MOLD BEHAVIOR, HEAT TRANSFER AND QUALITY OF BILLETS CAST WITH IN-MOLD ELECTROMAGNETIC STIRRING

by

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ABSTRACT

Mold behavior, mold-related quality and the columnar-to-equiaxed transition influenced by in-mold EMS were examined by performing trials at two steel companies, metallurgical examination of the billet samples and mathematical modelling. The thermal fields in the walls of billet molds (102×102mm and 127×178mm) and in the cooling water were monitored by a system of thermocouples as the EMS was switched on and off during the continuous casting of several heats. The effect of electro-magnetic stirring on mold heat extraction was found to be negligible. The mold temperatures and cooling water temperatures are strongly dependent on the mold/billet gap which is affected by dynamic distortion of the mold tube. In the case of the square mold, the time-dependent mold distortion resulted from boiling adjacent to the cold face due to low water velocity and poor water quality. In the rectangular mold, differential expansion of the wide and narrow faces of the mold led to periodic wall movement at the midface causing cycling in the mold and water temperature. Both effects completely dominated any potential influence of EMS on mold heat extraction.

Cooling water velocities measured in separate experiments and the mold temperature profiles were input to a two-dimensional heat-flow model to establish mold heat-flux profiles. A steep taper of 2.6%\%/m in the upper regions of the mold increased heat extraction compared to previously published heat-flux data in 0.8%\%/m tapered-molds. However, due to the periodic wall movement in the rectangular mold, the heat flux declines to lower values periodically.
The calculated heat flux profiles were employed in a one-dimensional transient heat flow model to predict superheat removal from the liquid pool under a variety of assumed fluid flow conditions. The major heat flow effect of EMS was inferred to be one of increasing the convective heat flow at the solidification front leading to earlier superheat extraction from the liquid steel.

Solidification structures in billet samples collected during the trials were examined. The columnar-to-equiaxed transition in continuous casting takes place provided all the superheat is removed from the melt and there is a sufficient density of nuclei present in the pool. At superheats of <20° C in the tundish, high heat extraction in the mold and remelting of the mold generated nuclei facilitate the removal of the superheat well within the mold and the columnar-equiaxed transition is triggered after 10-15 mm of shell growth on both the inside and outside radius faces. At higher superheat in the tundish, the liquid pool leaves the mold with residual superheat which takes longer to remove because of the declining fluid flow. Even though all the superheat is removed lower in the machine, the columnar-equiaxed transition occurs only if dendrite debris generated in the vicinity of the mold has survived in their descent through the superheated liquid. The effect of carbon on the columnar-to-equiaxed transition appears to stem from its influence on facilitating dendrite arm remelting and the survival of the dendrite fragments till the pool reaches sub-liquidus temperature. EMS extracts more superheat by maintaining a steep temperature gradient in the thermal boundary ahead of the solidification front and achieves an earlier columnar-equiaxed transition.

Electro-magnetic stirring appears not to affect either the
average depth or the variation of depth of oscillation marks across a given face. However, the electro-magnetically driven flow dominates the turbulence at the meniscus due to the input stream and stabilizes a meniscus shape with the result that the oscillation marks are also of a well-defined shape unlike the unstirred billets.

No influence of EMS was found on the formation of "hooks" or the fine equiaxed crystal zone near the surface. The influence of EMS on inclining the growing dendrites appears to not come into effect until about 1 mm of shell has formed. It appears that the existence of the momentum boundary layer where the velocity of the rotating steel falls to zero at the surface is the reason for the absence of the influence of EMS on the subsurface solidification.

Rhomboidity and off-corner crack formation were found to depend, as reported by previous researchers, on mold distortion and its dynamic nature. The absence of any effect of EMS on these defects is due to its lack of effect on mold heat transfer and thus mold distortion.
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Electro-magnetic stirring (EMS) in the mold of continuous billet casting machines has emerged as a new technique for the control of solidification structure. The induced fluid flow due to EMS has been proven to give rise to an earlier columnar-equiaxed transition resulting in an enlarged equiaxed zone. In addition to structural refinement, EMS is known to offer improved surface and subsurface quality and to permit higher superheats as well as reduce the number of breakouts. It has been suggested that with the introduction of in-mold EMS technology it is possible to increase casting speed and hence the total throughput. There are numerous publications which consider EMS not merely as a means to modify the progress of solidification but also as a potential tool that could alleviate many of the quality problems and ensure a smooth, efficient casting operation.

The genesis of many of the surface and subsurface defects such as off-corner cracks, deep and uneven oscillation marks and off-squareness is in the mold. Optimising the mold-design variables including mold taper, wall thickness and copper grade as well as operating parameters like cooling water velocity, oscillation stroke/frequency, metal level and lubrication is known to reduce these mold-related defects. Presently, the influence of in-mold EMS on the frequency and severity of these defects is not clear. Understanding the interplay of EMS with mold behavior and resulting billet quality is crucial to the successful application of in-mold EMS technology in continuous-casting machines. Consequently, the primary aim of this study is to investigate the effects of in-mold EMS on mold behavior and mold related quality in continuous billet casting.
Most of the literature concerning EMS consists of industrial reports which advertise a certain process or hardware. In spite of the assurances from these publications, the acceptance of EMS as a standard technique to improve the quality of continuously cast steel has been rather slow, particularly in the North American steel industry. Such reluctance appears to have both economic and technical origins. From an economic standpoint, EMS is expensive to install and operate: the capital cost to equip a four strand billet caster with EMS alone is well in excess of one million dollars. On the technical side, there are many unresolved questions concerning the optimum EMS conditions i.e., number of stirrers, direction of stirring, power input and frequency for a given casting operation. In part, this situation exists because the precise mechanism by which EMS alters the structure is poorly understood: efforts to study EMS-related phenomena from a fundamental viewpoint are rather few. Successful as the application of EMS is projected to be, realising the full potential of this new technology clearly lies in elucidating the basic phenomena responsible for structure modification by this technique. The main focus of this work is to examine the EMS-structure relationship in a mechanistic way.

Structural refinement by EMS has been hitherto believed to result from a combination of two phenomena:

(i) It is suggested that stirring causes the root of secondary and tertiary arms of the columnar dendrites to remelt, thus providing an effective source of nuclei which later form the equiaxed zone.

(ii) With in-mold stirring an increase in the heat transfer in the mold is
reported by some investigators. This was attributed to the fluid flow which causes the hotter liquid to move from the central pool to the solid-liquid interface. This incremental heat transfer in the mold is believed to dissipate the superheat rapidly from the melt. Thus chances for survival of the nuclei generated by the above mechanism are improved and an earlier transition from the columnar mode of growth to an equiaxed or branched columnar mode takes place.

Dendrite arm remelting is known to contribute to equiaxed dendritic solidification in ingot casting, and it has been quoted as a potential source of nuclei in continuous casting wherein the stream from the tundish creates turbulence in the mold and in the upper sprays. It is not clear, however, whether the main influence of EMS on structure is one of creating additional nuclei. Commercial steel contains ample nucleating sites in the form of inclusions and nucleation alone is not a problem per se in achieving the columnar-equiaxed transition.

The argument that the thermal conditions in the liquid pool must be conducive for growth of potential nuclei (which are always present in commercial steel) and that the primary influence of EMS could be the extraction of heat more rapidly from the solidifying steel is quite valid. In support of the argument there is some evidence to indicate that the heat transfer in the mold is altered considerably in the presence of the induced fluid flow due to EMS. Firstly, there are instances where researchers have shown an increase in shell growth with stirring, i.e., shell thickness at the corners of square billets was found to have increased and was comparable with that at the midface. Secondly, an improvement
in the heat transfer to the mold was observed in terms of an increase of 0.25 to 0.5°C in the temperature rise of the cooling water. It is important to examine the changes to heat flux profiles in the mold as a result of EMS more carefully since heat extraction in continuous-casting billet molds is a complex phenomenon and has a significant bearing on billet quality.

Thus the present work was undertaken to examine the mold behavior and mold-related quality as influenced by in-mold EMS and to focus on the mechanism by which structural refinement takes place. The approach taken was to characterise the thermal field in the mold by inserting an array of thermocouples in the mold wall at the same time as billet samples were taken for subsequent examination with and without stirring during a given heat. The temperature data was used in a mathematical model to deduce the heat-flux extracted from the mold. Based on this the influence of electro-magnetic stirring on mold heat extraction was determined and in this respect it is the first study of its kind on EMS technology. In addition, the work involves billet sample analysis to assess the overall influence of in-mold EMS on mold-related quality. Also, the cast structures were evaluated in terms of the length of columnar zone and an attempt was made to define the role of EMS in structural refinement.

Prior to presentation of details of the experiments, results and discussion, it is considered apt and relevant to present a comprehensive and up-to-date overview of the various metallurgical aspects of in-mold EMS. Although stirrers, either conductive or inductive, can be placed in the mold, sub-mold or in the region of final solidification, in billet casting attention has focussed primarily on
in-mold stirring with the liquid pool being rotated about the axis of the mold. In
the literature survey, in Chapter II, notwithstanding the emphasis on in-mold EMS,
wherever necessary, references are made to sub-mold stirring including EMS at the
final stage of solidification. Attempts are made to cover the entire range of benefits
that in-mold EMS is purported to offer including the earlier columnar-equiaxed
transition, reduction in the number of blowholes and pinholes, even distribution and
reduction of inclusion banding, reduction in the severity of center segregation and
porosity, reduction in the number of cracks, reduction in the depth of oscillation
marks, more uniform shell growth, and reduction in the number of breakouts as
well as other features such as inclination of dendrites, changes to dendrite arm
spacing and formation of negative segregation zone. The mechanisms postulated to
explain the columnar-to-equiaxed transition and the reported instances of EMS
affected heat flux changes in the mold are also examined.
The operating principle of an electro-magnetic stirrer relies on creating a rotating magnetic field which induces Foucault currents in the liquid steel. The interaction of the induced current ($\mathbf{J}$) and the magnetic field ($\mathbf{B}$) results in the development of an electromagnetic force ($\mathbf{F} = \mathbf{J} \times \mathbf{B}$) which causes rotation of the metal. Stirrers can be classified into different categories as shown in Fig. 2.1. But in billet casting the most common stirrer is of the inductive, in-mold continuous rotary type. Several patents exist for in-mold EMS; the most publicised, however is the 'MAGNETOGYR' by Rotelec of France and IRSID, which employs low frequency AC inductive stirring.

There has been a notable growth in the application of electromagnetic stirring to continuous casting systems in recent years (Fig. 2.2) and which has sparked numerous reviews of the state-of-the-art of EMS technology. A summary of the influence of in-mold EMS on various aspects of the continuous casting process, viz., solidification structure, subsurface and internal quality, operation of the machine and heat transfer in the mold is presented in this chapter.

2.1 EFFECT OF EMS ON THE EQUIAxed ZONE

The primary reason for introducing EMS systems to continuous casting is to reduce the expanse of columnar dendrites. A large columnar zone enhances the propensity for the formation of internal cracks, increases segregation and leads to structural anisotropy. An extensive equiaxed zone is highly desirable,
especially when the reduction ratios in the ensuing rolling operation of the near-net shape cast strands are small. With the recent emphasis on casting of small cross-section strands where the columnar zone is proportionately longer, there is greater willingness to incorporate EMS in billet casters, especially those producing special bar quality (SBQ) grades.

That induced fluid flow refines the grain structure\textsuperscript{1,2} and that damping the natural convection in the melt promotes the growth of coarse columnar dendrites\textsuperscript{1,3} has been known for a long time. Enhancement of the equiaxed zone was achieved by many investigators by inducing motion in ingot casting.\textsuperscript{14-19} In continuous casting pioneering work was done by Poppmeier et al\textsuperscript{20} who examined the influence of electromagnetic stirring in the mold of a continuous caster and reported substantial refinement in solidification structures. Almost all of the later publications show that enhancing fluid flow by EMS reduces the columnar dendritic zone. However, the extent to which structural benefits are obtained seems to be a complex function of superheat, composition, machine design, stirring time, frequency, mode of stirring and the location of the stirrer. With in-mold stirring in purview, the interplay of these factors on the EMS-influenced structures is discussed here and an overview of the proposed mechanisms to explain the influence of EMS on structure is presented thereafter.

\subsection*{2.1.1 Effect of Superheat}

It is well known that low superheat in steel promotes the formation of an extensive equiaxed zone. It has been shown by several
investigators\textsuperscript{2,1-2,8} that the percentage of the equiaxed zone decreases with higher superheats even with electromagnetic stirring (Fig. 2.3). But Alberny and Birat\textsuperscript{2,9} have observed that in the case of sub-mold stirring the columnar front is stopped once the strand enters the stirring zone. Although the length of the columnar zone increases with superheat, as shown in Fig. 2.4, its upper limit is fixed by the location of the sub-mold stirrer. This leads to the question that if the submold stirrer is placed immediately below the mold, will the columnar zone terminate at the nominal mold-exit shell thickness?

It is of interest to note that Takeuchi et al.,\textsuperscript{3,0,3,1} who studied structural refinement in stainless steel slabs stirred below the mold have observed that there is a "window" of superheat values within which stirring is beneficial and stirring intensity could be varied to increase the percentage of equiaxed zone. Beyond a critical value of superheat, EMS was found not to give rise to any improvement in structure. In billet casting, however, proponents of in-mold stirring\textsuperscript{2,3-2,6} have not reported any upper or lower limits of superheat wherein effects of EMS are dominant.

\subsection*{2.1.2 Effect of Composition}

The improvement of cast structure with EMS is clearly steel grade dependent. The response to electro-magnetic stirring is different in the three carbon ranges: below 0.18, 0.18 to 0.45 and above 0.45 wt\%.\textsuperscript{2,3,2,8} For billet casting, Beitleman et al\textsuperscript{2,2} report that the best results with stirring are obtained for steel grades with carbon content in the range 0.18 to 0.45\%. A smaller
equiaxed zone is persistently present in steels with carbon contents lower than 0.18\%, even with stirring. For grades with carbon greater than 0.45\%, the equiaxed zone is conspicuously smaller even with in-mold stirring.

In the case of bloom casting, Kitamura et al\textsuperscript{32} show that as the carbon content is increased the equiaxed zone increases until a carbon content of 0.20\% is reached as shown in Fig. 2.5. There is a steep decline in the equiaxed zone fraction at 0.52\%C and a steady rise as the carbon content is increased. It appears from this plot that the best results with EMS are obtained in steels with carbon content greater than 0.50\%. A similar result was also reported by Bauer and Wagner\textsuperscript{33} while investigating the influence of EMS on slab structures and by Miyahara et al\textsuperscript{34} for blooms. Contrary to these observations, results from the studies of Mori et al\textsuperscript{27}, Suzuki et al\textsuperscript{35} and Ujhe et al\textsuperscript{36} indicate that an increase in the carbon content of the steel beyond 0.4\% gives rise to a reduction in the equiaxed zone length as shown in Fig. 2.6.\textsuperscript{35}

It is, however, generally agreed that steels with carbon content of 0.4-0.5\% respond poorly to fluid flow and the structures are predominantly columnar. This is attributed to the end of the peritectic reaction at 0.45\%C. It should be pointed out that a sharp increase in columnar zone length was observed at 0.38\%C in unstirred continuously cast sections and Bommaraju et al\textsuperscript{37} attributed this to a possible shift of the end of the peritectic reaction towards lower carbon contents due to the presence of austenite stabilisers like Mn in the steel.

Beyond carbon steel grades, Beitleman et al\textsuperscript{22} have found that
resulphurised grades (0.06-0.13%S) exhibit smaller equiaxed zones even with the in-mold EMS. Higher solute content in an alloy is known to give rise to an earlier transition to equiaxed structure. It is not clear as to why the addition of sulphur to steel was not effective. However, Longnecker et al.\textsuperscript{38} at Lukens Steel Company pointed out that increasing alloy content in the steel facilitates an earlier columnar-to-equiaxed transition when stirring is conducted below the slab mold. Takeuchi et al.\textsuperscript{30,31} have also found that in stainless steel slabs cast with submold stirring, the addition of Ti gives rise to an extensive equiaxed zone. It appears clear that optimal stirring parameters would be different for each specific steel grade and the superheat at which it is cast.

### 2.1.3 Effect of Stirring Time

Regarding the duration of stirring necessary to achieve an acceptable structure, there is very little information available. Moore\textsuperscript{39} suggests that 14s of stirring time or a stirring length of 50 to 60cms is sufficient for arresting columnar growth and establishing the equiaxed zone. Kor,\textsuperscript{40} while examining the influence of circumferential stirring on steel ingots, reports that a relatively "short" stirring time at the very beginning of solidification is sufficient to bring about the development of an equiaxed structure early in the solidification process. Increasing the residence time of the strand through the stirrer by reducing the casting speed is definitely more advantageous in creating a larger equiaxed zone\textsuperscript{26}; Kanno et al.\textsuperscript{25} have shown that for steels with 0.6-0.8%C the percentage of equiaxed crystal zone steadily drops with increasing casting speed as shown in Fig. 2.7.
Criteria for the selection of optimum stirrer length and stirring time appear to be quite arbitrary in the design of EMS units. The earlier versions of in-mold stirrers designed by J. Mulcahy Enterprises (formerly Ferrco Engineering) are placed about 30cms below the meniscus level. But presently these stirrers as well as the Rotelec stirrers span the entire mold length and produce vigorous stirring action at the meniscus.

2.1.4 Effect of Stirring Parameters

Beitleman et al\(^2\) have shown that stirring at frequencies below 4Hz and above 10Hz are not as effective as those within this range. Figure 2.8 shows that the equiaxed zone increases slightly with the frequency up to 10Hz. This result is attributed to the increase in rotational speed caused by the higher frequency which leads to better stirring despite the accompanying reduction in the stirring torque.

The effect of stirring power itself (expressed usually as the stirring current in amperes), seems to have a strong influence on the percentage of equiaxed zone. Though there are suggestions that weak electromagnetic power is sufficient to trigger the columnar to equiaxed transition, many investigators, eg., Hagiwara et al\(^4\) employing a rotating permanent magnet around the mold, found that as the stirring intensity is increased, the size of the equiaxed crystal zone increases as can be seen in Fig. 2.9. Morikawa et al\(^3\) have observed, in their study of linear stirring in experimental molds, that higher velocity of steel not only resulted in an increased equiaxed zone width, but also in a reduction in the size of...
the equiaxed crystals. In their study of sub-mold stirring, Suzuki et al., observed that too high a stirring power, in fact, tends to decrease the equiaxed zone. They attributed this to a possibility of hot steel being drawn to the bottom of the pool by strong stirring torque.

It appears that presently the selection of the optimum power input and frequency is made by a procedure of trial and error. Evidently the optimisation has to be done for each specific grade taking into consideration the effects of superheat, casting speed etc., until a sound theoretical base is developed.

### 2.1.5 Effect of Mode of Stirring

It is not clear whether all modes of stirring give rise to comparable structural benefits. While rotary stirring has always been shown to extend the equiaxed zone in billet and bloom casting, in slab casting Yamahiro et al. observed no improvement at all in 0.01-0.08% carbon steels. It is not clear if this is due to the grade of the steel or the superheats employed. Birat and Chone also report that linear stirring, as experimentally developed by IRSID in Allevard and Dillinger, did not increase the size of the equiaxed zone. But Marr et al. of the British Steel Corporation as well as Maede et al. of Nippon Steel claim that linear stirring in the mold is suitable for slab casting. In billet casting, however, rotary stirrers are very popular and are known to give some of the desired structural benefits.
2.1.6 Effect of Location of the Stirrer

Little is known about the most desirable location for the stirrer. Whether the stirrer should be placed at the meniscus, or at some distance below it, is unclear. Irrespective of the location, in-mold stirrers appear to have a definite edge over the sub-mold units. Besides having no effect on the subsurface and surface cleanliness, stirrers placed in the secondary cooling zone have been found to increase the equiaxed zone only on the outer radius with columnar crystals reaching the center of the billet on the inner side in curved mold casters.\(^4\)\(^7\) Suzuki et al\(^3\)\(^5\) have also shown that with submold stirring the equiaxed zone was 7 to 20% longer on the outer than the inner radius. Stirring in the mold, on the contrary, has been found to produce an extensive equiaxed zone on the inside as well as on the outside radius.\(^3\)\(^5\)\(^4\)\(^6\)

2.1.7 Mechanism of Structural Refinement by EMS

There have been several attempts to explain the strong influence of electro-magnetic stirring on cast structure. Johnston and co-workers\(^{14,15}\) suggested that electro-magnetic stirring plays a strong role in crystal fragmentation to generate pronounced equiaxed crystal growth in undercooled melts of Sn-Pb alloys. Reiterated by O'hara and Tiller,\(^{18,19}\) crystal multiplication is now believed by many as the primary influence of electro-magnetic stirring.\(^8\)\(^-\)\(^10,\)\(^6\)\(^0,\)\(^4\)\(^8\)\(^-\)\(^5\)\(^2\) Crystal multiplication occurs in two ways, first of all by remelting of the root of the dendrite arms and secondly by mechanical fragmentation due to fluid flow. Tzavaras\(^4\)\(^8,\)\(^5\)\(^5\) suggests that the dendrite fragmentation is quickly accomplished as
the flow velocities are raised beyond 25cm/s.

In addition to creating nuclei to form the central equiaxed zone, EMS is also believed to facilitate their survival by changing the thermal conditions in the center of the pool. Convective flow is believed to establish shallow temperature gradients at the solidification front so that nuclei could survive to grow in the presence of columnar dendrites. This would not be possible unless the enthalpy of the liquid is reduced through one of the following mechanisms suggested in the literature.

(i) Tzavaras postulated that as the fluid velocity is increased hotter steel is brought to the solid-liquid interface and could reduce the growth rate of the shell. A reduction in the shell thickness i.e., the conduction path permits more heat to be withdrawn from the system. Jacobi and Wünnenberg have observed greater heat transfer through the mold in the presence of fluid flow and related it to the faster dissipation of superheat in the melt.

(ii) Takeuchi et al proposed that in the presence of fluid flow segregated molten steel at the solidification front is replaced by molten steel of higher liquidus temperature from the bulk. The central pool is constitutionally supercooled and gives rise to the nucleation of and growth of equiaxed crystals as well as to the survival of dendrite segments.

It must be emphasized, however, that these mechanisms are purely conceptual. There is hardly any experimental evidence to support that
fragmentation, mechanical or thermal, of dendrites as the primary influence of EMS. Thermal conditions in stirred melts have never been monitored, although there have been several experiments revealing enhancement of heat transfer to the mold with the introduction of fluid flow. Section 2.4 discusses this aspect of EMS.

2.2 EFFECT OF EMS ON THE COLUMNAR DENDRITES

In addition to a shortening of the columnar zone, the influence of EMS is also reflected in changes to the direction of growth of dendrites and spacing between the dendrite arms.

2.2.1 Inclination of Columnar Dendrites

Electromagnetic stirring causes the columnar crystals to grow inclined opposite to the direction of fluid flow.\textsuperscript{20,40,44,45,52-58} Jacobi et al.\textsuperscript{53} suggest that the thickness of the skin layer where the columnar dendrites show an inclination is an approximate indication of the zone of action of the stirrer. Poppmeirer et al.\textsuperscript{20} suggest that the observed shortening of the length of the columnar zone could be partially due to bent dendrites.

Takahashi\textsuperscript{56} found that the angle of inclination (\(\Theta\)) of the dendrite increases with the increasing flow velocity (U) and decreasing solidification
rate (V). The relation is shown by,

\[ \theta = 22.49 \times 1.77 \times 10^{-1} \log \left( \frac{3.72 \times 10^{-3} u^{2.08}}{v} \right) \]

Figure 2.10 shows a plot of the angle of inclination vs. the solidification rate for different melt velocities. Although several industrial reports support Takahashi's findings, Fredriksson et al. in a recent paper have shown in Al-Cu alloys, the angle of inclination was independent of the flow rate, but showed an increase at low solidification rates. Increasing Cu-content was also found to increase the angle of inclination. But in the high carbon steels that angle of inclination is smaller and Chone et al. attributed this to a larger mushy zone which retards the fluid rotation. It should also be pointed out that not all dendrites are inclined at the same angle; Komatsu et al. have reported that the angle of inclination of the dendrites influenced by fluid flow from the first stage of nucleation was larger than that of dendrites which are influenced during subsequent growth as can be seen in Fig. 2.11.

### 2.2.2 Dendrite Arm Spacings

It is well known that in continuously cast structures, the primary dendrite arm spacing increases from the outer edge toward the center. The spacing between secondary arms shows an initial increase, but at some distance close to the center begins to decrease. Poppmeier et al. report that when fluid flow is enhanced by EMS the primary as well as secondary dendrite arm spacing is finer. Similar observations were made by Takahashi, Kitagawa et al. and
Fredriksson et al.\textsuperscript{58} Contrary to these results, recent investigations by Yamahiro et al.\textsuperscript{44} (Fig. 2.12) show that the primary as well secondary dendrite arm spacings tend to increase as the molten steel flows at higher velocity, and the differences between stirred and unstirred become small where the flow region ends suggesting that, with stirring, there is a drop in the solidification rate. It is interesting to note, as referenced earlier, that in their studies stirring did not give rise to any improvement in the size of equiaxed zone. It will be pointed out later that in this study an increase in heat transfer to the mold was observed.

Marr et al.\textsuperscript{45} of BSC have demonstrated that in aluminium-killed low-carbon steels, lower superheats resulted in reduced spacing and a finer dendritic structure indicative of higher solidification rates with stirring. However, with high superheat the opposite was true: the effect of stirring was to coarsen the dendritic structure. They also pointed out that in stirred Si-killed steels the dendritic spacing is not much different from the unstirred heats even in the low superheat casts. Kitagawa et al.\textsuperscript{60} have observed that the higher the carbon content of the steel, the larger the dendrite arm spacing.

Considerably more could be learned about solidification conditions in a stirred melt from a study of the dendrite arm spacings. Considering the inconsistency of the results from different researchers more work is needed in this area.
2.3 EFFECT OF EMS ON SHELL GROWTH

It emerges clearly from the EMS literature that, with the introduction of fluid flow, the progress of shell growth is noticeably modified. Alberny et al\textsuperscript{1} were the first to point out that under the influence of rotary stirring, the non-uniformity of the shell growth is reduced in the casting of rounds. Similar observations were made by Yamahiro et al\textsuperscript{8,4} in slab casting and Ayata et al\textsuperscript{6,1} in bloom casting.

Besides increasing the uniformity of shell growth, EMS is known also to reduce the shell thickness\textsuperscript{5,4}. While the shell thickness was seen to decrease at the center of the wideface (see Fig. 2.13), Ayata et al\textsuperscript{6,1} observed that the corner shell thickness increased as shown in Fig. 2.14. Yamahiro et al\textsuperscript{8,4} found that the shell thickness of stirred slabs is smaller than for the non-stirred. This observation, they suggest, is consistent with the reduced solidification rates inferred from the dendrite arm spacings.

2.4 INFLUENCE OF EMS ON HEAT TRANSFER IN THE MOLD

As mentioned earlier, one of the basic requirements for enlargement of the equiaxed zone is to facilitate the survival of nuclei by changing the thermal environment in the liquid core. Tzavaras\textsuperscript{8,9} pointed out that with induced fluid flow, hotter steel is brought to the solid-liquid interface. This could reduce the growth rate of the shell and increase the net heat flow through the thinner solid shell. Results from several experiments in slab and bloom...
casters indicate that the shell thickness at the midface of a given transverse section exhibits a reduction in size. Fluid flow induced through a rotating mold and with the use of a rotating submerged nozzle have been shown to give rise to increased heat withdrawal from the mold. Jacobi and Wünnenberg have measured heat fluxes in a ceramic mold fitted with a Cu-chill plate as one of the walls. With the use of stirrers, they found better heat transfer to the chill plate. Based on these results they have suggested that EMS enhances the heat extraction from the mold and thus lowers the superheat in the steel.

There have been several instances where the total heat removal from the mold has been monitored based on the difference in the temperature of inlet and outlet cooling water. In thin-walled bloom molds (275mm square), with molten flux lubrication, Yamahiro et al have reported a 10% increase in the mold heat flux compared to conventional thick-walled molds and when fluid flow was introduced by electro-magnetic stirring a steady rise in mold heat transfer was observed as the flow velocity of the steel was increased, Fig. 2.15. At 0.8 m/s, a 10% increase in the amount of heat extracted was registered and attributed to a reduction in the boundary layer thickness for heat conduction in molten steel. It is interesting, however, that there was no structural improvement in the 0.01-0.08% carbon steels cast in these molds. Marr et al have noticed a detectable rise in heat transfer in their experimental 150mm square molds with a copper wall thickness of 42mm. When oil lubrication was employed, the water temperature difference showed an increase of 0.5°C over a normal level of 8°C which was found to be equivalent to 40KW or a 6% increase. However, with powder lubrication, there was no measurable difference and this was attributed to variations in metal
level in the mold.\textsuperscript{45}

Alberny et al,\textsuperscript{1} in their first report on the IRSID magneto-rotative-continuous-casting process, reported a water temperature increase of 0.25°C with EMS in 135mm round billets equivalent to a 2.5% increase in mold heat flux. A recent paper by Sakamoto et al\textsuperscript{65} of Nakayama Steel Works also points out, as shown in Fig. 2.16, considerable increase in the overall mold heat transfer in steels with 0.01% and 0.05% carbon. These results were obtained while casting 135mm square billets in a 12mm thick, 800mm long mold equipped with a Rotelec stirrer.

One of the problems in monitoring the mold heat transfer is to account for the heat generated by the stirring coils which is added to the cooling water temperature if a separate cooling system is not in place for the coil. Alberny et al\textsuperscript{1} have maintained that half the rise in the cooling water temperature difference results from the heat generated in the coils while the other half was attributed to increased heat transfer between the steel and the mold. The heating effect of the inductors was assessed calorimetrically on an empty mold by Marr et al\textsuperscript{45} who found that in coil windings amounted to just over 55KW per inductor while the extra load on the mold cooling water, due to the eddy current induction heating of the copper walls, amounted to less than 7KW. The latter amounted to little over 1% of the normal heat removal of 660KW by the mold cooling water during casting.

The increase in heat extraction from the mold, as observed in
the rise in the cooling water temperature in molds equipped with EMS, is supposedly attained in two ways: firstly fluid flow brings the hot liquid closer to the solid-liquid interface and secondly, by reducing the growth rate of the solidifying shell, EMS facilitates heat conduction through it. The latter would have been particularly true if the rate controlling factor in heat extraction is heat conduction through the solid shell. However, as is well known, it is the air gap width that is crucial in transferring heat from the strand to the mold and thus there is reason to believe that the air gaps too must be lower at the stirred billet-mold interface.

2.5 EFFECT OF EMS ON QUALITY

There have been a number of publications which suggest in-mold EMS leads to improved quality of the cast strand. Following is a synopsis of the metallurgical benefits that in-mold stirrers have been shown to effect.

2.5.1 Blowholes and Pinholes

Numerous studies have shown that the number of pinholes and blowholes from the surface/subsurface of the strand is reduced when in-mold stirring is employed and consequently surface defects of the rolled bar are noticeably diminished as can be seen from Fig. 2.17 and Fig. 2.18.1,3,6,7,8-10 This aspect of EMS has facilitated casting of low deoxidised steels or the so-called pseudo-rimmed steels. Fluid flow at the solidification front is believed to prevent nucleation of bubbles and to aid physical removal of growing bubbles.9 Kitamura et al7,2 have shown that stronger stirring virtually eliminates blowholes,
while with even mild stirring, the region of blowhole formation shifts to a higher oxygen content. The difference in the critical oxygen content to prevent blowhole formation, with and without mold-EMS, is reported to be 20-25ppm.

