A FUNCTION OF TIME)"

LPRINT
LPRINT
LPRINT 'Sec. ; ; 1; ; 2; ; 3; ; 4; ; 5; ; 6; ; 7; ; 8; ; 9; ; 10; ; CORREX'
LPRINT
LPRINT Tb = 0
LPRINT USING "####"; INT(Tb); SPC(2);
V = 1
DO
LPRINT INT(T(V, 1)) - 273;
V = V + 1
IF V > 10 THEN EXIT DO
END IF
LOOP
LPRINT INT(T(9, 1)) - 273
110 DO
Tb = (J - 1) * ts
Tb1 = ABS(L1 / Vc - Tb)
Tb2 = ABS(L2 / Vc - Tb)
Tb3 = ABS(L3 / Vc - Tb)
Tb4 = ABS(L4 / Vc - Tb)
Tb5 = ABS(L5 / Vc - Tb)
Tb6 = ABS(L6 / Vc - Tb)
Tb7 = ABS(L7 / Vc - Tb)
Tb8 = ABS(L8 / Vc - Tb)
IF Tb = 0 OR Tb1 < 5 OR Tb2 < 5 OR Tb3 < 5 OR Tb4 < 5 OR Tb5 < 5 OR Tb6 < 5 OR Tb7 < 5 OR Tb8 < 5 THEN GOTO 120 ELSE GOTO 130
120 LPRINT USING "####"; INT(Tb); SPC(3); INT(T(1, J * 1)) - 273; SPC(2);
I = 1
DO
LPRINT USING "####"; INT(T(I, J - 1)) - 273; SPC(2);
I = I + 1
IF I > (N - 1) THEN EXIT DO
END IF
LOOP
LPRINT USING "####"; INT(T(N, 1)) - 273; SPC(3); INT(T(N, J)) - (273 + 90)
130 J = J + 1
IF J > N THEN EXIT DO
END IF
LOOP
END
Appendix A

RESULTS OF THE PROGRAM (NORMAL WATER RATE)

CASTING PARAMETERS

1. SLAB WIDTH = 1.2 m
2. LIQUIDUS TEMPERATURE = 1789 K
3. TUNDISH TEMPERATURE = 1817 K
4. CASTING SPEED = .015 m/second
5. CASTING TIME = 17.9 min.
6. WATER RATE:
   (a) SPRAY RING = 891/min.
   (b) ZONE-1A = 3801/min.
   (c) ZONE-1B = 2061/min.
   (d) ZONE-2 = 2841/min.
   (e) ZONE-3 = 2061/min.
   (f) ZONE-4 = 1291/min.

NODE TEMPERATURES (CALCULATED AS A FUNCTION OF TIME)

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Forest: Mould
Zone-1A/1B: Zone-2
Zone-3: Zone-4
Withdrawal
Appendix A

RESULTS OF THE PROGRAM (REDUCED WATER RATE)

CASTING PARAMETERS

1. SLAB WIDTH = 1.2 m
2. LIQUIDUS TEMPERATURE = 1789 K
3. TUNDISH TEMPERATURE = 1817 K
4. CASTING SPEED = 0.015 m/second
5. CASTING TIME = 17.9 min.
6. WATER RATE:
   (a) SPRAY RING = 89 l/min.
   (b) ZONE-1A = 380 l/min.
   (c) ZONE-1B = 206 l/min.
   (d) ZONE-2 = 284 l/min.
   (e) ZONE-3 = 183 l/min.
   (f) ZONE-4 = 119 l/min.

NODE TEMPERATURES (CALCULATED AS A FUNCTION OF TIME)

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Appendix B

INDEX OF TRANSVERSE CRACKS AND GRAIN SIZE OF THE CAST STRUCTURES OF STEELS A, B, C AND D HAVING VARIOUS TITANIUM CONTENTS, CAST USING WATER RATES 0.756 l/kg AND 0.736 l/kg.

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<th>Steels</th>
<th>Ti (wt. %)</th>
<th>Water rate (l/kg)</th>
<th>Nr. of slabs</th>
<th>ASTM Nr., (%)</th>
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# Appendix C

## MAIN SPECIFICATION OF CONTINUOUS CASTER

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## Appendix D

### SPECIFICATION OF THE MOULD POWDER

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