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Mathematical and Physical Modelling of a Single-Belt Casting Process

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• P. Netto 1998



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**To my wife Claudia, my son
Vinicius and my parents.**

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ABSTRACT

The quality of the strip produced in a near-net-shape casting process is strongly linked to the metal feeding system. In a single-belt caster, the delivery system has major importance. It determines how the liquid metal will be fed onto the cooling belt and so it is responsible for an even distribution of the metal in the width and in the thickness. An alternative metal delivery system was proposed and a comprehensive mathematical model, including heat transfer, fluid flow and stress studies was used as a tool to perform this task. This model included three-dimensional flows with fully coupled momentum and heat transfer. Turbulence effects as deduced from the standard κ - ϵ equations, macroscopic solidification, as well as flow through porous media were also modeled. Based on the temperature fields obtained, a further simple model for stress was employed to calculate the displacements and the stress distribution throughout the forming strip. The model was validated through comparison with water model flows and semi-analytical solutions for solidification.

Besides the mathematical model, an experimental apparatus was developed, reproducing as closely as possible the type of metal-substrate contact occurring in a single-belt caster. Casting of thin strips at high speeds was achieved and a parametric study was carried out, for the most important variables affecting the horizontal casting process. An inverse heat conduction model was used to evaluate the interfacial heat fluxes and heat transfer coefficients.

The standard extended nozzle configuration did not provide a smooth flow in the reservoir. Recirculation zones besides a nonuniform delivery of the metal to the belt were observed. Turbulence on the region of the strip was mainly affected by the speed of the conveyor belt. Divergences in the solid fractions between the

center and the edges of the strips were found to increase with increasing turbulence at the exit gap. The values of the morphology constant C and the overall heat transfer coefficient h were considered the most important inputs of the model. Furthermore, modelling turbulence through a κ - ϵ model proved to be critical for this particular configuration. The solid shells predicted by the κ - ϵ model were much closer to the analytical solutions than the ad-hoc viscosity model predictions.

The adoption of a flow modifier within the extended nozzle eliminated the recirculation zone in the reservoir and yielded a smoother flow to the belt. Such improvements in the flow distribution were decisive to ensure more uniform solidification along the width of the strip. Shortcomings from the use of the filter were overcome with changes in the geometry of the reservoir.

In early solidification, the strip was found to displace inwardly, due to the cooling and contraction of the solid shell. Tensile stresses were calculated at the cooling surface, whereas compressive stresses were found in the middle of the strip. The constitutive model and the material properties affected more the predictions of displacements than the stress calculations.

Interfacial heat fluxes and heat transfer coefficients measured on the strip casting simulator were highly dependent on the contact metal-substrate-coating. Values of q and h varied threefold for the various coatings and levels of roughness investigated. Heat fluxes were also affected by the strip thickness and the initial superheat. Correlations were derived to predict peak heat fluxes and the evolution of q after the peak, for the various coatings. A relationship was observed between the peak heat transfer coefficient and the total solidification time. The SDAS predicted according to the calculated cooling rates correlated well with SDAS measured in Al-Si alloys

RÉSUMÉ

La qualité des bandes produites via un procédé de coulée directe est directement reliée au système d'alimentation en métal. En coulée sur courroie unique, le système d'alimentation joue un rôle majeur. En effet, il détermine la capacité à alimenter correctement la pièce et est donc directement responsable de la distribution spatiale en métal. L'utilisation d'un modèle mathématique complexe, tenant compte des transferts de chaleur, des flux turbulents de métal liquide et des tensions au sein du produit en cours de solidification, a permis de développer un nouveau système d'alimentation. Le modèle mathématique a, en particulier, pris en compte la tridimensionnalité des flux, couplés aux écoulements turbulents. Les effets liés aux turbulences, déduits des équations standards κ - ϵ , la solidification macroscopique ainsi que les flux de métal à travers les zones poreuses ont aussi été modélisés. Les tensions internes en tout point de la bande ont été calculées, et ce à partir des calculs de distribution des températures. Le modèle a été validé par expériences sur des écoulements d'eau et par comparaison avec des cas semi-analytiques de solidification.

Outre la modélisation mathématique, un appareil, reproduisant le plus correctement possible les contacts métal liquide-support de coulée du système de coulée sur courroie unique, a été développé. L'étude de la coulée de bandes à haute vitesse a ainsi pu être réalisée, et ce en tenant compte des paramètres affectant principalement le procédé de coulée. Un modèle inverse de conduction de chaleur a été utilisé pour évaluer les flux interfaciaux et les coefficients de transfert thermiques.

Le système d'alimentation originel a donné lieu à un flux irrégulier de métal, conduisant à des zones de recirculation et à une distribution irrégulière du

métal dans les bandes. Les turbulences proches de la bande dépendaient pour une large part de la vitesse de la courroie. Les inégalités de fraction solide entre le centre et le bord de la bande ont augmenté de concert avec les turbulences en sortie de réservoir. Les valeurs de la constante de morphologie C et du coefficient général de transfert thermique h se sont avérées être les entrées déterminantes du modèle. En outre, la modélisation du flux turbulent via l'usage du modèle κ - ϵ a été cruciale pour un tel système d'alimentation. Les structures des bandes prédites par ce modèle κ - ϵ ont été plus proches des solutions analytiques que celles prédites par le modèle de viscosité ad-hoc.

L'adoption d'un filtre a permis d'éliminer la zone de recirculation dans le réservoir, assurant ainsi une alimentation en métal liquide plus régulière sur la courroie. La conséquence directe en fut une amélioration significative de l'uniformité de la solidification du produit sur sa largeur. Les inconvénients liés à l'adoption du filtre ont été surmontés par une modification de la géométrie du réservoir.

En début de solidification, la contraction de la partie externe de la bande par refroidissement induisaient un rétrécissement de la bande vers le centre. Les tensions ont été trouvées positives sur la surface de refroidissement, alors que la compression a été prédite au centre de la bande. Le modèle constitutif et les propriétés du matériau, plus que les calculs de tension, ont affectés les prédictions des déplacements.

La mesure des flux interfaciaux de chaleur et les coefficients de transfert thermique s'est avérée dépendante des contacts métal-support de solidification-revêtement. Les valeurs de q et h ont triplé suivant les revêtements utilisés et les rugosités étudiées. Les flux de chaleur ont également été affectés par l'épaisseur

de la bande et par la température initiale du métal liquide. Des correlations ont été dérivées de cette étude pour prédire le pic de flux thermique et l'évolution de q après ce pic, et ce pour les différents revêtements. Il a été trouvé que le temps de solidification de la bande et la valeur du pic du coefficient de transfert thermique sont reliés. Les mesures des flux de chaleur ont été validées par comparaison de l'espacement des branches secondaires des dendrites entre des alliages Al-Si et les calculs via le modèle inverse de transfert de chaleur.

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INTRODUCTION

The iron and steelmaking industry in North America and other developed countries has faced great challenges in recent years to maintain its competitiveness. In the beginning of the 80's, ceramics, plastics and composites represented a major threat to the steelmaking industry. Some industries, like the aeronautical, replaced many parts, like engines parts, originally made by metals (special classes of steel) with other materials of better performance, especially with respect to some mechanical properties.

During the 80's, many special classes of steel were developed, such as HSLA (High-Strength Low-Alloy), providing a resurgence of the steelmaking industry. Most of those special classes are actually classified as a sort of an alternative material.

Despite the major developments concerning the quality of steels, the increase in productivity and efficiency of the process steps were not remarkable. From the beginning of the 80's, much research has been directed to develop so called "near net shape casting" on a commercial scale. Various processes, like roll processes and belt processes are being tested in many different countries^(1,2). For steel castings, the major developments have been the thin slab casters, developed in the last ten years. The first thin slab caster aiming at a final strip production

was developed in 1987 by SMS Schloeman Simag⁽³⁾. The process is called CSP (Compact Strip Production) and since then a number of CSP plants have been built in North America.

Recently, Cramb⁽³⁾ reviewed the trends in the strip casting of steel. In Figure 1-1, he compares the achieved and potential productivities of casters. As seen, the Nippon Steel Twin Roll has productivity similar to the Thin Slab process, but considerably lower than the conventional continuous casting process. The only process that has the potential to match the productivity of the conventional machines is the MEFOS single-belt process. The author also gives a comparison of the cast speeds achieved by the various technologies (Figure 1-2), where it is shown that the final strip thickness in the roll processes is much less than the intended strand thickness in the MEFOS project. The reason is that, according to Spitzer and Schwerdtfeger⁽⁴⁾, the philosophy with the single-belt caster is that hot rolling is necessary for most steel grades, prior to cold rolling, to obtain strip properties comparable to the conventional continuous casting process. So, in this process, the belt speed and the strand thickness are 1 m/s and 10 mm, respectively. This allows hot rolling with 50 to 80% reduction and eases the requirements on surface quality and dimensional uniformity⁽⁴⁾. For the twin-roll process to cast thicker strips, the rotational speed would have to be reduced and the minimum roll diameter would need to increase, to approximately 3 meters. Due to the additional cost and troubling operational conditions, this hypothesis has been discarded. Another drawback of the twin-roll process is its inability to deal with the contraction occurring during solidification of low carbon steels (0.08% range). The contraction is due to the peritectic reaction, making it difficult the contact between the roll and the strip, and requiring even larger rolls for low-C steel castings⁽⁴⁾.

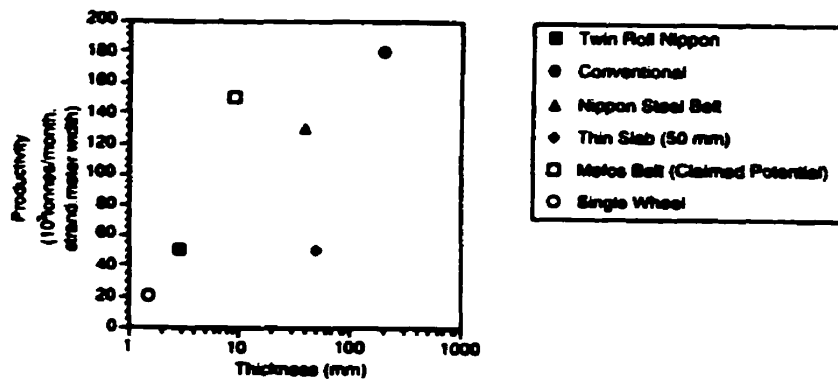


Figure 1-1. Achieved and potential productivities of casters⁽¹⁾.

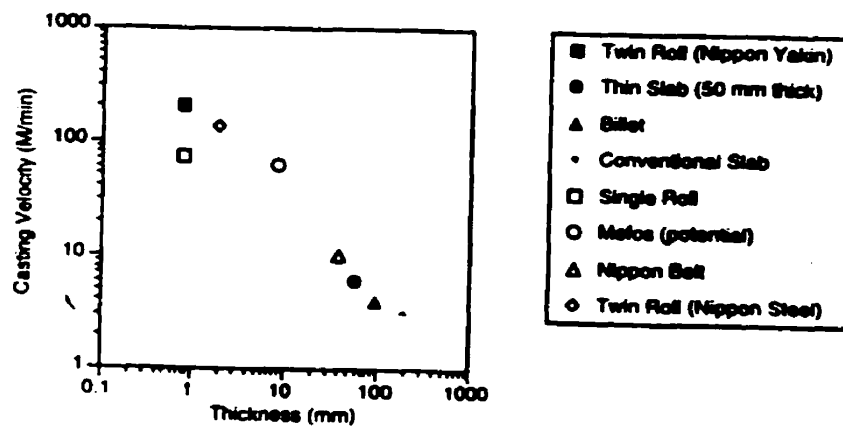


Figure 1-2. Achieved casting speeds of various tecnologies⁽¹⁾.

In order to achieve good results when trying a new process, laboratory or pilot plants can be useful, but most of the times their costs are not low.