2.5.2 Inclusions and Slag Entrapment

Centripetal forces acting on inclusions in rotary stirring draws them to the center of the pool and causes them to rise to the paraboloid surface of the melt as shown in Fig. 2.19. Electro-magnetic stirring in the mold was, thus, reported by many authors to result in a substantial decrease in subsurface inclusions. Alberny et al claim that accumulation of non-metallic inclusions toward the inside radius of billets cast on a 9.5m radius machine has also been eliminated with the introduction of their 'Magnetogyr'. Birat and Chone point out that EMS may not be effective in preventing such accumulation in machines with smaller radii. Kor suggests that the position of inclusions with diameter less that 0.1mm would depend on conditions such as angular frequency, magnetic field strength and casting speed which determines the average stirring time.

Widdowson and Marr have observed that, with open pouring, inclusions are entrained in the solidification product. Though a reduction of non-metallics was observed in the stirred zone, it was accompanied by positive segregation at the termination of stirring. A recent study of inclusion distribution in 100mm square billets by Currey and Pickles has shown that, with in-mold EMS, the number of inclusions in the central region of the billet can increase by
about 28%. While Widdowson and Marr\textsuperscript{8,5} have suggested the use of flux powder, Alberny et al\textsuperscript{1} have incorporated off-center teeming with periodic scooping of the slag pool at the top, to reduce the entrapment of inclusions in the steel.

In addition to inclusions, slag entrapment in the outer skin has also been reported to decrease with the use of electro-magnetic stirrers in the molds of continuous casters.\textsuperscript{1,4,6,8,1}

2.5.3 Oscillation Marks

Among the changes to surface and subsurface characteristics that are brought about by in-mold EMS, the influence on oscillation marks is substantial. Welburn et al\textsuperscript{6,8} have noticed that when in-mold EMS is applied to square billets, the shape of the oscillation marks changes in a characteristic manner which reflects the shape of the meniscus in the mold (Fig. 2.20). Higher power stirring was found to break down the reciprocation pattern. Other reports\textsuperscript{2,0,8,5} insist that non-optimum stirring conditions and excessive stirring can lead to an increase in the depth of oscillation marks. Yamahiro et al\textsuperscript{4,5} have observed, in bloom casting with flux powder, that the depth of oscillation marks decreases with flow velocity (Fig. 2.21). In billet casting, where the lubrication is by rape-seed oil, Sakamoto et al\textsuperscript{5} report that the depth of the oscillation marks decreased with increasing stirring power as shown in Fig. 2.22. They attributed this observation to liquid steel being forced against the mold wall at the meniscus.
2.5.4 Negative Segregation

The negative segregation zone, an area depleted in solute elements, is a characteristic feature of submold stirring, although there is evidence that in-mold stirring also produces it. Negative segregation is not apparent as a 'white band' in sulphur prints and macro-etches even though chemical analysis reveals its presence (Fig. 2.23). Jacobi et al. have shown that the negative segregation is limited to the area of inclined dendrites of the stirred zone. Yamahiro et al. did not record any solute depletion 1-2mm below the slab surface (possibly because of high solidification rates), but noted the presence of a large negative segregation zone 5 to 15mm below the slab surface.

The factors that influence the extent and severity of the negative segregation zone appear to be flow velocity and the carbon content. Tsunoi et al. have shown in their tests on an experimental mold, that the 'white band' appears when flow velocities exceed 0.35-0.36 m/s at the solidification front. Negative segregation was found to increase with flow velocity and with decreasing carbon content. The extent of negative segregation or the "washed depth" seems to be more influenced by fluid flow for low-carbon steels than for high-carbon steels. It is suggested that the percentage of carbon in steel determines the width of the mushy zone and when the mushy zone is short (eg., low carbon steels) washing of the interdendritic liquid by the flowing steel is more thoroughly and readily accomplished resulting in increased degree of negative segregation.
Clearly, the most widely quoted mechanism for white band formation is the solute washing mechanism wherein the fluid flow penetrates the dendrite mesh to sweep out enriched interdendritic liquid and disperses the solute rapidly through the remaining liquid. However, this mechanism fails to explain the presence of a solute enriched zone corresponding to the end of stirring and with strong white band formation. Kollberg argued that the acceleration and deceleration of the liquid ahead of the solidification front causes the negative segregation zone. Kor has proposed a mechanism based on changes to growth rate at the start and end of stirring, while Bridge and Rogers have shown that white band formation can occur because of the changes to relative velocities of the liquidus and solidus isotherms in accordance with general macrosegregation theories.

There has been considerable interest of researchers in the topic of negative segregation notwithstanding the lack of evidence indicating that negative segregation produces a deterioration in mechanical properties of the rolled product. The visual appearance of the segregation as a lightly etched area in an otherwise deeply etched section has not only given rise to its name, but is probably also the most undesirable feature of the white band. Its continued presence after hot working can be a deterrent to customer acceptance, mostly on cosmetic grounds.

It appears that although a complete elimination of a zone depleted in solute content is never accomplished, the extent of negative segregation can, nevertheless, be reduced by stirring at the lowest magnetic field strength that would bring about the much desired equiaxed zone early in the solidification process. This is clearly not a problem with in-mold stirring, where the white band does not
appear 'white' in macro etches.

2.5.5 Positive Segregation

The extent of positive segregation along the center-line is much higher than the negative segregation. Extensive segregation, confined to a narrow central region in continuously cast strands can be highly detrimental to product quality. Severe macrosegregation is often associated with pronounced columnar dendrite structures and all efforts to promote equiaxed crystal solidification have been found to be fruitful in limiting the spread and intensity of macrosegregation at the centerline. In a recent study by Nakayama Steel Works the improvement in the center segregation of small cross-section billets (135mm square) was found to be minimal and was less than the extent of negative segregation found in the stirred region of the billets. It is well known, however, that the beneficial effect of in-mold EMS on center-line segregation, if any, is limited to steels with carbon less than 0.5 wt%. Above this carbon level, it has been observed that center-line segregation, in the form of V-segregates and continuous positive segregation along the center-line, tends to appear irrespective of the solidification structure. In high-carbon steels, in-mold stirring has been found to be highly inadequate to suppress center-line segregation. Sub-mold stirring at one or several locations, if supplemented with in-mold stirring, has been found to curb center-line segregation to a great extent even in high-carbon steels. The latest 'KOSMOSTIR-MAGNETOGYR' process developed by Kobe Steel employs multi-stage stirring and claims excellent results with regard to center segregation.
2.5.6 Central Unsoundness

Induced fluid flow is believed to prevent dendrite bridging thereby facilitating liquid feeding to accommodate solidification shrinkage and reduce the central unsoundness of the product. With in-mold stirring, extensive center-line cavities have been transformed into a more acceptable dispersed porosity. The packing of crystals at the final stage of solidification was observed to be greatly modified with multi-stage stirring.\textsuperscript{,7,100}

2.5.7 Internal Cracking

Rotary horizontal stirring in the mold is also suggested to be an effective means for preventing subsurface and internal cracks.\textsuperscript{,2,26,65} Adachi et al\textsuperscript{26} have shown that the index of subsurface cracks substantially decreases with increasing speed of a rotating permanent magnet (Fig. 2.24). It has been suggested that the finer dendritic arm spacing obtained via stirring lowers the susceptibility to cracking. Sakamoto et al\textsuperscript{65} and Nishi et al\textsuperscript{67} of the Nakayama Steel Works have observed, as depicted in Fig. 2.25, a reduction in the "index" of longitudinal corner cracks in billets with the introduction of Rotelec stirrers in their molds and have attributed it to enhanced uniformity of the solid shell thickness in the mold by stirring. Bastian,\textsuperscript{101} however, suggests that when in-mold stirring is employed with powder flux lubrication there is a potential risk of increasing the longitudinal cracks if the stirrer power is not suited to the casting speed and mold powder characteristics. Simcoe\textsuperscript{102} reports that halfway cracks in the 127×172mm sections at Chaparral Steel were eliminated with the use of in-mold electromagnetic
2.6 INFLUENCE OF EMS ON OPERATION OF BILLET CASTERS

In addition to refining the structure and improving the quality of the cast product, in-mold EMS is known to allow greater flexibility in the operation of the continuous caster under less than optimum conditions such as excessive superheat. Some steel companies employ high superheat in combination with EMS as a standard practice because the hotter steel facilitates the float-out of inclusions\(^8\) and reduces the risk of nozzle blockage.\(^{103}\)

Tzavaras\(^7\) suggests a possible shortening of the liquid pool with stirring, which could be translated into increased casting speed and productivity. Gibbons and Rawson\(^{103}\) at the BSC Rotherham Works claim that higher casting speeds were achieved when they employed the Rotelec stirrers in the mold and report that "without mold EMS, the performance achieved (on the caster) would be adversely affected!"

That in-mold EMS reduces the frequency of breakouts and guarantees a smoother operation is well documented in the literature.\(^5,100,102\) Figure.2.26 shows the influence of EMS on the 'index' of break-out frequency. This supports Simcoe's\(^{102}\) statement that at a high production shop like Chaparral Steel, EMS is one way of improving quality without sacrificing production.
2.7 CONCLUSIONS

The overall picture of the in-mold EMS technology is one of considerable promise to the growing mini-mill industry. The benefits from incorporating EMS in the mold are many and cover almost all facets of production and quality of continuously cast steel. They can be summarised as follows:

1. The most dominant influence of EMS is to enlarge the equiaxed zone with concomitant improvement in internal quality as manifested in the reduction of
   i) frequency of subsurface and midway cracks
   ii) severity of center-line segregation
   iii) extent of center-line porosity

2. In-mold stirring offers better surface quality in terms of shallower oscillation marks.

3. EMS in the mold, as opposed to sub-mold stirring, offers a reduction in the number of pinholes and blowholes and facilitates the casting of low-deoxidised steel grades.

4. Inclusion bands that appear adjacent to the inside curved wall of billets cast on a curved mold machine are dispersed.

5. From an operator standpoint, EMS allows casting at high superheats, which in turn facilitates float-out of inclusions and reduces the risk of nozzle blockage. Also the risk of breakouts is reduced and it is possible to increase the casting speed and achieve a higher level of productivity.

Based on the summary of the numerous reports of the
performance of in-mold EMS it is clear that this technology has immense potential. But the application of in-mold EMS system in the North American steel industry has been neither widespread nor altogether successful. There are instances when the advantages claimed with the use of EMS have not been fully realized in test trials and the application of EMS was discontinued. Considering the number of factors that influence the EMS-structure relationship, consistent improvement in quality can be assured to the customer only when these variables are suitably selected for each different grade, size and shape cast on a given continuous casting machine. It is important to establish "windows" for casting variables such as superheat and casting speed and EMS parameters like the frequency and power input.

There have been few, if any, attempts to arrive at such guidelines for the continuous casting process equipped with one or the other brand of in-mold EMS unit. Both from the supplier and the user point of view, hitherto, the emphasis has been to assert the possible benefits of this new technology. The application of EMS to operating billet mold systems has been quite empirical and consequently the EMS technology has not secured a firm place in continuous casting. Optimization of the EMS technology must begin with an effort to understand its role in the solidification process and the interplay of the casting variables on it. There is a large incentive to conduct research to understand the effects of EMS from a fundamental viewpoint of heat transfer, solidification and fluid flow.
Figure 2.1. Classification of electro magnetic stirrers based on location, direction of motion, type of magnetic field and type of motion.
Total number of EMS units.

Number of billet EMS units.

Figure 2.2. World-wide growth in the application of EMS.
Figure 2.3. Effect of superheat on the ratio of equiaxed crystal area in 280×350mm blooms subjected to sub-mold stirring at 5m and 10m below the meniscus.$^{2,5}$
Figure 2.4. Effect of superheat (measured in the tundish) on the length of columnar zone in sub-mold stirring. 'a' is the half width of the bloom and 'b' is the shell thickness corresponding to the stirrer. Plot on the left: without stirring. Plot on the right: with stirring.
Figure 2.5. Effect of carbon content on the equiaxed zone fraction in 380×550mm blooms cast with and without stirring. The equiaxed zone is measured on the inside curved radius and the superheats are between 25 and 35°C.³
Figure 2.6. Effect of carbon content on the ratio of the equiaxed zone in 247×300mm blooms stirred below the mold.\textsuperscript{35}
Figure 2.7. Effect of casting speed on the ratio of the equiaxed crystal area in 280×350mm blooms subjected to sub-mold stirring at 5m and 10m below the meniscus.\textsuperscript{25}
Figure 2.8. Influence of the frequency of the in-mold EMS on the equiaxed zone width in 133×133mm billets.22
Figure 2.9. Influence of the stirring intensity of the rotating in-mold stirrer on the ratio of equiaxed crystal zone of blooms.
Casting speed = 1.6 m/min; Superheat = 30-50°C
Figure 2.10. Relation between the inclination angle of the dendrite (θ) and the solidification rate (V) for different flow velocities of the bulk liquid (U)."
Figure 2.11. Relation between flow velocity of molten steel and the angle of inclination of the dendrites. The steel was cast in an experimental mold (1m long) with a rotary stirrer fitted at mid-height.
Figure 2.12. Effect of in-mold EMS on the dendrite arm spacing in 250mm × 250mm blooms of 0.01-0.08%C steels cast at 0.6 to 1.1 m/min speed cast in an experimental 'thin walled' mold.
Figure 2.13. Influence of in-mold stirring on the growth of solids shell in 300×400mm blooms cast at 0.45 m/min.
Shell thickness was measured in three low carbon heats by adding sulphur into the 900mm long mold.⁶¹
Figure 2.14. Effect of in-mold stirring on the shell growth of unstirred (left picture) and stirred (right picture) blooms (300×400mm). Shell growth was delineated by sulphur addition in the mold.
Figure 2.15. Relationship between the velocity of molten steel (as a result of in-mold EMS) on the mold heat extraction in experimental thin walled molds casting 250mm square blooms of 0.01-0.08%C at 0.6 to 1.1m/min.
Figure 2.16. Effect of in-mold EMS on the mold heat flux based on the rise in the cooling water temperature. Length of mold = 800mm : Mold wall thickness = 12mm Mold taper = 0.8mm/800mm : Casting Speed = 2.0m/min Superheat = 20-45°C
Figure 2.17. Influence of rotating permanent magnet (in-mold) stirrer on the number of pinholes in 185×800 blooms of AISI 9260 grade steel.
Figure 2.18. Effect of in-mold permanent rotating magnet type stirrer on improvement in structures of low-deoxidised steels. C = 0.05% Si = 0.002% Mn = 0.26% Al = Trace
Superheat = 18°C Rotation speed = 230 RPM Casting speed = 1.7 m/min
Figure 2.19. Schematic description of the action of centrepetal forces in aiding floating up of inclusions.¹
Figure 2.20. Effect of in-mold EMS on the appearance of oscillation marks.

a. 140mm square billet - unstirred
b. 140mm square billet - stirred
c. 115mm square billet - stirred at high power
Figure 2.21. Effect of in-mold EMS on the depth of oscillation marks in experimental thin walled molds casting 250mm square blooms of 0.01-0.08%C at 0.6-1.1 m/min and lubricated by mold flux. 

![Graph showing the depth of oscillation marks versus velocity of molten steel.](image)
Figure 2.22. Influence of in-mold EMS on the depth and variation in the depth of oscillation marks on 135mm square billets cast in an 800mm long mold with a 12mm wall thickness and 0.8% per meter taper with rapeseed oil lubrication.
Figure 2.23. Negative segregation of carbon in the surface layer of 250mm square blooms of 0.01 to 0.08% C subjected to in-mold EMS.
Figure 2.24. Effect of increasing the stirring speed of the in-mold EMS unit (rotating permanent magnet type) on the index of subsurface cracks in 300×400mm blooms of AISI 5155 grade.²⁶
Figure 2.25. Influence of in-mold EMS on prevention of longitudinal corner cracking in 135mm square billets cast in 12mm thick, 800mm long, 0.8%/m taper molds with rapeseed oil lubrication.
Figure 2.26. Effect of the in-mold EMS on the reduction of break-out frequency index.⁶⁷
3. SCOPE OF THE PRESENT WORK

It is clear from the literature review that EMS technology is purported to be a panacea for several quality problems. However, producers of continuously cast steel often have been disappointed when exaggerated claims were not met and the high cost of installation was not justified in terms of the resulting improvement in product quality and saleability. The application of EMS has been quite empirical and there are no clear guidelines regarding optimum values for the casting variables and EMS parameters that give rise to consistent quality improvements in each grade of steel. In part, this situation exists because of the lack of understanding of the control of the solidification process by EMS. In order to understand, implement and properly execute in-mold EMS technology in billet casting, the first step is to clarify the mechanism of structure modification in the presence of fluid flow from a fundamental standpoint. The interplay of the various casting variables and EMS parameters on the EMS-structure relationship also must be addressed.

Mechanistically, in-mold EMS is believed to cause crystal multiplication on the one hand and facilitate the survival of those crystals on the other. While the crystal multiplication could be because of either one or a combination of melting-off and breaking-off of dendrite arms, the nuclei are believed to be preserved in a suitable thermal environment created and maintained by the stirrer. It is important to determine how the enthalpy of the melt is reduced with in-mold stirring. In view of this, the extent of improvement in the heat transfer from the liquid pool to the mold needs to be assessed. Hence, the subject of this
research work is to characterise the thermal fields in the mold accurately both in the presence and the absence of EMS induced flow. With such a rigorous thermal analysis, the changes to heat extraction from the steel can be focussed on in order to shed light on the role of heat transfer in structure modification.

The present status of the conventional billet casting industry is possibly a deterrent for successful introduction of stirring technology. Conventional billet casting itself is plagued with many mold-related problems like off-squareness, longitudinal and off-corner cracking and the formation of deep oscillation marks. It is well known\textsuperscript{37,105-107} that mold-related billet quality is most profoundly influenced by the thermo-mechanical behavior of and heat extraction in the mold which is, in turn, influenced by steel composition, superheat and mold cooling water velocity amongst other variables. Based on the mold/shell interaction at the meniscus, optimum mold design and operating conditions have been formulated.\textsuperscript{108} The billet industry has yet to grasp and implement many of the recommended design/operating features and it is not known if the introduction of EMS in the existing poorly designed and operated molds could alleviate the problems inherent within the system. Studies based on measuring the overall heat-flux in molds fitted with electro-magnetic stirrer have shown increased heat extraction and more even shell growth. These heat-flux calculations are based on the temperature difference between the inlet and outlet water and do not shed any light on the actual distribution of heat-flux over the face of the mold, the thermal fields in the mold wall, its mechanical response and the resultant changes to the shell growth. Also, there is no information on the needs of the mold system in light of the new thermal load and shell formation associated with EMS. This may be one of the
reasons why the full advantage of in-mold EMS has not been realized, particularly when so many mold systems are operating much below the standards set by researchers. Thus, in some instances EMS technology is being discarded because the improvements to quality and productivity do not justify the cost of installing and operating a stirrer. In view of the current status of the application of the in-mold EMS technology to non-optimum mold systems of the billet casters, it is important to assess the impact of EMS on mold behavior and mold-related quality.

3.1 OBJECTIVES

The two main goals of this dissertation are to determine the changes to mold heat transfer in the presence of EMS and to examine the effect of in-mold EMS on mold-related quality. These aims are not mutually exclusive, but form a unified and coherent approach to resolve the overall influence of stirring on the casting process. In striving towards this, the following objectives were set out:

1. To monitor the mold heat transfer and at the same time to collect billet samples for subsequent examination to represent both the stirred and unstirred casting process.
2. To examine the billet samples metallographically to assess the billet quality.
3. To examine the thermal fields in the mold and the metallurgical features of the billets cast under conditions of stirring vis-a-vis no stirring.
4. To distinguish the phenomena inherent to the casting process from those manifest by the stirrer.
5. To analyze the changes to heat transfer in the presence of EMS together with its impact on mold behavior. This will help to understand the mold-shell behavior under the influence of fluid flow, and aid in optimizing the mold system to obtain the best results with EMS.

6. In light of the knowledge of the changes to heat-flux profiles in the mold and from careful examination of the structures cast within the mold walls, to delineate the relative importance of crystal multiplication or suitable thermal fields to generate the structural changes.

3.2 METHODOLOGY

The methodology employed essentially consisted of industrial data acquisition from operating billet casters, use of mathematical modelling and metallurgical examination of the billet sections sampled during the industrial trials. The use of a commercial billet caster as an experimental unit is not new, but this is the first time the application of in-mold EMS has been subjected to scrutiny of this kind. The specifics of the route taken in this project are highlighted below:

- To characterise the mold heat extraction, an array of thermocouples was inserted into the walls of the copper mold tube and the temperature distribution was logged over a number of heats.

- The cooling water temperature at the inlet and outlet of the water flow was also measured to evaluate the overall heat transfer from the mold.

- In addition to this, the velocity of the cooling water along the cold face was
measured in a separate experiment.

- The temperature distribution was converted to a heat-flux distribution using a two-dimensional transient model with the additional input of heat extraction on the cold face.

- The temperature distribution of the mold obtained at successive time intervals and the calculated heat-flux distribution were analyzed to understand the mold behavior.

- The changes to heat-transfer profiles as a result of stirring were also noted.

- Billet samples collected during the industrial experiments were sliced, surface ground, polished, sulfur printed, etched and assessed for structural refinement, cracks, rhomboidity and oscillation marks. The links between the mold behavior and the quality of the billet were examined and outlined.

- A one-dimensional transient heat flow/solidification model was employed to compute the shell growth for fully backmixed as well as stagnant fluid flow conditions. The results of this model in combination with the measurements of structural refinement as well as heat-flux information were used to explicate the mechanism of structural changes in the continuously cast sections in the presence of in-mold EMS.

- By careful examination of the billet structure the relative importance crystal
multiplication or creation of suitable thermal fields in generating the structural changes was delineated. This will help establish guidelines for optimum stirring parameters.
4. INDUSTRIAL TRIALS AND ANALYSIS

The current study to characterise the thermal response of continuous casting billet molds and the quality of the steel cast in these molds as affected by in-mold electromagnetic stirring consisted of planning, and executing two industrial trials and analyzing the results. The first trial was conducted at Eastern Steelcasting, a division of IVACO Inc., at L'Original, Ontario, Canada, following a preliminary experiment at the University of British Columbia to measure the water flow distribution in the Eastern Steelcasting molds. Results from this trial necessitated further trials mainly due to scale deposition on the mold wall leading to excessive mold distortion, high mold temperatures and boiling in the cooling water channel. Eastern Steelcasting, which was employing EMS on a test basis, decided that currently for their product range, the technology was not justified. The second trial was performed at the Chaparral Steel Company in Midlothian, Texas.

In this chapter the description of two industrial trials, viz., details of casting machine, EMS system, water velocity measurement, determination of temperature fields, and sample collection is presented. Appendix I gives an account of a separate test of water-flow measurements at the Charter Electric Melting Company in Chicago, Illinois. Final sections of this chapter describe the analytical procedures followed to examine the acquired industrial data and the billet samples. The notation followed throughout the thesis to recognise the four faces of the billet/mold is shown in Fig. 4.1.
4.1 FIRST TRIAL: EASTERN STEELCASTING

The first plant trial was undertaken at Eastern Steelcasting. This mini-steel plant has a four strand curvilinear 'CONCAST' billet caster which produces 89-203mm (3.5 to 8in) square billets with an annual capacity of 0.4 mT supplied from an electric furnace of 65T capacity. During the time of the test 100mm (4 in) square sections were being cast and the test mold was a single taper (0.8%/m), 81.28cm (32in) long mold with a 12.7mm (0.5in) thick wall. Details of the caster are presented in Table 4.1.

At the time of the industrial trial, the caster was equipped with an in-mold electro-magnetic stirrer on one of its strands on a test basis. The EMS stator was wound around the steel baffle tube which sits concentrically around the curved copper mold tube of 102mm (4in) square cross section as shown in Fig. 4.2. The baffle tube had similar curvature as that of the mold to maintain a uniform gap for the flow of water. It should however be pointed out that at the corners, an enlarged water passage exists (Fig. 4.3) owing to the square corners of the baffle tube which surround the rounded corners of the mold. In order to facilitate the positioning of the EMS stator so as to cover almost the entire length of the mold, the inlet plenum was reduced to a height of 25.4 mm and the coil was placed in the outlet plenum. Cooling of the EMS stator windings was provided by the mold cooling water flowing through the outlet plenum.

The EMS stator was a two-phase induction motor operating at 250-260 volts and at a current rating of 280-300 amps. The mains frequency of
60Hz was inverted to 8-10Hz to facilitate adequate rotation in the liquid metal.

4.1.1 Water Flow Measurement

It is important that the velocity and pressure of the cooling water be known accurately because it is employed to determine the heat-flux distribution from the measured mold temperatures. Also, it is necessary to check the uniformity of cooling water velocity around the periphery of the cooling channel. After preliminary tests at UBC, it was decided to conduct the water flow trials at the steel plant so that the hydrodynamic conditions that prevail during the actual operation would be closely simulated. Also, an in-plant test would assist in calibration of the flow meters on the caster, and provide an accurate measurement of local velocity and pressure in the cooling channel for each flow rate.

Owing to the difficulty of setting up the commercially available pitot-static tubes in the narrow water channel, a system developed by Berryman et al., incorporating separate pitot tubes and static taps was employed with some modifications. A total of 9 pitot tube/static tap assemblies were installed in a discarded mold tube as shown in Fig. 4.4. The pitot tubes were simply stainless steel 'hypodermic needle' tubing (1.5mm O.D. and 1.0mm I.D) bent to point vertically downward into the flow of water and placed centrally in the water gap (Fig. 4.5). The other end of the pitot tubes was anchored with a set screw in a steel bolt. The steel bolts were screwed into the drilled and tapped holes in the walls of the discarded mold tube. Each bolt was inserted from the inside of the mold tube and screwed in until the bolt was flush with the outside surface of the
mold wall. At this point the projection of the pitot tube out of the bolt was such that the bent end of the pitot tube was placed centrally in the water channel and was pointing vertically downwards. The static taps were co-axial holes of 1.0mm in diameter drilled in steel bolts that were mounted flush with the cooling water side of the mold wall. Each bolt, with a hole to measure the static pressure was positioned about 35-40mm below the bolt holding the pitot tube.

The pressure taps and pitot tubes were connected to copper tubing which was soldered to female quick disconnect joints. A U-tube mercury manometer was used to measure the pressure difference between the pitot tube and the adjacent static tap. The manometer was found to be considerably more accurate than the differential pressure gauges used by Berryman et al.\textsuperscript{10,9} The two ends of the manometer were connected to male quick disconnects with PVC-tubing. Three Teflon valves were attached to the manometer as shown in Fig. 4.6 to facilitate measurement of the differential pressure.

The instrumented mold, plumbing and manometer were shipped to Eastern Steelcasting. The mold tube was installed in a mold housing, mounted on the casting platform and connected to the mold water supply system when the caster was not in operation. A high-pressure air pipeline with a male quick disconnect was made available to unplug any of the tubes in the event of clogging from dirt in the cooling water. The measurements were carried out by first closing valves 2 and 3, opening 1 (see Fig. 4.6) and connecting the ends of the manometer to the selected set of pitot and static tubes. Once the tube was filled with water, valves 2 and 3 were opened. Valve 1 was closed after the system was stable and
the difference in the mercury level noted. After closing valves 2 and 3, valve 1 was once again opened and the manometer was disconnected. The procedure was repeated for each set of pitot and static tubes; the water flow rates tested were 17.7, 18.9, 22.1 and 25.2 l/s.

4.1.2 Mold Temperature Measurement and Sample Collection

An array of thermocouples was installed at the midface and off-corner locations of the outside curved wall and the left straight wall of the mold. Vinyl-coated copper-constantan (Type T) thermocouple wires (in one sheath) with a wire diameter of 0.51mm (0.020in) were located in rows along the axis of the mold with reduced spacing in the vicinity of the meniscus. The layout of the thermocouples is shown in Figs. 4.7 and Fig. 4.8.

The thermocouple hot junction was securely embedded into the mold wall with the help of a threaded copper plug that fitted into a tapped flat-bottom hole in the mold wall. The depths of these holes were carefully measured with a micrometer and are shown in Table 4.2. At one end of the copper-constantan thermocouple the insulation was stripped for a length of 2-3mm. The bare ends were twisted to form the hot junction by passing it through the axial hole in the plug (Fig. 4.9). The plug was then screwed into the mold wall so that the hot junction was mechanically forced against the copper at a depth of about 6mm from the cold face.

Owing to the proximity of the EMS stator, thermocouple leads
could not be brought out of the mold via the steel liner. Instead, the leads were laid down in grooves milled 1.5mm deep in the copper wall of the mold. A thermally conducting, water insoluble epoxy was applied to hold the wires in the grooves. The width of these grooves is shown in Fig. 4.7 and Fig. 4.8. The leads were then taken out of the mold housing through the inlet plenum via pressure fittings.

The inlet and outlet cooling water temperatures were measured by two separate thermocouple probes specially built to fit into threaded holes in the walls of the mold housing. These probes consisted of copper-constantan thermocouples which were embedded in small copper blocks held in an insulating plastic as shown in Fig. 4.10.

After checking the electrical continuity of the thermocouples, the mold was assembled and shipped to Eastern Steelcasting for installation on the caster. The thermocouple leads were connected to a 2280A Fluke Data Acquisition System for recording the millivolt signals during casting and for compensation of the cold junction.

Sequence casting is practiced at this mini-mill and the data acquisition was carried out only during the middle 30 minutes of every heat. Owing to the interference of the induced EMF in the thermocouple wires caused by the pulsating magnetic field, it was not possible to obtain reliable millivolt readings when the EMS was on. The procedure adopted consisted of monitoring mold temperatures over a period of 15 minutes, initially with the stirrer on for 5
minutes and then subsequently with the stirrer off. The influence of EMS on mold temperatures was then assessed by examining the millivolt signal generated in the thermocouples immediately after the stirrer was turned off, while stirring action was present in the liquid pool. This could be compared with the signal generated in the thermocouples after the stirring action was completely damped out. This procedure described was repeated twice, time permitting, during each heat and samples were taken from sections cast when the stirrer was on and when the stirrer was off.

One of the problems encountered during the industrial trial was the frequent malfunctioning of the data logger due to overheating. This led not only to limiting the time of monitoring the temperature signals, but also to poor performance of the tape recorder which rendered several of the magnetic tapes unreadable. There was also an excessive loss of thermocouples over the course of the campaign, probably due to the turbulence in the water channel. Temperatures of the outside curved wall (near the meniscus) were sometimes too high and were out of range of the Cu-constantan thermocouple. Another problem was the off-squareness of the test strand; when the rhomboidity became excessive the trial was terminated.

To begin with, in each heat, the EMS and the datalogger were turned on. A request was placed with the operator at the pouring spout to take the tundish temperature. After allowing the travel time for a transverse slice in the mold to reach the gas-cutting station a billet sample was taken. The billet sample was cut to desired length by operators at the torches on request. The heat number and the sample number were marked for identification. At this point the EMS was switched off and another billet sample was taken to represent the 'no-stirring''
condition in the mold. Time permitting, this procedure was repeated in each heat. Towards the end of the heat the data logger was stopped and loaded with a new tape cartridge in preparation for the next heat.

A total of 12 heats were cast with the instrumented mold; Table 4.3 lists their composition. As can be seen, three ranges of carbon were available for the test, i.e., 4 heats of 0.32 - 0.35% carbon, 7 heats of 0.05 - 0.09% carbon and finally one heat of 0.61% carbon. The cooling conditions for the 12 heats are given in Table 4.4.

At the end of the campaign, the mold was stripped from the housing. The samples collected during the campaign were palleted and shipped to UBC together with the mold tube.

4.2 SECOND TRIAL: CHAPARRAL STEEL COMPANY

The second industrial trial was conducted at Chaparral Steel Company, which, although considered a mini-mill, produces 1.2 mT annually from scrap melted in two electric arc furnaces and cast through two curved continuous casters. The second trial was performed on the 'A'-caster which is a 4-strand, 7.9m radius 'CONCAST' machine, each strand fitted with an in-mold electromagnetic stirrer. The cast size is most frequently 127mm x 171.5mm (5in x 6 3/4in). Chaparral Steel employs in-mold EMS on all its grades and is in the process of optimising the stirring parameters. Relevant information regarding the caster and the electromagnetic stirrer is presented in Table 4.5.
It should be pointed out that the Chaparral stirrer is, in many ways, different from the stirrer employed at Eastern Steelcasting. The former is, encased in a stainless steel box and is placed around the steel baffle tube in the outlet plenum of the mold housing (Fig. 4.11). Also the stator in the stirrer at Eastern Steelcasting consists simply of wound insulated electrical wire while the conductors in the Chaparral stirrer are hollow copper tubes carrying their own cooling water. The pH and temperature of the coil cooling water are carefully controlled at Chaparral as well. Details of the power supply system for the stirrer are withheld owing to patents pending in the U.S.A.

4.2.1 Water Flow Measurement

Similar to the experiment at Eastern Steelcasting, the water flow distribution in the annulus between the mold and the water jacket (baffle tube) was measured. Pitot tubes and static pressure taps were initially installed in a discarded mold tube at UBC and the test was conducted on the shop floor on a down day after re-fitting the mold into the mold housing. The test was carried out at water flow rates of 14.2, 20.5, 23.0, 26.5 and 30.6 l/s.

4.2.2 Mold Temperature Measurement

Based on the experience of the previous trial, a few modifications were implemented in this experiment. First of all, it was decided that the thermocouple leads instead of passing through the water channel must exit the mold via the baffle tube. This was possible since the EMS coil was in the shape
of a box around the baffle tube and not as a loose winding as in the case of the Eastern Steelcasting stirrer. Thus the EMS stator could be lifted in and out of the mold housing. The sequence of assembly of the baffle tube, EMS stator and mold with top plate, in that order, is shown in Fig. 4.12 In order that the thermocouple leads could be taken out through the baffle tube, the mold and baffle tube had to be assembled together and lowered inside the housing with the EMS stator in place. To facilitate such an arrangement, it was necessary to sever the flanges on the baffle tube and later provide adequate sealing between the inlet and outlet plena. This changed the scheme of assembly of the mold housing as shown in Fig. 4.13.

Since the EMS stator fitted snugly around the baffle tube, the gap intervening them is not large enough to permit the laying out of the thermocouple wires, especially along the straight walls of the baffle tube. However, by transposing the EMS stator towards the outside curved wall, sufficient gap could be opened on the other side for passage of the leads out to the top of the coil from where the thermocouple wires could be taken out of the mold housing.

To accommodate the high temperatures seen in the Eastern Steelcasting molds, chromel-constantan thermocouples were selected. These Type E thermocouples also have a higher millivolt output per degree as compared with the copper-constantan type. Beaded, 24 gauge bare wire thermocouples were used after fitting with insulating shrink tubing.

The inside curved wall of the mold and the baffle tube were drilled to provide the holes for placement of the thermocouples. These holes were
tapped and their depth measured (Table 4.6). A new plug design, shown in Fig. 4.14, was employed to embed the thermocouple bead in the mold wall. The thermocouple wires were taken out across the water gap through plugs provided in the drilled and tapped holes of the baffle tube as shown in Fig. 4.15. The plug in the baffle tube had a separate counter sunk screw sealing the hole with the help of a rubber O-ring. A total of 24 thermocouples were installed to monitor the copper wall temperatures and their layout is shown in Fig. 4.16. The thermocouple wires were terminated in the outlet plenum on top of the EMS stator and soldered to Cu-extension wires.

The temperature of the cooling water was monitored at several locations inside the mold housing. The inlet water temperature was measured by a copper-constantan as well as a chromel-constantan thermocouple. The outlet water temperature was monitored at the outlet pipe of the mold housing as well as in the outlet plenum above the EMS stator. Two thermocouples, one of the T-type and the other of the E-type, were mounted on the two off-corners of the inside curved wall. Added to these, the temperature of water flowing in the annulus was separately monitored by four chromel-constantan thermocouples mounted in the baffle tube, as shown in Fig. 4.17, and at different heights in the mold as shown in Fig. 4.18.

The thermocouple leads were bundled and taken out of the mold housing and the lead wires were connected to the datalogger. While the inlet water and the outlet water thermocouples were read out as compensated millivolts, the rest were monitored directly as millivolts. Later the outlet water temperature
was used as compensation in converting these voltages into temperatures.

Together with the monitoring of the thermocouples by the datalogger, two chart recorders were employed to record the analog signal from the thermocouple wires. In the presence of the electromagnetic field, the thermocouple signals recorded by the datalogger exhibited severe interference in as much as the voltages could not be translated into meaningful temperatures. However, the chart recorder had sufficient inertia that it did not respond to the changing voltage induced in the thermocouples so that a readable output was possible. When the EMS was on, the chart recorder pen oscillated about a mean value. When the EMS was switched off, the signal remained around this mean value; but the oscillation disappeared. In each heat, after sufficient data was recorded on the datalogger, time permitting, the chart recorder was used to monitor pairs of thermocouples. The EMS was switched on and off several times for 1-2 minute intervals during the casting of individual heats.

A total of 23 heats were monitored covering the entire range of 0.14-0.60% carbon that the steel mill casts. Where possible, the frequency of the EMS was changed within the limits of 4Hz to 10Hz. The mold water flow rate was set at the maximum possible value. Tables 4.7 and 4.8 give the composition and casting conditions for the 23 heats.

Representative tundish temperature measurements were taken for each billet sample. Samples were cut for the EMS - on/off conditions and numbered by the operators of the torch cutting unit.
4.3 ANALYSIS OF THE INDUSTRIAL TRIALS

After successful completion of the industrial trials, the analysis of the temperature data recorded by the acquisition system and the examination of the billet samples were carried out. Also the instrumented mold was inspected and its permanent distortion measured.

4.3.1 Data Analysis

The cassette tapes containing the millivolt signals were read into the main frame computer at UBC. A number of Fortran programmes were written to accomplish the following.

(i) The millivolt signals were converted into temperature arrays.

(ii) Average and standard deviation of the temperature of each individual thermocouple were obtained over the entire heat.

(iii) Time-temperature plots were developed to indicate the changes in the thermal field in the mold from the time EMS was switched off. As mentioned earlier, thermocouple output in the presence of EMS was rendered unusable by the electromagnetic field and could not be used in this analysis.

Having obtained the temperature data and knowing the velocity and pressure of cooling water, a two-dimensional unsteady state mathematical model developed by Samarasekera and Brimacombe\textsuperscript{10,5,10,6,11,0,11} was applied to back calculate the axial heat-flux profile. A detailed description of the model is available.
in the literature.

Heat-flux profiles, in combination with knowledge of composition, superheat and casting speed, were employed in a one-dimensional unsteady-state heat-transfer model to predict shell growth. The formulation of the model and its important features are documented by Bommaraju.\textsuperscript{112}. The model was modified, however, to include solidification in the sprays and the radiation cooling zone. Also, a new subroutine was added to average the nodal temperatures outside the thermal boundary layer ahead of the solidification front to simulate conditions of complete mixing. In addition to the shell growth, the temperature gradient at the solid/liquid interface as well as solidification rates were calculated using the model.

4.3.2 Billet Sample Analysis

Transverse slices were cut from the billet samples after discarding the torch-cut ends. The sections were surface ground and sulphur prints were made. The number of mold-related cracks and their location were noted. The location of the cracks was designated according to the notation shown in Fig. 4.1. The sulphur prints were also used to measure rhomboidity (off-rectangularity in the case of the Chaparral trial). The sign for the rhomboidity was assigned as shown in Fig. 4.1.

The transverse sections were then macro-etched in 50:50 HCl solution and the length of the columnar zone was measured. Selected transverse sections were polished to submicron level and etched in a hot saturated picric acid
bath to reveal the dendritic structure clearly for careful examination.

Billet samples were later sandblasted to remove the adherent oxide scale to facilitate examination of the oscillation marks. The depth of the oscillation marks was measured at the midface and off-corner locations with the aid of a profilometer. 

1 1 3
Table 4.1.
Details of billet caster/EMS equipment at the Eastern Steelcasting.

<table>
<thead>
<tr>
<th>Type of Machine:</th>
<th>Curvilinear billet caster</th>
</tr>
</thead>
<tbody>
<tr>
<td>Machine designed by:</td>
<td>Concast</td>
</tr>
<tr>
<td>Radius of the machine:</td>
<td>7.9m (26ft)</td>
</tr>
</tbody>
</table>

Details of the mold system:
- Length of copper mold: 81.28 cm
- Thickness of copper mold: 12.7 mm
- Constraint type: Two-sided
- Water gap: 4.7625 mm (3/16 in)
- Taper: 0.8% per meter (single taper)

Details of the spray cooling:
- Length of spray zone: 4.83 m (6ft)
- Spray water consumption: 7.57 l/s (120 gpm)

Details of in-mold EMS system:
- Designed by: Mulcahy Enterprises
- Operating frequency: 10 Hz
- Nominal amperage: 280-300 A
- Nominal voltage: 250-260 V
- Type of motion: Rotary in horizontal plane

Details of the mold oscillation:
- Stroke length: 24.60 mm
- Frequency of oscillation: 72 cpm
- Nominal Casting speed: 47 mm/s - 59 mm/s
- Negative Strip Time: 0.28 - 0.23 s
Table 4.2.
Location of thermocouples in the mold wall.

<table>
<thead>
<tr>
<th>Number of thermocouples</th>
<th>Depth of thermocouples (mm)</th>
<th>Distance from top of mold (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>OUTSIDE CURVED WALL</strong></td>
<td></td>
<td></td>
</tr>
<tr>
<td>At the midface location</td>
<td></td>
<td></td>
</tr>
<tr>
<td>1</td>
<td>6.0</td>
<td>80</td>
</tr>
<tr>
<td>2</td>
<td>6.20</td>
<td>90</td>
</tr>
<tr>
<td>3</td>
<td>6.15</td>
<td>100</td>
</tr>
<tr>
<td>4</td>
<td>6.10</td>
<td>110</td>
</tr>
<tr>
<td>5</td>
<td>5.90</td>
<td>120</td>
</tr>
<tr>
<td>6</td>
<td>6.10</td>
<td>130</td>
</tr>
<tr>
<td>7</td>
<td>5.95</td>
<td>140</td>
</tr>
<tr>
<td>8</td>
<td>5.75</td>
<td>150</td>
</tr>
<tr>
<td>9</td>
<td>5.65</td>
<td>160</td>
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<td>10</td>
<td>5.65</td>
<td>180</td>
</tr>
<tr>
<td>11</td>
<td>5.70</td>
<td>200</td>
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<td>12</td>
<td>5.55</td>
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<td>13</td>
<td>5.60</td>
<td>250</td>
</tr>
<tr>
<td>14</td>
<td>5.40</td>
<td>360</td>
</tr>
<tr>
<td>15</td>
<td>5.10</td>
<td>500</td>
</tr>
<tr>
<td>16</td>
<td>6.05</td>
<td>700</td>
</tr>
<tr>
<td>At the off-corner location</td>
<td></td>
<td></td>
</tr>
<tr>
<td>17</td>
<td>5.60</td>
<td>80</td>
</tr>
<tr>
<td>18</td>
<td>5.85</td>
<td>90</td>
</tr>
<tr>
<td>19</td>
<td>5.30</td>
<td>100</td>
</tr>
<tr>
<td>20</td>
<td>5.15</td>
<td>110</td>
</tr>
<tr>
<td>21</td>
<td>5.45</td>
<td>120</td>
</tr>
<tr>
<td>22</td>
<td>5.40</td>
<td>130</td>
</tr>
<tr>
<td>23</td>
<td>5.65</td>
<td>140</td>
</tr>
<tr>
<td>24</td>
<td>5.55</td>
<td>150</td>
</tr>
<tr>
<td><strong>LEFT STRAIGHT WALL</strong></td>
<td></td>
<td></td>
</tr>
<tr>
<td>At the midface location</td>
<td></td>
<td></td>
</tr>
<tr>
<td>25</td>
<td>5.95</td>
<td>80</td>
</tr>
<tr>
<td>26</td>
<td>5.70</td>
<td>90</td>
</tr>
<tr>
<td>27</td>
<td>6.05</td>
<td>100</td>
</tr>
<tr>
<td>28</td>
<td>6.00</td>
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<td>29</td>
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<td>34</td>
<td>5.75</td>
<td>180</td>
</tr>
<tr>
<td>35</td>
<td>5.85</td>
<td>200</td>
</tr>
<tr>
<td>At the off-corner location</td>
<td></td>
<td></td>
</tr>
<tr>
<td>36</td>
<td>6.20</td>
<td>90</td>
</tr>
<tr>
<td>37</td>
<td>6.25</td>
<td>110</td>
</tr>
<tr>
<td>38</td>
<td>5.65</td>
<td>130</td>
</tr>
<tr>
<td>39</td>
<td>5.10</td>
<td>150</td>
</tr>
<tr>
<td>40</td>
<td>5.35</td>
<td>180</td>
</tr>
</tbody>
</table>
Table 4.3.  
Composition of heats cast during the industrial trial at Eastern Steelcasting.

<table>
<thead>
<tr>
<th>Heat Number</th>
<th>C</th>
<th>Mn</th>
<th>Si</th>
<th>P</th>
<th>S</th>
<th>Cu</th>
<th>Sn</th>
<th>Cr</th>
<th>Ni</th>
<th>Mo</th>
<th>B</th>
<th>Liquidus (Deg C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>0.32</td>
<td>0.85</td>
<td>0.21</td>
<td>0.009</td>
<td>0.019</td>
<td>0.17</td>
<td>0.006</td>
<td>0.25</td>
<td>0.07</td>
<td>0.02</td>
<td>0.0</td>
<td>1502.9</td>
</tr>
<tr>
<td>2</td>
<td>0.33</td>
<td>0.91</td>
<td>0.23</td>
<td>0.008</td>
<td>0.020</td>
<td>0.22</td>
<td>0.007</td>
<td>0.24</td>
<td>0.08</td>
<td>0.02</td>
<td>0.0</td>
<td>1501.4</td>
</tr>
<tr>
<td>3</td>
<td>0.35</td>
<td>0.87</td>
<td>0.30</td>
<td>0.005</td>
<td>0.021</td>
<td>0.18</td>
<td>0.005</td>
<td>0.26</td>
<td>0.08</td>
<td>0.02</td>
<td>0.0</td>
<td>1499.8</td>
</tr>
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Table 4.5.
Details of the billet caster/EMS equipment at the Chaparral Steel Company.

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<th>Curvilinear billet caster</th>
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<td>Machine designed by:</td>
<td>Concast</td>
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<tr>
<td>Radius of the machine:</td>
<td>7.9m (26ft)</td>
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</table>

<table>
<thead>
<tr>
<th>Details of the mold system:</th>
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<tr>
<td>Length of copper mold:</td>
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<td>Thickness of copper mold:</td>
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<tr>
<td>Constraint type:</td>
</tr>
<tr>
<td>Water gap:</td>
</tr>
<tr>
<td>Taper:</td>
</tr>
<tr>
<td>2.6%/m from 0-330mm</td>
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<tr>
<td>0.48%/m from 331-812.8mm</td>
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<td>Spray water consumption:</td>
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<td>Spray water flux:</td>
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<td>Nominal amperage:</td>
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<td>Type of motion:</td>
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Table 4.6.
Location of thermocouples in the inside curved wall of the copper mold.

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<th>Distance from top of mold (mm)</th>
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Table 4.7

Composition of heats cast during the industrial trial at Chaparral Steel Company.

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<th>S</th>
<th>Cu</th>
<th>Sn</th>
<th>Cr</th>
<th>Ni</th>
<th>Mo</th>
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Heat 1

Composition of heats cast during the industrial trial at Chaparral Steel Company.
Table 4.8.
Casting conditions of heats cast during the industrial trial at the Chaparral Steel Company.

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<th>Heat Number</th>
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<th>Superheat (Deg.C)</th>
<th>Water Temperature (Deg.C)</th>
<th>Casting Speed (mm/s)</th>
<th>EMS Parameters</th>
<th>Remarks</th>
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(Table 4.8 contd., next page)
Table 4.8 (contd.).
Casting conditions of heats cast during the industrial trial at the Chaparral Steel Company.

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Table 4.8 (contd.).
Casting conditions of heats cast during the industrial trial at the Chaparral Steel Company.
Figure 4.1. Designation of the different faces of the billet/mold, sign convention for rhomboidity and notation of location of the off-corner cracks
Figure 4.2. Arrangement of the EMS coil around the baffle tube in the mold housing during the industrial trial at the Eastern Steelcasting.
Figure 4.3. Enlarged water passage at the corners of the mold/baffle tube assembly.
Figure 4.4. Layout of pitot tube and static tap assemblies on the mold tube.
Figure 4.5. Schematic drawing of pitot tube and static pressure tap installed in the steel liner.
Figure 4.6. Differential pressure manometer.
Figure 4.7. Layout of grooves and thermocouples in the outside curved wall.
Figure 4.8. Layout of grooves and thermocouples in the straight wall.

Note: All dimensions in Millimeters
Figure 4.9. Copper plug used to secure thermocouple in the mold wall (note also adjacent groove for thermocouple lead).
Figure 4.10. Thermocouple housing for water temperature measurement.
Figure 4.11. Photograph of the box-shaped EMS coil that fits around the mold-baffle tube arrangement employed by the Chaparral Steel Company during the industrial trial.
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Figure 4.13. Assembly of the modified mold housing used during the industrial trial at the Chaparral Steel Company.
Figure 4.14. Modified copper plug design used during the industrial trial at the Chaparral Steel Company.
Figure 4.15. Schematic of the passage of the thermocouple wire through the baffle tube across the water channel.
Figure 4.16. Location of the thermocouples on the inside curved wall of the mold.
Figure 4.17. Schematic of thermocouple in the baffle tube to measure the water temperature.
Figure 4.18. Location of thermocouples to measure the water temperature in the annulus between the battle tube and the mold tube.
5. MOLD BEHAVIOR STUDY

This chapter systematically documents the results of the in-plant measurements of mold temperature, water temperature, water velocity and the computer analysis of the temperature data in order to facilitate a coherent discussion of heat transfer and mold behavior in the presence and in the absence of electromagnetic stirring. The results of the two industrial trials described in Chapter 4 are presented separately. This chapter also includes a description of the mold condition in terms of mold distortion and surface cleanliness which, as will be shown later, are crucial to the understanding of mold behavior.

5.1 WATER VELOCITY DISTRIBUTION

The water velocity distribution was measured in three continuous casting mold systems. Tables 5.1-5.3 list the measured velocities at different volume flow rates for the mold systems at Eastern Steelcasting, Charter Electric Melting and Chaparral Steel. Fig.5.1 shows a plot of water velocity versus water flow rate at 140mm from the top of the mold at Eastern Steelcasting. It can be seen that at all flow rates the water velocity at the midface of the outside curved wall is 2.25m/s lower than that at other levels. Figure 5.2 shows the distribution of velocity on the outside curved wall. The midface velocity is the lowest at any given flow rate whereas at the corner of the outside curved wall and the left straight wall (near the meniscus) the velocity is a maximum. The water velocity measurement from Charter Electric Melting Company are shown in Figs. 5.3 and 5.4. Again the water velocity at the outside curved wall is lower.
than elsewhere although the difference is less than that at Eastern Steelcasting.

In the case of the Chaparral Steel Company, the water velocity on the inside and outside curved walls was quite similar. Figs. 5.5 and 5.6 show the water velocity distribution at different points in the water channel. Nonetheless, it should be noted that the velocities observed on the straight walls are consistently higher than those on the curved walls by approximately 0.5-1.0m/s.

5.2 MOLD TEMPERATURE DISTRIBUTION

As described earlier, the temperature distribution was monitored in two molds. The results from the trial at Eastern Steelmaking are given first, followed by those from the trial at Chaparral Steel. Temperature measurements were not made at Charter Electric Melting Company.

5.2.1 Eastern Steelcasting

Although a total of twelve heats were monitored and data was collected from all twelve, magnetic tapes from Heats 1 and 2, and parts of tapes from Heats 4, 8, 9, 11 and 12 could not be read into the mainframe computer at U.B.C. This was probably due to the malfunctioning of the datalogger during recording. Hence the following presentation of mold temperature distribution is based on data collected from Heats 3, 5, 7 and 10 and part of the data collected in Heats 4, 8, 9, 11 and 12.
After preliminary analysis of the data, it was learned that thermocouples 3, 5, 7, 8, 11, 15, 17, 20 to 23, 30, 31 and 38 had failed. Buffeted by the turbulent cooling water, most of these thermocouples either broke off or lost contact with the mold. Fortunately, however, a sufficient number of thermocouples survived at the midface location of the outside curved wall to permit heat-flux calculations. Temperatures recorded by the thermocouples on the left straight wall and at the midface were used only for comparison with those on the outside curved wall.

Details of the scanning procedure of the data acquisition system are given below. The datalogger scans one transverse plane after another of the thermocouple array. The sequence of scanning is 1, 25, 2, 17, 26, 36, 3, 27, 4, 18, 28, 37 etc. (see Table 3.2). Although the scanning of all the thermocouples is done in a fraction of a second, the data acquisition system takes about 2 to 3 seconds to process the information and store it in the magnetic tape cartridge. The data corresponding to a one scan is stored on the magnetic tape as a single "block". Thus the time period between two successive blocks is approximately 2 to 3 seconds.

As anticipated, the E.M.F induced from the electromagnetic field was superimposed on the D.C millivolt signal of the thermocouples. The datalogger was unable to distinguish the D.C. signal from the the low frequency A.C noise. Temperatures calculated on the basis of the recorded millivolts were often either negative or too high to be recorded by the system. Some times the signal was out of range for the datalogger and was not logged. The thermocouples measuring water
temperature were not directly in the electromagnetic field, so that the datalogger could record the signals successfully with minimum noise.

Thus the mold temperature data collected while the EMS was on was discarded and, instead, the immediate effects of switching the EMS off were examined by focussing on the first four blocks of data collected after the EMS was stopped. The thermal condition of the mold recorded in the first block immediately after the EMS was switched off was considered to be under the influence of persistent fluid flow. By observing the thermal field after the EMS was switched off, i.e., the temperatures recorded in the first four blocks (corresponding approximately to 10-12s), attempts were made to assess the influence of the EMS on the mold's temperature and the heat extarction.

Fig.5.7 shows the temperature distribution at the midface of the outside curved wall for Heat 3 (0.35%C). The peak temperature on the outside curved wall drops by more than 50°C in the four blocks after the EMS was switched off, whereas the temperatures below 175mm from the top of the mold remain unchanged. The left straight wall temperatures in the meniscus region are shown in Fig.5.8 and a gradual reduction in the temperatures from the first to the fourth blocks can be seen. It should also be noted that the temperatures of the straight wall are lower than those of the outside curved wall.

The temperature distribution of the curved and straight walls for Heat 4 with a similar carbon content is shown in Figs.5.9 and 5.10. As in the case of Heat 3, the temperatures in the meniscus region drop in the first three
blocks; but they rise in the fourth block to reach the same level as in the first block. The temperatures of the straight wall (Fig.5.10) are lower than those of the curved wall as in the previous heat.

Figs.5.11. and 5.12 show that for Heat 5 with 0.05%C, the temperature drop after switching the EMS off exhibit the same pattern as in Heat 4. The temperatures initially reduce and then rise. The temperatures in the fourth block, however, do not quite reach those of the first block as in the case of Heat 4.

In Heat 7 (having 0.07%C), the temperatures change in yet another way. This is seen in Figs.5.13 for the curved wall and 5.14 for the straight wall. On the curved wall, the first four temperatures in the top 130mm of the mold drop in the first four blocks, whereas the temperature at 160mm shows an initial increase then a subsequent drop. Also, there is a rise in temperature on the straight wall between 175 and 200mm below the meniscus. But for Heat 10 (0.05%C), the mold wall temperature exhibits an initial drop and a further rise (see Figs.5.15 and 5.16). The highest temperatures are obtained in the Heat 12, and these decline in the first four blocks, as shown in Figs.5.17 and 5.18.

It can be concluded from these plots that the temperature changes in the mold when the EMS is switched off are localized to the meniscus region. Thermocouples positioned below 200mm from the top of the mold did not record significant changes when the EMS was switched off. The mold temperature in the meniscus region appears to undergo considerable change when the EMS is
off. The changes to the mold temperature profile over successive blocks of data do not reveal a unique pattern.

Continuous time-temperature plots were drawn in order to further examine the temperature in the mold not only at the instant of switching the EMS off, but also for prolonged periods. Figure 5.19 shows the thermal profile of the curved wall at the midface for 125 blocks of data (corresponding to 290 seconds). After excluding the thermocouples which failed, the temperatures at 80, 90, 110, 130, 160, 180, 200, 220, 260, and 360mm from the top of the mold have been plotted. The average mold temperature at each given location was calculated from the data of all the blocks collected after the EMS was switched off and is represented by the dotted line across the plotted temperature signal. Each division on the Y-axis corresponding to the temperature curve is 20°C. It is clear from this plot that the temperature of the mold does not undergo significant changes beyond 180mm from the top. It can be seen that, although the temperatures at 80, 90 and 130mm drop in the first 5 to 6 seconds, they subsequently rise and fluctuate. Their drop at about 70s after the EMS is switched off is accompanied by a rise in the temperature at 180mm by almost 40°C, which could be attributed to changes in meniscus level. A similar situation can be noticed 280-290s after the EMS was switched off. The afore mentioned changes to the temperatures at the higher and lower part of the mold are in opposite directions and so can be due to meniscus level fluctuations. Excluding these, the temperatures down the length of the mold appear to be fluctuating mostly in the same direction. In other words, a drop in any one of the temperatures is accompanied by a reduction in the others. The exceptions to this are the temperatures at the 180
and 200mm levels which remain almost constant at 128.4°C and at 132.5°C respectively.

Figure 5.20 shows a similar plot for the off-corner temperature of the outside curved wall of the mold. It can be observed readily that the average temperature at 220mm from the top of the mold is about 100°C higher than at the midface (Fig 5.19). The amplitude of the fluctuation is more than at the corresponding location at the midface. The temperatures of the straight wall at the midface and off-corner are shown in Figs.5.21 and 5.22 respectively. Once again the temperatures decrease after the EMS is switched off, but they rise thereafter and there is a gradual change at the meniscus as indicated by the rise in the temperature at the 160mm level. It should be noted that the straight wall temperatures are lower than those at the curved wall, and at the off-corner of the straight wall, the temperature is lower than at the midface. It is clear from Figs.5.19-5.22 that at any given distance from the top of the mold, the temperatures of the midface and off-corner levels of the curved wall as well as the straight wall rise and fall at the same time.

In Fig.5.23, a comparison is made between the average midface and off-corner temperatures of the outside curved wall. The average is based on all the temperature data obtained after the EMS was switched off. The 2-σ standard deviation limits of each temperature are marked on the plot. The plot reveals that the characteristic drop of mold wall temperature at midface below the meniscus is not evident in the off-corner location. The off-corner temperature at the 225mm level is higher than that at the midface. Also a comparison of the average straight
wall and curved wall temperatures at the midface in Fig. 5.24 clearly indicates that the straight wall is much cooler than the curved wall, especially at the meniscus. The decrease below the peak temperature seen in the curved wall temperatures is absent on the straight wall.

In Heat 3, the EMS was switched on and off a second time. Figures 5.25 and 5.26 show the temperatures of the curved and straight walls of the mold in data set 3B of Heat 3 after the EMS was switched off. As in the case of the first set, 3A, after an initial drop, the temperatures rise and fluctuate. Also, the mold temperature is nearly constant at 180mm from the top of the mold and below; and once again the midface temperatures are lower at the straight wall than at the curved wall. As can be seen in Fig 5.27, where the two wall temperatures are compared, the straight wall temperatures are closer to the curved wall temperatures at their peak values than in the case of the first data set, Fig. 5.24.

Figure 5.28 shows the thermal field of the mold walls in Heat 4A after the EMS was switched off. Compared to the curved wall temperatures in data sets 3A and 3B, the temperatures in set 4A are higher at all levels above 180mm, probably because of a higher meniscus level. As in the previous plots (Figs. 5.19 and 5.25), there is a drop in the temperature at levels 80, 90, 110 and 130mm, but the temperature at 160mm rises. A rise in the temperatures at 80, 90, 110 and 130mm levels is always accompanied by a decrease in the temperature at 160mm from the top of the mold. It is also clear that the temperatures at 80-130mm from the top of mold start to decrease
when the temperature at 160mm begins to rise. This pattern suggests that most of the fluctuation in the meniscus thermocouples is due to metal level changes.

Figure 5.29 compares the average straight wall and curved wall temperatures at the midface location. This plot reveals that the standard deviation of the thermocouple at 120mm on the straight wall and 200mm on the curved wall is much greater than the standard deviation at other levels.

In Heat 5A the carbon content of the steel was 0.05% and the water flow rate was increased to 25.23 l/s. As can be seen in Fig.5.30, the mold temperatures for this heat were lower than for the medium carbon heats (3A, 3B, and 4A). Also, the fluctuations of temperature are considerably lower when compared to the previous heats. Clearly, the metal level is between 130 and 160mm from the top of the mold. Also, as in the previous heats, the initial drop in the temperature after the EMS is switched off does not appear to be significant relative to ensuing fluctuations. Figure 5.31 shows that the average midface temperature of the straight wall is higher than that of the curved wall at the meniscus.

In Heat 6A (0.08%C steel), the temperatures of the curved wall, as shown in Fig.5.32, are higher than in Heat 5A and they fluctuate with greater amplitude. The average curved wall temperatures, as depicted in Fig.5.33, are once again higher than those of the straight wall.

It is interesting to note that in Heat 7 (0.07%C), as shown in
Fig.5.34, the mold temperature fluctuations have a greater amplitude. The average mold temperature (Fig.5.35), however, is lower than in any other heat. Figures 5.36 to 5.38 show the thermal field of the curved wall in Heats 8A, 9A and 10A. It is again evident that whenever temperatures at 80, 90, 110 and 130mm levels decline, there is a rise in the temperature at the 160mm level and vice versa. It is clear from these plots that much of the fluctuation in the mold wall temperatures is due to metal level changes. It can also be seen that the drop observed in the temperatures at some levels when the EMS was switched off is accompanied by an increase in the temperatures at other levels; for example, the drop in temperature at 160mm in Fig.5.37 is accomanied by a rise in temperature at 110 and 130mm levels. This indicates that when the EMS is switched off, there is a drop in the meniscus level.

In the last heat, 12A (0.61%C), the thermocouples at the 110 and 130mm level have failed; Fig.5.39 shows the changes in the output of the remaining thermocouples. There is a steep drop in the temperature at all levels immediately after the EMS was switched off. It should be pointed out, as shown in Fig.5.40, the mold wall is the hottest when compared to any of the previous heats, and uniquely there is a distinct second peak near the meniscus.

5.2.2 Chaparral Steel Company

The thermal field of the inside curved wall was monitored in a total of 23 heats during the second trial at Chaparral Steel Company. Only two of the 24 thermocouples placed in the mold wall failed during the assembly and the
remainder performed well throughout the entire trial. The entire mold length was scanned from top to bottom, and this was followed by recording the temperature of water at several levels. The time taken for a single scan of the mold and water thermocouples was under 2s.