On the other hand, the theoretical approach, represented by the solution of the conservation equations is far from simple. However, with the recent advances in modeling techniques and computing power, developing models that combine practicality and reality has been possible. Such models are significantly cheaper than the pilot plants/experiments that must be constructed and carried out, respectively, every time changes in the parameters of the process are necessary.

A good mathematical model must incorporate empirical correlations, so that its predictions can approximate the real conditions of that particular system.

Modeling continuous casting processes is not a simple task. A complete model should comprise both fluid flow and heat transfer, with phase change. There are phenomena in the macroscopic and microscopic scale that need to be taken into account. To model such processes, one can employ well-known software packages for modelling fluid flow, such as FIDAP, PHOENICS, etc. and incorporate heat transfer and phase change. The non-linearity of the equations and its boundary conditions contribute to the complexity of the problem.

The geometry and the nature of the flow are also factors that can increase the difficulty of the problem. Intricate geometries, with walls in the computational domain or turbulent flows over small openings cause major changes in the variables over a short domain. These steep variations have to be handled carefully to avoid divergence in the numerical method, as well as to provide the correct solution. Some special numerical techniques must be employed in those cases.

Similarly, the velocity abruptly goes to zero when solidification is complete in a phase-change process.

For systems in which external momentum sources are the most relevant, such as in twin-roll processes, natural convection effects can be neglected. However, for a belt-caster fed by a tundish, those effects should be accounted by means of the Boussinesq approximation, if important.

Another important aspect to be considered when modeling fluid flow is the presence of flow modifiers, like porous media. The governing equations must be corrected in the domain of the flow modifier, i.e., the pressure drop through the modifier must be linked to the porosity of the medium and to the pore diameter.

For the mathematical model to be efficient and useful for practical purposes, it is necessary that it incorporates realistic boundary conditions. In the specific case of single-belt processes, one of the most important boundary conditions is the interfacial heat transfer between the solidifying strip and the moving substrate. The determination of interfacial heat fluxes and heat transfer coefficients has been the subject of exhaustive studies in the literature. However, most of these measurements have considered heat transfer between a static mold (foundry and conventional continuous casting processes) and the cast metal. Also, the casting speeds were much lower (usually zero) and the strand thicknesses and solidification times involved considerably higher, than those in near-net-shape casting processes. These process variables are of primary importance for heat flux characteristics, as shown by Wang and Matthys⁽⁹⁾. The contact pressure is another important variable for these fast-cooling processes. One good example of this is the fact that strips produced with the Planar Flow Casting process present heat transfer coefficients

approximately one order of magnitude higher than those developing during Free Jet Melt Spinning, for the same strip thickness and casting speed⁽⁵⁾. The basic difference between these processes is that in the first the puddle is constrained by an applied pressure, which improves its contact with the substrate (single-wheel), whereas in the second, it is not.

One current drawback to the development of the near net shape processes is the quality of the strips produced. Major problems have been reported concerning cracks and sticking of steel to the coating and extensive research has been carried out^(6,7). Enabling the continuous strip casting is also a challenge, with respect to the coatings used in the processes⁽⁶⁾. The quality of a thin strip can be defined as dimensional uniformity and the absence of any cracks or lap marks in the surface.

Regarding the quality of the strip produced by a near net shape process, it is strongly linked to the feeding system. In a single-belt caster, the delivery system is of major importance. It determines how the liquid metal will be fed onto the cooling belt and so is responsible for an even distribution of the metal in the width and in the thickness of the cast strip. Trying to predict the flow behavior and the heat transfer rates are of undoubtable relevance to the final quality of the strip. As mentioned earlier, this task can be accomplished by both mathematical and physical modeling. Finally, the prediction of the stresses developing during solidification of the steel strip on the belt can give valuable information regarding the potential quality of the strip and also the design of novel delivery systems.

1.1 OBJECTIVES OF THIS STUDY

The main purpose of this Ph.D. work is to help identify the potential of horizontal strip casting and to explore some of the difficulties related to the quality of strips produced by these single-belt strip casting processes (Fig. 1-3). A metal delivery system is proposed and a comprehensive mathematical model, including heat transfer, fluid flow and stress studies is used as a tool to perform this task. This model includes three-dimensional flows with fully coupled momentum and heat transfer. Turbulence effects as deduced from the standard κ - ϵ equations, solidification, as well as flow through porous media are modeled. Based on the temperature fields obtained, a further simple model for stress is employed to calculate the displacements and the stress distribution throughout the strip.

Along with the mathematical model, an experimental apparatus was developed for the present research, which aimed at reproducing, as closely as possible, the type of metal-substrate contact occurring in a single-belt caster. Casting of thin strips at high speeds is pursued and a parametric study is carried out, for the most important variables affecting the horizontal casting process. An inverse heat conduction model is used to evaluate the interfacial heat fluxes and heat transfer coefficients.

The following specific objectives were pursued in this PhD project:

1. To develop and validate a three-dimensional mathematical model for fully-coupled fluid flow and heat transfer of a proposed novel configuration for horizontal belt casting. The model was based on a finite-difference method and the turbulence effects in the fluid flow

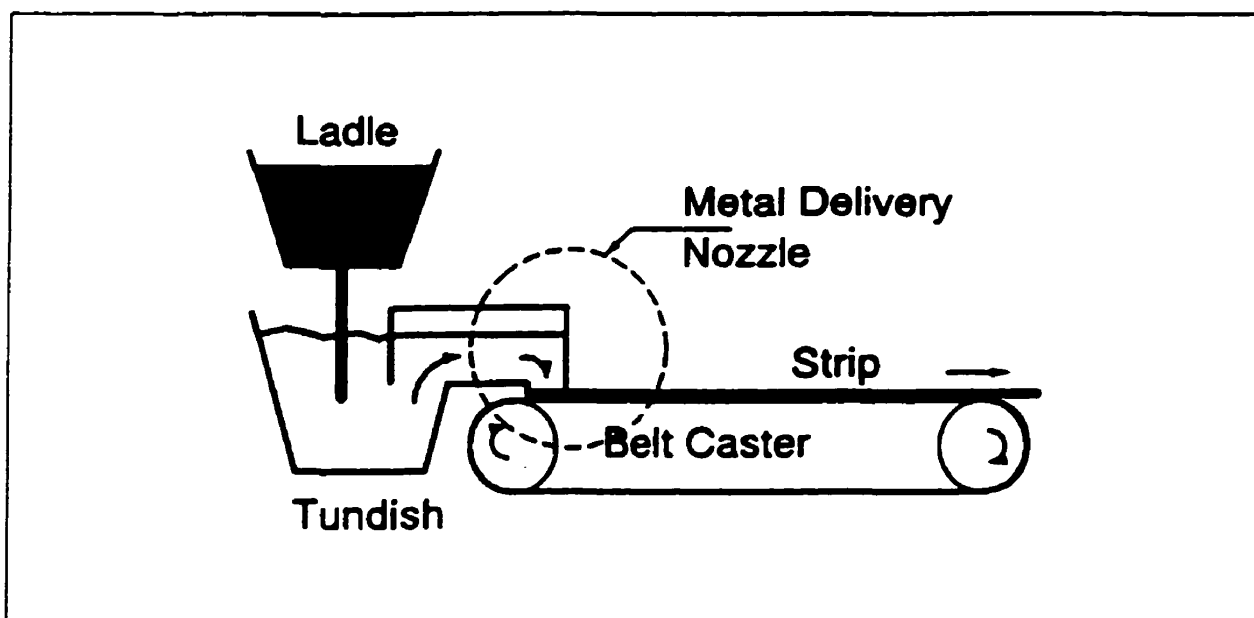


Figure 1-3. Schematic of a single-belt caster configuration⁽⁸⁾.

were accounted for by a Low Reynolds number κ - ϵ model. An enthalpy-porosity scheme was employed to model metal solidification, together with continuum equations for the conservation of mass, momentum and energy in a solid-liquid phase change system. In addition, the effects of the inclusion of a flow modifier in the delivery region were accounted for by employing the classical equations for flow through porous media.

2. To develop and validate a model to predict qualitatively the evolution of stresses and displacements of the solid shell formed on the cooling belt. A thermal-elastoplastic approach was adopted for the stress model, for the main sources of stresses originated from the sharp temperature gradients across the solidified shell. Due to the huge numerical complexities involved, the stress model and the fluid flow/heat transfer were uncoupled. The intention of this work is to enable the researcher to identify qualitatively what locations throughout the strip are more prone to defects and to learn how the solidifying strip deforms. This way, a simple approach will be employed to give a first insight on this important matter.
3. To simulate the influence of some important variables of the process and, more important, to determine the most suitable conditions to the casting of strips of different thicknesses. To achieve this objective, modifications in the geometry of the delivery system and other operational conditions were performed, in addition to the inclusion of the flow modifier. Physical properties, thickness and relative position of the "filter" were some parameters evaluated concerning

the porous medium. In determining better casting conditions, one of the main parameters was the uniformity of the flow along the width of the strip: a more uniform flow yields a more uniform solidification, reducing the divergence between the center of the strip and the edges that is responsible for some of the defects encountered in the strips cast on the single-belt casters⁽⁴⁾.

4. To construct a strip casting simulator that would match as close as possible the intended industrial conditions for the single-belt processes. This simulator would allow casting of very thin strips at fairly high substrate speeds. It should also allow studying the influence of the most important process variables on the interfacial heat transfer characteristics, these variables being the substrate roughness and type of coating applied to the substrate, the strip thickness and inlet superheat.

1.2 THESIS OUTLINE

A comprehensive literature review is given in Chapter 2. The survey includes details concerning the most important near-net-shape casting processes and their current stage of development, for the casting of molten steel. Also, the mathematical formulation and numerical tools available for the modelling of such processes were reviewed, including a number of approaches for modelling the flow through porous media. The experimental techniques for the determination of the interfacial heat transfer characteristics between a solidifying metal and a moving substrate are also assessed in this Chapter.

In Chapter 3, the fundamental equations for turbulent fluid flow/heat transfer and the enthalpy-porosity approach for solidification are stated, together with the classical equations for flow through porous media. The numerical treatment for the complex formulation is also given, along with the presentation of grid independence and convergence criteria. Boundary conditions for both the fluid flow and heat transfer problems are discussed in Chapter 3, as well.

Chapter 4 corresponds to the simulations run with the mathematical model for the proposed metal delivery system, without the flow modifier. Geometrical parameters as well as operational conditions were varied and their influence on the fluid flow/heat transfer results discussed. Also, the effects of the assumed values for the thermophysical properties and the boundary conditions adopted were determined. The impact of modelling the turbulent flow with a two-equation $k-\epsilon$ model was assessed and its benefits highlighted.

The inclusion of the flow modifier, a reticulated ceramic filter, in the computational domain is introduced in Chapter 5. The influence of the filter on the flow properties and its contribution to solidification uniformity are evaluated. Recommendations regarding suitable casting conditions for different strip thicknesses are eventually given.

The model validation is presented in Chapter 6. The fluid flow predictions were compared with the velocity measurements carried out by Jefferies[®] on a water model. To validate the solidification results, some idealized conditions were simulated by the model and compared with semi-analytical solutions. Finally, the SDAS (Secondary Dendrite Arm Spacings) predicted with the use of the model simulations and relationships existent in the literature are compared with the scarce

data available for single-belt casters.

Qualitative predictions for the stresses and displacements on the solid shells are given in Chapter 7. Discussions are also included about the importance of the constitutive models and elastoplastic properties of the material to the results. An attempt to validate qualitatively the model closes this Chapter.

The strip casting simulator constructed for the experimental work is shown in detail in Chapter 8. A parametric study is presented, accounting for the effect of the most relevant process variables in the interfacial heat transfer between solidifying aluminum strips and a moving substrate onto which various coatings were industrially applied. The analysis stresses the importance of the contact metal-substrate-coating on the heat transfer characteristics and the practical implications of the experimental findings on the development of horizontal strip casting.

The most important conclusions drawn from this work are presented in Chapter 9, together with claims to originality and suggestions for future work.

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LITERATURE SURVEY

This chapter is divided into four topics:

- a) near net shape casting processes, particularly single-belt processes;
- b) mathematical models for strip casting, with emphasis on single-belt processes;
- c) porous media applied to strip casting;
- d) stress models for continuous casting.

2.1 THIN STRIP CASTING TECHNOLOGY

Near Net Shape Casting is a major development for the whole metallurgical industry. In these processes, the conventional hot mill, comprising of the slab reheating furnaces and hot rolling mills could be partially, or totally, eliminated, causing a substantial reduction in equipment and operational costs. Some of the advantages of near net shape casting are:

- i) reduced capital costs, for investment and operation;
- ii) energy savings;
- iii) higher productivity, as a consequence of the reduction of the number

- of rolling passes;
- iv) higher flexibility;
- v) hot deformation is still permitted in some cases, if required;
- vi) finer solidification structure, which is more suitable for direct cold rolling;
- vii) chemical homogeneity⁽¹⁾.

The final product can be as thin as 25-50 mm for slabs, and as 0.5-20 mm for strips.

Bessemer⁽²⁾ was the first to envisage the hypothesis of direct casting. His patent is the basis of the current roll processes. Since then, numerous attempts have been made to develop a number of different new technologies. Some literature reviews have been written on these attempts⁽³⁻⁵⁾.

In 1991, Hamel⁽³⁾ presented a study about the state of the art of these new technologies. The many different processes were described and their stage of development reported. Another detailed description of the most important plants carrying out tests has been given in Reference (4).

Presently, the most widely used processes are the roll processes (single and twin) and the belt ones (also single and twin). Although some projects are already in commercial use, most of them remain in development. Furthermore, near-net-shape casting of stainless steel has been more successful than casting of carbon steel. Differences in the steel microstructure and the resistance to oxidation favour strip casting of stainless steel⁽⁴⁾.

According to Rohde et al⁽⁶⁾, the development of thin strip casting processes faces challenges in three major areas:

- i) geometry of the strip;
- ii) surface of the strip and,
- iii) physical properties.

For the next few years, a major change in the structure of the worldwide steel industry is predicted⁽⁵⁾. Most plants under construction are based upon the casting of thin slabs between 50 and 90 mm thick.

Cramb⁽⁵⁾, in his recent review, mentioned the existence of a few pre-industrial pilot facilities for strip casting of steel. The aim is the production of 1-3 mm thick strips.

Since the thickness of the strips is much less than the conventional slab thickness, it is necessary to have a higher casting speed if the new machines are to be competitive. Speeds in the range of 50-130 m/min are therefore common for such processes. The high speeds associated with these processes imply less residence time of the metal in the mold, which may require further study of the variables involved.

For the twin-roll casters, the roll design and life are major issues⁽⁵⁾. For the belt processes, the delivery system and the geometrical uniformity of the strip are the most important points still to be improved. For the twin-belt casting of steel, major problems with surface quality, together with the accumulation of inclusions

towards the upper face, and problems with side containment were reported^(7,8).

Figure 1-1 shows the achieved and potential productivities of some strip casters. It is seen that the belt processes are the ones that match more closely the productivities of conventional continuous casting processes.

As mentioned before, the vast majority of near-net-shape casting processes are still in development. The substitution of the conventional continuous casting will not be an easy task for metallurgists. Teoh⁽⁸⁾ outlined some main requirements for the slab/strip casting processes. Here is a summary:

- i) Availability of appropriate process control system for the furnace, to supply molten metal with the desired temperature and chemistry and in a continuous fashion;
- ii) Stability of the meniscus level in the mold, requiring close proportionate control of metal flowrate through an appropriate delivery system;
- iii) High casting speeds, yielding high productivities;
- iv) Strict control of the effect of process parameters on product quality;
- v) Controlled hot reduction to break up solidification structures and to regenerate a more uniform and fine-grained microstructure.

2.1.1 SINGLE-BELT CASTER

The most important project of a single-belt caster currently under development is the MEFOS strip casting project. The aim of MEFOS and its partner MDH is to develop a flexible strip casting technique, for both carbon and

stainless steel⁽⁹⁾.

From a liquid metal supply system, the steel runs to a water-cooled belt. The molten steel solidifies on the moving belt at a speed between 0.5 and 1 m/s. The strips are intended to be 450 mm wide and between 2-10 mm thick.

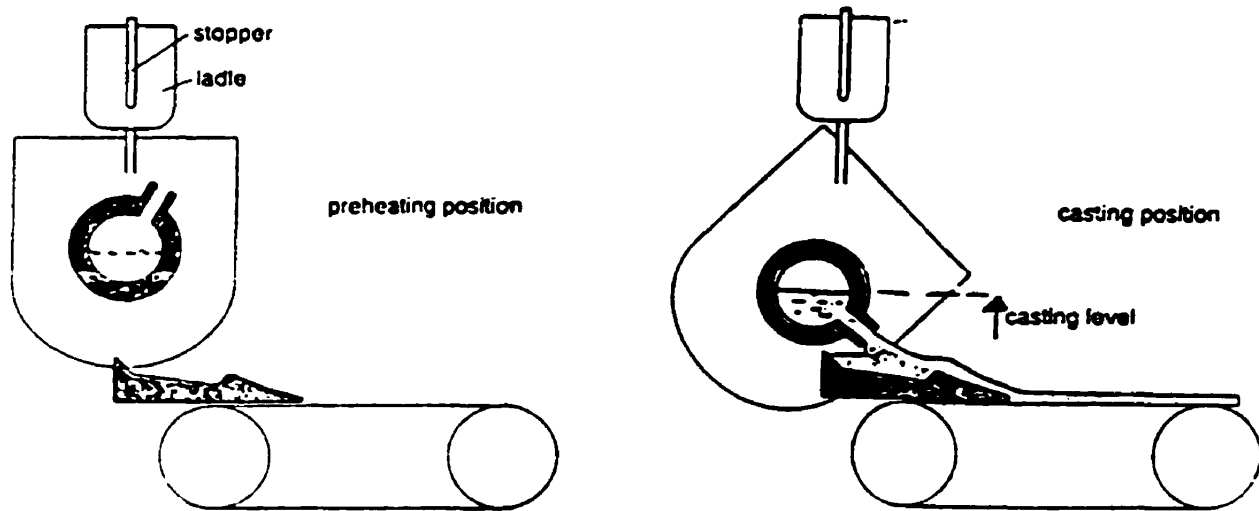
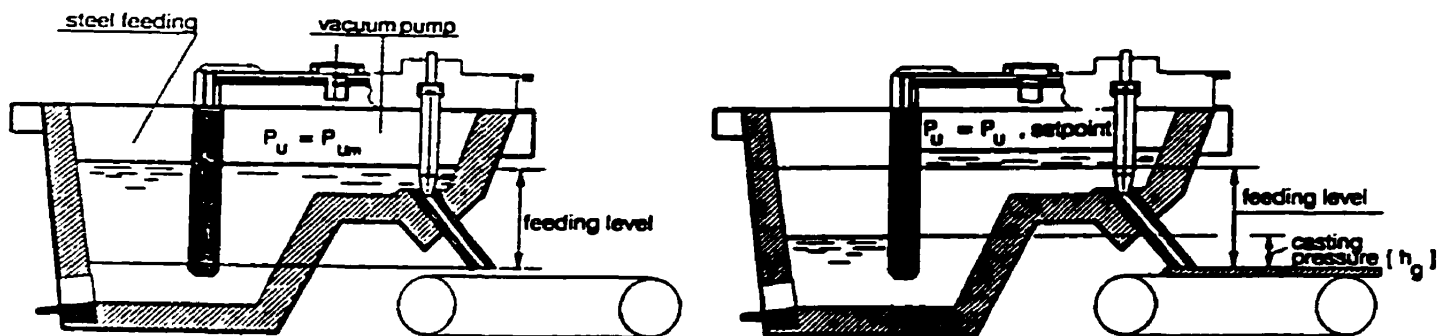
As recognized by MEFOS, developing the ideal metal delivery system is a major objective and will probably be a task continuing over the whole project⁽⁹⁾. The main task is to induce molten steel to run out evenly onto the belt, at the correct thickness gauge and to be of uniform width. Figures 2-1 and 2-2 show the feeding systems explored in the above-mentioned study: a tiltable tube and a low pressure system^(9,10).

Achieving the aforementioned task of dimensional uniformity depends on mastering some essential factors influencing strip surface and shape:

- i) flow conditions of the liquid metal at the point of feeding onto the cooling belt;
- ii) cooling conditions.

In order to optimize the above-mentioned factors, some requirements have to be satisfied by the design of the feeding system⁽¹¹⁾:

- i) the average relative velocity between the liquid metal and the belt must be approximately zero at the point of feeding, i.e., the metal should be laid down in an isokinetic manner;

TFS (Tube Feeding System)Figure 2-1. Tiltable tube feeding system of MEFOS⁽⁹⁾.**LPS (Low Pressure System)**Figure 2-2. Low-pressure feeding system of MEFOS⁽⁹⁾.

ii) the metal should be fed evenly onto the belt at the desired size (width and thickness of the strip).

The Mannesmann low-pressure feeding system provides, in principle, better physical conditions for feeding the liquid metal. On the other hand, the tube feeding system is much simpler to handle. A detailed description of this system can be found in Reference (10). There is a geometrical piece attached to the tube over which the metal flows before reaching the belt (Figure 2-1). That piece has two main functions:

- i) minimization of turbulence at the entrance;
- ii) uniformity of strip width and thickness, due to the efficient distribution of the steel onto the cooling belt.

Both techniques suffer from lack of positive pressure applied to the substrate during solidification, possible oxidation of the upper surface, the possibility of surface waves and probable erosion of the nozzles over extended periods, owing to the passage of relatively high-velocity steel⁽¹²⁾.

The non-satisfaction of the above-mentioned requirements can cause some important phenomena, which will affect the quality of the strip. Such phenomena are: hydraulic jumps, and the formation of spray and foam on the exposed surface⁽¹¹⁾.

In the low-pressure system, these problems are avoided by reducing the casting pressure. Since the metal is delivered from a bigger height than that

required for the potential energy (mgh) to match the kinetic energy ($mv^2/2$), the 'natural' velocity of the liquid metal needs to be damped prior to its contact with the belt.

Other systems, which do not possess independent setting of metal speed and casting slot thickness, have to make use of a frictional process for retarding the flow, like throttling. Scholz et al⁽¹¹⁾ have shown that the outflow speeds for operation with liquid steel may be more than 30% lower, owing to the roughness of the refractory lining and to the dimensioning of the front slot length under constant casting pressure.

More recent details on the current status of the MEFOS/MDH process were given by Spitzer and Schwerdtfeger⁽¹³⁾. They emphasized the apparent advantage of the single-belt processes over the twin-roll ones, in terms of productivity. One of the problems related to the development of the twin-roll processes is the requirement of very large rolls to achieve productivity levels comparable to the conventional continuous casting processes. For single-belt processes, there are no geometrical restrictions for productivity. The idea is to enable hot rolling with 50 to 80% reduction in 10 mm thick strands.

Another advantage of the single-belt process suggested by the authors is the applicability to a wide range of steel grades. With respect to the surface quality, the initial advantage of the as-cast twin-roll strips can be compensated by the hot rolling step in the single-belt process.

Information was provided on the internal quality of the strips, thickness

uniformity, inertization of the cooling zone with argon and the effect of feeding on the shell surface quality. Very low macrosegregation and microsegregation in the same range of the conventionally cast slabs were reported. Although the results were encouraging, they recognized once again the difficulty of even distribution of the liquid metal along the belt width, and at a velocity matching the belt speed.