Figure 5.41 presents the temperature responses measured in Heat 3A (0.31%C) at different levels in the inside curved wall from 80mm to 400mm below the top of mold after the EMS was switched off. Each division on the ordinate corresponding to the temperature curve is 10°C. It is clear from this plot that the mold wall experienced periodic high and low temperatures primarily at 110, 120, 130 and 140mm levels near the meniscus and also at the 300, 325 and 350mm levels well below the meniscus. Figure 5.42 shows the average temperatures from top to bottom of the mold with the 2-σ limits of the standard deviation depicted as bars. With the meniscus level normally set at 100-125mm, there are fluctuations in the temperatures not only near the meniscus, but also much below it, as suggested above.

Figures 5.43 and 5.44 show the temperature profiles for Heat 4A with a similar carbon content as that of Heat 3A. While in Heat 3A the temperatures at 110, 120 and 130mm show a decline when the EMS is switched off, there is a reverse trend in the mold temperature pattern in Heat 4A. Also noteworthy in Heat 4A is the temperature at 225mm which is as low as 50-60°C and which exhibits occasional sharp fluctuations. These sudden increases are not accompanied by any changes in the other temperatures. Yet another interesting feature in these plots is the sudden reduction in temperature at 90 and 100mm (at
times of 70s, 190s and 270s in Fig. 5.41 and 200s, 280s and 370s in Fig. 5.42) accompanied by a concomitant increase in temperatures at 250mm, 275mm, 300mm and 325mm from the top of the mold.

In Heat 7A having 0.45%C, the thermal field and its fluctuation with time (as shown in Figs. 5.45 and 5.46) are similar to those observed in the medium carbon grades (Heats 3A and 4A). At the time the EMS is switched off and later, changes in temperature are minimal until about 10s.

Figs. 5.47 and 5.48 show the temperatures in the mold during the Heat 9A having 0.19% C. Here there is a considerable drop in the temperatures at the instant the EMS was turned off. But this fluctuation can be seen as one of many during the time that the EMS was turned off. All these observations recur once again for Heat 16A (0.23% C), as can be seen in Figs. 5.49 and 5.50.

Based on the thermal fields described in Figs. 5.41 to 5.50, the following salient features can be delineated.

1. The mold temperatures at levels 110, 120, 130 and 140mm from the top of the mold fluctuate periodically.

2. The temperature of the mold at 300, 325 and 350mm from the top of the mold rises and falls together with those mentioned above.

3. At the instant the EMS is switched off, there are no conspicuous changes in mold temperatures.
4. Temperature changes immediately after the EMS is turned off are similar to the observed normal variation in the mold temperature during the casting operation without the EMS.

5. The temperature at 225mm from the top of the mold is as low as 50-60°C and exhibits an occasional rise for brief periods of time.

6. The occasional sudden drop in the temperature recorded by the thermocouples at 90 and 100mm corresponds to a simultaneous rise in the temperature at 250, 275, 300 and 325mm.

7. The temperatures at the 160, 170, 180, 200 and 225mm levels seem to be least affected by the phenomenon that is causing the observed fluctuations in the temperatures above and below them.

Together with the datalogger, two strip chart recorders were used to monitor selected thermocouples in a few heats. After collecting the data with the datalogger and acquiring the representative billet samples, the EMS was switched on and off several times to record output on the chart paper. Each of the two chart recorders had two pens but at a given time only one pair of thermocouples could be monitored simultaneously.

The fluctuation observed in the millivolt output under the influence of the EMS was of the same frequency as the frequency of the voltage input to the stator of the electro magnetic stirrer. This could be checked in Heat 18 (0.61%C), for example, where the millivolt output from Thermocouple 7 (140mm from the top of the mold) was recorded while the frequency of the the EMS was
changed instantaneously from 10Hz to 4Hz. The chart recording is shown in Fig. 5.51. It can be seen that the amplitude of the oscillation has increased and its frequency has reduced.

Use of the chart recorder proved to be beneficial, because the inertia of the pens prevented them from responding fully to the rapidly oscillating signal induced in the thermocouples by the EMS. Thus a readable output was obtained from the thermocouples being monitored with the induced voltage appearing as a superimposed oscillation. This can be seen in Fig. 5.51.

The amplitude of the superimposed oscillation was not the same for all the thermocouples. This is evident in Fig. 5.52 (Heat 16 - 0.23%C) where the induced voltage in the signal from Thermocouple 10 (170mm) is seen to be less than that from Thermocouple 6 (130mm). When the EMS was turned off, the pattern of signal variation was found to be similar for the two thermocouples. This is also apparent in Fig. 5.53 (Heat 18, 0.61%C).

In Fig. 5.54, the signals from Thermocouples 5 and 11 located at 120mm and 170mm from Heat 22 (0.17%C) were examined by switching the EMS off and running the chart at different speeds. Figure 5.55 shows a similar plot for Heat 23 (0.29%C) where thermocouples 4 (110mm) and 5 (120mm) are compared and Fig. 5.56 presents the signals from Thermocouples 23 (550mm) and 24 (600mm). These plots reveal that there is no discernible change in the temperature in the mold owing to the presence of the electromagnetic rotation of the solidifying steel.
The continuous time-temperature plots (Figs. 5.41, 5.43, 5.45, 5.47 and 5.49) developed from the digitized signals of the thermocouples have indicated periodic fluctuation of the mold temperatures after the EMS was off. The analog recording of the thermocouple signals using the chart recorder in Fig. 5.53, for example, shows the variation of the millivolt output from the Thermocouple 7 reducing even with EMS on. When the output from selected thermocouples was recorded for prolonged periods of time with the EMS on and with the EMS off, it was seen that the periodic rise and fall of the temperature is present even with EMS on. These charts owing to their length are not presented here.

5.3 MEASUREMENT OF WATER TEMPERATURE

5.3.1 Eastern Steelcasting

The inlet and outlet mold water temperatures also were measured in addition to the copper tube temperatures. The superimposed induced voltage from the EMS was minimal in the signals from these thermocouples, probably because they were not directly in the electromagnetic field. Figure 5.57 shows a plot of the inlet and outlet water temperatures before and after the EMS was switched off in Heat 3A. With regard to the outlet water temperature, the fluctuations persist even after the EMS was switched off. It should be noted that there is no significant change in the outlet water temperature at the instant when the EMS is switched off. In Heats 3B and 4A (Figs. 5.57 and 5.58 respectively) similar observations are made.
In Heat 5A (0.05% C) there is a noticeable change (see Fig. 5.60) in the pattern of water temperature fluctuations. Firstly, the fluctuation in the outlet temperature is much less compared to the signal from the medium carbon heats (Figs. 5.57 to 5.59). There is, however, as in previous heats, no observable difference in the outlet water temperature immediately after the EMS was switched off. The inlet water temperature is affected by the presence of the EMS, but remains constant after the EMS is switched off.

A slight fluctuation of the outlet water temperature appears in Heats 6A (Fig. 5.61) and 7A (Fig. 5.62). In Heat 8A (0.06% C), however, the amplitude of the fluctuations increases, as shown in Fig. 5.63, to the levels observed in Heats 3A, 3B and 4A. From this time onward until the end of the campaign, the amplitude of the fluctuations in the outlet water temperature signal progressively increase. Figure 5.64 from Heat 10A, and Fig. 5.65 from Heat 11A demonstrate this finding. The highest fluctuations were recorded in Heat 12A as shown in Fig. 5.66.

The average and standard deviation for the inlet water temperature are shown in Fig. 5.67. It can be seen that the inlet water temperature is higher with the EMS turned on, which may be a manifestation of the electromagnetically induced voltages. Figure 5.68 shows the average and standard deviation of the outlet water temperature over the entire campaign. The standard deviation decreases in Heat 5A and increases from Heat 7 onward. The highest fluctuation, as mentioned earlier, occurred in Heat 12A.
In the second trial at Chaparral Steel, water temperatures were monitored at several levels in the mold housing. As mentioned earlier, the inlet and outlet temperatures were recorded by two thermocouples which were located at the entry of the water flow into the housing and at the exit. Also, the temperature of the water flowing upwards in the annulus between the mold and the water jacket was recorded at four levels (80, 130, 275 and 700mm from the top). In addition, two separate thermocouples (one E-type and the other T-type) were mounted on the top plate to register the water temperature in the outlet plenum. These two thermocouples were placed in levels corresponding to the two off-corner levels of the inside curved wall. The E-type thermocouple was located toward the right straight wall side and the T-type thermocouple was mounted toward the left straight wall side of the inside curved wall.

Figure 5.69 shows the water temperature recorded at different levels in the mold housing after the EMS was turned off in Heat 3A. The inlet water temperature recorded by the two thermocouples at the entry, and the thermocouple placed in the water channel at 700mm from the top of the mold are similar. The average inlet water temperature at the entrance to the water channel (the 700mm location) was 19.5°C (with 0.254° standard deviation) and the averages of the two thermocouples at the water entrance to the mold housing were 20.1° (0.097° standard deviation) and 19.1° (0.161° standard deviation) respectively. Figures 5.70, 5.71 and 5.72 corresponding to Heats 4A, 7A and 9A respectively, also show that the inlet water temperature was measured accurately by all three.
The outlet water temperature recorded by the thermocouple at the water exit from the mold remains at 27.4°C (0.234° SD) in Heat 3A, 28.3°C (0.540° SD) in Heat 4A, 28.2°C (0.550° SD) in Heat 7A and 27.4°C (0.681° SD) in Heat 9A. The average temperature recorded by the E-type thermocouple at one of the off-corner levels in the outlet plenum is equal to that recorded at the mold exit. But the signal from the E-type thermocouple located in the outlet plenum was periodically fluctuating. This fluctuation is of a similar nature as that seen in the mold wall temperatures. The thermocouple at the off-corner location (T-type) measured lower average water temperature which, however, fluctuated like the signal from the E-type thermocouple. It is interesting to note from Figs.5.69 to 5.72, that the fluctuation of the E- and T-type thermocouples situated on the two off-corners of the inside curved wall are such that whenever one off-corner water temperature begins to drop, the other tends to rise.

The water temperature steadily rises as the water flows from the bottom to the top of the water channel as can be seen in Fig.5.73. The average water temperature and standard deviation (2-σ) are shown in this plot from Heat 3A. It is evident from Figs.5.69 to 5.72 that the water temperatures at the 80, 130 and 275mm levels exhibit periodic fluctuation. The periodicity is the same as seen in the outlet water temperature recorded by the E-type thermocouple at the off-corner location in the outlet plenum. All four temperatures rise and fall at the same time. The water temperature, recorded by the T-type thermocouple at the other off-corner location in the outlet water plenum, is lower than the above four,
and as described earlier, fluctuates in an opposite sense. While the thermocouples at the 80, 130 and 275mm levels in the water channel and the E-type thermocouple in the outlet plenum show water temperatures on the rise, the T-type thermocouple records the water temperature in the outlet plenum at the other off-corner location to be declining.

It should be noted that the rise and fall in the outlet water temperatures (in the vicinity of the copper mold) correspond to the fluctuations in the mold temperatures observed in earlier plots (Figs. 5.41, 5.43, 5.45, 5.47 and 5.49). From these, it is seen that the outlet water temperature may fall or rise at the point of the EMS-switch off. If the outlet water temperature is rising while the EMS is on, an increase in the temperature continues to be recorded after the EMS is off. On the other hand if the outlet water temperature is declining during the EMS, a reduction in the temperature is seen after the EMS is off. As such, there appears to be no distinct temperature change solely because the EMS was switched off or on.

It was observed that the output from the thermocouples in the water channel measuring the water temperature was severely affected by the presence of the EMS. Also, the signal from the E-type thermocouple recording the outlet water temperature in the outlet plenum was superimposed with induced voltage due to the EMS. Fortunately, however, the T-type thermocouples recording the outlet plenum water temperature and the exit water temperature as well as the E-type and T-type thermocouples at the entry of water into the mold housing were not affected by the EMS to a great extent in the earlier heats in the campaign.
Toward the end of the campaign the interference was found to increase.

Table 5.4 shows the average/standard deviation for the water temperatures with the EMS turned on and off. Figures 5.74 to 5.77 show the fluctuations in the temperatures before and after the EMS for the Heats 3A, 4A, 7A and 9A. These plots clearly show that there is no observable change in the outlet water temperature after the EMS was turned off. Also the fluctuation in the outlet water temperature (T-type) in the absence of the EMS are similar to those recorded while the EMS was on.

5.4 MOLD CONDITION

5.4.1 Eastern Steelcasting

The distance between the opposite walls of the mold received from Eastern Steelcasting was measured prior to the plant trials. The measurements were made at the midface and the two off-corners for the pair of straight walls as well as for the curved walls. Figure 5.78 shows the dimension profiles for the straight walls and Fig.5.79 shows those for the curved walls. The 0.6%/m taper was completely lost for the curved walls and is retained only below 350mm for the straight walls. Also, there is considerable mold wall bulging at the meniscus area, predominantly on the curved walls. Figure 5.80 compares the distance between faces for straight and curved walls, and it is clear that the curved walls have significant permanent deformation in the meniscus area while the straight walls have a negative taper extending from the mold top to 350mm below the meniscus.
At the end of the 12-heat campaign, the mold tube was stripped from the housing and sent to U.B.C. for re-measurement of internal dimensions. Figure 5.81 shows the midface distortion of the straight and curved walls. While the straight walls remain without any taper above 350mm, the curved walls appear to have tucked inward at about 125mm from the top of the mold. Figures 5.82 and 5.83 compare the distortion of the straight walls and curved walls before and after the trials. The straight walls appear to have lost their negative taper while retaining, however, the positive taper below 350mm. The curved walls have clearly caved in. Below 200mm, however, the as received shape is retained.

The mold surface was also photographed after the trials. Figure 5.84 shows the surface of the outside curved wall and left straight wall while Fig.5.85 is a photograph of the straight wall and the inside curved wall. Evidently, considerable scale formation took place on the outside curved wall in the meniscus area. The other surfaces are clean except for the heat tint, commonly found on copper tube molds.

5.4.2 Chaparral Steel Company

The mold used in the second set of trials at Chaparral Steel was a brand new "reformed" tube. The mold had 2.6 %/m taper for the first 330mm and a 0.48 %/m over the remainder of the mold. Figure 5.86 shows the distortion of the curved walls after the second campaign. There is slight distortion at the midface, but, significantly, it is at one of the off-corners. It is clear that the second taper (0.48% per m) is completely lost at the midface. The distortion of
the straight walls is shown in Fig. 5.87, and once again there is a slight distortion at the midface in the meniscus area.

5.5 MOLD HEAT-FLUX DISTRIBUTION

5.5.1 Eastern Steelcasting

In order to convert the measured mold temperature distribution to heat-flux profiles, as earlier mentioned, a two-dimensional transient mathematical model was employed. In addition to the temperature data, input to the model consisted of the pressure in the inlet water line recorded during each heat and the water flow rate selected by the operating foreman, depending on the grade of steel. Water velocity measurements, described in Section 5.1.1 were then read into the model. The appropriate water velocity was selected depending on the water flow rate employed.

As described in Section 5.4.1, scale deposits were observed on the outside curved wall and had to be accounted for in the model. Since the actual scale thickness and its rate of growth during the campaign were unknown, an arbitrary scale thickness of 0.04mm\textsuperscript{111} was assumed in all the computer runs.

The mold temperature distribution was presented in section 5.2.1 where the temperature profile of the mold was seen to be fluctuating with time in each heat. Since one of the goals of this exercise is to determine the heat-flux profile with and without the EMS, it was decided to back calculate
heat-flux profiles based on the first block of data obtained after switching the EMS off and comparing it with the heat-flux profile obtained from the fourth block of data (obtained approximately 10-12s later). Preliminary calculations have indicated clearly that a given change in the heat-flux at the hot face is reflected in the mold temperature in about 2 to 3s. With this in mind, a comparison of the heat-flux profiles resulting from the first and fourth blocks of temperature data should give an indication of the changes to the heat-flux at the point when the EMS was switched off.

Figure 5.88 shows the hot face and cold face temperatures calculated from the model for the Heat 3 (first block temperature). Also, the measured and calculated mold temperatures resulting from the input heat-flux profile are shown. Figure 5.89 shows the heat-flux profile for Heat 3 based on the temperatures in the 1st and 4th block of data after the EMS was switched off. It can be seen that the meniscus level for the 4th block is lower than for the 1st block. Also it is evident that the high heat-flux region in the meniscus area is larger for the 1st block than that obtained for the 4th block of data. The heat-flux profile from the 1st block also exhibits a higher peak heat-flux. It should be pointed out, however, that the two heat-flux profiles are quite similar below 150mm.

The heat-flux profiles for Heat 4 are shown in Fig.5.90. There is little difference in the meniscus levels and also the difference in the meniscus heat-flux values is smaller. Figures 5.91 to 5.94 show the heat-flux profiles for the low carbon (0.05-0.09%C) Heats (5A, 6A, 7 and 8A). In Heat 5A, with the same
metal level, the meniscus heat-flux is lower, whereas in Heat 6A there is little difference between the two heat-fluxes. With a lower meniscus level, a higher peak heat-flux resulted in Heat 7. In Heat 9A, there is no difference in the heat-flux profiles and the metal level is the same for the two cases.

Figure 5.95 shows the heat-flux profiles for Heat 12 having 0.61% carbon. This plot depicts extremely high heat-flux values (9000-10,000 kW/m²) in the meniscus area as well as at 300-450mm from the top of the mold. The two heat-flux profiles are quite similar.

5.5.2 Chaparral Steel Company

It is clear from the plots of the mold temperature distribution that the temperatures periodically rise and fall during casting. In order to determine the heat-flux distribution, the average and the standard deviation of the mold temperatures were calculated. The heat-fluxes corresponding to maxima and minima above and below the average mold temperature profile were determined with the help of the model previously described.

The 23 heats cast in the second set of trials can be grouped into three carbon ranges, viz., 0.14%-0.23%, 0.31%-0.38% and 0.40-0.48%. The average temperature values and their periodic fluctuation for several heats in each group were first examined and found to be very similar. Finally three heats were selected one from each carbon group for the development of heat-flux profiles.
Figure 5.96 shows typical high and low heat-flux profiles for Heat 3A with 0.31%C. While the higher heat-flux profile has three peaks in the meniscus region, the lower profile exhibits only one. The peak heat-flux changes from above 4000 kW/m² to about 2200 kW/m² for the heat-flux representing the lower values of mold temperatures.

The highs and lows of the temperature distribution resulted in a heat-flux pattern (shown in Fig.5.97) for Heat 7A with 0.45%C. While the peak heat-flux was above 4500 kW/m², i.e., more than that observed for a 0.31%C steel, the lower value is at about 2800 kW/m². Only two distinct peaks were observed with the high heat-flux profile. Figure 5.98 shows the heat-flux profiles for Heat 9A (0.19%C). The two heat-flux profiles are similar to the 0.31%C steel except that there are only two peaks.

A comparison of the heat-fluxes resulting from the higher temperature profiles is shown in Fig.5.99 for Heats 3A, 7A and 9A. Figure 5.100 shows the heat-fluxes resulting from the low temperature profiles of the same heats. The nominal heat-flux to the mold at any time would be fluctuating between these two limits set by the standard deviation of the mold temperatures. It can be perceived that the major changes to the heat-flux profiles are only in the meniscus region where the heat flow can decrease to almost 1800 kW/m² from a high of 4500 kW/m².
Table 5.1. Water velocity distribution in the mold at Eastern Steelcasting.

<table>
<thead>
<tr>
<th>Water Flow Rate (L/s)</th>
<th>Measured water velocity (m/s) at given location around the mold tube</th>
<th>Average water velocity (m/s) calculated from volume flow rate</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Inside Curved Wall 100mm from top</td>
<td>Outside curved face</td>
</tr>
<tr>
<td>17.66</td>
<td>5.28  4.50  2.94  8.64  4.85</td>
<td></td>
</tr>
<tr>
<td>18.93</td>
<td>5.83  4.98  3.91  5.75  5.20</td>
<td></td>
</tr>
<tr>
<td>22.00</td>
<td>7.35  6.60  5.12  7.13  6.27</td>
<td></td>
</tr>
<tr>
<td>25.23</td>
<td>8.33  7.64  6.05  7.78  6.92</td>
<td></td>
</tr>
</tbody>
</table>

The table shows the measured water velocities at various locations around the mold tube, along with the calculated average water velocities from the measured flow rates.
Table 5.2. Water velocity distribution in the mold at Charter Electric Melting Company.

<table>
<thead>
<tr>
<th>Water Flow Rate (L/s)</th>
<th>Measured water velocity (m/s) at given location around the mold tube</th>
<th>Average water velocity (m/s) calculated from volume flow rate</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Inside Curved Face</td>
<td>Outside curved face</td>
</tr>
<tr>
<td>9.46</td>
<td>4.85</td>
<td>2.94</td>
</tr>
<tr>
<td>15.77</td>
<td>6.82</td>
<td>6.24</td>
</tr>
<tr>
<td>22.08</td>
<td>10.94</td>
<td>10.37</td>
</tr>
</tbody>
</table>
Table 5.3. Water velocity distribution in the mold at Chaparral Steel Company.

<table>
<thead>
<tr>
<th>Water Flow Rate (L/s)</th>
<th>Measured water velocity (m/s) at given location around the mold tube</th>
<th>Average water velocity (m/s) calculated from volume flow rate</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Inside Curved Face</td>
<td>Outside curved face</td>
</tr>
<tr>
<td>19.24</td>
<td>7.08</td>
<td>*</td>
</tr>
<tr>
<td>20.50</td>
<td>7.49</td>
<td>*</td>
</tr>
<tr>
<td>23.03</td>
<td>9.09</td>
<td>*</td>
</tr>
<tr>
<td>26.50</td>
<td>10.22</td>
<td>*</td>
</tr>
<tr>
<td>30.60</td>
<td>13.12</td>
<td>*</td>
</tr>
</tbody>
</table>

* Flow-line failed at the start of the experiment.
Table 5.4. Average and standard deviation of water temperature measured during the industrial trial at Chaparral Steel Company.

<table>
<thead>
<tr>
<th>Heat Num</th>
<th>INLET WATER TEMPERATURE (Deg C)</th>
<th>OUTLET PLENUM WATER TEMPERATURE (Deg C)</th>
<th>MOLD EXIT WATER TEMP.</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>E-type Thermocouple</td>
<td>T-type Thermocouple</td>
<td>E-type Thermocouple</td>
</tr>
<tr>
<td></td>
<td>EMS - ON</td>
<td>EMS - OFF</td>
<td>EMS - ON</td>
</tr>
<tr>
<td>3A</td>
<td>Average</td>
<td>std. dev.</td>
<td>Average</td>
</tr>
<tr>
<td></td>
<td>20.1</td>
<td>0.097</td>
<td>19.4</td>
</tr>
<tr>
<td>4A</td>
<td>19.7</td>
<td>0.450</td>
<td>20.5</td>
</tr>
<tr>
<td>7A</td>
<td>21.5</td>
<td>0.422</td>
<td>21.2</td>
</tr>
<tr>
<td>9A</td>
<td>20.3</td>
<td>0.513</td>
<td>20.9</td>
</tr>
</tbody>
</table>
Figure 5.1. Water velocity (at midface) at 100mm from the top of the mold (corresponding to the nominal meniscus level) for different water flow rates in the mold system at Eastern Steelcasting.
Figure 5.2. Water velocity at 100mm from the top of the mold (corresponding to the nominal meniscus level) adjacent to the outside curved wall for different water flow rates in the mold system at Eastern Steelcasting.
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Eastern Steelcasting
Heat 5-Set A

MID FACE OF CURVED WALL

MIDFACE OF STRAIGHT WALL

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Heat 9-Set A  
Eastern Steelcasting  
Outside Curved Wall at Mid-Face

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Temperature At Thermocouple Location, Each Div. = 20 Deg.C

Heat 10-Set A
Outside Curved Wall at Mid-Face
Eastern Steelcasting
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Heat 7-Set A
Chaparral Steel Company
Inside Curved Wall at Mid-Face

Temperature At Thermocouple Location, Each Div. = 10 Deg C

Time After Switching The EMS Off (s)

Figure 5.45. Plot of the outside curved temperatures at midface vs. time after the EMS was switched off in Heat 7-set A (Chaparral Steel). Each division on the ordinate is 10°C. The dotted line represents the average temperature shown adjacent to the ordinate. Thermocouple location (mm) is marked above the dotted line.
Heat 7 -Set A
Chaparral Steel Company
Inside Curved Wall at Mid-Face

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Heat 16-Set A
Chaparral Steel Company
Inside Curved Wall at Mid-Face

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Before the campaign

After the campaign

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6. BILLET QUALITY EVALUATION

Billet samples collected in each heat to represent the EMS-on and the EMS-off conditions were examined for off-squareness (off-rectangularity in the case of Chaparral Steel), mold related cracks, oscillation marks and cast structure. Results of the study are presented in this chapter.

6.1 OFF-SQUARENESS

Off-squareness or off-rectangularity was measured as the difference in the diagonals of the billet. After noting the casting direction, the "sign" for the off-squareness was assigned as shown schematically in Fig. 4.1. If the tilt of the billet is toward the left straight wall a negative sign was given. Billets tilted toward the right straight wall were given a positive sign for their rhomboidity.

Table 6.1 lists the measured rhomboidity of all the billet samples collected during the 12-heat campaign at Eastern Steelcasting. The magnitude and the direction of rhomboidity is shown in Fig. 6.1 to vary from 6.95 mm to −5.60 mm chronologically in the same heat as well as from one heat to the next. There is no difference in the rhomboidity of stirred and unstirred billets. Also, the extent of rhomboidity appears to be independent of carbon content of the steel. The direction of rhomboidity seems to change throughout the campaign and in some cases even within a single heat. The superheat does not appear to have any influence on the rhomboidity of the billets.

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The off-rectangularity of the Chaparral’s billet sections is shown in a tabular form in Table 6.2. It can be seen, once again, that there is no discernible difference in the off-rectangularity between the stirred and unstirred billet sections. Also, the direction and the extent of this defect fluctuated significantly from 12.5 mm —11.0 mm during the campaign. Even in a single heat (for example, Heat 9), the extent of off-rectangularity changed from one billet to the next. There is no obvious effect of either the carbon content or the superheat of the steel on this defect.

6.2 OFF-CORNER CRACKING

Examination of the billet samples from the campaign at Eastern Steelcasting revealed that there were no off-corner cracks. In the second campaign at Chaparral Steel, however, off-corner cracks were found in most billets typically 5—6 mm from the surface of the billet and 15—20 mm from the corner. These cracks appeared on one or many off corner sites and the surface of the billet nearest to the root of the crack is slightly concave. Table 6.2 lists the number of cracks found at each off-corner location and the total number of cracks in a given billet section. Heats 17 and 21 did not produce any cracked billets, whereas the rest had cracks at one off-corner or the other. Figures 6.2 and 6.3 show the sulphur prints from Heat 3 (0.31%C) and Figs. 6.4 and 6.5 show those of Heat 7 (0.45%C). In spite of the equiaxed structure in the off-corner region, billets from Heat 9 (0.19%C) exhibit numerous cracks as shown in Figs. 6.6 and 6.7.
6.3 OSCILLATION MARKS

The depth of the oscillation marks on the two opposite faces corresponding to the straight walls of the mold was measured by using a profilometer. The two curved faces were not characterized because they were deformed by the pinch rolls. Table 6.1 lists the measured depths at two off-corner levels and the midface of billets from Eastern Steelcasting. There is considerable variation of the depth of oscillation marks across a given face; often the depth at the off-corner location is larger than at the midface.

Fig. 6.8 shows a plot of the average depth of the oscillation marks versus the carbon content of steel. It is clear that higher carbon steels have much shallower oscillation marks compared to the 0.05-0.09%C steels.

It is interesting to note that there is little difference between the stirred and the unstirred billets in terms of the depth of these marks, variation of the depth across a given face or the local maximum. Figures 6.9—6.11 show the stirred and unstirred billet surfaces from Heats 3 (0.35%C), 10 (0.05%C) and 12 (0.61%C). The oscillation marks on the stirred billet are similarly shaped reflective of the meniscus profile set up by the rotating steel in the mold. Also as is evident from Figs. 6.9—6.11, in all carbon ranges, the shape of the oscillation marks on the unstirred billets is highly irregular.
6.4 CAST STRUCTURE

The cast structure of the billet samples was analyzed in terms of the columnar-to-equiaxed transition and the substructure of the steel solidifying close to the mold wall. In this section, the results of the measurements and characteristics of the structures are presented together with the calculations of shell thickness and gradients at the solid-liquid interface made with the help of a one-dimensional transient solidification model.

6.4.1 Eastern Steelcasting

The billets cast at Eastern Steelcasting were of 101.6mm square section as mentioned earlier. Table 6.3 lists the columnar zone length measured from the two opposite faces of the billets corresponding to the inside and outside radii of the machine. The length of the columnar zone measured from the outside radius was plotted against the carbon content of the steel in Fig. 6.12. In the case of the 0.05-0.09%C steels, there was no equiaxed zone and the columnar dendrites had grown to the centre of the unstirred billet. With stirring, however, the columnar-to-equiaxed transition was triggered at about 20-25mm from the outside curved wall. In the case of the 0.61%C steel, while the unstirred billet had no equiaxed zone, the length of columnar zone reduced to 15mm with stirring. Figure 6.13 shows the presence of a branched dendritic structure after the cessation of the columnar dendritic front in the stirred billet. In the absence of stirring, however, the columnar dendrites grow to the centre of the billet with increased primary dendrite arm spacing and extensive secondary arm growth as shown in
Fig. 6.14. In the case of 0.31-0.35%C there is an extensive branched columnar zone even in the absence of stirring, which further increases with the introduction of the EMS.

For low- and high-carbon steel billets, which are not stirred the length of the columnar zone is similar on the two opposite sides of the billet corresponding to the inside and outside radii of the machine. Both columnar fronts reach the geometric centre of the transverse slice of the billet as shown in Fig. 6.15. In the medium-carbon range (0.31-0.35%), however, there is a substantial difference between the two columnar zone lengths. The columnar front ceases to grow earlier on the outside radius than on the inside radius.

In the case of the stirred billets, however, the difference between the two columnar zones varies differently with carbon content. In 0.31-0.35% carbon steels and 0.61% carbon steel, the columnar zone length measured from both the inside radius face or the outside radius face are similar, as shown in Fig. 6.16. In the case of the low-carbon grades, there is a substantial difference in the length of the columnar dendritic zone measured from the two sides. The columnar-equiaxed transition was closer to the outside radius face as opposed to the inside radius face.

A careful comparison of the difference of the columnar zone length on the two faces of the etched transverse slices shown in Figs. 6.15 and 6.16 reveals that the difference between the two diminishes as the columnar zone adjacent to the outside radius face decreases. The difference also is small when the
columnar zone has grown to the centre from the bottom face.

It is well known that the effect of superheat on the size of the equiaxed zone is quite strong. The relation between superheat in the tundish and the length of columnar zone was also examined in this study. But in the range of superheats under which these billets were cast, i.e., 45.3°C to 85.3°C, a correlation was not observed between superheat and the length of the columnar zone. The most dominant influence seems to be only that of the carbon content of steel and whether the billet was stirred or not.

The structure of the steel close to the surface of the billet was examined by polishing and etching longitudinal sections. Figures 6.17 and 6.18 show the subsurface structures of stirred and unstirred billets from the Heat 10B (0.05%C). In these billets, columnar dendrites begin to grow from very close to the surface. In the stirred billet, the columnar dendrites are on the same plane perpendicular to the mold wall for shell thickness of 1mm and then bend into the flow of the steel. In the case of Heat 3A (0.35%C), both the stirred (Fig. 6.19) and the unstirred billet (Fig. 6.20) show a fine equiaxed crystal zone adjacent to the surface. After the chill structure the unstirred billet has fine columnar dendritic zone whereas the stirred billet has columnar crystals which bend into the flow after 1.0 to 1.5 mm. Similar structures can be observed in both stirred and the unstirred billets from the Heat 12(0.61%C) as shown in Figs. 6.21 and 6.22. In all the high-carbon billets a large chill zone is seen in the vicinity of the oscillation marks.