2.2 MATHEMATICAL MODELLING IN CONTINUOUS CASTING OF STEEL

Numerous mathematical models have been presented for continuous casting of steel. Below, a small sample will be described.

By means of a mathematical model, Cherepanov⁽¹⁴⁾ et al accounted for the formation of structural defects in a continuously cast ingot of a "FERMANAL" (30%Mn,9%Al,1%C) type-steel. Because of the remarkably different thermophysical characteristics, the "FERMANAL" type-steel has temperature field distributions, dendritic structure formation and residual stresses quite different from the carbon steel. Also, the best cooling conditions were investigated, with the aim at achieving low residual stresses and a uniform dendritic structure. This model, although it was not employed to strip casting, brings some useful information to the latter, regarding the influence of the thermophysical properties on the quality of the product and also on the stress fields.

A latent heat method was introduced by Stefanescu and co-workers⁽¹⁵⁾, who claimed that it would be a more accurate method for solving the heat source term in the heat conduction equation. They also used experimental cooling curves to

calculate the solid fractions. Their work is limited to heat transfer and solidification kinetics for castings. The stresses developing during solidification and cooling were also discussed.

Toledo and co-workers⁽¹⁶⁾ concluded that there is an optimum cooling intensity in continuous casting secondary cooling. Their experimental results show that the central segregation decreases as the cooling intensity increases up to a given point. Beyond that point, any additional cooling intensity would be harmful to the centreline quality. The two main parameters studied in order to interpret the results were: solidification time in the billet center and the solid fraction. Stress fields were also determined as a function of the cooling rate.

Hector Jr. and Li⁽¹⁷⁾ studied growth instability of the freezing front during solidification of pure metal castings. They considered just the conduction problem between the mold and the metal. A spatial perturbation was superimposed on the mold surface temperature, and represented by a wavelength function. The model considers air gap nucleation when the contact pressure between the mold and the metal drops to zero. The applicability of the model appears quite limited, because of the idealised conditions.

The models mentioned so far in this section treated heat transfer by conduction, between the solidifying shell and the mold. When metal convection effects are important to describe and to account for heat transfer phenomena, those models no longer apply. Including fluid flow in a comprehensive model bears significant computational, as well as intellectual, costs. It is necessary to include the momentum equations and, for turbulent flows, to solve also equations for

transport generation, diffusion and dissipation of turbulence. Using the most suitable model is the main task of the researcher, and so is the choice of the boundary conditions.

Recent upgrades in the power of computational machines have made it possible to increase the complexity of the models, making them more realistic. In the past few years, attempts have been made to include microscopic phenomena and to couple heat transfer models with stress models, providing a more acceptable overview of the whole process.

Modeling turbulent flows with the help of a $k-\epsilon$ model is not a simple task. Beyond the computational cost, there is also a lack of reliable experimental data that make it difficult to validate the results of the model. Furthermore, applying wall functions is necessary to represent turbulence damping effects near boundaries. Nevertheless, the application of those functions cannot be extended for all types of flow. They are currently used for high Reynolds number flows.

Much work has been done in the past few years on turbulence modelling of continuous casting of steel. Recently, Huang and Thomas⁽¹⁸⁾ have presented a tri-dimensional transient two-phase flow and heat transfer model based on a finite-volume numerical algorithm. Turbulence $k-\epsilon$ was also included in the continuous casting of steel. The flow model incorporated interactions with other transport phenomena, such as argon gas bubble injection. Their results were then compared with experimental results presented in the literature. The effects of some casting parameters, such as casting speed, submerged nozzle depth and argon injection rates were predicted.

Aboutalebi et al⁽¹⁹⁾ presented a 2D fully-coupled fluid flow, heat, and solute transport model, aimed at analyzing turbulent flow, solidification, and the evolution of macrosegregation in a continuous billet caster. Turbulence was accounted for by a modified version of the low-Reynolds number k- ϵ model. Some of the main parameters studied were: casting speed, steel grade, nozzle configuration, carbon segregation and billet shape. Also, the trajectory of inclusion particles was included in the model. Reasonable agreement with available experimental measurements was reported.

Mahapatra et al⁽²⁰⁾ presented some comprehensive work the mold behavior and its influence on strand quality for steel slabs. The first part of the work involved the measurement of industrial data related to mold temperatures, together with mathematical modelling. The other part concerned heat transfer in the mold, with emphasis on the behavior of the mold flux and its implications on the formation of defects in the solidified shell. Some of the variables studied were: steel grades, depth of the submerged entry nozzle, casting speed and viscosity and melting point of the powder lubricant (flux).

Seyedein and Hasan⁽²¹⁾ presented a 3D model of coupled turbulent flow, heat transfer and solidification in a continuous slab caster for stainless steel. The turbulent characteristics of the flow in the pool and in the mushy zone are represented by a low-Reynolds k- ϵ model. The process variables studied were the initial superheat and the casting speed. Very good agreement was reported between the numerical predictions and the experimental data available.

Other mathematical models for continuous slab-casting machines have been

reported in the literature. Thomas et al⁽²²⁾ developed separate 2D fluid flow models of the nozzle and a section through the mold, facing the broad face. The highly turbulent flow was simulated using a k- ϵ turbulence model incorporated in the commercial software program, FIDAP. The flow patterns and velocity fields were compared with measurements conducted using a water model, and showed reasonable agreement. The effects of nozzle angle, casting speed, mold width and turbulence were also investigated.

Flint⁽²³⁾ used the commercial code PHOENICS to describe the coupled heat transfer, solidification and fluid flow in the mold and secondary cooling zones. His model is a 3D, steady-state version, with a k- ϵ formulation for turbulence. Full-scale mould water modelling experiments were conducted in conjunction with the mathematical model, to validate the fluid flow results.

Some other models are available for continuous casting^(24,25). Although there are a few variations in the theoretical formulation, the variables studied with these models and the results obtained are in general agreement with the previously mentioned models. The techniques employed for modelling the phase change and the turbulent flow will now be discussed.

2.2.1 ENTHALPY-POROSITY TECHNIQUES

The phase change occurring during solidification is regarded as a moving boundary problem. Earlier models generally employed moving grids to account for this phenomenon. However, in the past 10 years or so, fixed grid techniques have been developed, aimed at simplifying the computational approach, without loss of applicability to realistic systems.

A review of the fixed grid techniques for phase change problems was carried out by Voller and Thomas⁽²⁶⁾. The essential characteristic of these techniques is that the evolution of latent heat is accounted for in the governing energy equation by defining either a total enthalpy, an effective specific heat coefficient or a heat source term. Consequently, the space grid remains fixed throughout the calculation. The advantages of these techniques are outlined and discussed by the authors, as well as their current applications. A comparison was also presented between the 'apparent heat capacity' and the 'source based' methods.

Some characteristics of the fixed grid methods are⁽²⁷⁾:

- i) it is not necessary to provide explicit conditions governing heat flow at the solid-liquid interface, because of the continuum formulation;
- ii) there is a relationship between the evolution of latent heat and temperature, determined by the relationship between the solid fraction and temperature. Most authors employ linear relationships or relationships based on phase-equilibrium diagrams.

Voller and Prakash⁽²⁷⁾ presented the following equations for Newtonian, incompressible, laminar flow:

$$\text{Continuity:} \quad \frac{\partial v}{\partial y} + \frac{\partial w}{\partial z} = 0 \quad (2-1)$$

$$y\text{-momentum: } \frac{\partial(\rho v)}{\partial t} + \nabla(\rho uv) = \nabla(\mu \nabla v) - \frac{\partial P}{\partial y} + S_y \quad (2-2)$$

$$z\text{-momentum: } \frac{\partial(\rho w)}{\partial t} + \nabla(\rho uw) = \nabla(\mu \nabla w) - \frac{\partial P}{\partial z} + S_z + S_b \quad (2-3)$$

$$\text{Energy: } \frac{\partial(\rho h_s)}{\partial t} + \nabla(\rho u h_s) = \nabla\left(\frac{k}{C_p} \nabla h_s\right) - S_h \quad (2-4)$$

$$S_h = \frac{\partial(\rho L_f)}{\partial t} + \nabla(\rho u L_f) \quad \text{where: } H = h + L_f \quad (2-5)$$

where: $U = (v, w)$ and

$$S_y = -Av ; S_z = -Aw ; S_b = \rho g \beta (h_s - h_{ref}) / C_p \quad (2-6)$$

The Darcy's law can be applied in the mushy zone, considering the liquid fraction (f_l) as the porosity:

$$U = -\left(\frac{\kappa_p}{\mu}\right) \cdot \nabla P \quad (2-7)$$

One of the formulations for the permeability κ_p is given by the Carman-Kozeny equation:

$$\kappa_p = \frac{\epsilon_p^3}{550^2 (1 - \epsilon_p)^2} \quad (2-8)$$

Inserting equation (2-8) into Darcy's equation, one obtains:

$$\nabla P = -C \frac{(1 - \epsilon_p)^2}{\epsilon_p^3} \mu U \quad (2-9)$$

This equation suggests the following form for A in equation (2-6):

$$A = -C \frac{(1 - f_L)^2}{(f_L^3 + q)} \quad (2-10)$$

The Carman-Kozeny equation is derived on extension from the Darcy law for porosity. It is used to modify the momentum equations in the mushy zone. As the solid fraction $(1 - f_L)$ approaches 1, the superficial velocity becomes zero. The constant C is called the morphology constant and was set to 1.6×10^3 while q is a very small number introduced to avoid division by zero. The flow in the mushy zone is governed by the Darcy law, in the case of a columnar dendritic structure.

The latent heat L_f released during solidification is taken into account in the source term of the energy equation.

Hibbert et al⁽²⁸⁾ have also proposed an exponential function relating the evolution of latent heat and temperature. His assumption was based on the fact that grain formation occurs faster near the end of the solidification of alloys, particularly steels. Therefore, microscopic phenomena like nucleation and grain growth also have great influence on determining the suitable function.

The so-called mushy zone is modelled as a porous medium, mostly by means of the Darcy model. At this point, it is important to note that this model introduces a constant C that needs to be empirically determined for a particular

system. Darcy's model corrects the velocity in the mushy zone such that the shape of that zone and its dimensions will be greatly influenced by the constant used. Other techniques for velocity corrections in the mushy zone are discussed elsewhere⁽²⁹⁾.

Generally, the above-mentioned techniques, which are known as enthalpy-porosity methods, are recommended when the phase change taking place (solidification) is only a part of a complex fluid flow problem.

Bennon and Incropera⁽³⁰⁾ developed a consistent set of continuum equations for the conservation of mass, momentum, energy and species in a solid-liquid phase change system. They considered the constituents of a multicomponent system as separable phase components and, to non-inert systems, they overcame this additional difficulty with equilibrium-phase diagrams or empirical relationships. The conservation equations were cast as a summation of conservation equations for each phase, and some supplementary relationships were introduced in the model in order to achieve closure of the system. Their scheme will be employed in this work.

The so-called enthalpy porosity technique can be employed to model the melting of a pure metal⁽³¹⁾, as well as the melting of alloys. Some authors have also attempted to correlate microscopic phenomena to the macroscopic features⁽³²⁾. One of the difficulties here is the size of the grids that would have to be employed to represent the microscopic phenomena. A very small control volume, mandatory to fit the requirements of the problem would lead to an exaggerated number of nodes, implying huge computational costs.

The models for transport phenomena during solidification were reviewed by Ni and Incropera⁽³³⁾. Advantages and shortcomings of continuum and two-phase approaches were emphasized. Volume-averaging procedures inherent in the two-phase model were used to develop an extension of the continuum model that would include important microscopic phenomena, such as solutal undercooling, nucleation and shrinkage, without creating huge computational demands. Despite the benefits claimed by the authors, no practical examples have been reported in the paper, nor subsequently in the literature.

Recently, Voller⁽³⁴⁾ presented a new numerical method for solving solidification of alloys: its main development is the use of a mixed explicit/implicit time integration scheme replacing the previous explicit scheme. According to the author, the new scheme leads to significant savings in computing time and allows for a straightforward treatment of key phenomena, such as eutectic reactions. Comparisons between numerical and similarity solutions are shown.

Shyy and co-workers⁽³⁵⁾ presented a modified κ - ϵ model to continuous ingot casting operations. They proposed a correlation between f_s and the liquid fraction. To correct the velocities in the mushy zone, the well-known Carman-Kozeny equation was used, with a Darcy coefficient of 10^6 . They reported variations in the mushy zone thickness with casting speed. The authors also stressed the importance of modeling turbulence in such a system by a κ - ϵ model, to predict important features related to the determination of the dominant transport mechanism in heat transfer. The heat transfer can be controlled either by conduction or by convection. Their work introduces the idea that turbulence plays a dominant role in the bulk melt and upper mushy zones regarding overall heat transfer to the solidifying

ingot.

2.2.2 TURBULENCE MODELLING

A fluid motion is described as turbulent if it is rotational, intermittent, highly disordered, diffusive and dissipative⁽³⁶⁾.

The models for turbulence can be classified according to the number of equations considered: the zero equation models use only PDE's for the mean fields and no differential equation for turbulence quantities. All one-equation models require the input of a length-scale distribution, based on experimental information and hope!⁽³⁶⁾

Among the two-equation models, the κ - ϵ is the only one, when compared with the κ - l and κ - w models, that permits the turbulent Prandtl number to have a reasonable value ($Pr_t = 1.3$), which will fit the experimental data for the speed of the various entities at locations far from the walls, without modification of any constants⁽³⁶⁾. In the κ - ϵ models, ϵ is the rate at which the turbulent kinetic energy κ is destroyed by molecular viscous stresses.

Patel et al⁽³⁷⁾ reviewed the several turbulence models for near-wall and Low-Reynolds number flows. The models were tested under different flow simulations and compared with the limited experimental data. They remarked on the importance of the constant f_μ on the model's performance and emphasized, to a lesser extent, the importance of choosing the constants f_1 and f_2 , to be in agreement with the experimental data.

Markatos⁽³⁶⁾ presented a comprehensive review of the mathematical modelling of turbulent flows. He acknowledged the difficulty of modelling turbulence in some specific applications, such as the ones that involve low-Reynolds number effects and rapid compression or expansion. The review is concluded with a summary of the advantages and disadvantages of the various turbulence models, in order to help a modeller choose the one most suited to his particular application.

Recently, Lan et al⁽³⁸⁾ evaluated the predictions of six κ - ϵ turbulence models, for flow in a continuous casting billet-mold water model. They carried out experimental measurements of the velocity and turbulence with laser Doppler velocimetry. They observed that the high-Re number model predicted greater turbulence activity than its low-Re number counterpart. The high-Re number model was found to better predict the kinetic energy values in the centerline, whereas the low-Re number models were more accurate in predicting the axial velocity component. As for the damping functions, Rousseau et al⁽³⁹⁾ presented a brief comparison of some standard f_μ and f_2 functions, for various low-Re number models.

2.3 MODELLING OF NEAR-NET-SHAPE CASTING PROCESSES

Numerous studies have been conducted in the modelling of new rapid solidification processes, such as melt spinning, thin strip and thin slab casting⁽⁴⁰⁾.

The splat-quench solidification^(41,42) is a member of the family of spray

deposition processes. It is particularly attractive for working with alloys where problems arising from segregation, brittle phases and large grain size make conventional methods inappropriate⁽⁴¹⁾. Initial cooling rates of up to 10,000°C/s were reported for this process.

Mathematical models were also presented for planar flow melt spinning processes⁽⁴³⁻⁴⁶⁾. In this type of process, the clearance between the nozzle and the rotating wheel is small and the dominant force in controlling molten metal flow is the viscous drag generated by the rotating wheel⁽⁴³⁾. Therefore, the process is distinct from the chilled block process, where the nozzle is placed at some distance away from the rotating wheel and molten metal flow is controlled by the applied pressure drop across the nozzle.

The first mathematical model for the twin-roll process was presented by Miyazawa and Szekely⁽⁴⁷⁾. Many models have already been presented for twin-roll thin strip casting processes⁽⁴⁸⁻⁵⁰⁾. In this kind of process, the metal is usually supplied through a submerged entry nozzle into the rotating bank of two rolls, which rotate in opposite directions. The way the metal is delivered and the location where the solidification is completed are two of the most important operating conditions in the twin-roll processes. Therefore, some models presented in the literature focused in changes in the design of the metal delivery system.

There are a few mathematical models for belt-caster processes in the literature. Farouk et al⁽⁵¹⁾ studied some process variables for aluminum and steel casting in a twin-belt caster. Their model predicted the influence of belt speed, strip thickness, nozzle width and heat removal rates at the belt-cast interface. The

local cooling rates were calculated and used to predict the secondary dendrite arm spacing in the cast. As a thermal boundary condition for their model, the authors used heat fluxes measured at the belt-cast interface during the solidification of steel and aluminum on a stationary water-cooled surface. The model was two-dimensional and included κ - ϵ turbulence, with a low-Reynolds number model developed by Spalding and Launder. They followed Asay and Szekely⁽⁵²⁾, assuming that the molecular viscosity within the mushy zone could be expressed by the same equation used to describe the molecular viscosity in the liquid core, but multiplied by a factor of 20, in order to allow for the effect of solid matrix on the flow resistance. A uniform velocity at the nozzle exit, immediately before the cooling by the refrigerated belt, was also considered. This approach simplifies the hydrodynamics of the liquid pool. Depending on the variation of the liquid level in the tundish, this simplification can constitute a drawback of the model.

Gerber and Souza⁽⁵³⁾ presented a tri-dimensional, steady-state model for the Hazelett thin-slab steel casting process. In the parametric study of the twin-belt caster, they focused on the influence of the overall heat transfer coefficient, steel superheat, casting speed and nozzle diameter on the growth of the solidification front within the caster. However, they considered turbulence isolated to a narrow region in the beginning of the belt and its effect was not taken into account in the model.

Truelove⁽⁵⁴⁾ presented a fluid flow model for the single-belt caster that had been in development by BHP Australia. His model included the κ - ϵ turbulence model and employed a body-fitted coordinate grid. However, no attempts to include phase-change phenomena were reported.

Some other earlier studies were presented for horizontal casting onto a substrate^(55,56).

Finally, Jefferies⁽⁵⁷⁾ recently presented a model for a similar system (single-belt), considering flow in two dimensions and using an artificially enhanced viscosity to model turbulent flow. Her model also incorporated the presence of a porous medium in the domain of the delivery system. She reported that the choice of adopting a simple turbulence model was due to the difficulty in applying a κ - ϵ model for flows through porous media. Contrary to Farouk's work, she employed a non-uniform vertical grid. Her results for fluid flow were compared with the results of water model and mathematical models. Results from the latter were tested against analytical solutions. An interesting finding of Jefferies' work is the way the flow can be modified in the presence of a porous medium. Figures 2-3 and 2-4 reproduce pictures from her work showing the remarkable influence of the 45 ppi filter used to smooth the flow.

2.4. INVERSE HEAT TRANSFER

The precise determination of the dynamic heat fluxes and heat transfer coefficients between a solidifying metal and a chilling substrate is of utmost importance for casting processes.

Recent attempts to develop near-net-shape casting have shown that one of the most important aspects to be considered is the interfacial heat transfer between the liquid melt and the moving substrate⁽⁵⁸⁾. Usually, one-dimensional heat conduction is considered for this kind of interfacial contact. Different techniques

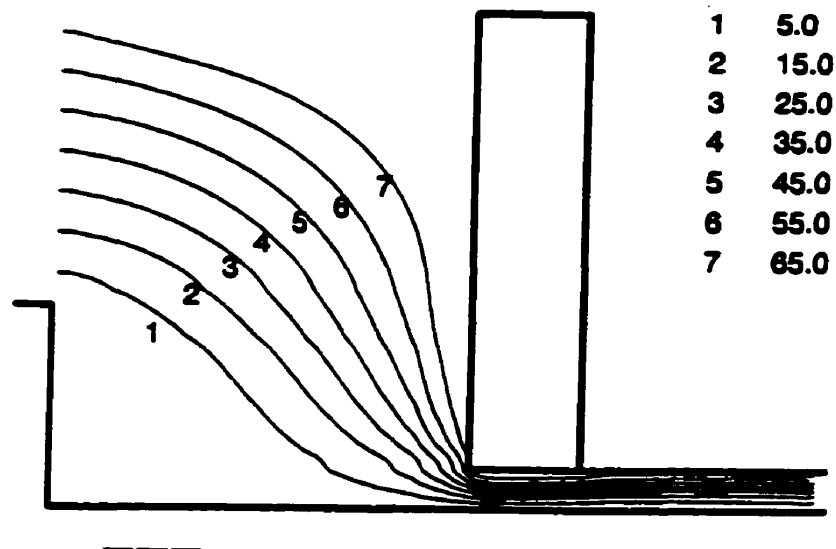


Figure 2-3. Simulations of fluid flow in the delivery system, without filter⁽⁵⁷⁾.

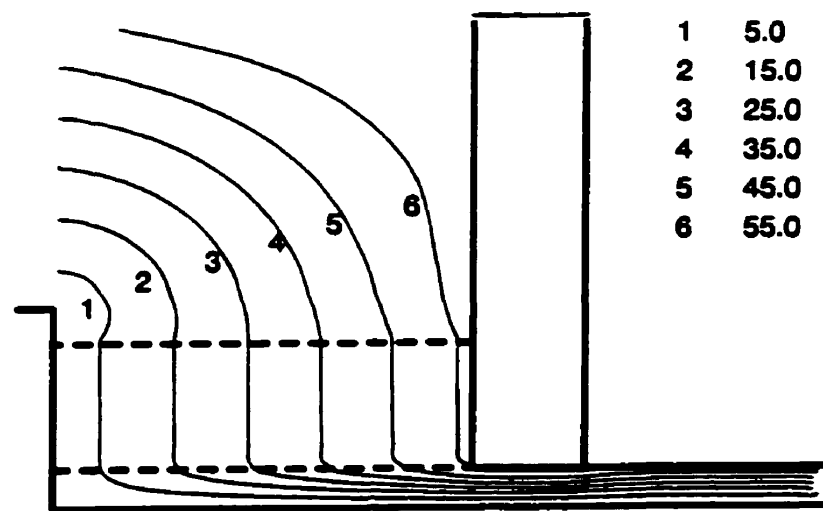


Figure 2-4. Simulations of fluid flow in the delivery system - 45 ppi filter present⁽⁵⁷⁾.

have been developed to solve the corresponding Fourier equation:

$$\frac{\partial}{\partial z} \left[k \frac{\partial T}{\partial z} \right] = \rho C_p \frac{\partial T}{\partial t} \quad (2-11)$$

The evaluation of the heat transfer characteristics (q and h) enables the cooling rates within the metal to be calculated. This, in turn, is valuable to the prediction of microstructural properties, such as the SDAS (Secondary Dendrite Arm Spacing). An accurate analysis of the interfacial heat transfer must take into account the formation and evolution of an air gap between metal and mold, caused by the shrinkage of the solidifying metal, and thermal contraction of the colder solid metal.