The sub-surface etches were also examined to observe possible
differences in the initial solidification phenomenon at the meniscus between stirred and unstirred billets. There was no evidence of meniscus "hooks" in the low carbon steels (0.05-0.09%C). In the case of 0.35%C and 0.61%C steels beneath several oscillation marks meniscus solidification took place as shown in Figs. 6.18 to 6.21 both in the case of stirred and unstirred billets.

Structural details of the macroetched billet sections from Heats 1 to 4 (0.33-0.35%C) were examined to determine the difference in the progress of solidification under the EMS induced flow and otherwise. The location of the "white band" and the "dark band" was noted in terms of its distance from the nearest edge and is presented in Table 6.4. It is clear that the difference in shell growth is negligible between stirred and unstirred billets.

6.4.2 Chaparral Steel Company

The length of the columnar zone measured from the two faces of the billet corresponding to the inside and outside radii of the machine are presented in Table 6.5. Heats 19 (0.40%C), 20 (0.14%C) and 21 (0.17%C) are resulphurised grades containing 0.094 to 0.119% sulphur; the remaining are of plain carbon and low alloy grades. There is a considerable variation in the superheat values calculated as the difference of tundish temperature and the liquidus temperature of the steel. The superheats range from as low as 3°C to a high of 52.9°C.

The relation between the length of the columnar zone and the
superheat of the steel was examined first. Figure 6.23 shows a plot of the length of the columnar zone measured from the outside curved wall in the stirred billet samples for the different carbon ranges and the resulphurised grades. It is clear from this plot that higher superheats result in a delayed columnar-to-equiaxed transition. Figure 6.24 is a similar plot for the same billets, but shows the length of columnar zone measured from the inside radius. In spite of the large scatter, it can be seen that at high superheats there is increased columnar growth. Also it is observed that the different carbon ranges occupy different levels of the scatter band.

In the case of the unstirred billets, a similar relation between the length of columnar zone measured from the outside radius face and the superheat in the steel is shown in Fig. 6.25. In the case of the columnar zone length measured from the inside radius face (Fig. 6.26), however, when the superheat is lower than 20°C, the columnar zone is much shorter than for superheats above 20°C. At higher superheats, the columnar zone changes only marginally with increase in superheat.

A comparison of the columnar zone length (measured from the inside curved wall) between the stirred and unstirred billets is shown in Fig. 6.27 as a function of the carbon content of steel. In the case of heats with superheats greater than 20°C, at all carbon levels, the stirred billets have a shorter columnar zone length. In both stirred and unstirred billets, the length of columnar zone is smaller in the 0.21-0.39% carbon steels compared to the steels with lower or higher carbon ranges. In the case of heats with superheats less than 20°C, the columnar zones are shorter, and there is little difference in the stirred and unstirred billets.
Figure 6.28 shows a plot of the length of columnar zone measured from the outside curved wall in the case of stirred and unstirred billets. In unstirred billets, the length of the columnar zone decreases as carbon is increased from 0.17 to 0.37, but increases at 0.40% carbon. The dependence of the columnar zone (from outside radius) on the carbon in the steel appears to be of a similar nature as the dependence of the inside radius columnar zone. With stirring, however, the influence of carbon on the length of columnar zone measured from the outside curved wall is not well defined. For heats with superheats lower than 20°C, there is little difference in the extent of columnar zone (either on the outside radius or on the inside radius) in stirred and unstirred billets.

In resulphurised heats, Heat 19 with 0.40%C steel and 0.119wt% sulphur has etched structures similar to the plain higher carbon steels (0.40-0.50%C) in terms of the extent of the columnar dendritic zone. In Heat 20 with 0.14%C and 0.117%S, as can be seen in Table 6.5, the columnar zone is long even at low superheat. Also, in Heat 29 (0.17%C and 0.094%S) longer columnar zones resulted in spite of low superheat values.

The length of the columnar zone measured from the inside radius is plotted against that measured from the outside radius in Fig. 6.29 for the stirred billets and in Fig. 6.30 for the unstirred billets. It can be seen from Fig. 6.29 in stirred billets, the columnar zone length on the inside curved wall is slightly longer than the length of the columnar zone on the outside curved wall. In the unstirred billets, however, the relation between the columnar zone length on the two radii at high superheats is different from that at superheats less than 20°C.
The difference in the columnar zone length on the two radii is plotted against the columnar zone length measured from the outside radius for the stirred billets in Fig. 6.31 and for the unstirred billets in Fig. 6.32. In the stirred billets, the columnar zone length on the outside curved wall did not exceed 35mm and the difference in the upper and lower columnar zone lengths appears to be only dependent on the carbon content of steel as shown in Fig. 6.33. In the unstirred billets, the columnar zone from the outside radius is often longer than 35mm. As shown in Fig. 6.32, the difference in the two columnar zone lengths decreases steadily to zero as the columnar zone on the outside radius extends to the geometric centre of the transverse billet section.

The variation in the difference in the length of the two columnar zones was also examined in relation to the superheat. At low superheats (20°C), the difference in the columnar zone length on the two radii is small in both stirred and unstirred billets. At higher superheats the difference in the lengths of the two columnar fronts was unrelated to the superheat in the steel.

As was shown earlier, the columnar zone from outside radius was quite independent of the carbon content of steel whereas the columnar zone from the other radius was influenced by the composition. Thus the difference in the columnar lengths is large in the lower and higher carbon steels while the medium-carbon grades exhibit a smaller difference. Figure 6.34 shows a plot of the difference of the columnar fronts for unstirred billets. In low- and high-carbon grades, the difference is much smaller than in the case of the medium-carbon steels. It must, however, be pointed that the smaller difference in the columnar
lengths in unstirred billets are a result of long columnar fronts which have grown to the centre of the billet. In the case of stirred billets, smaller differences in the medium-carbon grades are result from shorter columnar fronts.

The arrest of the columnar dendritic growth on the outside curved wall and the inside curved wall in the unstirred billets of Heat 4 is shown in Figs. 6.35 and 6.36. The columnar front is sharply terminated on the outside curved wall at 30mm from the surface of the billet (Fig. 6.35). On the inside curved wall (Fig. 6.36), although the columnar dendritic front stops at about 45mm, it is interesting to note that at 30-31mm from the surface random branching of the columnar front appears as well as considerable coarsening of the columnar dendrites. While on the outside radius face, the branched columnar growth had stopped the columnar front entirely, on the inside radius face the majority of the columnar dendrites continue to grow and only some of the individual columnar dendrites are stopped. Figures 6.37 and 6.38 show the columnar-to-equiaxed transition for stirred billets of Heat 4. On the outside curved wall, as well as on the inside curved wall, the columnar dendrites terminate their growth at 24-25mm. However, few of the columnar dendrites continue to grow further into the equiaxed area from the inside curved wall.

Figures 6.39 to 6.41 show a series of photographs of the columnar-to-equiaxed transition adjacent to the outside and inside curved walls for the stirred and unstirred billets of Heat 8 (0.30%C). As in the case of Heat 4, it can be seen that the columnar-to-equiaxed transition takes place earlier on the outside curved wall than on the inside curved wall. But at the same time when
the columnar dendrites are arrested on the outside curved wall, there is considerable branching of the columnar dendrites on the inside curved wall and a majority of the columnar dendrites continue to grow around clusters of branched columnar growth. With stirring, however, the columnar-to-equiaxed transition takes place earlier at 23mm on the outside curved wall and 26-27mm on the inside curved wall. In the case of the stirred billet structure on the inside curved wall, it can be seen that between 23 and 26mm, the columnar front is not inclined as before and some branched columnar crystals can be found in this area.

The columnar growth for stirred and unstirred 0.50%C steel (Heat 6) is presented in Figs. 6.43—6.46. Unlike the case of 0.31%C steels (Heats 4 and 8), the unstirred billets produce columnar zones that almost reach the centre with extensive secondary arm growth. Adjacent to the outside curved wall (Fig. 6.43) the columnar front terminates and an extensive branched columnar dendritic array appears, whereas the initial columnar front continues to grow to the centre (Fig. 6.44). A similar observation can be made in the stirred billets of Heat 6 (Figs. 6.45 and 6.46). Although the columnar dendritic zone is shorter than in the unstirred billets, there is considerable difference in the columnar zone on the inside and outside curved wall.

Based on the data presented in Table 6.5 the relation between the frequency of EMS and the length of columnar zone was investigated. There appears to be no noticeable difference in the length of the columnar dendritic zone grown in melts stirred at different frequencies.
6.4.3 Model Predictions

In order to examine the effects of stirring in the liquid pool on superheat removal and the gradients ahead of the solidification front, the one-dimensional 1-D transient heat-flow model was applied with several imposed conditions. For these calculations the mold heat-flux determined from Heat 3A (0.31%C) cast at Chaparral Steel was selected. The spray heat-flux, was obtained from the correlation of Sasaki et al\textsuperscript{114} where the heat transfer coefficient, $h$, is calculated as

$$h = 708 W^{-0.75} T_b^{-1.2} + 0.116$$  \hspace{1cm} (6.1)

where,

$\dot{W}$ = Spray water flux

$T_b$ = Surface temperature of the billet.

The spray water flux was determined as 6.8 l/s.m\textsuperscript{2} based on average spray water consumption of 7.57 l/s (120 gpm).

The heat-flux profile employed in the model to calculate solidification parameters is shown in Fig. 6.47. The parameters selected for examination included the shell profile, change in the gradient at the solidification front (°C/cm), and the fraction of solid in the mushy zone. Recent investigation by Mahapatra\textsuperscript{115} has shown that the gradient at the solidification front, i.e., the liquidus isotherm, $G$ (calculated as per Eq.6.2) is an important parameter and that
the columnar-to-equiaxed transition could be triggered if $G$ is reduced to $1.0 \, ^\circ C/cm$ or lower.

\[ G = \frac{T_i - T_{i-1}}{\Delta X} \]  

where,

- $T_i$ is the temperature of the first node below the liquidus temperature
- $T_{i-1}$ is the temperature of the neighbouring node above the liquidus temperature.
- $\Delta X$ is the node size.

The fraction of solid in the mushy zone ($f_s$) is determined from

\[ f_s = \frac{T_1 - T_i}{T_1 - T_s} \]

where,

- $T_1$ = liquidus temperature
- $T_s$ = solidus temperature
- $T_i$ = temperature of the node in the mushy zone.

The calculations were performed, first of all, for stagnant pool conditions by assuming that the thermal conductivity of liquid steel is the same as that of solid steel. In Fig. 6.48 the growth of solid shell and the change in the gradient at the solid-liquid interface are plotted as a function of distance from top
of the mold and dwell time in the machine. The shell thickness at which the gradients come to zero is approximately 33mm for a superheat of 80°C.

Figure 6.49 shows profiles of the width of mushy zone corresponding to different fractions of solid. The width of mushy zone is determined at every time step corresponding to different fractions of solid (0.1, 0.1-0.2, 0.2-0.4, and 0.4-0.6). The sudden increase in the mushy zone width corresponding to 0.1 fraction, at the point where the gradients come to zero, indicates that the centre of the pool is under the liquidus from this time onwards and progressive solid growth brings about the reduction in the width of this mushy zone. It is also clear that at this instant when gradients ahead of the solidification front become close to zero, all the superheat is lost in the remaining liquid and the progress of solidification is not unidirectional from thereon. With the provision of nuclei in the melt, solidification can proceed at sub-liquidus temperatures provided the heat transfer coefficient at the surface of the billet is enabling the withdrawal of latent heat. In Fig. 6.50, the shell profile and the gradients are plotted on the 7.9m (26ft) radius machine of the Chaparral Steel Company. It is clear that the full superheat removal is accomplished approximately at the midpoint of the curved machine at a shell growth of 33mm.

The model was run for several values of superheats in the liquid; the shell thickness corresponding to complete superheat removal is plotted in Fig. 6.51. It can be seen that with the thermal conductivity of liquid equal to that of the solid (stagnant conditions), beyond 10°C the influence of the magnitude of superheat on the location in the machine where it is removed from the melt is
minimal.

The apparent thermal conductivity in liquid was increased to seven times that of the solid and the calculations were once again performed to simulate mixing in the pool. Figure 6.51 shows that the melt superheat is removed much earlier compared to the previous case.

In order to see the effects of stirring on the thermal conditions of the central pool, the width of the thermal boundary was calculated after estimating the bulk velocity of the liquid steel $V_\infty$ and the momentum boundary layer thickness ($\delta$) given by,

$$
\delta = \frac{5.0 \times x}{\sqrt{(V_\infty x)/\nu}}
$$

where,

$x$ = distance from the leading edge

$\nu$ = Kinematic viscosity of steel

The velocity of the liquid steel was obtained by measuring the nominal deflecting angle of the dendrites and using the correlation plot developed by Takahashi shown in Fig. 2.11. The average deflection angle measured from macroetches is 30° and the resulting velocity is approximately 30cm/s. Using these values the momentum boundary layer thickness was calculated and the thermal boundary
layer thickness given by the following equation was used in the model.

\[ \delta_T = \frac{\delta}{(Pr)^{1/3}} \]

\[ \delta \approx \frac{1.026}{Pr} \]

where,

\[ Pr = \text{Prandtl number for liquid steel.} \]

In the model, the thermal boundary was placed in front of the solid-liquid interface with the assumption that the penetration of the flowing steel into the interdendritic array is negligible. All the model temperatures outside the thermal boundary layer were averaged after every time step and the model was run until the transverse one-dimensional slice moves to the straightening zone of the machine. The results are shown in Fig. 6.51. As can be seen, the removal of superheat from the melt in this case is not as effective as raising the thermal conductivity of the liquid steel. Also, the magnitude of the superheat (between 0-40°C) has a strong influence on the shell thickness corresponding to full removal of superheat from the melt. Beyond 40°C superheat, however, the influence is minimal.

The model was also run by combining a raised liquid thermal conductivity and back mixing of the liquid pool. The results show that the most effective superheat removal can be accomplished by this combination. With the exit shell thickness from the mold at 9-10mm, all the superheat is removed within or just outside the mold.
By limiting the backmixing to within the mold, however, the shell thickness corresponding to full superheat removal shifts to larger values after 20°C. It can be inferred from this that with the best possible thermal conditions in the liquid melt, the mold can completely remove about 20°C of superheat.

Based on the results of this mathematical model, the following conclusions can be made:

1. Irrespective of its magnitude, all the superheat in the melt can be removed by the time a 34mm shell has formed even in the case of a completely stagnant pool.

2. Either raising the thermal conductivity of the liquid steel or incorporation of backmixing outside the thermal boundary layer or a combination of the two increases the efficiency of superheat removal. Raising the thermal conductivity in the liquid applies to the unstirred casting process where the fluid flow is essentially because of the impingement of the input stream. The case of backmixing combined with a raised apparent thermal conductivity in the liquid corresponds more closely to the electro-magnetically mixed pool.

3. By involving back mixing and raised liquid thermal conductivity it is possible for the mold to remove 20°C superheat from the melt.
Table 6.1. Rhomboidity and the depth of oscillation marks of billets cast during the industrial trial at the Eastern Steelcasting.

<table>
<thead>
<tr>
<th>Heat Number</th>
<th>Carbon Number</th>
<th>Sample Number</th>
<th>Stirred or Unstirred</th>
<th>Superheat (°C)</th>
<th>Rhomboidity (mm)</th>
<th>Depth of oscillation marks in micro meters</th>
<th>Left straight face</th>
<th>Right straight face</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Carbon</td>
<td>Sample Number</td>
<td>Stirred or Unstirred</td>
<td>Superheat (°C)</td>
<td>Rhomboidity (mm)</td>
<td>Off-corner near inside radius</td>
<td>Off-corner near outside radius</td>
<td>Off-corner near inside radius</td>
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<td>1</td>
<td>0.32</td>
<td>A</td>
<td>Stirred</td>
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<td>195 312 671 176 135 498</td>
<td>218 273 183 472 195 356</td>
<td></td>
</tr>
<tr>
<td></td>
<td>B</td>
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<td></td>
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</tr>
<tr>
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<td>347 160 373 174 195 175</td>
<td>201 362 610 213 356 236</td>
<td></td>
</tr>
<tr>
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</tr>
<tr>
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<td>246 250 191 271 301 279</td>
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<tr>
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<td></td>
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</tr>
<tr>
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<tr>
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</tr>
<tr>
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<td>46.6</td>
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<tr>
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"sign" for rhomboidity is as shown in Fig.4.1. (contd... page 254)
Table 6.1 (contd..) Rhomboidity and the depth of oscillation marks of billets cast during the industrial trial at the Eastern Steelcasting.

<table>
<thead>
<tr>
<th>Heat Number</th>
<th>Sample Number</th>
<th>Stirred/Unstirred</th>
<th>Superheat (Deg C)</th>
<th>Rhomboidity (mm)</th>
<th>Left straight face</th>
<th>Right straight face</th>
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<td>Off-corner near inside radius</td>
<td>Midface</td>
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"*sign* for rhomboidity is as shown in Fig.4.1. (End of Table 6.1)
Table 6.2. Off-rectangularity and number of off-corner cracks in billets cast during the industrial trial at the Chaparral Steel Company.

<table>
<thead>
<tr>
<th>Heat Number</th>
<th>Carbon Number</th>
<th>Sample Number</th>
<th>Stirred or Unstirred</th>
<th>Superheat (Deg C)</th>
<th>Off-rectangularity (mm)</th>
<th>Off - Corner Cracks</th>
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<td>Unstirred</td>
<td>21.5</td>
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<td>Unstirred</td>
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<td>+3.0</td>
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**sign** for off-rectangularity and "location" of off-corner cracks are shown in Fig. 4.1. (contd... Page. 256)
Table 6.2.(contd..) Off-rectangularity and number of off-corner cracks in billets cast during the industrial trial at the Chaparral Steel Company.

<table>
<thead>
<tr>
<th>Heat Number</th>
<th>Carbon</th>
<th>Sample Number</th>
<th>Stirred or Unstirred</th>
<th>Superheat (Deg C)</th>
<th>Off-rectangularity (mm)</th>
<th>Off-Corner Cracks</th>
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<tr>
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<td></td>
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<td></td>
<td></td>
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<td>44.5</td>
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<td>Unstirred</td>
<td>44.5</td>
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<td></td>
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<td>Stirred</td>
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<td>0 0 2 0 1 2 0 1 6</td>
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<td>Stirred</td>
<td>23.0</td>
<td>-5.0</td>
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<td></td>
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<td>Stirred</td>
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<td>-1.5</td>
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<td>11.1</td>
<td>Stirred</td>
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<td>Unstirred</td>
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<td>+1.5</td>
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<td>-0.5</td>
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<td>Unstirred</td>
<td>38.0</td>
<td>-3.0</td>
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<td>Stirred</td>
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<td>-4.0</td>
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<td>Unstirred</td>
<td>37.4</td>
<td>-6.0</td>
<td>0 0 5 0 3 3 0 0 17</td>
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<td>0.23</td>
<td>16.1</td>
<td>Stirred</td>
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<td>-5.5</td>
<td>0 0 2 0 3 1 0 3 8</td>
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<td></td>
<td>16.2</td>
<td>Unstirred</td>
<td>52.1</td>
<td>-6.0</td>
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<td></td>
<td>16.3</td>
<td>Stirred</td>
<td>50.4</td>
<td>-5.0</td>
<td>0 0 2 0 2 2 0 0 6</td>
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<td>Unstirred</td>
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<td>-4.5</td>
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<td></td>
<td>16.5</td>
<td>Stirred</td>
<td>40.9</td>
<td>-2.5</td>
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<td></td>
<td>16.6</td>
<td>Unstirred</td>
<td>40.9</td>
<td>-3.5</td>
<td>0 0 2 0 0 0 0 1 3</td>
</tr>
</tbody>
</table>

*"sign" for off-rectangularity and "location" of off-corner cracks are shown in Fig.4.1.
| Heat Number | Carbon Number | Sample Number | Number of off-corner cracks | Number of off-rec. Superheat | Number of off-rec. Heater | (Deg C) Temperature (°F) | Total Number of Cracks | Heat Number | Carbon Number | Sample Number | Number of off-corner cracks | Number of off-rec. Superheat | Number of off-rec. Heater | (Deg C) Temperature (°F) | Total Number of Cracks |
|-------------|---------------|---------------|-----------------------------|-----------------------------|----------------------------|--------------------------|--------------------------|-------------|---------------|----------------|-----------------------------|-----------------------------|----------------------------|--------------------------|--------------------------|--------------------------|
| 22          | 0.17          | 22.1          | 22.2                       | 22.3                        | 22.4                       | 22.5                       | 22.6                       | 22.7                       | 22.8                       | 22.9                       | 22.10                       | 22.11                       | 22.12                       | 22.13                       | 22.14                       |
| 20          | 0.17          | 20.1          | 20.2                       | 20.3                        | 20.4                       | 20.5                       | 20.6                       | 20.7                       | 20.8                       | 20.9                       | 20.10                       | 20.11                       | 20.12                       | 20.13                       | 20.14                       |
| 18          | 0.61          | 18.1          | 18.2                       | 18.3                        | 18.4                       | 18.5                       | 18.6                       | 18.7                       | 18.8                       | 18.9                       | 18.10                       | 18.11                       | 18.12                       | 18.13                       | 18.14                       |
| 17          | 0.82          | 17.1          | 17.2                       | 17.3                        | 17.4                       | 17.5                       | 17.6                       | 17.7                       | 17.8                       | 17.9                       | 17.10                       | 17.11                       | 17.12                       | 17.13                       | 17.14                       |
Table 6.3. Length of columnar zone in billets cast during the industrial trial at Eastern Steelcasting.

<table>
<thead>
<tr>
<th>Heat Number</th>
<th>Carbon</th>
<th>Sample Number</th>
<th>Superheat (Deg C)</th>
<th>Length of Columnar Zone (mm)</th>
<th>Outside radius</th>
<th>Inside radius</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>0.32</td>
<td>A Stirred</td>
<td>45.3</td>
<td>15</td>
<td>16</td>
<td>17</td>
</tr>
<tr>
<td></td>
<td></td>
<td>B Unstirred</td>
<td></td>
<td>27</td>
<td></td>
<td>40</td>
</tr>
<tr>
<td>2</td>
<td>0.33</td>
<td>A Stirred</td>
<td>59.5</td>
<td>17</td>
<td>17</td>
<td>47</td>
</tr>
<tr>
<td></td>
<td></td>
<td>B Unstirred</td>
<td></td>
<td>26</td>
<td></td>
<td></td>
</tr>
<tr>
<td>3</td>
<td>0.35</td>
<td>A Stirred</td>
<td>64.9</td>
<td>17</td>
<td>18</td>
<td></td>
</tr>
<tr>
<td></td>
<td></td>
<td>B Unstirred</td>
<td></td>
<td>25</td>
<td>45</td>
<td></td>
</tr>
<tr>
<td></td>
<td></td>
<td>C Unstirred</td>
<td></td>
<td>28</td>
<td>40</td>
<td></td>
</tr>
<tr>
<td></td>
<td></td>
<td>D Unstirred</td>
<td></td>
<td>16</td>
<td>17</td>
<td></td>
</tr>
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<td>4</td>
<td>0.34</td>
<td>A Stirred</td>
<td>66.7</td>
<td>15</td>
<td>15</td>
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</tr>
<tr>
<td></td>
<td></td>
<td>B Unstirred</td>
<td></td>
<td>29</td>
<td>47</td>
<td></td>
</tr>
<tr>
<td></td>
<td></td>
<td>C Stirred</td>
<td></td>
<td>17</td>
<td>17</td>
<td>50</td>
</tr>
<tr>
<td></td>
<td></td>
<td>D Unstirred</td>
<td></td>
<td>31</td>
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</tr>
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<td></td>
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<td></td>
<td>50</td>
<td>50</td>
<td>50</td>
</tr>
<tr>
<td></td>
<td></td>
<td>C Unstirred</td>
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<td>50</td>
<td>50</td>
<td>50</td>
</tr>
<tr>
<td></td>
<td></td>
<td>D Unstirred</td>
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<td>50</td>
</tr>
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<td>50</td>
<td></td>
</tr>
<tr>
<td></td>
<td></td>
<td>C Stirred</td>
<td></td>
<td>50</td>
<td>50</td>
<td>50</td>
</tr>
<tr>
<td></td>
<td></td>
<td>D Unstirred</td>
<td></td>
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<td>50</td>
<td>50</td>
</tr>
<tr>
<td>7</td>
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<td>36</td>
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</tr>
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<td></td>
<td>B Unstirred</td>
<td></td>
<td>50</td>
<td>50</td>
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</table>

(contd... next page)
Table 6.3a. Length of columnar zone in billets cast during the industrial trial at Eastern Steelcasting.

<table>
<thead>
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<th>Heat Number</th>
<th>Carbon Number</th>
<th>Sample Number</th>
<th>Stirred or Unstirred</th>
<th>Superheat (Deg C)</th>
<th>Length of Columnar Zone (mm)</th>
<th>Outside radius</th>
<th>Inside radius</th>
</tr>
</thead>
<tbody>
<tr>
<td>8</td>
<td>0.06</td>
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<td>Stirred</td>
<td>62.9</td>
<td>21</td>
<td>32</td>
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</tr>
<tr>
<td></td>
<td></td>
<td>B</td>
<td>Unstirred</td>
<td></td>
<td>50</td>
<td>50</td>
<td></td>
</tr>
<tr>
<td></td>
<td></td>
<td>C</td>
<td>Stirred</td>
<td>58.5</td>
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<td>32</td>
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</tr>
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<td></td>
<td></td>
<td>D</td>
<td>Unstirred</td>
<td></td>
<td>50</td>
<td>50</td>
<td></td>
</tr>
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<td>33</td>
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</tr>
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<td></td>
<td>50</td>
<td>50</td>
<td></td>
</tr>
<tr>
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<td>C</td>
<td>Stirred</td>
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</tr>
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<td>Unstirred</td>
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</tr>
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<td></td>
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<td>C</td>
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</tr>
<tr>
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<td>D</td>
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</tr>
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<td>Unstirred</td>
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<td>50</td>
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</tr>
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<td></td>
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<td>50</td>
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<td>Stirred</td>
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<td>D</td>
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Table 6.4. Location of white and dark solidification bands in stirred and unstirred billets cast at the Eastern Steelcasting trial.

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<th>Heat Number</th>
<th>Carbon Number</th>
<th>Sample Number</th>
<th>Stirred or Unstirred</th>
<th>Location of white band (mm)</th>
<th>Location of dark band (mm)</th>
</tr>
</thead>
<tbody>
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<td>1</td>
<td>0.32</td>
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<td>Stirred Unstirred</td>
<td>5.0</td>
<td>8.5</td>
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<td></td>
<td></td>
<td>Unstirred</td>
<td>4.0</td>
<td>9.0</td>
</tr>
<tr>
<td>2</td>
<td>0.33</td>
<td>A B</td>
<td>Stirred Unstirred</td>
<td>4.5</td>
<td>8.5</td>
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<td></td>
<td></td>
<td>Unstirred</td>
<td>5.0</td>
<td>8.0</td>
</tr>
<tr>
<td>3</td>
<td>0.35</td>
<td>A B C D</td>
<td>Stirred Unstirred</td>
<td>5.5</td>
<td>9.0</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td>Unstirred</td>
<td>5.0</td>
<td>9.5</td>
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<td>Stirred</td>
<td>4.5</td>
<td>9.5</td>
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<td>Unstirred</td>
<td>5.0</td>
<td>9.5</td>
</tr>
<tr>
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<td>3.25</td>
<td>8.5</td>
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<td></td>
<td></td>
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<td>4.5</td>
<td>9.0</td>
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<td></td>
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<td>Stirred</td>
<td>5.0</td>
<td>9.0</td>
</tr>
<tr>
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<td></td>
<td></td>
<td>Unstirred</td>
<td>4.5</td>
<td>8.25</td>
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</table>
Table 6.5. Length of columnar zone in billets cast at the Chaparral steel company during the second industrial trial.

<table>
<thead>
<tr>
<th>Heat Number</th>
<th>Carbon</th>
<th>Sample Number</th>
<th>Stirred or Unstirred</th>
<th>Frequency of EMS (Hz)</th>
<th>Length of Columnar Zone (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td></td>
<td>Stirred Unstirred</td>
<td>Outside radius</td>
<td>Inside radius</td>
</tr>
<tr>
<td>1</td>
<td>0.36</td>
<td>1.2</td>
<td>Stirred</td>
<td>6</td>
<td>24</td>
</tr>
<tr>
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<td></td>
<td>1.2</td>
<td>Unstirred</td>
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<td>30</td>
</tr>
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<td>Stirred</td>
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<td>24</td>
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<tr>
<td></td>
<td></td>
<td>2.2</td>
<td>Unstirred</td>
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<td>3</td>
<td>0.31</td>
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<td>4</td>
<td>11</td>
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Table 6.5a. Length of columnar zone in billets cast at the Chaparral steel company during the second industrial trial.

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Table 6.5b. Length of columnar zone in billets cast at the Chaparral steel company during the second industrial trial.