One of the first significant works on interfacial heat transfer between a solidifying shell and a mold was presented by Ho and Pehlke⁽⁵⁹⁾. They briefly grouped the mathematical methods used to solve this problem: (1) purely analytical; (2) semi-analytical and empirical and (3) numerical techniques based on finite difference, or finite element, methods. When employing a purely analytical method, one must assume a constant interfacial heat transfer coefficient. By contrast, the methods in group 2 do not attempt to solve rigorously the Fourier heat equation, but rather use semi-analytical or curve fitting solutions to analyze experimental data. However, none of the methods from either group 1 or 2 are suitable when drastic changes occur in the temperature at the metal-mold interface. This condition is the case for most strip casting processes. The authors carried out some casting experiments and compared the transient interfacial heat transfer coefficients obtained using two different cylindrical casting geometries, differing

principally in the placement of chills. They concluded that, at the later stages of solidification, the metal and the chill may stay in nonconforming solid contact, in the case of bottom chilled castings, or an interfacial clearance gap may develop in the case of top chilled castings.

Many other papers have pursued the solution of the interfacial heat transfer problem at the casting-mold interface. Some⁽⁶⁰⁾ focused on the measurement of the air gap as a boundary condition at the interface, while others^(61,62) proposed analytical solutions to the problem. Lee et al⁽⁶³⁾ carried out a numerical analysis for the solidification of a pure metal in a two-dimensional rectangular enclosure with combined effects of the interfacial thermal resistance and natural convection in the melt. The effect of the contact resistance in the evolution of the solid-liquid interface was determined.

Beck⁽⁶⁴⁾ was one of the precursors in the development of numerical techniques for the solution of the inverse heat conduction problem. His technique includes finite-difference equations based on the Crank-Nicholson procedure and a least-squares minimization with a regularization approach, to calculate instantaneous heat fluxes at the interface based on measurements of localised temperature transients within the substrate.

Das and Paul⁽⁶⁵⁾ developed an algorithm based on the boundary element technique for the determination of interfacial heat transfer coefficients in casting and quenching processes.

Trovant and Argyropoulos⁽⁶⁶⁾ proposed a novel technique that would

minimize the error associated with selecting boundary conditions without experimental data. The technique is based on an advance knowledge of the air gap formation and its influence on the interfacial heat transfer coefficient. This information is used to formulate a coupled mathematical model that would simulate the evolution of the air gap and predict the transient cooling conditions at a given mold wall.

The heat transfer modelling of rapid solidification of molten metals droplets on a cold substrate has been subject of extensive investigation^(67,68). Some of these models are accompanied by experimental work^(68,69), with the objective of clarifying the nature of the impact of the rapidly fallen droplet onto the substrate and its consequence on spreading and on the heat transfer to the substrate. In these models, however, the transient heat transfer coefficients are not calculated, and a constant value is usually assumed.

Experimental work was also conducted to study the heat transfer between the mold and the solidified shell through the mold flux in continuous casting of steel⁽⁷⁰⁾.

For horizontal thin strip casting processes, an analytical solution was previously reported in the literature⁽⁷¹⁾, as well as the use of a least squares method. In this work, temperatures were measured in the solidifying metal freezing onto an experimental water-cooled belt and compared with those calculated by the mathematical model.

In the past few years, a number of researchers have tried to investigate the

influence of the main casting variables on interfacial heat transfer rates. Since the present work shares the same objective with respect to casting in horizontal belt machines, special attention will be given to a number of these. To achieve their goals, most of the researchers used a combination of experimental measurements and the solution of the inverse heat conduction problem with nonlinear estimation techniques.

Chen and co-workers⁽⁷²⁾ investigated some of the parameters involved in interfacial heat transfer behavior, during free-jet casting of Wood's alloy onto a moving substrate. The thickness of the ribbons varied from 0.1 to 0.25 mm. They solved Fourier's equation using the assumption of a semi-infinite region, for points on the substrate far from the interface metal-substrate. The effects of superheat and substrate were determined.

Kumar and Prahbu⁽⁷³⁾ solved the inverse heat transfer problem for the casting/chill interface on bar-type castings of aluminum base alloys. Experiments were carried out for different metal/chill combinations, with and without castings. The surface heat flux was estimated from the temperature data inside the mold and with the use of a nonlinear estimation technique. Relationships between the peak heat fluxes and the fluxes afterwards were derived in terms of the variables studied.

New experimental results for copper splat cooling are described by Wang and Matthys⁽⁷⁴⁾. The variables considered were melt superheat, substrate material and substrate surface finish. The interfacial heat transfer coefficients were evaluated through an iteration and interpolation procedure by matching the model

calculations to the temperature data obtained from the converted pyrometer readings. Much higher values of h were found than those measured for continuous casting and strip casting processes.

Muojekwu et al⁽⁷⁵⁾ presented relevant experimental work on the influence of mold surface roughness, mold material, metal superheat, alloy composition, and type of lubricant on the interfacial heat transfer between solidifying Al-Si alloys and a water-cooled chill. For the evaluation of heat fluxes, they used a method based on the sequential nonlinear estimation technique of Beck and co-workers⁽⁶⁴⁾. The SDAS was measured on the Al-Si samples and compared with various models reported in the literature. However, the solidification occurred on the surface of the chill dipped in the hot metal, ruling out the possibility of studying the influence of the casting speed on the interfacial heat transfer.

One experimental apparatus similar to the one employed in this work was constructed by Couture et al⁽⁷⁶⁾. It allowed for the horizontal strip casting simulation of low-carbon Al-killed steel strips onto a copper chill block, which moved slowly (5.5 cm/s). Their work focused on microstructural characterization and values of measured SDAS were given.

Recently, Wang and Matthys⁽⁵⁸⁾ presented a comprehensive assessment of the data available for the various near-net-shape casting configurations. They concluded that a significant amount of data for twin-roll casters and melt spinning had already been generated; however, for other processes, such as horizontal belt casters, little data are available and even these data have been obtained under diverse experimental conditions. Some correlations between h and the casting

speed or strip thickness are proposed, and the influence of process parameters is also discussed.

2.5 FLOW THROUGH POROUS MEDIA

Flow through porous media has been the subject of study for a long time. Although the phenomena involved occur at the microscopic scale, the most suitable approach has been to treat the region as a macroscopic one, and to consider homogeneity within the porous medium and equilibrium with the fluid at the interface. Empirical or semi-empirical treatments, with emphasis on parameters such as permeability, have been applied.

The most important parameter in the study of flows through porous media is one of permeability. Roughly, permeability is a measure of the relative cross sectional area of pores available through which fluid can flow. Permeability is related to the porosity and to the pore size of the medium. A number of equations have been derived for determining the permeability, including the one below, for Poiseuille flow through tubes⁽⁵⁷⁾:

$$\kappa_p = \frac{\epsilon_p D^2}{32} , \text{ where:} \quad (2-12)$$

κ_p is the permeability, ϵ_p is the porosity of the porous medium and D is the pore size.

Once the permeability is determined, the next step is to calculate the pressure drop across the medium and to correct the flow. Some empirical

relationships are available.

The most widely accepted relationship between pressure gradient and flowrate is that by Darcy⁽⁷⁷⁾. Darcy based his correlation on the frictional drag occasioned by the porous medium. His model is only valid for so-called Darcian flows, i.e., where Re number is < 0.1 .

$$\frac{\mu u}{\kappa_p} = -\frac{\Delta P}{L} \quad (2-13)$$

Darcy's law neglects the effects of a solid boundary and inertial forces on the flow through porous media. For high permeabilities, the inertial effects may be significant. They were later incorporated by Forchheimer⁽⁷⁸⁾.

$$\alpha_p \mu u + \beta_p \rho u^2 = -\frac{\Delta P}{L} \quad (2-14)$$

Indeed, experimental observations indicate that the pressure drop in the bulk of a porous medium is proportional to a linear combination of flow velocity (representing frictional drag) and the square of the flow velocity, to account for inertial terms⁽⁷⁹⁾. This relationship is equivalent to the equation later presented by Ergun⁽⁸⁰⁾, currently known as Ergun's equation:

$$\frac{\Delta P}{L} = \left[\frac{150 (1 - \epsilon_p)^2 \mu}{D^2 \epsilon_p^3} \right] u + \left[\frac{1.75 (1 - \epsilon_p)}{D \epsilon_p^3} \right] \rho u^2 \quad (2-15)$$

Since Forchheimer's pioneering studies, various models have been proposed

for this type of flow. The primary advantage of these models is the attempt to specify the constants α and β in terms of the properties of the fluid and of the porous medium⁽⁸¹⁾.

More recently, Brinkman⁽⁸²⁾ proposed modifications to the "Darcy" model, introducing a new term to take into account wall effects.

$$\frac{\mu_f u}{\kappa_p} + \mu_{eff} \left(\frac{d^2 u}{dx^2} \right) = - \frac{\Delta P}{L} \quad (2-16)$$

This equation states that there is an equilibrium between the forces acting on a volume element of fluid, i.e., the pressure gradient ($\Delta P/L$), and the damping force caused by the porous mass ($\mu_f u / \kappa_p$) and the divergence of the viscous stress [$\mu_{eff}(d^2 u / dx^2)$].

Currently, these models are incorporated in the equation describing pressure gradients through a filter. The introduction of the corrections proposed by Forchheimer and Brinkman will depend on the particularities of the system being studied.

The effects of a solid boundary on flow and mass transfer in a porous medium originate from momentum diffusion caused by the boundary frictional resistance, in addition to the bulk frictional drag induced by the solid matrix, as characterized by Darcy's law⁽⁸³⁾.

Other variations of the Darcy model for porous media are the Darcy-Lapwood and the Darcy-Lapwood-Brinkman models⁽⁸⁴⁾. The first introduces one

term related to the porosity and the second incorporates the Brinkman modification, as well.

$$\frac{\mu_f u}{\kappa_p} + \rho \left(u \frac{du}{dx} \right) \frac{1}{\epsilon_p^2} + \mu_{eff} \left(\frac{d^2 u}{dx^2} \right) = -\frac{\Delta P}{L} \quad (2-17)$$

As a matter of fact, most of the recent work related to porous media have included corrections advocated by Brinkman and Forchheimer for non-Darcian flows^(85,86). Since these corrections will be used in this work, the complete equations are presented later.

You and Chang⁽⁸⁷⁾ have recently employed a modified procedure to determine flow properties in non-Darcian flow, such as the permeability κ_p and the inertial constant F . The data pairs (pressure drop and centerline velocity) collected from experiments conducted over a wide range of air flowrate in the air channel are used for calculating first trial solutions of a modified momentum equation, in terms of constants expressing the permeability and inertia. The foregoing trial solutions for κ_p and F are then used in the original momentum equation for solution in the whole domain. Their interesting findings were: 1) the permeability κ_p approaches a finite value, roughly 10^{-4} , as the porosity ϵ approaches 1; 2) the variation of the inertial constant F with porosity ϵ is moderate, indicating that the flow resistance due to the presence of a porous matrix is not a strong function of porosity.

Tian⁽⁸⁸⁾ investigated fluid flow and filtration phenomena associated with filtration of liquid aluminum using reticulated ceramic filters, similar to the ones

intended to be used in this study. He found that fluid flow through this type of filter could be described by Darcy's law up to a Reynolds number of 8, and the permeability of this type of filter ranged from 9×10^{-9} to $4 \times 10^{-8} \text{ m}^2$.

Other interesting work⁽⁸⁹⁾ concluded that the pressure drop through ceramic foam structures does not depend on fluid properties, only on pore structure. A 2D body-fitted coordinate model was employed to study the flow of liquid Aluminum, air and water through a ceramic filter. The specific permeability was also found to only be a function of pore structure and no simple relationship was derived for the effect of porosity and dimensionless pressure drop on permeability. To validate the model, the permeabilities of the filters were measured and compared with calculated values.

In the 80's, numerous studies^(79,81,83,90-93) were presented with respect to non-Darcian flows through porous media. Some of the most important findings were:

- At high Rayleigh numbers, non-Darcian effects are very important: low Ra regions correspond to Darcian and high Ra (≥ 200) to Forchheimer flow;
- The relative importance of the inertial effects can be characterized by a modified Grashof number;
- As Pr decreases, the effect of inertia increases.

On the metallurgical field, Beckerman et al⁽⁸⁶⁾ studied natural convection in a vertical enclosure filled with a porous medium. They have shown that at high Da

numbers, the inertia and viscous terms must be included simultaneously to obtain realistic predictions of the Nu number. Also, they found that the relative importance of the inertia term increases with decreasing Pr number and that Forchheimer's extension should always be included for $Pr \leq 1.0$, which therefore applies to all liquid metals.

Other attempts to model fluid flow through porous media have been reported lately for many different applications. Travkin et al⁽⁹⁴⁾ presented a single-phase turbulent model for regular and random porous structure media with random capillary morphology. It is a highly theoretical model based on theories of space averaging and stochastic Markov diffusion. This way, its applicability for practical purposes remains limited.

Recently, Kececioglu and Jiang⁽⁹⁵⁾ provided experimental evidence for various flow regimes, for water flowing through a bed of randomly packed spherical beads. Lee and Yang⁽⁹⁶⁾ modelled flow through a bank of circular cylinders and determined the relative importance of the Forchheimer term according to the Reynolds numbers. The same was done by Choi et al⁽⁹⁷⁾ for the Brinkman term. As for the determination of the permeability κ_p and the inertia coefficient F , Slimi et al⁽⁹⁸⁾ suggested the following relationships:

$$\kappa_p = \frac{\epsilon_p^3 d_p^2}{150 (1 - \epsilon_p)^2}; \quad F = \frac{1.75}{\sqrt{150} \epsilon_p^{3/2}} \quad (2-18)$$

2.5.1 TURBULENT FLOW THROUGH POROUS MEDIA

Modelling flow through porous media is a controversial issue, especially when it comes to turbulent flow. In most applications, the flow through porous media is laminar, since the overall porosity and pore size are usually small⁽⁹⁹⁾. The flow speeds are also small in most of the cases. The focus of controversy is the exact moment of regime transition from laminar to turbulent flow and its relation with respect to deviations from Darcy's law. Some authors first advocated that the transition from laminar to turbulent flow within porous media occurred simultaneously with deviations from Darcy's law⁽¹⁰⁰⁾. However, experimental evidence may prove the contrary⁽¹⁰¹⁾. Therefore, doubt still exists over the most suitable way to model turbulent flow through porous media. Below is the description of some developments on this issue.

Wen and Ingham^(102,103) have developed a numerical method for improving the rate of convergence in the numerical solution of the conservation equations for flows through a thin filter of very large resistance. They proposed an average pressure correction formula. Their method is based on global mass conservation and the average pressure correction formula is derived from a modified Darcy law for a porous medium. However, no attempts were reported to modify the κ - ϵ turbulence equations, because of the very small thickness of the porous medium employed.

Rudraiah⁽⁸⁴⁾ studied turbulence convection in porous media with non-Darcy effects. He incorporated the effect of turbulence via eddy viscosity and eddy thermal diffusivity. He concluded that, for high Rayleigh numbers, the effect of turbulence was to increase the heat transport, while that of the permeability was

to dampen it.

Masuoka and Takatsu⁽¹⁰⁰⁾ proposed a pioneering zero-equation model based on the assumption that Forchheimer flow resistance and dispersion are caused by turbulence mixing (diffusion) in porous media. They also considered the Darcy flow resistance as a molecular drag force and the Forchheimer one as a turbulent drag force in their turbulence model. In this view, the deviation from the Darcy to the Forchheimer law coincided with the transition from laminar to turbulent flow. The basic assumption of his model was questioned by Nield⁽¹⁰⁴⁾, who pointed out that actual turbulence occurs at **Re** numbers at least one order of magnitude higher than the **Re** at which deviation from Darcy's law is observed. According to this observation, Forchheimer resistance and dispersion would not be caused by turbulence, rather being affected by it.

McDonald et al⁽¹⁰⁵⁾ claimed the observation of turbulent characteristics in the flow through a porous medium, at **Re** numbers larger than 300. In such cases, **Re** would be defined based on the pore diameter.

Prescott and Incropera⁽¹⁰⁶⁾ presented a κ - ϵ model for the mushy zone of a binary metal alloy. The basis of this model is the adoption of a damping term in the k - ϵ equation, so as to reduce the turbulence energy in the mushy region.

Antohe and Lage⁽⁹⁹⁾ recently proposed a microscopic turbulence model for incompressible flow in porous media. Their model included the following terms: convective inertia, pressure gradient, Darcy, Forchheimer, Brinkman and the Boussinesq-Oberbeck term. The porous medium related terms were included in the

Navier-Stokes equations. The authors simulated unidirectional fully-developed flow through porous media and they reported that the effect of a porous matrix in a small permeability medium is to damp turbulence, whereas for a high permeability medium, the analysis was inconclusive. For non-developed flow, turbulence can increase or decrease the pressure drop, in comparison to the laminar flow.

2.6 STRESS MODELLING IN THE CONTINUOUS CASTING OF STEEL

In the casting of metals, the transient temperature distribution will frequently result in the development of transient stresses that are large enough to cause plastic deformation during the process, and yield residual stresses in the body after it cools down to work/room temperature⁽¹⁰⁷⁾. The viscoelastic behavior of the material will also affect the distribution of residual stresses.

A solidifying body develops residual stresses immediately after solidification and is never in a state of zero stress. The freezing front is of great importance in modeling stresses. The key to a successful model is to assign proper thermal and mechanical boundary conditions at the freezing front.

In a constrained casting, when the particle first solidifies, it is subjected to tension and since the yield stress is initially zero, its behavior is at first plastic. As the particle cools, the tensile stress on it increases and remains equal to the yield stress corresponding to its temperature, until the rate of increase of stress upon it is less than the rate of increase of its yield stress⁽¹⁰⁸⁾. Then, the solid enters the

region for elastic behavior. The stress decreases and changes the sign, reaches the value of the yield stress and behaves plastic thereafter⁽¹⁰⁸⁾. Each newly solidified layer is subjected to instantaneous thermal stress due to the thermal gradient through the thickness of the solid⁽¹⁰⁹⁾.

There can be various sources for stress and deformation in a solidified shell: the thermal load, phase changes and phase transformations and metal pressure are some of them. Most often, the stresses are mainly generated by the thermal contractions (strains) that accompany cooling in the solid state⁽¹¹⁰⁾. Tensile stresses usually develop within the shell as contraction of the casting is opposed by lateral constraint⁽¹¹¹⁾.

The thermal stresses are partially relieved by viscoplastic mechanisms, viscous creep having relative importance in some cases. However, from a mechanical point of view, the strain generated by creep is indistinguishable from plastic flow, especially at high temperatures, so that only their combined effect is experimentally observed⁽¹¹²⁾.

To calculate the sources of stress mentioned above, constitutive equations have been developed and relate the stresses to the corresponding strains. Other equations to be solved simultaneously are the equilibrium equations and the compatibility equations, relating force to stress and strain to displacement, respectively⁽¹¹³⁾. In a rapidly changing environment, the strain rate could significantly affect the stress field⁽¹¹⁴⁾.

Other phenomena of influence are phase transformations, which create

dilatational (inelastic) strains in the casting⁽¹¹⁴⁾, and the metallostatic pressure exerted on the solidifying shell, which can be responsible for creep bulging in large vertically-cast conventional castings⁽¹¹⁰⁾.

2.6.1 OCCURRENCE OF CRACKS IN CONTINUOUS CASTING OF STEEL

Brimacombe et al⁽¹¹⁵⁻¹¹⁷⁾ have carried out extensive work investigating the occurrence of cracks in continuous casting of billets and slabs. In pioneering works, they have explained some of the reasons for the formation of the various types of cracks in casting. In Reference (117), it is seen that the six most common types of cracks result from high tensile strains and stresses acting on internal regions of the solid shell, where the ductility and strength are low, i.e., the temperature zone comprised between the solidus and 40-70° C below the solidus. The occurrence of reheating motivated by excessive secondary cooling causes the surface of the strand to expand and imposes a tensile strain within the interior of the solid shell, leading to so-called halfway cracks⁽¹¹⁵⁾. On the other hand, surface cracks may occur if the stresses on the surface are tensile, due to changes in the cooling rate, interaction with the mold, phase transformations and multidimensional effects⁽¹¹⁰⁾.

Other factors are also reported in the literature. Alloy content has a great influence on the likelihood of crack formation. Kim et al⁽¹¹¹⁾ have reported that Fe-0.1%C steels are well known in industry as a grade particularly difficult to cast, due to the likelihood of a "breakout". It was reported⁽¹¹⁸⁾ that this behavior can be explained by the reduced heat transfer to the mold, yielding thinner strands, more likely to rupture. The thinner strands are justified by the Fe-C diagram, in which the widest freezing range occurs at 0.1 %C⁽¹¹⁹⁾. Brimacombe and Sorimachi⁽¹¹⁷⁾ and

Kelly et al⁽¹²⁰⁾ proposed that the reduced heat transfer for 0.1%C steels was due to the peritectic phase transformation (δ to γ), which is accompanied by a substantial shrinkage. On the other hand, Wray⁽¹²¹⁾ concluded that the solidifying shell of a casting will be stronger the higher the carbon content, because the weakening effect of the carbon is easily overwhelmed by the strong effect of the decreasing solidus temperature.

2.6.2 STRESS MODELLING

Stress modeling of casting processes has been receiving increasing attention lately. The advances in computational techniques and computing power have made it possible to deal with such complex phenomena with more realistic approaches. Some phenomena involved were recently discussed by Thomas⁽¹¹⁰⁾. They include: coupled fluid flow and heat transfer, phase changes, thermophysical properties varying with temperature, microscopic phenomena, etc.

The essential features for any mathematical analysis of thermal stresses occurring during solidification are⁽¹²²⁾:

- i) the material constitutive relationship (stress-strain);
- ii) the mechanical properties of the casting at high temperatures;
- iii) the heat transfer analysis, including the determination of the thermophysical properties of the material and
- iv) the boundary conditions for thermal problem and stress formulation.

Nevertheless, to accomplish the formulation of a successful model for the complex stress problem, many aspects have to be dealt with, such as^(123,124):

- i) In most cases, the thermal and the stress model should be coupled, often creating huge numerical difficulty;
- ii) A general constitutive model, representing all phases present (liquid, mushy and solid) should be employed;
- iii) Transient boundary conditions require sophisticated formulation for both the thermal and mechanical models;
- iv) Microstructural effects may be relevant in determined cases.

As such, some simplifications and assumptions are needed in order to obtain a fair prediction of the stresses and strains generated in a casting process. The extent and the nature of these simplifications will depend on each particular problem. The goal is to achieve a qualitatively sound result and a fairly approximate quantitative value, with reasonable computational demands. That is the challenge researchers in the area have been facing over the last twenty years.

The so-called constitutive equations comprise stresses generated by the following phenomena: elastic strain, viscoplastic strain, thermal strain and phase transformed strain. For each strain component, there is a constitutive relation. The sums of the strains or stresses give the total strain or stress generated.

In stress modelling, some features and assumptions have already been widely discussed. Some of them and their implications will be briefly mentioned in sequence.

Common simplifications in stress modelling are the assumptions of plane strain and plane stress states within the casting. Most problems encountered are,

strictly speaking, three-dimensional. However, the equations for three-dimensional problems are difficult to solve and, thus, alternatives have been developed to reduce as many problems as possible to a two or even one-dimensional form.

- Plane stress: if the body is very thin and, most importantly, if the temperature gradients along the thickness are negligible, considering the normal and shear stresses to be a function only of the two other dimensions is possible. This is the case of plane stress.

- Plane strain: on the other hand, when one dimension of the body is very large in comparison to the two others and the thermal gradients along this dimension are very small, the stresses will be function of only the two other dimensions. This is the plane strain assumption.

The variation of plane strain is the generalized plane strain, where the strain in the largest dimension is a constant, instead of zero, as in the case of pure plane strain⁽¹²⁵⁾.