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Figure 6.1 Variation of rhomboidity of 42 billet sections cast during a 12-heat campaign at Eastern Steelcasting.
Figure 6.2 Sulphur print of billet sample from Heat 3 - (0.31% C) - stirred at 4-Hz.
Figure 6.3 Sulphur print of billet sample from Heat3 - (0.31%C) - Unstirred.
Figure 6.4 Sulphur print of billet sample from Heat 7 - (0.45% C) - Unstirred.
Figure 6.5 Sulphur print of billet sample from Heat 7 - (0.45% C) - Stirred at 4 Hz.
Figure 6.6 Sulphur print of billet sample from Heat9 - (0.19%C) - Stirred at 10Hz.
Figure 6.7 Sulphur print of billet sample from Heat 9 - (0.19% C) - Unstirred.
Figure 6.8 Influence of carbon content on the depth of oscillation marks on the billets cast at Eastern Steelcasting.
Figure 6.9 Oscillation marks on the billet samples from Heat3 - (0.31% C).
Figure 6.10 Oscillation marks on the billet samples from Heat 12 - (0.61% C).
Figure 6.11 Oscillation marks on the billet samples from Heat10 - (0.05% C).
Figure 6.12
Plot of length of columnar zone measured from the outside radius face vs. the carbon content in the steel.
Figure 6.13 Macroetch of the stirred transverse section from Heat 12 - (0.61% C).
Figure 6.14 Macrotetch of the unstirred transverse section from Heat12 - (0.61% C).
Figure 6.15 Comparison of the lengths of columnar zone from the inside and outside radius faces in unstirred billets cast at Eastern Steelcasting.
Figure 6.16 Comparision of the lengths of columnar zone from the inside and outside radius faces in stirred billets cast at Eastern Steelcasting.
Figure 6.17 Subsurface etch of the unstirred low carbon (0.05%) billet from Eastern Steelcasting.
Figure 6.18 Subsurface etch of the stirred low carbon (0.05%) billet from Eastern Steelcasting.
Figure 6.19 Subsurface etch of the stirred medium carbon (0.31%) billet (Heat3) from Eastern Steelcasting.
Figure 6.20 Subsurface etch of the un stirred medium carbon (0.31%) billet (Heat 3) from Eastern Steelcasting.
Figure 6.21 Subsurface etch of the stirred high carbon (0.61%) billet (Heat 12) from Eastern Steelcasting.
Figure 6.22 Subsurface etch of the unstirred high carbon (0.61%) billet (Heat12) from Eastern Steelcasting.
Figure 6.23 Relation between the length of columnar zone measured from the outside radius face and the tundish superheat for the stirred billets from Chaparral Steel.
Figure 6.24 Relation between the length of columnar zone measured from the inside radius face and the tundish superheat for the stirred billets from Chaparral Steel.
Figure 6.25 Relation between the length of columnar zone measured from the outside radius face and the tundish superheat for the unstirred billets from Chaparral Steel.
Figure 6.26 Relation between the length of columnar zone measured from the inside radius face and the tundish superheat for the unstirred billets from Chaparral Steel.
Figure 6.27 Effect of carbon content on the length of columnar zone measured from the inside radius face in the stirred and unstirred billets from Chaparral Steel.
Figure 6.28 Effect of carbon content on the length of columnar zone measured from the outside radius face in the stirred and unstirred billets from Chaparral Steel.
Figure 6.29  Plot of length of columnar zone measured inside radius face vs. the outside radius face in stirred billets from Chaparral Steel.
Figure 6.30 Plot of length of columnar zone measured inside radius face vs. the outside radius face in unstirred billets from Chaparral Steel.
Figure 6.31  Plot of the difference in the lengths of columnar zone measured inside and outside radius faces vs. the length of columnar zone on the outside radius for stirred billets from Chaparral Steel.
Figure 6.32  Plot of the difference in the lengths of columnar zone measured inside and outside radius faces vs. the length of columnar zone on the outside radius for unstirred billets from Chaparral Steel.
Figure 6.33 Plot of the difference in the lengths of columnar zone measured inside and outside radius faces vs. carbon content of steel for stirred billets from Chaparral Steel.
Figure 6.34  Plot of the difference in the lengths of columnar zone measured inside and outside radius faces vs. carbon content of steel for unstirred billets from Chaparral Steel.
Figure 6.35 Macroetch of transverse billet section from the Heat4 - unstirred - 0.31%C showing the structure on the outside radius - midface.
Figure 6.36 Macroetch of transverse billet section from the Heat4 - unstirred - 0.31%C showing the structure on the inside radius - midface.
Figure 6.37 Macroetch of transverse billet section from the Heat4 - stirred - 0.31% C showing the structure on the outside radius - midface.
Figure 6.38 Macroetch of transverse billet section from the Heat 4 - stirred - 0.31% C showing the structure on the inside radius - midface.
Figure 6.39 Macroetch of transverse billet section from the Heat8 - unstirred - 0.30%C showing the structure on the outside radius - midface.
Figure 6.40 Macroetch of transverse billet section from the Heat8 - unstirred - 0.30%C showing the structure on the inside radius - midface.
6.41 Macroetch of transverse billet section from the Heat8 - stirred - 0.30% C showing the structure on the outside radius - midface.
Figure 6.42 Macroetch of transverse billet section from the Heat8 - stirred - 0.30%C showing the structure on the inside radius - midface.
Figure 6.43 Macroetch of transverse billet section from the Heat 6 - unstirred - 0.45% C showing the structure on the outside radius - midface.
Figure 6.44 Macroetch of transverse billet section from the Heat6 - unstirred - 0.45%C showing the structure on the inside radius - midface.
Figure 6.45 Macroetch of transverse billet section from the Heat6 - stirred - 0.45%C showing the structure on the outside radius - midface.
Figure 6.46 Macroetch of transverse billet section from the Heat6 • stirred • 0.45%C showing the structure on the inside radius – midface.
Figure 6.47 Heat-flux profile employed in the one-dimensional heat transfer model calculations.
Gradient At The Liquidus Isotherm (Deg.C/cm)

Figure 6.48 Shell thickness and the gradient at the liquidus isotherm during the progress of solidification.
Figure 6.49  Width of the mushy zone corresponding to fraction of solid of <0.1, 0.1-0.2, 0.2-0.4 and 0.4-0.6 and the gradient at the liquidus isotherm during the progress of solidification.
Figure 6.50 Shell thickness and the gradient at the liquidus isotherm during the progress of solidification plotted along the outside radius curve of the machine.

MIXING = 0  
KFACTOR = 1  
CARBON = 0.31 WT%  
SUPERHEAT = 80.0 DEGC.
Figure 6.51 Shell thickness where the superheat is removed vs. the magnitude of superheat for different simulated fluid flow conditions in the liquid pool.
7. DISCUSSION OF MOLD BEHAVIOR

7.1 FIRST TRIAL: EASTERN STEELCASTING

7.1.1 Water Velocity Distribution

Results of the water velocity distribution from three mold systems were presented in Section 5.1. In the case of Eastern Steelcasting and Charter Electric Melting, it is clear from Fig. 5.1, 5.3 and 5.4 and Tables 5.1 and 5.2 that at all water flow rates the velocity in the passage along the outside curved wall is lower than over the other three faces. It is also of interest to note that the water velocity is highly non-uniform on the outside curved wall as shown in Fig. 5.2.

The reason for the maldistribution in the bottom of the water channel (Tables 5.1 and 5.2) is linked to the fact that the water inlet to the mold is located on the inside curved radius side of the caster (Fig. 4.2) and the inlet plenum is small. For the flow to be uniform, the water has to redistribute in the inlet plenum and enter the water channel equally on all sides. Normally the housing for a billet mold is designed such that the inlet plenum extends from the bottom plate to about a third of the total height (700—800 mm) of the housing. As pointed out in Chapter 4 (Section 4.1, Fig. 4.2), the placement of the EMS coil within the mold housing restricts the height available for the incoming water to a mere 2.54 cm. Consequently the redistribution of the water prior to entry into the water channel is hampered resulting in maldistribution of water in the lower part.
of the water channel and a reduction in water supply to the outside curved face which is farthest from the point of water entry to the mold.

Uneven water distribution at the entry to the water channel should, however, not result in different velocities in the upper part of the water channel. Normally it would be expected that the pressure gradients between the different faces would equalize the water velocities at the top of the water channel. Calculations of flow distribution in a mold system where the entry of water into the channel on one face was blocked indicated that although widely disparate velocities existed near the inlet, a balanced velocity distribution is rapidly attained in the upper regions of the water channel. But in the case of Eastern Steelcasting and Charter Electric Melting, as shown in Fig. 3.2, an enlarged water passage exists at the corners of the mold/baffle tube. This is because the baffle tube is manufactured by welding together four steel plates resulting in square corners while the mold has rounded corners. At these corners, resistance to water flow is a minimum. As is evident in Fig. 5.2 the highest water velocity was measured at the corner bounded by the outside curved wall and the left straight wall. Such a path of least resistance to water flow at each of the four corners would isolate the four faces in terms of fluid flow and would cause each side of the water annulus to act independently with the result that uniform water velocity on all four faces is not achieved.

Results of water velocity measurements at Chaparral Steel Company (Figs. 5.5 and 5.6 and Table 5.3) support this explanation. The mold housing in this case is identical to that at Eastern Steelcasting; the inlet plenum
was shrunk to accommodate the EMS coil (Figs. 4.11 and 4.22) and the water entry to the mold is adjacent to the inside radius face with the result that the outside curved wall is farthest from the entry point. The baffle tube, once again is similarly constructed i.e., by welding four steel plates. The difference, however, was that the square corners of the baffle tube were rounded by welding in suitably contoured fillets. As is evident in Fig. 5.5, there is no noticeable difference in the water velocity on the inside curved wall which is close to the water inlet and the outside curved wall which is about 140 cm farther away from it.

As will be shown in the ensuing discussion, the lower water velocity on the outside curved wall in the case of Eastern Steelcasting led to local overheating resulting in boiling in the water channel and scale deposition. The resulting non-uniform heat-extraction from the adjacent faces has also led to non-uniform shell growth and rhomboidity. In fact, the trial had to be discontinued because the rhomboidity of the test strand was excessive and was fluctuating from one heat to the next and from sample to sample.

As seen in Fig. 5.1, there is a difference of 1 m/s in water velocity between the inside radius wall and the straight walls. This result is possibly due to the variation in the width of the water gap along the straight walls and the curved walls of the mold. In the case of Eastern Steelcasting, the water jacket was bolted on the plate separating the inlet and outlet plena (see Fig. 4.2). The mold, fitted to the top plate (with the help of the support plates that lock into the slots machined near the top of the mold), is slid into the baffle tube. It is clear that the uniformity of the mold/baffle tube gap is dependent on the
top-plate being parallel to the plate where the water jacket is fitted inside the housing to within close tolerances. These tolerances appear to be better in the case of molds from Charter Electric Melting where the velocity of the water on the inside and two straight walls was very similar (Fig. 5.3).

7.1.2 Mold Condition

Results of the mold distortion measurements for the Eastern Steelcasting trial indicate that the mold supplied for the test (as shown in Figs. 5.78—5.80) was severely distorted. The distortion was non-symmetric and the distance between the curved walls in the two off-corner regions differed by 1.5 mm and between the straight walls by 0.5 mm. While on the straight walls there was a slight taper below 350 mm (from top of the mold), the upper portion was practically devoid of any taper. The curved walls did not have the pre-set 0.8%/m taper; in addition the curved walls also exhibited severe bulging near the meniscus (100—125 mm) region. According to the log books, this mold was used only for 10 heats before the trial. As pointed out by Brimacombe et al, such severe non-symmetric distortion is a result of poor centering of the mold when the mold was initially assembled or could be due to a damaged mold liner.

After the campaign of 12 heats, the straight walls did not undergo significant changes in their shape (Fig. 5.82), but the distance between the curved walls decreased in the meniscus region (75—175 mm) and increased below 175 mm (Fig. 5.83). In fact, at precisely 125 mm from top of the mold, the curved walls caved inwards.
Mold distortion measurements between heats reflect the accumulation of plastic strain retained in the mold after the casting has ended. These measurements are an important clue to the understanding of mold behavior in response to the temperature distribution it experiences. Distortion of the billet mold is normally reflected as an outward bulge.\textsuperscript{11,10,17} Inward displacement of the mold wall is quite rare\textsuperscript{17} but does occur in extreme cases when the mold wall becomes too hot, softens and is pushed inward by the water pressure alone. It is also possible that the inward (permanent) deflection of the curved walls is a result of outward bulging of the straight walls and rotation of the corners. There is considerable evidence to suggest that the outside curved wall temperatures were close to the softening temperature and hence plastically deformed. On the outside curved wall scale deposit was found extending from about 100 mm to 180 mm at the mid face (Fig. 5.84). This is truly an indication of boiling in the cooling water channel,\textsuperscript{11} an outcome of the low water velocity on the outside curved face and poor water quality (see Section 7.1.1 and Section 7.1.3). Once the scale deposits, its effect is magnified as time progresses because it retards the heat transfer to the cooling water and increases the mold temperatures which further enhances the rate of fouling. Figures 5.19—5.40 reveal that during the 12—heat campaign, the measured mold temperatures progressively increased to reach almost 385°C in the last heat.\textsuperscript{†} Considering that the softening temperature of DHP 122 copper is only around 350—400°C,\textsuperscript{18} it is very probable that the hot face temperatures would surpass this limit and weaken the wall.

\textsuperscript{†}A reduction in the mold temperature in Heat 5A is attributable, as will further be discussed in Section 7.1.3 on mold temperatures, to a rise in the water flow rate and a reduction in the carbon content of the steel causing the boiling to temporarily subside.
It is clear from the photographs of the four walls presented in Figs. 5.84 and 5.85 that scale formation took place only on the outside curved wall. This observation reveals that the heat flow was different on the outside curved wall compared to the other faces of the mold. Indeed, lower mold wall temperatures were observed on the left straight wall (Figs. 5.24, 5.27, 5.29, 5.33 and 5.35). This differential heat transfer also led to rhomboidity of billets cast on the test strand. A discussion of this aspect is presented in Section 8.2 of Chapter 8.

7.1.3 Mold Temperature Distribution

The mold temperature profiles of the outside curved wall and the left straight wall, shown in Figs. 5.7—5.40, unambiguously show that the outside curved wall temperatures were higher than the straight wall temperature in the meniscus area (80-200 mm from the top of the mold) in all heats except Heat 5 (0.05 %C). The outside curved wall temperatures appear to increase progressively from heat to heat.

In order to understand the higher temperatures of the curved wall in the meniscus region, the focus must be on the scale deposits found on the mold wall (see the previous section) and the low water velocity measured on this wall (Section 7.1.1). It is clear from Tables 4.4 and 5.1 that the water velocity on the outside curved wall for Heat 3-set A was 6 m/s (for a flow rate of 24.3 l/s), while for Heat 3-set B and Heat 4-set A, it was reduced to 4.1 m/s (for a flow rate 18.9 l/s). At the inlet water pressures of 250-380kPa (Table 4.4), the measured water velocities (4—6 m/s) are lower than the minimum water velocity needed to
prevent nucleate boiling as shown in Figs. 7.1 and 7.2. Samarasekera and Brimacombe\textsuperscript{106,105} developed these boiling behavior diagrams for a mold wall thickness of 9.53 mm based on the meniscus heat fluxes calculated from the experimental study of Singh and Blazek\textsuperscript{120} which are lower than those obtained in recent studies\textsuperscript{107} and also those found in the present study from the trials at Chaparral Steel Company (Fig. 5.99). Thus in this thicker-walled mold (12.7 mm) at Eastern Steelcasting, it is possible that boiling occurred adjacent to the outside curved wall. The boiling water, owing to poor quality, deposited scale which further increased the mold wall temperature causing increased fouling. This progressive increase in the scale thickness and its spread is undoubtedly the reason for the higher wall temperature observed during the later heats in the campaign.

The drop in the wall temperature in Heat 5 is a result of an increase in the water velocity to 6 m/s, an increase in the inlet water pressure to 400 kPa, and a decrease in carbon content of steel to 0.05%. It is well known that lower carbon steels wrinkle to \(\delta\rightarrow\gamma\) solid state transformation which increases the mold/shell gap and reduces the heat transfer to the mold.\textsuperscript{119} The highest mold temperatures observed in Heat 12 (0.61 %C) have the same origins. For this heat the water flow rate has been reduced to 17.7-18.2 l/s resulting in water velocities of 3.10-3.25 m/s (Table 5.1); the inlet water pressure was reduced to 220kPa (Table 4.4) and the 0.61 % carbon content of the heat is associated with a high heat flux as shown by Singh and Blazek.\textsuperscript{120} As will be shown later these observations are consistent with the fluctuations measured in the outlet water temperature and the mold wall temperature.
The left straight wall temperatures depicted in Figs. 5.8, 5.10, 5.12, 5.14, 5.16 and 5.18 are about 100°C lower than the curved wall temperatures in the meniscus region. There was no scale formation on the straight walls (Fig. 5.84). The measured water velocity distribution showed that at all the flow rates, the left straight wall is cooled by water flows with velocities 2 m/s higher than on the outside curved wall (Fig. 5.1). Except for Heat 3-set B, Heat 4-set A and Heat 12—sets A and B, the water velocity on the straight wall was 8 m/s which is slightly above the boiling/no boiling lines of Fig. 7.1. However, in heats where the velocity on the straight wall was only 6 m/s, nucleate boiling was probably occurring. The absence of scale deposits and the lower straight wall temperatures indicate, however, that the extent of boiling was less compared to the adjacent outside curved wall.

One of the goals of this work has been to examine the changes of the mold wall temperatures after turning the EMS motor off, i.e., to determine the influence of the EMS on mold heat extraction. The most significant change to the mold temperatures was seen in the first four blocks of data after the EMS was switched off, and is limited to a small region in the meniscus area ranging from 100 mm to 160 mm below top of the mold (Figs. 5.7—5.18). Over the remaining 175 mm to 700 mm of the mold, the temperatures are practically unaffected. There is a reduction in the temperatures in the first four blocks of data in Heats 3A (Figs. 5.7&5.8) and Heat 12 (Fig. 5.18). However, in other heats, for example, 4A (Figs. 5.9, 5.10), 5B (Figs. 5.1, 5.12) the temperatures initially drop and then rise. In Heat 10A (Figs. 5.15&5.16), the temperature at the 160 mm location increases. Such changes in temperature in the meniscus area could be
either due to a possible reduction in the heat flux after the EMS was switched off or due to a change in the meniscus level. If the initial reduction in the temperatures (in the meniscus area) was due to a reduction in heat flux, it is not clear why the temperatures have risen back as in some cases (Figs. 5.9—5.12, 5.15 and 5.16). A drop in the metal level could also cause the observed temperature reductions. When the metal level drops, it is obvious that the mold temperature must show an increase around the new metal level, but again this was seen only in Heat 7 (Fig. 5.13), where the temperature at the 160 mm level shows an initial increase, then a subsequent drop while the temperature in the top 130 mm of the mold continuously drops. Thus the examination of the first four blocks of data alone does not clearly reveal the cause of these mold temperature changes.

However, continuous temperature-time plots (Figs. 5.19—5.22, 5.25, 5.26, 5.28, 5.30, 5.32, 5.34 and 5.36—5.39) showing the thermal profiles of the mold over prolonged periods of time after switching the EMS off shed more light on the changes that occur in the mold temperature at the instant when the EMS was turned off. Although the temperature signals appear to have fluctuations of different frequencies and amplitudes, there are a few instances, when the temperature profile unmistakably depicts a gradual metal level variation. A typical meniscus movement can be inferred from Fig. 5.19 (at 70s after EMS) where a gradual reduction in the temperature at 80-130 mm is accompanied by a simultaneous increase at the 160 mm level. Similarly in Fig. 5.34 (at 270s after EMS), as the temperature drops gradually at 80-180 mm levels, there is an increase in the temperature at 220 mm from the top of the mold. This once again, represents a change in the metal level in the mold.
It is also evident that much of the high amplitude fluctuation observed also stems from a similar cause. Figs. 5.36—5.38 show clearly in three consecutive heats that as temperatures at 80-130 mm (at the midface of the curved wall) rise, the temperature at 160 mm shows a decrease and vice-versa. Similar observation can also be made in Fig. 5.32 for Heat 6A. Thus the high amplitude fluctuations are certainly related to changes in metal level.

In addition to the component of the high amplitude variation of the temperature signal that reflects metal level fluctuations, there is an overlapping fluctuation that is more frequent but of a lesser amplitude. This can readily be related to the boiling occurring on the wall. The phenomenon of mold temperature fluctuation due to boiling was addressed by Samarasekera and Brimacombe. It was suggested that although the cooling water temperature is well below its saturation temperature in the bulk, boiling is triggered in a superheated layer adjacent to a hot mold. Although a superheat of 5 to 10°C is required initially, once boiling commences, heat transfer to the water increases and the surface temperature decreases so that boiling may diminish or cease altogether. The reduced heat transfer then causes the surface temperature to rise once again until boiling resumes and the cycle is repeated. As will be shown later, the outlet water temperatures also reflect fluctuations that are a direct result of the time-dependent nature of heat transfer imposed by boiling by changing the mold temperature, mold distortion and thus the mold/billet gap. Discussion of this aspect will be taken up later in the next section.

In view of the above considerations, the reduction of the mold
temperatures seen after the EMS was switched off do not appear to be directly attributable to EMS. Although in most cases, the wall temperatures only decline after the EMS off, there are instances, for example, in Heat 6A (curved wall midface—Fig5.32), where the temperature at 130 mm and 260 mm rises. It is clear that the poor metal level control and the occurrence of boiling have caused much of the fluctuation; and the effect of the EMS on this fluctuating mold temperature is not clear.

7.1.4 Water Temperature Distribution

From the water temperature distribution plots for the EMS-on, EMS-off conditions, shown in Figs. 5.58 to 5.56, it is clear that the thermocouple monitoring the inlet water temperature was affected to some extent by the noise from the electromagnetic field, whilst the outlet water thermocouple appears to be relatively free from interference. There is, however, a periodic fluctuation in the outlet water temperature both for EMS-on and EMS-off conditions, as can be seen from Figs. 5.58, 5.59 and 5.61–5.66. This variation of the water temperature in the outlet plenum is akin to the mold temperature fluctuations. As suggested by Samarasekera and Brimacombe, the mold temperature fluctuations originate from the boiling cycle adjacent to the cold face near the meniscus. Owing to the changing thermal field, the mold distortion pattern would change from time to time resulting in a frequently changing mold/billet gap and heat transfer to the water. Thus the fluctuation in the outlet water temperature, which is a measure of the variation the overall heat transfer, could be directly linked to boiling in the water channel.
As pointed out earlier, the curved wall temperatures decline in Heat 5 because of an increase in water velocity and decrease in the carbon content. Accordingly, the outlet water temperatures also decrease in Heat 5 (Fig. 5.50). From Heat 6 onward, as can be seen in Figs. 5.61—5.66 and 5.68, the fluctuations of the outlet water temperature increase in amplitude indicating that the thermal cycling and mold wall movement was also increasing in amplitude. In Heat 12, where the carbon was increased to 0.61% and the water velocity was reduced, together with strong mold temperature fluctuations, the periodic variation of the outlet water temperature was of the highest amplitude (Figs. 5.66 and 5.68).

From the plots of variation in the outlet water temperature (Figs. 5.58—5.66), it is clear that there is no observable change when the EMS was turned off. The influence of the boiling events in the water channel appears to be more dominant on the variation of the outlet water temperature than the effect, if any, of electromagnetic stirring.

### 7.1.5 Mold Heat-flux Distribution

The heat-flux profiles were calculated based on the measured temperatures at 80, 90, 110, 130, 160, 180, 200, 220, 260, 360 and 700 mm from the top of the mold for the first and fourth blocks of data. It is clear from Figs. 5.89 to 5.95 that at 175 mm (from top of mold) the heat-fluxes from the first and fourth blocks are identical. The difference in the heat-flux profiles is limited to a small region extending from the metal level to about 180 mm from the top of the mold. The difference in the heat-flux values in the meniscus region appears to
be a function of the meniscus level. For example, in Heat 3A (Fig. 5.89), the lower heat flux near the meniscus in the fourth block is a direct result of the lower meniscus level. When the meniscus level is unchanged, the overall change in the heat flux near the meniscus is negligible (Figs. 5.91, 5.92, 5.94 and 5.95).

From the preceding analysis it is possible to conclude that the heat-flux distribution is not affected by the presence of EMS-induced fluid flow in the mold. It should, however, be emphasized that owing to the failure of several thermocouples in the meniscus area, the calculated heat-flux profiles are approximate and they only semi-quantitatively depict the thermal conditions of the mold. Added to this is the problem that the effect of scale formation and its progressive growth during the campaign are not well understood. The assumption of a constant scale thickness in the heat-flux calculations calls into question the validity of the heat-flux profiles, especially for the heats cast in the latter half of the campaign. Also as shown before, there is overwhelming evidence indicating that boiling in the cooling channel has led to rapid fluctuations in the outlet water temperature. It is possible that the effect of the EMS was masked by the boiling events, which led to fluctuations in the outlet water temperature and overall heat transfer.

In view of this uncertainty, a second trial was performed at Chaparral Steel with a different thermocouple system and using the highest water flow rates. The trial at Eastern Steelcasting has clearly demonstrated the importance of the design of the cooling water channel and the need to employ high water velocities in the mold. The square corners of the water jacket were found to sustain non-uniform water velocity in the mold water channel which originated from
an inadequate inlet plenum. Also, the operating practice of adjusting the water flow rate to suit the grade of steel, i.e., high water flow rates for low carbon steels and low water flow rates for high-carbon steels is flawed. The highest possible water velocities of the order of 11-12 m/s must be employed for all grades of steel. The formation of scale on the outside curved wall is also an indication of the poor quality of the cooling water. Improving the water quality is clearly an essential step towards improving the operating practice.

7.2 SECOND TRIAL: CHAPARRAL STEEL COMPANY

7.2.1 Water Velocity Distribution

In the case of the mold system at Chaparral Steel Company, the water velocities were similar on the inside and outside curved walls (Figs. 5.5 and 5.6 and Table 5.3). As suggested in Section 7.1.1, the uniformity of water flow is likely due to the uniformity of cross-section of the water passage since the corners of the baffle tube were contoured to those of the mold tube. However, along the straight walls of the mold tube, the water velocities appear to be 0.5 m/s—1.0 m/s higher than along the curved walls. The cause for this appears to be, once again, non-concentricity of the mold tube and baffle tube. In the modified mold housing at Chapparel Steel (see Section 4.2.2), the mold tube is held by support plates that fit into the slots provided at the top of the tube. These support plates are bolted to the top plate. The water jacket is slid over the mold and held in place at the top by right-angled fillets and suitable screws. This assembly, containing top plate, mold and water jacket, was slid into the mold housing
(Fig. 4.23), wherein the EMS stator was already in place. The concentricity of the mold tube and baffle tube depended critically on how much they were displaced during the assembly. The baffle tube was anchored at the top to the top plates; its position effectively fixed by the seal provided at the plate separating the inlet and outlet plena and the looseness of the mold fitted, again, only at the top and held at the bottom by the seal in the bottom plate. There was no other provision to ensure that the gap is uniform around the mold.

The importance of the concentricity of the mold tube within the baffle tube, which governs the uniformity of cooling water velocity, lies in the fact that the variation in this water velocity around the mold tube could be a potential source of uneven heat transfer and uneven distortion.\textsuperscript{10} The effect of a difference of 1 m/s could affect the mold wall temperature by 2 to 4.5°C (depending on the mold heat flux).\textsuperscript{†} If the water velocities were low enough (6—8 m/s as in the case of Eastern Steelcasting), the lower velocity on the inside curved wall (by 1 m/s, see Fig. 5.1) could trigger intermittent boiling and increase the difference in temperature of the different faces. In the case of Chaparral Steel the operating flow rates were set higher (27.4 l/s or 435 gpm) providing a water velocity of 11—12 m/s (Fig. 5.5). In this case the variation of 1 m/s water velocity from one face to another would not affect the mold heat transfer to a great extent.\textsuperscript{10}

\textsuperscript{†}These calculations were performed using the heat-flux profile generated for the Heat 3 (0.31% C), shown in Fig. 5.96. The two-dimensional heat flow model (see Chapter 4) was run for a water velocity of 10.11 m/s and 11.11 m/s. The temperatures of the cold face and hot face were compared.
7.2.2 Mold Condition

The double-tapered mold appears to have retained the original taper along its length (Figs. 5.86 and 5.87) except for a slight outward bulge in the region of 50-100 mm. In terms of an overall profile, however, the mold has been distorted. The distance between the opposite faces at the midface is the smallest on the curved faces and differs from one of the off-corners' by about 0.5 mm at the bottom of the mold. The distance between the straight walls at the midface is the largest and differs from one of the off-corners by a similar amount as above.

Thus it is obvious that the curved walls have bowed inward at the midface and the straight walls bulged outward. Accomodating the difference (of the distance between the faces) at the midface and the two off-corner locations, a transverse slice through the mold would be as shown in Fig. 7.2. It must be emphasized that this is the shape of the cold mold. As will be seen later, the mold temperature distribution also suggests that the hot mold is similarly shaped; only the bulge on a given wall and the concavity of the adjacent wall are periodically reversed.

7.2.3 Mold Temperature Distribution

It is clear from the plots of temperature distribution of the inside curved wall (Figs. 5.41, 5.43, 5.45, 5.47 and 5.49) that the temperature of the mold wall was oscillating between a high and low value. The oscillation of the
mold temperature predominantly occurs from 80 mm to 140 mm as well as from 300 to 350 mm from the top of the mold. The frequency as well as the amplitude of these oscillations is far less than the mold temperature fluctuations observed in the Eastern Steelcasting trial.†

In order to understand the origin of this mold temperature variation, attention must be drawn first to the possibility of metal level changes being the cause. It is well-known that the peak mold temperature is always a few millimeters below the metal level owing to the longitudinal heat flow in the upper region of the mold (which is not in contact with steel) in conjunction with the transverse flow to the cooling water. This would then mean that metal level in the mold temperature profiles shown in Figs. 5.41–5.49 must lie within a few millimeters of the thermocouple located 120 mm from the top of the mold. Heat-flux profiles calculated from these temperature profiles (Figs. 5.99 and 5.100) also show that the prediction of the measured mold temperatures (with the two-dimensional heat-flow model) can be done only by setting the mean meniscus level at 115 mm. Thus if the metal level rises, temperatures at the 100 and 90 mm levels must increase. The fluctuation observed in Figs. 5.41, 5.43, 5.45, 5.47 and 5.49 clearly reveals that, if the temperature at 110 mm from the top of the mold is decreasing, then there is a simultaneous drop in the temperature at 80, 90, 100; 120, 130 and 140 mm locations to a large extent, a minor change in temperatures at 160, 170, 180 and 200 mm locations and again a significant change in the temperatures at the 300, 325 and 360 mm level. Thus it can be concluded that the observed mold temperature oscillation is not due to metal level fluctuations.†

†(Note that the scale on the ordinate is only 10°C in Figs. 5.41, 5.43, 5.45, 5.47 and 5.49)
Metal level control at Chaparral Steel is achieved by controlling the casting speed and a possibility exists that the variation in casting speed is causing these mold temperature fluctuations because casting speed affects shell/mold gap. The metal level control is linked to the casting speed, i.e., the rotational speed of the withdrawal rolls so that changes to metal level resulting from variation of the tundish level and hence the variation of the input flow rate is adjusted by either lowering or increasing the casting speed. In order to address this indirect influence of the tundish head on mold temperatures, casting speed charts† obtained during the trial were carefully examined. The casting speed signal was found in some places to reduce or increase steadily for 5 to 10 minutes. During the start up or shut down there were sharp changes; otherwise the casting speed was found to be reasonably constant to within ±0.0254 m/min (1 ipm). Thus there was no evidence to indicate that the periodicity of the mold temperature fluctuation was related to casting speed variations.

Oscillation marks on the surface of these billets do open up the mold/billet gap and retard the shell growth. Their periodic appearance is also one of the ways in which mold heat transfer is altered, but the time scale is too short. With the frequency of oscillation set at 160 cycles per minute (Table 4.5), oscillation marks form every 0.375s at a casting speed of 2.0 m/min (80 ipm). It can be seen from the plots (Figs. 5.41, 5.43, 5.45, 5.47 and 5.49) that the time period between one peak and the next appears to be much longer varying from 30 to 80s.

†Casting speed is recorded routinely at Chaparral Steel on a chart recorder hooked up to the withdrawl rolls.
The mold-billet gap also can be significantly altered by the mold distortion itself. Samarasekera et al.\textsuperscript{10} observed in the measured temperature distribution of adjacent walls of a square mold, that the minimum heat flux on the straight walls correspond to the local maximum on adjacent curved walls. Owing to the two-sided constraint of the mold tubes the peak bulge occurs lower on the unconstrained curved walls. It was suggested that while the straight wall bulges out, it causes an inward movement of the curved walls. This takes place by rotation of the corners that normally are at much lower temperature than the mid-face and are structurally rigid. In the present investigation, although all the four sides of the rectangular mold are constrained, the expansion of the broad face is greater owing to its greater dimension than the narrow face. This extra expansion is accommodated by causing an inward movement of the adjacent face and a rotation of the corner joining them as shown in Fig. 7.3. Once the curved walls of the mold bulge outward causing inward movement of the straight wall at the mid-face, there can be a reduction in the curved wall temperature and a net contraction together with an increase in the straight wall temperature and a net expansion. This would trigger the movement of the straight walls away from the billet which would rotate the corners and try to bring the curved walls closer to the billet surface. And once again the curved wall starts to heat up and expand. In a square mold it can be envisaged that the wall movement would stop sometime after the start of casting and a steady gap would be maintained. In the case of the Chaparral rectangular mold, the differences in the width of the straight and curved walls would give rise to different extents of wall movement for the same temperature rise and a steady state is never reached. Thus the inward and outward movement of the curved wall is continuously triggered by an opposite
movement of the straight and vice versa. This wall movement would stop when the casting is terminated. Indeed the distortion plots indicate that the curved wall has bulged out and the straight wall moved inward at the midface at the end of the campaign.

This time-dependent distortion of the mold walls is clearly not limited to the meniscus region as is evident in the simultaneous oscillation of the mold temperature down the length of the mold up to a distance of 350 mm from top of the mold (Figs. 5.41, 5.43, 5.45, 5.47 and 5.49). The thermal oscillation persists, as can be seen from the distortion plots in Fig. 5.86 for the curved walls, all over the steep taper (2.6%/m) region of the mold. Thus in the region where the large taper maintains a relatively small mold/billet gap, the above suggested mold distortion periodically alters the gap to result in the mold temperature fluctuations. Below 350 mm, the taper is reduced to 0.48%/m and large gaps open up desensitizing the heat-flux to the changes in the distortion of the mold.

This periodic movement of the mold walls through rotation of the corners and the large gap in the lower part of the mold could be a potential source of off-corner cracks. As the results of the billet quality evaluation indicate, there has been extensive cracking in these rectangular billets. In the ensuing discussion of the formation of these cracks in Section 8.3, it will be shown that the periodic bulging of the midface away from the corners is the primary cause.

With regard to the effect of the EMS on mold temperatures, the continuous temperature-time plots (Figs. 5.41, 5.43, 5.45, 5.47 and 5.49) reveal
that at the time when the EMS is switched off the temperatures either increase or
decrease depending on whether the mold wall is pulling away or approaching the
billet surface. Thus if the mold temperatures are rising (mold wall moving inward)
and the EMS is switched off, an increase in mold temperatures will continue to be
seen. Also, if the mold temperatures are dropping off prior to the switching off of
the EMS, then a continued decline would be observed. It is clear from the analog
signals of the strip charts (Figs. 5.52—5.56) that such fluctuations exist whether the
EMS is on or off.

From the above analysis, it is evident that the effects of the
EMS on mold temperatures are minimal. The thermal field in the mold appears to
be controlled predominantly by the mold shape and the concomitant variation in the
heat-flux resulting from a changing mold/billet gap. The influence of the EMS on
the net heat-flow into the mold is unambiguously much smaller than the fluctuation
of the mold-gap arising from the mold distortion. As will be shown in the next
section, even from the standpoint of changes to the outlet water temperature, there
is no indication that the EMS affects heat transfer significantly.

7.2.4 Water Temperature Distribution

The plots of water temperature distribution in the water
channel (Figs. 5.69—5.72) show that the temperature of water flowing past the
thermally oscillating mold changes identically to the mold temperature fluctuations.
Also the two thermocouples mounted inside the mold housing on either corner of
the inside curved wall facing the water exiting from the cooling water channel
(Fig. 4.28) record periodic fluctuations like those seen in the mold temperatures and the water channel temperature mentioned above. These results clearly point out the periodic change in the heat-flux, an outcome of the varying mold/billet gap.

Figures 5.74—5.77 show the water temperature for approximately four minutes (240s) prior to EMS-off and for a similar duration after. It can be readily seen that the outlet plenum water temperature recorded by the T-Type thermocouple exhibits identical fluctuations before and after EMS. This reaffirms that the mold temperature fluctuations was occurring even while EMS was on.

One interesting observation is the difference in the oscillation of the signal output from the E and T-Type thermocouples mounted in the outlet plenum on either off-corner of the inside curved wall (Fig. 4.28). This can be seen in Figs. 5.69—5.72. While the water exiting from one side of the inside curved wall shows an increase in temperature, water leaving the water channel from the other off-corner location shows a decrease in its temperature. This further explicates the mechanism of mold wall movement through rotation of the corners which are cold and rigid causing mold temperature fluctuation. Clearly the water flowing in plug-flow at different locations of the water channel is at different temperatures indicative of the gross differences in mold heat transfer at the two off-corner locations. The rise in the temperature of the off-corner locations would mean that the mold tube is twisting, opening up a gap in one off-corner and closing the gap on the other. This twist is occurring at the same time as mold wall movement caused by corner rotation, and could arise due to either poor tolerances in the
constraint plates or if the mold tube is placed non-concentrically within the mold.

The water temperature distribution (after the EMS was switched off) at the various places in the flow of cooling water (Fig. 4.28) is shown in Figs. 5.69—5.72. There is no indication that at these locations the temperature of water shows any conspicuous changes at the point the EMS was switched off. Figures. 5.74—5.77 show the outlet water temperature measured inside the outlet plenum and at the water exit point of the mold housing for both EMS-on and EMS-off. Neither of the thermocouples show evidence that mold heat transfer is altered when the EMS was switched off.

7.2.5 Mold Heat-Flux Distribution

From the above discussion it is clear that the effects of the EMS on heat transfer, if any, is negligible in view of the nominal fluctuations of the heat-flux extracted by the mold. Hence the mold heat-flux calculations were performed to arrive at a statistical maximum and minimum based on the 2-σ standard deviation of the mold temperatures in three heats of 0.19, 0.31 and 0.45 °C, (Fig. 5.96, 5.97 and 5.98). It should be pointed out that the heat-flux profiles, shown in Figs. 5.99 and 5.100 are also statistically the maximum and minimum. Thus the instantaneous heat-flux could be higher or lower depending on the extent of the mold gap.

The heat flux curves (Fig. 5.96, 5.97 and 5.98) exhibit the same general shape. The changes to heat transfer with time reflect variation of the
mold/billet gap down the length of the mold. This change in the gap width is caused by shrinkage of the steel, mold distortion, mold taper and the resistance of the growing shell to ferrostatic pressure which attempts to push the shell towards the mold. The sharp reduction of the heat-flux within the first one second of dwell time is due to mold distortion with the peak bulge appearing 60-80 mm below the metal level.\textsuperscript{10} As the mold regains taper, a rise in the heat-flux is seen between 1 to 3 s. Between 3 to 5 s the heat flux drops off once again; and increases from 5 s until 7 s of dwell time when the steep taper of 2.6 \%/m ends. With the provision of the large taper of 2.6 \%/m the heat-flux would be expected to stay high after the mold recovers from the initial bulging below the meniscus. The reduction in mold heat flux between 3 to 5 s could be because of the distortion of the mold wall a second time; as the mold wall tucks back in the mold temperatures would rise between 5 to 7 s; also the mold temperatures oscillate along with those at the meniscus (0 to 1 s) dwell time indicating extensive mold wall movement. From then on there is a steady drop in the mold heat extraction due to a combination of the large gap opening up due to the small taper, 0.48 \%/m. The increased ferrostatic head forces the billet to bulge and causes the off-corner cracking.

The peak heat flux values of 4000-4500 kW/m\textsuperscript{2} (Fig. 5.99) are in agreement with heat-flux profiles obtained in billet molds (0.8 \%/m single taper) by Samarasekera et al.\textsuperscript{10} Between 6 to 12 s dwell-time (300-500 mm from top of the mold), the heat-flux values (2200-3000 kW/m\textsuperscript{2}) are higher comparable to the literature.\textsuperscript{10} Evidently the steeper taper (0.26 \%/m) of the mold wall (to about 300 mm from the top) has reduced the mold/billet gap and enhanced the heat
extraction. Owing to the paucity of temperature data, the heat-flux profiles are not accurate below the 600 mm position from the top of the mold.

The heat-flux profiles for three different carbon steels are compared in Figs. 5.99 and 5.100. The peak heat flux values at the meniscus decrease from 4500 kW/m² for the 0.45 %C steel to 4000 kW/m² for the 0.19 %C steel; for the 0.31 %C, the meniscus heat flux is 4200 kW/m². These values are consistent with the earlier published results. The overall heat extraction slightly increases between 0.19% to 0.45% carbon. As suggested by Grill and Brimacombe this is due to the decrease in the amount of δ-ferrite and a reduction in the concomitant solid-state shrinkage. It is not clear, however, as to why the heat-flux profile for the 0.31 %C steel exhibits an extra peak at a dwell time of 1.75 s.

The mold wall movement inferred from the oscillation of the thermal field in the mold is essentially the reason for the high and low heat-flux profiles. The most significant difference in the high and low heat-flux profiles pertains to the meniscus region (115 mm to 140 mm or 0 to 1 s dwell time). There is also considerable reduction in the heat-flux in the lower part of the mold, pertaining mainly to the 0.48 %/m taper region. Thus thermal cycling of the mold, recorded for the first time in this study, has undoubtedly undermined the benefits of the double taper in the mold in terms of superior heat extraction capability. As will be seen in Chapter 8, the thermal oscillation of the mold also presents a unique problem with respect to mold-related cracking.
Figure 7.1. Boiling behavior diagrams for casting of low and high carbon steels.
Figure 7.2. Schematic of the permanent distortion of the rectangular mold tube after the campaign at Chaparral Steel.
Figure 7.3. Schematic of the periodic movement of adjacent faces of the rectangular mold and rotation of the corners.
8. DISCUSSION OF THE BILLET QUALITY

8.1 OSCILLATION MARKS

8.1.1 Summary of the results

Based on the results of the examination of the billet surfaces (Section 6.3 and Figs. 6.8-6.11) and the study of sub-surface structures (Section 6.4.1 and Figs. 6.17-6.22) the following conclusions can be drawn.

1. The depth of the oscillation marks decreases with increasing carbon content and the deepest oscillation marks are obtained in the 0.05-0.09% range, Fig. 6.8.

2. The depth of the oscillation marks vary significantly across a given face and the depth at the off-corner locations are often larger than at the midface (Table 6.1).

3. Electromagnetic stirring did not affect any of the following (Table 6.1).
   a) The average depth of the oscillation marks across a given face.
   b) The variation of the oscillation mark depth across a given face.
   c) The maximum depth measured in any given billet.

4. Electromagnetic stirring appears to affect the oscillation marks in two ways (Figs. 6.9-6.11).
   a) The shape of the oscillation mark across a given face changes with stirring.
   b) The spacing between the oscillation marks is more uniform with stirring than without.

6. In the sub-surface etches of low-carbon steels, (Figs. 6.17-6.18) columnar dendrites appear to have grown from the surface. In higher carbon grades (0.33-0.61%C),
however, a fine equiaxed crystal zone (often called the chill zone) is evident close to the surface (Figs. 6.19-6.22).

7. Both in terms of the formation of sub-surface laps or hooks and the extent of chill zone adjacent to the oscillation marks, there is no difference between the stirred and unstirred sub-surface structures.

8. The columnar dendrites of the stirred billet incline into the rotary liquid flow after about 0.5-1.0 mm of shell has formed.

8.1.2 Discussion

Although the oscillation marks have not been considered as a quality problem in the past, it is now clear\textsuperscript{107,121} that the effect of a deep oscillation mark is to locally widen the shell/mold gap, retard the heat flow to the mold and decelerate the solidification rate. Often, the depth of the oscillation marks is not uniform across a given face, not to mention around the transverse billet section. Thus the shell growth is not evenly retarded in the transverse plane in the vicinity of the oscillation mark. Consequently, oscillation marks are implicated strongly\textsuperscript{121} in sensitizing the billet to many mold-related defects, such as off-squareness and off-corner internal cracks. Thinner shell near the oscillation marks is a potential threat for break-outs.

The formation of the oscillation marks in billet casting is believed to be caused by mechanical interaction between the mold and solidifying shell during the negative strip time\textsuperscript{112,107,121} when the mold travels downward faster than the billet. Samarasekera et al\textsuperscript{110} have shown that billet molds distort
non-uniformly and acquire a bulged shape, especially near the meniscus. The bulge extends from above the meniscus and the position of the maximum bulge\textsuperscript{110} is located at 60-100 mm below the metal level. Owing to this mold distortion and the resulting negative taper in the meniscus region, the mechanical interaction between the reciprocating mold and the newly formed shell during negative strip results in a jamming action causing a buckling of the shell which in turn results in a surface depression. Partial freezing of the meniscus can occur during the negative strip followed by liquid steel overflow to form a hook in the sub-surface structure and bleed marks on the billet surface.\textsuperscript{112,107,121}

Samarasekera et al\textsuperscript{121} have identified the factors influencing the oscillation mark depth to be negative strip time, superheat, carbon content of the steel, mold taper and the type of mold tube support. These factors affect the shape of the mold at the meniscus in terms of the magnitude of the negative taper and hence the mold-billet interaction. The use of thicker walled mold tubes, large positive taper near the meniscus, four-side rather than two-side mold tube constraint, low superheat and short negative strip times were found to reduce the depth of the oscillation marks.

Formation of deeper oscillation marks and the absence of hooks, or laps, (in the sub-surface structures) of 0.05-0.09\% carbon steels cast during the trial at the Eastern Steelcasting is consistent with previous observations reported in the literature.\textsuperscript{112,121} The observation of shallower oscillation marks and meniscus solidification in higher carbon steels is also consistent with earlier work.\textsuperscript{112,121} In the case of low-carbon billets (0.05-0.09\%C), the mold/billet gap readily opens up as
the newly formed δ-ferrite transforms to γ-austenite.\textsuperscript{119} Due to this air gap, the heat fluxes reported for this carbon range are lower than for medium-carbon steels (0.33-0.35%C) where the shrinkage associated with the δ + L → γ reaction can be accommodated by the liquid so that a mold/billet gap does not form at the meniscus.\textsuperscript{120}

Thus the shell at the meniscus in low-carbon steels is hotter and thinner than for medium and higher carbon steels. These thin shells buckle more readily when squeezed by the negatively tapered mold during the negative strip time. Also, because of the lower heat fluxes, meniscus solidification rarely occurs in the low-carbon steels. In medium- and high-carbon steels, heat fluxes of 4000-4500 kW/m\textsuperscript{2} were calculated in the present work (Fig 5.100) which are much higher than the reported values for low-carbon steels.\textsuperscript{107,120} Accordingly, in the 0.3-0.6% carbon range, thicker shells form at the meniscus, and often the meniscus is partially frozen during the period of negative strip. In the ensuing positive strip period, an overflow occurs over the frozen meniscus. This appears as a hook in macroetches of longitudinal sections.

The absence of the fine equiaxed crystal zone in the sub-surface structures of low-carbon steels (Figs. 6.17, 6.18), can also be attributed to the lower heat fluxes in the meniscus region resulting in reduced undercooling in the liquid adjacent to the mold wall during the initial moments of solidification. In higher carbon steels, the width of this fine equiaxed crystal zone (Figs. 6.19-6.22) varies considerably along the longitudinal sub-surface etch. In the vicinity of the oscillation marks, the chill zone is wider and is related to a probable increase in
the mold heat-flux when the mold-billet gap is closed by the negatively tapered mold during the formation of the oscillation mark in the negative strip period.

The difference in the depth of oscillation marks at the off-corners and the midface across a given face can be attributed to the twisting of the mold tube relative to the billet. The mold wall temperature measurements at Eastern Steelcasting, presented in Figs. 5.19, 5.20 and 5.23 show that, as the temperature of the midface decreased with distance from the meniscus, the off-corner temperature increased and was about 100°C higher than the mold temperature at the midface (see Fig. 5.23). It can also be seen that the mold temperature at the off-corner fluctuated markedly at 150 and 220 mm from the top of the mold. This is indicative of relative motion between the mold tube and the billet shell. Other factors suggested by Samarasekera et al., namely, incorrect tolerances on the constraint plates and condition of the oscillator could also cause non-uniform depth of oscillation marks.

That EMS has no influence on the depth of oscillation marks or the extent of the chill structure or the formation of hooks is borne out by its lack of effect on mold heat transfer. Mold distortion and negative taper in the meniscus region, which have been cited as the main cause of oscillation marks would not be different if the heat extraction pattern is not significantly altered. It has already been shown in Chapter 7 that the influence of EMS on mold heat transfer is negligible. Thus it appears obvious that EMS would not have an effect of reducing the depth or variation of the oscillation marks, as claimed in the literature.
The effect of EMS on changing the shape of the oscillation mark is due to the characteristic meniscus shape resulting from the rotating steel. As the electromagnetic force field drives the molten flow in the rotary fashion, the surface of the steel pool becomes concave. The surface is raised at the outer edges, i.e., the meniscus, and is lowered at the center. In a square mold, as in the case of Eastern Steelcasting, the steel flow has to change direction at the corners and in doing so the metal level rises locally. Once past the corner, the meniscus level drops to a minimum. Once again, as it approaches the next corner the meniscus level rises. The oscillation mark, being a result of solidification and the mechanical interaction of the mold and newly forming shell (along this wavy meniscus) has the same shape as the meniscus.

In the absence of stirring there is considerable turbulence in the meniscus area, mainly due to the impingement of the input stream and the entrainment of gas. Consequently the metal level fluctuates and the oscillation marks on unstirred billets are irregular (Figs. 6.9-6.11). The spacing between them also fluctuates depending on the exact shape of the meniscus at the instant during the negative strip when the mold makes contact with the newly forming shell. Evidently the introduction of the electromagnetic stirring overcomes the liquid motion induced by the plunging input stream to yield evenly spaced oscillation marks.

It is well known that dendrites grow upstream into the flow of the molten metal. The sub-surface structure in the longitudinal sections (Figs. 6.18, 6.19 and 6.21) reveals that growth of the columnar front into the flow does not begin until 0.5-1.0 mm of shell growth. The width of
the non-inclined columnar dendritic zone closely corresponds to the thickness of the
momentum boundary layer (0.6 mm based on an estimate using Equation 6.1
presented in Chapter 6). The existence of this finite boundary layer within which
the velocity of the molten metal falls to zero could prevent the flow from affecting
the columnar dendritic growth in the first few seconds.

8.2 OFF-SQUARENESS

8.2.1 Summary of the results

The billet samples collected during the industrial trials exhibited
considerable off-squareness (—5.60 mm) to 6.95 mm) in the case of Eastern
Steelcasting (Table 6.1) and off-rectangularity (—11.0 mm to 12.5 mm) in the case of
Chaparral Steel (Table 6.2). Both the magnitude and direction of the distorted billet
shape vary from one heat to the next and even from one sample to another
(Fig. 6.1 and Table 6.2) in both of the campaigns. It is also clear from Tables 6.1
and 6.2 and Fig. 6.1 that EMS offers no particular advantage in reducing this
defect.

8.2.2 Discussion

Rhomboidity, off-squareness, off-rectangularity, ovality etc. are
synonyms for the distorted shape of the continuously cast product as it exits the
machine. In square and rectangular shapes, it is characterised frequently as the
difference in diagonals. Since excessive rhomboidity renders the billet unfit for
rolling, it is one of the few casting defects that is monitored on-line in many casting installations. The propensity to form corner and off-corner cracks at the obtuse angles of the distorted billet has drawn much attention to this defect and researches have suggested a variety of measures to control rhomboidity.\(^1\)\(^2\)

Non-symmetrical cooling of the billet either in the mold or in the sprays is known to be the primary cause of rhomboidity.\(^3\)\(^4\)\(^5\)\(^6\)\(^7\) Distortion of the mold into rhomboid shape,\(^3\)\(^4\)\(^6\) non-uniform oscillation marks,\(^7\)\(^8\) smaller cross-sections,\(^9\) high superheat,\(^10\)\(^11\)\(^12\) high casting speed,\(^11\)\(^12\)\(^13\) prolonged use of molds\(^9\)\(^10\) and increased wear at the bottom of the mold\(^12\) are some of the other factors that are known to increase rhomboidity. Steels with 0.18-0.25%C\(^7\)\(^8\)\(^9\) and higher carbon (>0.4%) grades\(^10\)\(^5\) are more prone to this defect.

Rhomboidity in the case of the billets cast at Eastern Steelcasting can be linked to the differential mold heat transfer on adjacent faces inferred from the following observations:

(i) Temperatures of the curved wall are much higher than on the straight wall (Figs. 5.24, 5.27, 5.33 and 5.35).

(ii) Water velocity adjacent to the outside curved wall is significantly smaller than that measured adjacent to the straight wall (Fig. 5.1).

(iii) There is evidence of boiling in the water channel adjacent to the outside curved wall leading to scale deposits as seen in Fig. 5.84.

(iv) The distortion of the curved walls is significantly different from that of the straight walls (Figs. 5.82 and 5.83).
Considering the differential heat transfer in the mold in the transverse plane, it is not surprising that non-uniform shell growth would result. Also, owing to the difference in the temperature profile in the mold, the mold itself could acquire a rhomboid shape.\textsuperscript{106,121} Samarasekera et al\textsuperscript{121} have pointed out that the distorted mold is the genesis of rhomboidity by virtue of its effect on the newly forming shell. Although the extent of the difference in the mold diagonals would be much smaller than the observed rhomboidity of the billet, it predisposes the shell profile at the acute corners to grow more rapidly than at the obtuse corners in the lower part of the mold. Consequently, as the billet leaves the mold, the acute corners are colder than the obtuse corners and when the sprays impinge on the billet below the mold, they may force the billet more off-square by cooling the hot and cold regions differentially.

There are two schools of thought regarding the alleviation of mold generated rhomboidity:

1. Employing low water flow rates in higher carbon steels to induce vigorous boiling in the water channel, whereby the cooling of the mold is made more uniform and rhomboid conditions are less likely to be present.\textsuperscript{106,127}

2. Attempts to suppress intermittent boiling by increasing water velocity,\textsuperscript{105} raising water pressure,\textsuperscript{105} increasing mold wall thickness,\textsuperscript{105} use of corrugated molds,\textsuperscript{128} enhancing surface roughness by machining horizontal serrations on the outside surface of the mold in contact with cooling water.\textsuperscript{129}

In heats with 0.05-0.09%\textsuperscript{C}, the water flow rate used was 25.2 l/s, as against
18.9-24.3 l/s for 0.32-0.35%C steels and 17.7-18.2 l/s for the 0.61%C steels (Table 4.4). Evidently the operating practice at Eastern Steelcasting is based on the principle of the so-called soft-cooling wherein the water velocities are lowered for higher carbon steels.\textsuperscript{106, 127} The effects of such practice are evident in the mold temperatures recorded in the campaign. Results of mold temperatures clearly show that by increasing the water velocity in Heat 5 (0.05%C), the mold temperatures reduced (Figs. 5.30 and 5.31); and a reduction of the water flow rate to 17 l/s\textsuperscript{†} in Heat 12 (0.61%C) led to the rise in the mold temperatures to reach the highest values recorded in the campaign (Figs. 5.39 and 5.40). Aided by the lower water velocities on the outside curved wall and poor quality of water, the reduction of water velocity has led to progressive scale deposition on the outside curved wall of the mold and to serious billet rhomboidity. Indeed the trial had to be discontinued because of the higher rhomboidity; and the mold tube was discarded. Thus the results of this study clearly reveal the need to ensure high and uniform water velocity in the water channel together with good water quality in order to minimize rhomboidity.

The off-rectangularity observed in the case of the Chaparral trial can be related to the oscillating thermal field of the inside curved wall (Figs. 5.41, 5.43, 5.45, 5.47 and 5.49) and non-uniform shell growth. As suggested in Chapter 7 the periodic fluctuation in the curved wall temperatures is a result of the displacement of adjacent walls in opposite directions (Fig. 7.3). As the 178 mm-wide curved wall expands more than the adjacent straight wall (127 mm-wide) and consequently pushes it outward and causes the rotation of the

\textsuperscript{†} At this flow rate water velocity on the outside curved wall was only 3 m/s as shown in Fig. 5.1.
cold corner joining the two faces. Thus the motion of one wall affects the adjacent walls giving rise to variance in the width of the gap separating the mold and shell, so that the heat extraction differs between adjacent faces and shell growth is non-uniform. Each transverse slice of the billet passing through a particular mold shape resulting from a constantly changing thermal field had a different off-rectangularity compared to the preceding one; hence the orientation of the off-rectangularity of billets varies with time.

As was clearly shown in Chapter 7, the influence of EMS on mold heat transfer was negligible compared to either the effects of the cooling water velocity or mold wall movement. Accordingly, the mold behavior pattern was the least affected by EMS. Considering that the billets from both campaigns acquired a distorted shape because of the mold behavior, it is not surprising that EMS had no effect on this defect.

To reduce the off-rectangularity, steps must be taken to control the wall movement by increasing the copper wall thickness or mechanically constraining the mold walls from moving.¹¹²

8.3 OFF-CORNER CRACKS

8.3.1 Summary of the results

In the case of Eastern Steelcasting there was no indication of off-corner cracks, whereas at Chaparral Steel, the rectangular billets exhibited cracks
at more than one off-corner location (Table 6.2). As can be seen from the sulphur
prints (Figs. 6.2-6.7), these cracks typically form 5-6 mm from the surface of the
billet and 15-20 mm off the corner. They often lie beneath a depression on the
surface. There was no difference in the extent of off-corner crack formation between
the stirred and unstirred billets (Table 6.2).

### 8.3.2 Discussion

Brimacombe et al.\(^{130}\) have shown that like other internal
cracks in continuously cast products, off-corner cracks are hot tears that initiate
close to the solidification front and the depth of the cracks directly reflects the local
shell thickness at the time the cracks are formed. Considering the mold-exit shell
thickness is around 9-10 mm (see Fig. 6.50), it is clear that most of the cracks
seen in the Chaparral steel billets located at 5-6 mm from the surface have
originated in the mold as in the case of earlier studies.\(^{130-132}\)

The mechanism of crack formation proposed by Bommaraju et
al.\(^{37}\) (shown in Fig. 8.1) is based on the creation of tensile strains at the
solidification front at the off-corner location by the bulging of the shell. Owing to
the relatively low temperature and strength of the corner of the billet, the bulging
of the shell does not extend to the corner but hinges about the off-corner region.
In the case of the Chaparral Steel billets, it appears that the mold wall movement
inferred from the mold temperature measurement is the primary reason for the
extensive cracking observed in these billets. The distorted shape of the cold mold
and the constantly changing dimensions of the mold at midface vs. corner (discussed
in Chapter 7) suggests that the newly forming shell would also have unequal dimensions at the midface and the off-corner locations. As can be seen from the heat-flux profiles shown in Figs. 5.96-5.100, the heat flux drops off steadily from about 380-400mm from the top of the meniscus. As suggested by Bommaraju et al\textsuperscript{37}, this is a region where the billet starts to reheat. The taper traces shown in Figs. 5.86 and 5.87 reveal that the mold taper is small (0.48 \%/m) below 350 mm. Thus the billet encounters a large gap in the lower part of the mold so that the midface location would be free to bulge and cause a tensile strain at the solidification front in the off-corner region leading to crack formation. Mold wear in the lower regions of the mold\textsuperscript{131} and lack of sub-mold strand-support\textsuperscript{132} could exacerbate the crack propagation.

Electromagnetic stirring does not appear to reduce mold-related crack formation. However, the effect of EMS on the thermal fields was shown to be small; thus the thermo-mechanical behavior of the mold, which is the likely source of the cracking, would thus be hardly affected by the electromagnetic stirring.

Remedial measures for rhomboidity must include increasing the taper\textsuperscript{130} in the lower half of the mold (330 mm to 800mm) to at least 1\% per m. It is also important to provide adequate strand support by use of foot rolls immediately below the mold,\textsuperscript{132} so that the propagating cracks can be controlled. Use of these foot rolls, although not popular in high production shops like Chaparral Steel, can limit to an extent the severity of cracking.
8.4 SOLIDIFICATION STRUCTURE

8.4.1 Summary of the results

From the examination of the cast structures (Chapter 6) of billets cast at Eastern Steelcasting and Chaparral Steel, the following conclusions can be made with respect to the extent of the columnar dendritic zone.

1. Electromagnetic stirring in the mold gives rise to consistent improvement in the equiaxed zone as is evident from Figs. 6.12, 6.15, 6.27 and 6.28.

2. Higher superheats in the tundish give rise to longer columnar dendrites (Figs. 6.23-6.26). While at superheats of 20°C, and lower, the cast structure is predominantly equiaxed with or without EMS (Figs. 6.27 and 6.28), the length of the columnar zone is reduced by EMS when the superheat is high.

3. The carbon content of the steel appears to have a strong influence on the columnar dendritic growth. As the carbon level is increased from 0.05 to 0.37%, the length of the columnar zone decreases (Figs. 6.12, 6.15, 6.27 and 6.28). For steels with 0.40%C, the columnar zone is once again longer. Beyond 0.40%C, however, as the carbon content in the steel is increased the columnar zone length decreases (Figs. 6.27 and 6.28).

4. In most cases, the length of the columnar zone is found to be longer adjacent to the inside radius face of the billet than on the side corresponding to the outside radius. This can be seen from Figs. 6.29 and 6.30. Equal columnar zone lengths adjacent to opposite faces were observed in billets with either predominantly equiaxed structures (Figs. 6.31 and 6.32) or structures wherein the columnar dendrites have grown close to the center of the billet.
It is clear from the above that three types of structures shown schematically in Fig. 8.2 are possible in these billets.

1. When the transition takes place within 10-15 mm of shell formation (tundish superheat <20°C) the columnar zone length is equal adjacent to both inside and outside radius faces.

2. As the transition is delayed to lower regions along the machine curvature, the difference in the upper and lower columnar dendritic length begins to first increase and then reduce as the two dendritic fronts approach the center of the billet (see Fig. 5.2).

3. When the columnar-to-equiaxed transition is not triggered at all, the columnar dendrites would grow to the geometric center of the billet.

8.4.2 Discussion

In the present investigation, the factors that appear to profoundly influence the columnar-to-equiaxed transition are the presence of EMS-induced fluid flow, superheat in the steel, carbon content and whether the columnar dendrites grow from the inside radius or the outside radius face. The mechanism underlying this phenomenon can be understood by first examining the columnar-to-equiaxed transition in unstimred billets.

The columnar-to-equiaxed transition has been the subject of numerous research papers and several mechanisms have been proposed in the
literature to explain its occurrence. These mechanisms can be grouped into two
categories: one which addresses the nucleation of crystals and one which focusses on
the growth of the nuclei to form the equiaxed zone. Almost all the mechanisms
proposed in the literature to explain the origin of the nuclei which constitute the
equiaxed zone can be applied to solidification in conventional billet casting machines.
The high meniscus heat-flux values in continuous casting molds, for example could
provide the nuclei according to the Big-Bang theory proposed by Chalmers, wherein the nuclei (which form the equiaxed zone in ingots) are produced in a thin
layer of thermally undercooled liquid in contact with the mold wall.

The theory of "detachment of chill crystals from the mold wall" proposed by Ohno and co-workers and the "dendrite arm remelting mechanism" proposed by Jackson et al are also applicable to continuous-casting considering the extensive fluid mixing in the mold region. Winegard and Chalmers' proposals of nucleation in the constitutionally supercooled region in front of the growing columnar front, and suggestions of heterogenous nucleation of equiaxed crystals in the thermally undercooled region ahead of the columnar front are other possible ways to invoke crystal generation in the continuous casting system.

Many experimental studies have identified one or other of the nucleation processes to be occurring in experimental ingots depending on the imposed conditions. Morando et al have shown that the Big-Bang mechanism is operative in normal casting processes. Ohno and Soda, by placing wire mesh filters in solidifying melts have shown that the dendrite-arm remelting mechanism dominates.
Mahapatra and Weinberg\textsuperscript{115,140} have shown in bottom-grown melts that low temperature gradient and high solute content are essential for nucleation and growth of equiaxed crystals and the arrest of columnar growth. However, it is not known as to which of these processes is likely to be dominant during solidification during continuous casting.

The mechanism by which the continued growth of the columnar dendrites is impeded by the equiaxed crystals has been discussed in much less detail in the literature compared to the attention devoted to the mechanism of nucleation of the latter. There are two aspects to the growth of the equiaxed crystals; the first is that the nuclei can grow only if the latent heat can be conducted out to the mold past the tips of the columnar dendrites and so the equiaxed dendrites must grow at a higher temperature than the columnar dendrites. The second aspect of the problem is how the equiaxed grains can halt the columnar growth.

It is clear from several studies that the columnar-equiaxed transition takes place when the temperature gradient ahead of the columnar front is either zero\textsuperscript{143} or a small positive value.\textsuperscript{115,140} Mahapatra and Weinberg\textsuperscript{140} postulate that the equiaxed crystals grow when the columnar front becomes unstable at critical temperature gradients ahead of it. Kisakurek\textsuperscript{144} observed that the formation of equiaxed crystals ahead of the columnar dendritic interface cannot immediately block columnar growth, but both types of crystals grow simultaneously for a definite period of time before the transition occurs.
Burden and Hunt\textsuperscript{143} and Fredriksson and Hillert\textsuperscript{145} have suggested that the columnar-to-equiaxed transition is caused by an accumulation of equiaxed crystals blocking the growth of the columnar dendrites or by attachment of the equiaxed crystals from the melt to the columnar front. The abrupt cessation of the columnar dendritic front resulting in columnar-to-equiaxed transition was explained by Burden and Hunt\textsuperscript{143} based on the measurements of dendrite tip temperature at different growth rates and temperature gradients in Al-2\% Cu alloy. It was found that for a high positive temperature gradient ahead of the dendrites, the dendrite tip temperature first increases and then decreases with increasing growth velocity. But for a zero or negative gradient, the temperature decreases continuously with increasing growth velocity. Based on the shape of the curves, shown schematically in Fig. 8.3, Burden and Hunt\textsuperscript{143} proposed that when sufficient density of nuclei in the liquid pool begin to grow at zero gradients in front of the columnar front, the latent heat liberated imposes a positive gradient at the columnar dendrite tip and reduces its growth rate. This leads to a reduction in the tip temperature and facilitates increased rate of heat extraction from the bulk liquid as well as enhancement of the growth rate of equiaxed crystals. As this process continues, a condition is reached where the columnar front has almost stopped and the equiaxed structure is growing rapidly. As suggested by Doherty et al\textsuperscript{146} the validity of the Burden and Hunt hypothesis is subject to the availability of sufficient number of nuclei ahead of the columnar dendritic front (at the time when the gradient at the solidification front is near zero).

Once again the above mechanisms of growth were formulated for several imposed conditions; for example, in the work of Mahapatra and
the solidification process was controlled to avoid *dendrite remelting* and *big-bang type of nucleation* and the influence of thermal gradients alone was found to determine the transition. Also, it is not clear how the Burden and Hunt mechanism, which is dependent both on the availability of nuclei and low gradients at the columnar interface, would apply to the continuous casting process.

It now seems clear much of the crystal generation in continuous casting can take place in the mold. However, the location of the columnar-to-equiaxed transition is close to the mold exit only (*where the exit shell thickness is about 10 mm*) when superheats are below 20°C (Fig. 6.26). When the tundish superheats are higher, the columnar-to-equiaxed transition does not take place until the steel moves farther away from the areas of crystal generation: for example, transition occurs only after 28-30 mm of shell has formed on the inside curved wall and 42-43 mm of shell has formed on the inside radius face in the 0.31-0.33% carbon range (Figs. 6.27 and 6.28). That low superheat casting gives predominantly equiaxed structures and that increasing the superheat increases the columnar zone length, is well known in ingot casting\(^{133,147}\) as well as in continuous casting\(^{148,149,150}\). Results of the present investigation are in accord with these earlier findings. In recognition of the fact that given an identical heat extraction scheme (excluding the variation in meniscus heat-flux), fluid flow conditions (not considering EMS), and composition, superheat in the tundish influences the cast structure as schematically shown in Fig. 8.2, it is important to examine how the superheat is removed in the continuous casting process.
8.4.2.1 The extraction of superheat during solidification

In considering the influence of superheat on structure, it must be remembered that the superheat values are calculated as the difference between the temperature of the steel in the tundish and the liquidus temperature. Needless to say, the temperature of the stream pouring into the mold is lower than that measured in the tundish. The temperature loss between the tundish and mold can be estimated by assuming the stream is back-mixed radially and in axial plug flow. The radiative losses for such a stream, also assumed to be cylindrical, pouring through a height of 0.5m between the tundish and the mold, casting a 127x178 mm (5in x 7in) section at 33.9mm/s (80ipm) is a minimum of 6 to 7°C. The calculation is shown in detail in Appendix II. Thus the increase in the length of the columnar zone above 20°C of superheat in the tundish (Figs. 6.25 and 6.26) must correspond to a maximum of 13 to 14°C of superheat in the mold.

Previously researchers have attempted to measure the temperature of liquid steel in the mold. Offerman\(^1\)\(^5\)\(^1\) found that within 1s of its entry into a copper mold, the temperature of the liquid steel adjacent to the submerged entry tube falls to near the liquidus. Wünnenberg and Jacobi\(^1\)\(^5\)\(^2\) continuously measured the reduction in steel temperature between tundish and mold as the steel is poured through a submerged nozzle (Fig. 8.4). They found that when the superheat in the tundish is below 15 to 20°C, the measured superheat in the steel appears to be close to zero, i.e., steel in the mold is very close to the liquidus temperature. At higher tundish superheat values, i.e., 20 to 40°C, the superheat measured in the mold does not exceed a maximum of 15°C. Thus it is
clear that after entering the mold, the steel loses considerably more temperature than the 6 to 7°C expected from radiation loss of open-poured streams as in the case of Eastern Steelcasting and Chaparral Steel.

This rapid loss of superheat in the mold can be accounted for by considering the peak heat fluxes measured at the meniscus. In the present work the calculated heat-flux values (at the meniscus) range from 4000-4400 kW/m², as shown in Fig. 5.99 for steels with carbon ranging from 0.19-0.45%. Such a high value of heat-flux provides significant undercooling at the meniscus to trigger copious nucleation according to Chalmer's Big-bang theory. Ohno and co-workers have postulated the generation of necked crystals at the mold; also, as mentioned earlier, the turbulence in the mold due to the plunging stream could remelt secondary dendrite arms as suggested by Jackson et al. Whatever the mechanism for crystal generation, the fluid flow in the mold would carry many of the mold-generated nuclei into the bulk of the pool. In ingot casting these crystals play a significant role in forming a sedimented crystal zone by falling through the static liquid pool. It is, however, difficult to imagine how many of these crystals would survive in a continuous casting mold due to the ongoing supply of hot steel. But by remelting in the liquid pool, they have an important effect in reducing the superheat. As shown in Appendix III, the amount of solid steel needed to be remelted to reduce the superheat of 1kg of steel by 10°C can be a maximum of 24.7 grams, i.e., 2.47%. Thus if the exit shell thickness, calculated (neglecting any solid remelting) in Chapter 6 (Figs. 6.48 and 6.50) is approximately 9.3 mm, remelting would reduce the shell to 9.07 mm in order to eliminate 10°C.
The precise number of nuclei that are generated at the mold wall and the actual fraction of these that remelt are unknown. Considering the higher peak values obtained in billet casting (4000 kW/m²-Fig. 5.99) compared to those reported in the literature¹⁵³ for slab molds (2500 kW/m²), one can speculate that billet molds would generate a larger number of nuclei. The turbulence in billet molds also should be greater due to the freely plunging stream which entrains gas than in slab molds where the pouring stream enters through a submerged nozzle. Thus one could expect lower superheats in billet molds for a given superheat in the tundish.

From the above analysis, and according to the experimental results of Wünnenberg and Jacobi¹⁵², it is likely that at tundish superheats of 20°C and below, the molten steel as it exits the mold is close to the liquidus temperature. At higher superheats the liquid crater leaving the mold carries with it some residual superheat varying from 1-2°C to 14-15°C depending on the magnitude of the pouring temperature.

The rate of removal of this residual superheat diminishes rapidly because convection in the liquid pool, caused by the input stream is confined to one or two mold lengths.¹³⁷ Thus the higher the residual superheat in the liquid which is leaving the mold, the longer is the time for its complete removal. If the columnar-equiaxed transition depends on complete superheat removal from the liquid, it will be substantially delayed when the superheat in the tundish is high.
8.4.2.2 Mechanism of columnar-equiaxed transition

For low superheats in the tundish (20°C), where there is little, if any, superheat left in the liquid even before the strand leaves the mold, the temperature gradient in the pool ahead of the columnar front would be close to zero. Considering that a sufficient number of nuclei (result of crystal generation at the mold wall or due to dendrite arm remelting) would be growing in front of the columnar interface, the columnar-to-equiaxed transition would be triggered well within the proximity of the mold exit. Hence, at low superheats (20°C in tundish), the columnar zone is arrested within 10-15 mm of shell growth.

The question remains as to why there is a steep rise in the length of the columnar zone as superheats in the tundish are increased beyond 20°C (Figs. 6.25 and 6.26). The answer may be found by considering the extraction of superheat from the liquid presented in Chapter 6 and examining the position where the temperature gradient ahead of the solidification front drops to zero, a condition required by the Burden-Hunt model of columnar-to-equiaxed transition. The model calculations in Chapter 6 (Figs. 6.48 and 6.49) indicate that the zero gradient at the solidification front is reached only when all the superheat from the melt is eliminated. Thus when the superheat in the tundish is high, i.e., when the liquid pool leaving the mold has residual superheat, columnar growth will continue to take place until all the superheat is removed. At such time when the central pool reaches the liquidus temperature, the growth of the equiaxed crystals in the melt would begin. The columnar-to-equiaxed transition would take place provided nuclei had survived as they descended from the mold. These nuclei would be much more
difficult to generate lower in the machine owing to the near stagnant condition of the liquid pool. Thus the transition must depend on the nuclei generated by one or more of the mechanisms like dendrite arm remelting in the upper parts of the machine where there is convection in the pool. If the residual superheat is very high and/or crystal generation (dictated by composition and fluid flow) restricted, it is likely that few nuclei that do form remelt. In this case of the columnar-to-equiaxed transition is delayed or does not take place at all, even though zero temperature gradient ahead of the solidification front is reached.

With this mechanism for columnar-to-equiaxed transition for unstirred billets in view, the effect of the in-mold stirring would be again to reduce the superheat in the liquid pool and to generate nuclei through a dendrite remelting mechanism. The removal of the superheat from the melt can take place by one of the following three possibilities.

1. Stirring, as proposed by researchers,\(^1\),\(^4\),\(^5\),\(^6\) could increase mold heat transfer and enhance superheat removal. Results of the present experiments (Chapter 6) clearly show that this is not true in billet casting. It has been demonstrated that the effect of the EMS on heat transfer, if any, is minimal.
2. It is possible, once again as suggested by other workers\(^4\),\(^5\),\(^6\)\(^1\) that superheat removal is enhanced by suppression of shell formation with EMS. Macroetches of sub-surface structure revealing white bands\(^3\),\(^7\) as well as exit shell thickness demarcated by the "dark bands\(^3\),\(^7\) clearly point out that there is no substantial difference in the shell growth (Table 6.4)
3. The superheat removal enhancement can be explained by the enhanced convective heat flow to the solidification front. This is the major heat flow
effect of EMS as was shown in Chapter 6 (Fig. 6.51). The effect of the increased heat transfer to the solidification front on the overall heat flux appears to be small and could not be detected in the present experiment. The magnitude of the enhanced heat removal from the liquid pool can be assessed only with a heat flow/fluid flow model of the mold region of the continuous caster.

8.4.2.3 Role of composition in columnar-equiaxed transition

In the proposed mechanism, one effect of carbon content would be to influence the availability of nuclei to grow as the equiaxed zone after the central pool loses its superheat. An increase in the carbon content enhances carbon build up at the root of secondary dendrite arms and facilitates the creation of dendrite fragments to serve as nuclei for the equiaxed zone. Thus as carbon content is increased, the columnar zone would be retarded. This can be seen in Figs. 6.12, 6.15, 6.27 and 6.28. The shortening of the columnar zone of lower carbon steels (0.14-0.17%C) after resulphurising suggests that microsegregation, as in the case of carbon, causes earlier remelting of the necks of the secondary arms. With normal sulphur levels these steels exhibit pronounced columnar dendritic growth.

Although the columnar zone decreases with carbon content only until about 0.38-0.39%; beyond 0.40% there is an increase in the columnar zone length. The effect of the carbon content above 0.4% is likely due to the influence of the peritectic reaction on the remelting of crystals. In the composition range of 0.17-0.40%C, γ-crystals that enter superheated liquid can only remelt by first
transforming to $\delta$ by the reaction $\gamma + L \rightarrow \delta + L$. The $\delta$ phase forms first on the surface of the $\gamma$-crystals and ensuing transformation proceeds by solid-state diffusion of carbon from the $\gamma$ to $\delta$ phase. The carbon diffusion limits the transformation rate and extends the survival time of the crystals. Thus the envelope of $\delta$-phase may effectively protect the $\gamma$-core from remelting entirely, depending on the magnitude of the superheat and the time spent in the superheated liquid. The $\delta$ phase also has a higher melting range and the crystals are more resistant to remelting. In steels with carbon content above the peritectic limit, the $\gamma$-crystals have a lower melting range and $\gamma + L \rightarrow L$ involves diffusion of carbon in the liquid phase which is considerably faster than in the solid.

According to the Fe-C equilibrium phase diagram, if this mechanism is correct, longer columnar zones should be found in steels having greater than 0.52% carbon. In this work, the critical value was found to be 0.40% (0.38% in the earlier work of Bommaraju et al\textsuperscript{37}) but this discrepancy may be attributed to the $\gamma$-loop stabilising effect of manganese present in the steels leading to a shift of the upper limit of the peritectic reaction towards lower carbon concentration.\textsuperscript{155}

This mechanism does not explain the longer columnar zone observed in the 0.05-0.09%C billets from Eastern Steelcasting (Fig. 6.15). In these low-carbon steels the microsegregation is smaller than in the high-carbon steels and thus the extent of dendrite arm remelting may be lower. This would mean that the availability of nuclei growing in the liquid ahead of the columnar front is inhibited and hence the delay or absence of the columnar-to-equiaxed transition. In addition
to this the lower meniscus heat-flux values (2500-3000 kW/m²) measured for the low-carbon steels¹²⁰ as compared to the 4000-4500 kW/m² observed for high-carbon billets(Fig. 5.100) could be another deterrent for an early columnar-equiaxed transition. As a consequence of low meniscus heat fluxes, the extent of thermal undercooling (at the mold wall close to the meniscus) would be lower in the low-carbon steels compared to the higher carbon grades, resulting in limited crystal generation and thus limited superheat extraction in the mold. The residual superheat in the melt leaving the mold would be larger and could reduce the density of crystals that could grow ahead of the columnar front, with the result that even though the superheat is completely removed later in the machine, the columnar-to-equiaxed transition would not occur. Evidence of reduced thermal undercooling can be inferred also from the sub-surface structures (Figs. 6.17 to 6.22). As pointed out earlier in Section 8.1, the observation of columnar dendritic growth starting from the surface of the billet in 0.05-0.09%C steels and the presence of a finite "chill" structure in the higher carbon billets is a clear indication that mold heat-flux, especially during the initial moments of solidification plays an important role in the columnar-to-equiaxed transition.

8.4.2.4 Role of machine radius in columnar-equiaxed transition

Figure 6.15 shows that the columnar dendrites have grown to the center in the low (0.05-0.09%) and high (0.61%) carbon billets, but there is a substantial difference (10-20mm) in the upper (inner radius) and lower (outer radius) columnar zone lengths in 0.33-0.35% carbon billets cast at Eastern Steelcasting. The columnar zone is significantly longer adjacent to the inside curved wall than to the
outside curved wall in the case of Chaparral billets as well (Figs. 6.29 and 6.30).

To understand this effect, the orientation of the solidification front with respect to the horizontal must be considered as the billet moves through the curvature of the machine. Referring to Fig. 8.2, it can be seen that although the direction of growth of the columnar front remains perpendicular to the heat extraction surface of the billet, its inclination to the horizontal is continuously changing on both radii. Thus the equiaxed crystals growing from the outside radius in competition with the columnar dendrites, under the influence of gravity, tend to settle on the columnar dendrites. The equiaxed crystals growing on the inside radius side tend to settle away from the slowly progressing columnar dendrites, thus permitting them to continue growth. Earlier work by Ruddle\textsuperscript{156} has demonstrated the effect of gravity in \textit{settling out} of equiaxed crystals after they have nucleated, so that the columnar zone at the top of a casting is not terminated by the presence of the equiaxed grains. Based on \textit{Stoke's law}, the settling velocity of spherical (0.5 mm diameter) particles of solid steel in liquid steel was found to be 0.908 mm/s (a detailed calculation is shown in Appendix IV). Considering that the growth velocity of the solidification front, especially in the lower part of the machine does not exceed 0.1-0.2 mm/s (these values are calculated from the results of the 1-D transient model in Chapter-6), the equiaxed crystals are capable of falling away from the advancing solidification front.

Evidence for the above argument can be seen in the macroetches of the transverse billet slices shown in Figs. 6.35 to 6.46. At the point of columnar to equiaxed transition on the outside curved wall, the columnar crystals on the inside curved wall also tend to branch slightly and in some instances the
equiaxed crystals attach to the growing columnar dendrites. Thus the difference in the columnar zone length corresponding to the two opposite radii of the machine can be explained on the basis of the geometry of the machine. Figure 6.32 shows that if the columnar zone is arrested at 20-25mm, the difference in the upper and lower columnar zone lengths is small. When the columnar zone length increases beyond this point, i.e., as the transverse slice of the casting proceeds into the lower half of the machine, the difference in the columnar zone lengths exhibits a steep rise owing to the dominating influence of orientation and gravity.

As the columnar zone length on the outside radius increases, so does the columnar zone length on the inside radius and the difference is gradually diminished as the columnar zone approaches the center of the billet. But Fig. 6.30 shows clearly that the length of the columnar zone length on the inside curved wall is always greater than that on the outside curved wall.

8.4.2.5 Role of in-mold EMS in columnar-equiaxed transition

Stirring causes an earlier columnar-to-equiaxed transition at all carbon levels and both on inside and outside curved radii (Figs. 6.12, 6.15, 6.27 and 6.28). The effect of the carbon content on the columnar zone length is of a similar nature as that seen in unstirred billets. Also the difference between the columnar dendritic zone lengths adjacent to outside and inside radius faces continues to exist even after stirring (compare Figs. 6.29 and 6.30). The difference between stirred and unstirred structures is simply that the columnar equiaxed transition is triggered earlier with stirring. From the preceding analysis this would mean that
the superheat is removed earlier in the solidification process.

Several earlier workers\textsuperscript{8-10,18,19,40,48-52} have showed that EMS-induced flow in a rotary fashion increases the extent to which the dendritic arms remelt. This aspect of EMS would help an earlier transition, but there is little need to invoke this mechanism to explain structural benefits. The superheat based effect of EMS alone can explain the results. At low superheats there is little difference in the lengths of columnar zone length between stirred and unstirred billets. Since at low superheats of 20°C and below, it has been shown that the residual superheat in the steel is removed by convection and remelting of nuclei generated at the meniscus. Owing to the turbulence of the pouring stream, a large number of dendrite arms remelt, but survive in the central pool since the superheat in the pool is virtually eliminated. The columnar-to-equiaxed transition could easily occur before or soon after the strand leaves the mold. Since carbon affects the ability of the nuclei to survive in a superheated pool, it is not surprising that there is no effect of composition in low superheat casting. Also, there is no difference in the columnar zone lengths of stirred and unstirred billets. The role of EMS would be, as explained below, mainly to reduce gradients ahead of the solid/liquid interface and accelerate superheat extraction. If the residual superheat is negligible, there is no particular role that the EMS would play to trigger the columnar equiaxed transition.

At this point it also becomes clear as to why in-mold stirring was found to be by far superior to sub-mold stirring\textsuperscript{35,87} As seen above the effect of stirring is mainly to remove superheat by enhancing convective heat flow
at the solidification front. The high heat-flux extraction capability of the mold supplements the influence of EMS on superheat withdrawal by increasing the convective heat flow at the solidification front. More importantly, much of the crystal generation is in the mold vicinity and remelting of the mold generated crystals is an effective way of superheat removal. By placing the stirrer away from the region of crystal generation and high heat-flux extraction, the usefulness of sub-mold stirrers would be reduced. Even in a stagnant pool all the superheat is removed by approximately halfway between the meniscus and withdrawal rolls. Placing the stirrer at this location can not help the survival of the dendrite debris generated in the upper regions of the machine. Thus it is essential that if sub-mold stirrers are used they be located immediately below the mold.

From the above analysis it can be suggested that the superheat in the tundish must be controlled depending on the carbon content of the steel while employing in-mold EMS in order to obtain the best structural benefits. For steels with a carbon content less than 0.20% and or carbon greater than 0.40%, it is best to employ the lowest possible superheats. Ideally the superheat in the tundish must be maintained around 20°C. This becomes more important when casting smaller sections at increased casting speeds.
Surface reheating

Surface cooling

Solid shell

Liquid steel

Expansion/contraction forces

Tensile stresses

Figure 8.1. Schematic diagram showing generation of an internal crack due to reheating and expansion of a billet face and consequent rotation of an adjacent corner\textsuperscript{37}.
Figure 8.2. Three possibilities of columnar equiaxed transition on the inside/outside radius faces.
Figure 8.3. Schematic of the simultaneous growth conditions of equiaxed crystals and columnar dendrites to result in an abrupt columnar-equiaxed transition.\textsuperscript{1,3}

$G, G_1, G_2$ are gradients at the solidification front. $C_1, C_2, C_3, C_4$ are columnar dendrite tip temperatures. $E_1, E_2, E_3$ are the tip temperatures of the equiaxed crystal.
Figure 8.4. Plot of superheat in the mold vs. super heat in the tundish.

[The diagram shows a scatter plot with a trend line. The x-axis represents superheat in the tundish in °C, ranging from 0 to 40. The y-axis represents superheat in the mould in °C, ranging from -10 to 25.]
9. SUMMARY AND CONCLUSIONS

A novel study has been conducted to establish the overall influence of in-mold electromagnetic stirring on mold behavior, heat extraction and the quality of billets. For the first time, the thermal field in the mold wall and the temperature of the cooling water were monitored while taking billet samples in two industrial trials at operating billet casters equipped with in-mold EMS. The temperature profiles were later converted to heat-flux profiles using a two-dimensional heat transfer model. The heat-flux profiles were employed to study the progress of solidification under simulated fluid flow conditions using a one-dimensional transient heat flow model. The billet samples were characterised in terms of oscillation marks, rhomboidity, off-corner cracks, subsurface structures and columnar zone lengths.

The study of the thermal fields of the mold representing the EMS-on and EMS-off conditions in a series of heats cast during the two campaigns has led to the following important new findings.

1. Analysis of the measured temperature profiles of the mold and cooling water after turning the EMS motor off indicated that the influence of in-mold electromagnetic stirring on mold heat transfer is small and is masked by the frequently changing mold/billet gap as a result of the dynamic distortion of the mold tube.

2. This study unambiguously demonstrated that non-uniform distribution of cooling water, low water velocity and poor water quality give rise to boiling in the water channel, overheating of the mold, scale deposition and severe mold
distortion in one campaign. This resulted in a time-dependent variation of the mold-billet gap and thermal fluctuation of the mold wall and the outlet water temperatures.

3. The thermo-mechanical behavior of rectangular molds was examined for the first time and it was discovered that the thermal field was subject to periodic fluctuation. For rectangular molds, it has been postulated that the differential expansion of the broad face vs. narrow face, causes the adjacent walls of the mold tube to move in diametrically opposite directions primarily at the midface, aided by the rotation of corners, giving rise to an oscillation of the temperature profile of the mold.

4. A steeper taper in the upper regions of the mold was found to give rise to higher overall heat extraction through the mold. The periodic change in the mold/billet gap, inferred from the mold temperature oscillation, was suggested to nullify the benefits of the large taper, in the rectangular molds.

Billet quality examination in terms of oscillation marks, rhomboidity and off-corner cracks has led to the following new findings.

1. EMS appears to affect neither the depth of the oscillation marks nor the variation of the depth across a given face. Also the formation of subsurface laps or hooks was not influenced by EMS.

2. Irrespective of whether EMS was used or not, rhomboidity was found to vary in both magnitude and direction from one billet sample to the next.

3. In-mold EMS does not appear to affect off-corner cracking. Periodic mold wall movement at the end of the steep taper of 2.6%/m and the bulging of the billet in the lower part of the mold where the taper is only 0.48%/m were
suggested to generate the off-corner cracks in the rectangular billets.

This study establishes unambiguously that the genesis of billet quality in the mold is related to the dynamic mold distortion. That in-mold EMS has no effect on the mold-related billet-quality is due to its lack of influence on the mold/billet gap and heat transfer.

The sub-surface structures of a number of billets were examined for the first time. The absence of chill structure in the subsurface etches of 0.05-0.09%C steels was attributed to low meniscus heat fluxes. In higher carbon steels, the chill zone was found to vary in thickness. In the vicinity of the oscillation marks, often, a larger equiaxed crystal area was found. This may be due to the high heat extraction during the negative strip time when the negatively tapered mold deforms the newly forming shell and forms the oscillation marks. It is clear from the study that the extent of the chill structure is not influenced by EMS.

In the study of the formation of the solidification macro-structure it was observed that the columnar dendrites of the stirred billet incline into the rotary flow only after about 0.5-1.0 mm of shell has formed. This may be because the momentum boundary layer prevents the flow from affecting the columnar dendritic growth during the initial few seconds of solidification.

A one-dimensional heat flow/solidification model was used for the first time to find the shell thickness where the superheat is removed from the
melt for several imposed limits of superheat in the mold and extent of thermal mixing while calculating the gradients at the liquidus isotherm.

The main contribution of this study of cast structure in stirred and unstirred billets of varying carbon and superheat is recognising the fact that several mechanisms suggested in the literature operate in the machine at the same time. The comprehensive influence of superheat, carbon content in the steel and the radius of curvature of the machine on the columnar-to-equiaxed transition in billet casting machines was explicated for the first time. It appears that the columnar-to-equiaxed transition takes place after complete removal of superheat from the melt and subject to the availability of sufficient density of dendritic debris resulting primarily from dendrite arm remelting. If the superheat is removed early in the machine where the crystal generating mechanisms operate, transition from a columnar-to-equiaxed mode of solidification readily takes place. For high superheat in the tundish, the superheat removal is delayed and the density of surviving crystals is reduced. Thus a longer columnar zone results. The influence of the carbon content is through facilitating the generation and survival of the crystals that grow equiaxially ahead of the columnar front.

Based on the evidence from macrostructures, it was pointed out that when the columnar-to-equiaxed transition takes place on the outside radius, the crystals growing ahead of the inside radius columnar front descend under gravity allowing the columnar front to grow on that side. If the transition occurs in the melt earlier in the machine where the inclination of the columnar front to the horizontal is small equal lengths of the columnar zone result on both the radii. The
difference in upper and lower columnar zone lengths increases with the distance from the meniscus.

This study suggests that the influence of the in-mold EMS on the quality of the billets cast is limited mainly to triggering an earlier columnar-equiaxed transition. The improvement of mold-related quality must most certainly constitute steps to control the dynamic mold distortion. Based on this study the following suggestions can be made.

1. Uniform and high water velocity must be ensured in billet molds by assuring uniformity of the baffle tube/mold tube gap and by employing high cooling water velocity.

2. Non uniform mold wall movement in rectangular sections can be minimized to an extent by increasing the wall thickness and possibly by mechanically constraining the mold wall.

3. It is important to ensure steeper taper in the lower part of the mold to control the bulging of the strand.

4. Sub-mold support rolls could be useful in controlling the off-corner crack formation especially when casting billets with aspect ratios greater than 1.

5. In order to achieve improved structural refinement with EMS in low and high carbon steels, control over the superheat in the tundish must be exercised. In steels with carbon lower than 0.20% and greater than 0.39% superheat in the tundish should not exceed 25°C.

6. Sub-mold stirrers located at mid-radius serve little purpose in control of the solidification structure. It is vital to place the stirrers as close to the meniscus as possible.
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APPENDIX-I

Water Velocity Measurements at Charter Electric Melting

After the trial at Eastren Steelcasting, other steel mills plants using in-mold EMS technology were sought to continue the experiment. The Charter Electric Melting Company in Chicago permitted the test to measure the water flow distribution in their molds.

The mold system at the Charter Electric Melting is similar to that at Eastren Steelcasting. To facilitate the placement of the EMS coil the inlet plenum was reduced in size. Also, the baffle tube is built by welding in four steel plates leaving square corners. As a result the water channel width is more at the corners than at the midface. The cooling of the EMS coil is accomplished by the mold cooling water. The molds are 9.525mm thick, and of 101.6×101.6 mm section size. The nominal water gap is 4.318mm.

Similar to the experiment at Eastern Steelcasting, the water flow distribution in the water channel around the mold tube was measured. Pitot tubes and static pressure taps were installed in a discarded mold tube at UBC and the test was conducted on site on a down day. The instrumented mold tube was fitted into the mold housing and the test was carried out at water flow rates of 9.46, 15.77 and 22.08 l/s. Results of this experiment are shown in Table 5.2.
APPENDIX-II

Temperature Drop of Pouring Stream

The temperature loss between the tundish and mold can be estimated by assuming the stream is back-mixed radially and in axial plug flow. The stream is assumed to be cylindrical with the following dimensions.

Stream diameter, \( D = 2.54 \times 10^{-2} \text{mm} \)

Area of cross-section of the stream \( A_s = \pi D^2 / 4 = 5.069 \times 10^{-4} \text{m}^2 \)

Height of the stream, \( H = 0.5 \text{m} \)

The residence time of the stream can be calculated as shown below:

Casting speed, \( S_b = 3.39 \times 10^{-2} \text{m/s} \)

Area of cross-section of the billet, \( A_b = 2.258 \times 10^{-2} \text{m}^2 \)

Volume flow-rate of steel, \( F = S_b \times A_b = 7.65 \times 10^{-4} \text{m}^3/\text{s} \)

Stream speed, \( S_s = S_b \times A_b = 1.509 \text{m/s} \)

Residence time of the stream, \( \tau = H / S_s = 0.33114 \text{s} \)

Radiative heat losses from the stream can be calculated by writing the heat balance Equation II.1.

\[
\rho V C \left( \frac{dT_s}{dt} \right) = A \sigma \varepsilon (T_s^4 - T_a^4)
\] II.1

where,

\( \rho = \text{Density of steel} = 7400 \text{kg/m}^3 \)
V = Volume of the stream

C_p = Specific heat of steel = 682 J/Kg °K

T_S = Initial surface temperature of steel = 1873 °K

t = Time (s)

A = Surface area of the Stream

σ = Stefan-Boltzman's constant = 5.68 \times 10^{-8} W/(M^2 °K)

ε = Emissivity = 0.9

T_a = Ambient temperature = 303 °K

Rate of temperature drop, \( \frac{dT_S}{dt} = 19.62 °C/s \)

Temperature drop expected in the steel = \( (\frac{dT_S}{dt}) \times \tau \)

= 6.496 °C
APPENDIX-III

Reduction of Superheat by Remelting Solid Steel

The amount of solid steel needed to be remelted to reduce the superheat of 1kg of steel by 10°C can be calculated as shown below.

\[ L = \text{Latent Heat of solidification} = 276000 \text{ J} / \text{kg} \]

\[ C_p = \text{Specific heat of steel} = 682 \text{ J} / \text{Kg °K} \]

\[ S_h = 10^\circ C \]

To cool down a Kg. of steel the amount to be remelted = \[ C_p \times S_h / L = 0.0247 \text{ Kg.} \]
APPENDIX-IV

Terminal Velocity of Falling Solid Sphere Through Liquid Steel

The terminal velocity, \( V_t \), of a falling sphere through a liquid can be calculated from the following equation IV.1 derived by equating the sum of the buoyant force and drag force to the weight of the sphere.\(^{15}\)

\[
V_t = \frac{2R^2(\rho_s - \rho)g}{9\eta}
\]

where,

\( R = \) Radius of the particle = \( 0.5 \times 10^{-4} \) mm
\( \rho_s = \) Density of solid steel = \( 8000 \) Kg/m\(^3\)
\( \rho = \) Density of liquid steel = \( 7000 \) Kg/m\(^3\)
\( g = \) Acceleration due to gravity = \( 9.81 \) m/s\(^2\)
\( \eta = \) Viscosity of liquid steel = \( 6.0 \times 10^{-3} \) Kg/m s.

The value of terminal velocity for a 0.5 mm particle is \( 9.08333 \) mm/s.

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