Another point to be considered in modelling stress in a casting is whether or not the liquid and mushy zones should be included within the computational domain. Although understanding the effect of the hydrostatic pressure and the behavior of the mushy zone has been recognized as fundamentally important^(110,126), the existing numerical methods have difficulties in simulating the corresponding phenomena. Therefore, a few simplifications have been proposed for these zones. The two most common are:

- i) Increasing Poisson's ratio ν to 0.5, while Young's modulus is set to a very small number. This makes the liquid phase close to being incompressible for mechanical loading and avoids singularity in forming the stiffness matrix, respectively⁽¹²⁷⁾;
- ii) Stripping away the liquid/mushy elements according to the heat transfer results and applying pressure boundary conditions in the solid domain⁽¹²⁰⁾. This approach represents the assumption that the shell begins accumulating stress at the solidus temperature.

As mentioned before, another important factor in determining stresses and displacements in castings is the heat transfer analysis. Since the stresses are mainly generated by contraction during cooling of the solid shell, the accurate determination of the transient temperature gradients may have a significant influence on the stress level of the casting.

In this regard, the most usual question prior to the development of a stress model is the necessity of coupling, or not, this to the heat transfer model generating the corresponding temperature fields. Funk et al⁽¹²⁴⁾ have presented a detailed discussion on this issue. Strictly speaking, the coupling is mandatory when the stresses and displacements calculated by the stress model have a significant influence in the evolution of the transient heat transfer. This influence is mostly present in the formation and growth of the air gap on the mold walls, which is a dynamic phenomenon, affecting simultaneously both temperature and displacement calculations. Nevertheless, coupling heat transfer-stress models generally implies huge computational demands and numerical difficulties: when the fluid flow pattern greatly affects the temperatures and the solidification interface, the natural choice

is to solve a fluid flow and heat transfer model first and then load the temperature fields in a stress model. The transference of files between different softwares, in a coupled manner, can be troublesome⁽¹¹⁰⁾. Furthermore, as pointed out by Funk et al⁽¹²⁸⁾, usually the FEM (Finite Element Method) grids employed in stress models are not as fine as the FDM (Finite Difference Method) grids, common in fluid flow/heat transfer models, since every FDM element is represented by only one node with one state variable, e.g., T , whereas a 3D FEM element consists of up to 20 nodes with three degrees of freedom per node.

The most important elements in stress modelling are the constitutive model and the mechanical properties of the casting at high temperature. The constitutive model comprises the elasto-viscoplastic behavior of the solid shell, which may be simplified according to each particular problem. As mentioned before, the strain generated by creep in many cases cannot be distinguished from plastic flow, which makes problematic its inclusion in the model. Thus, most models do not take into account the viscoplastic behavior of the shell. Others^(129,130), treat plastic and creep deformations in a unified manner via a "plastic-creep" strain rate. Both the numerical difficulties and accuracy of the results are greatly affected by the assumed constitutive model.

Another important phenomenon that needs to be considered is the inclusion of strain-hardening in the constitutive model. Errors due to neglecting this phenomenon may be appreciable⁽¹³¹⁾. Usually, the strain-invariance principle is used to accomplish the task: the modeller should compute plastic strains by assuming that the total strain remains the same, whether or not plastic flow occurs.

The difficulty inherent to the constitutive model has its origin in the lack of information on the mechanical properties of the solid shell and mushy zone at elevated temperatures. These properties depend on temperature, strain rate, thermal history, structure and composition⁽¹³²⁾. In a recent review of the state of the art in stress modelling, Thomas⁽¹¹⁰⁾ recognized the difficulty of measurements and the large discrepancies reported in the literature for mechanical properties of steel at high temperatures. Also, different testing methods had been reported^(133,134), which yielded divergent results.

A more detailed review of the most relevant papers for the present work is presented below.

Weiner and Boley⁽¹⁰⁸⁾ presented a classical analytical solution for the thermal stresses on a constrained casting. The Neumann's solution for solidification with constant surface temperature was adopted for the thermal problem and an elastic/perfectly plastic behavior considered for the stress solution. They found compressive stresses at the surface and tensile stresses in the core, the reverse of what would have been expected on the basis of purely elastic behavior.

The boundary conditions assumed by Weiner and Boley were criticized by Tien and Koump⁽¹²²⁾. First, the assumption of constant temperature on the cooling surface was regarded as physically inconsistent and, second, Tien and Koump considered the case where a solidifying beam is allowed to bend along the transverse direction, i.e., it is not constrained in that direction. In such a case, the restriction of total force across the beam is no longer applied and no reversion of the stress sign occurs during the cooling process. Therefore, their purely elastic

model predicted compressive stresses in the middle of the casting, sandwiched by tensile stresses on both the cooling surface and the shell-liquid interface, for the case of a simply-supported beam.

Brimacombe⁽¹¹⁵⁾ was one of the first authors to propose a stress model for continuous casting processes. He and collaborators presented a sequence of papers⁽¹¹⁵⁻¹¹⁷⁾ where the mechanisms of crack formation are thoroughly discussed based on an elastoplastic model and experimental observations. Useful suggestions and recommendations are given for reducing the incidence of cracks in billets and slabs. Later, he and co-workers^(129,135,136) adopted a 2D elasto-viscoplastic model for studying the causes of midface and off-corner panel cracks in steel ingots, assuming a state of plane stress.

Kelly et al⁽¹²⁰⁾ developed a 2D axisymmetric FEM formulation for the study of stresses and displacements in continuously cast steel billets. Translator programs were written for the communication between the FIDAP package, used for thermal solution, and the NIKE2D, for the stress calculation. Therefore, the thermal distortion of the mold, leading to the formation of the air gap, was taken into account to recalculate the temperature fields in the interface.

The occurrence of halfway cracks in continuously cast steel slabs was investigated by El-Bealy⁽¹³²⁾. A one-dimensional mathematical model was developed to represent the stress state in the middle of the width of the slab cross section. Creep, as well as elastoplastic deformation, was considered in the model. Industrial measurements were carried out and the results of the model were validated.

Tszeng et al⁽¹³⁷⁾ developed an uncoupled model to solve heat transfer and mechanical stresses in the continuous casting of low carbon steels. Their method was based on the temperature recovery scheme to represent the latent heat generation effects. First, the temperature fields were calculated and the MARC code for stress analysis was then used. They found final residual stresses to be compressive on the outer surface and tensile within the inner region.

Another uncoupled model for the continuous casting of steels predicted the maximum stress ratios to be at the locations of the bending rolls⁽¹³⁸⁾. This finding was based on the ratio of Von Mises stress and element yield stress, as an indicator for base cracking. An important conclusion of this work is that 2D models are not recommended for stress studies on square and/or rectangular sections, because of their inability to detect steep changes in temperature around corners. The stress ratios along casting direction are given in Figure 2-5.

Regarding the comparative magnitude of the stresses, Inoue and Wang⁽¹³⁹⁾ reported higher stresses near the surface of a continuously cast slab, rather than in the slab's center, where the temperatures are higher.

For beam blanks, Reza Aboutalebi et al⁽¹⁴⁰⁾ presented a two-dimensional finite-element analysis of thermal and mechanical stresses. Their main conclusion was that the supporting rolls in the beam blank caster play a significant role in reducing the distortion of the solid shell. Recently, Kim et al⁽¹²⁶⁾ proposed a thermomechanical model of the mushy zone, for beam blanks, besides a two-dimensional finite element model, coupling heat flow and deformation phenomena.

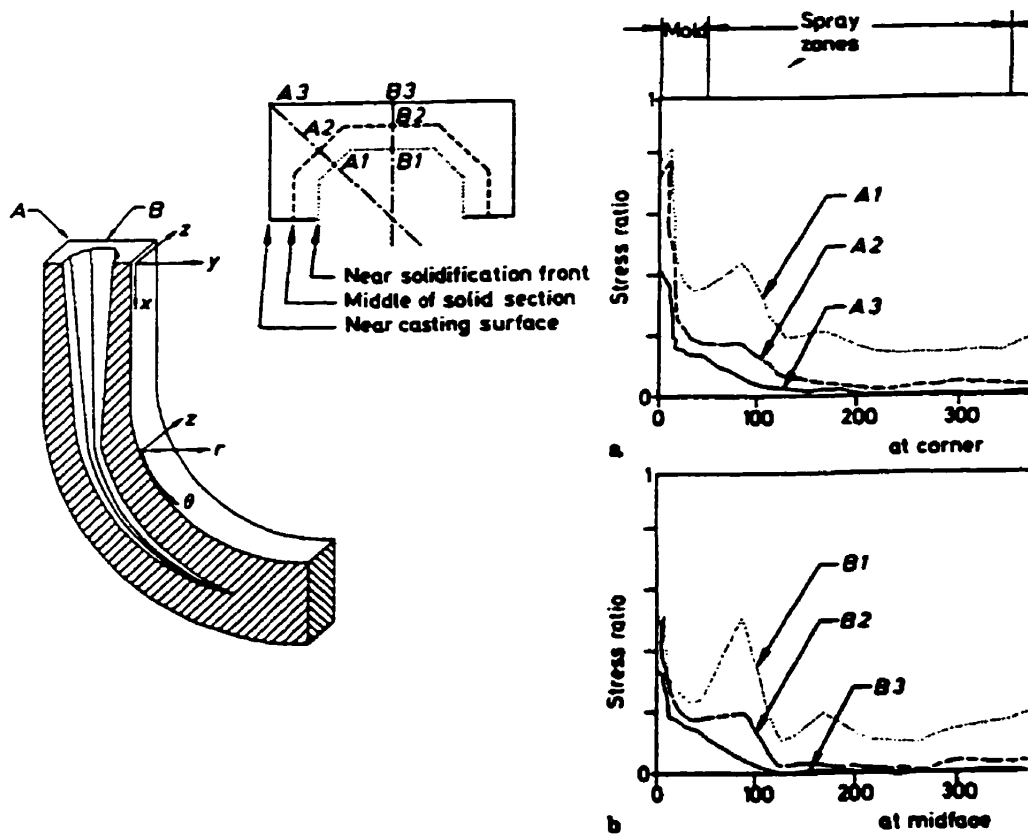


Figure 2-5. Stress ratio along casting direction for 0.25% C steel⁽¹³⁸⁾.

For a twin-roll continuous casting process, Ju and Inoue⁽¹⁴¹⁾ modeled coupled equations for mass and energy transport with phase change. The model was applied to evaluate the fields of temperature, velocity and stress/deformation to a low melting temperature metal.

The mechanical behavior of carbon steels was also subject of investigation. Wray⁽¹²¹⁾ concentrated his work on the effect of the carbon content on the plastic deformation at high temperatures. Stresses were measured for different steel grades, strain rates and temperatures. The plastic flow behavior of the austenite was also studied. Kozlowski et al⁽¹⁴²⁾ investigated four different forms of constitutive equations: constant structure, time-hardening, strain-hardening and simultaneous time and strain-hardening. This elasto-viscoplastic constitutive equations were compared in terms of the ability to simulate the mechanical behavior of steel under the conditions often encountered in continuous casting. One of the most important conclusions of this work is that none of the models was very effective at modelling creep for long periods.

Stress models have also been proposed for the casting of Aluminum and Aluminum alloys^(111,112,130,143,144). The challenges faced by the researchers are the same: estimated mechanical properties of the material at high temperatures, uncertainty in the mechanical behavior of the mushy zone and on the adopted boundary conditions. Nevertheless, some results obtained for Al castings proved useful for the present work, as will be discussed later.

Owing to the uncertainty from guessed mechanical properties and assumed constitutive behavior, in addition to errors resulting from the interaction with the

thermal model, calculating values of stresses in castings that are absolutely correct is nearly impossible. The most important objective in stress modelling is to determine the likelihood of crack formation in specific regions of the shell, that are more prone to failure. To achieve this objective, the calculated stresses are compared with the adopted failure criteria (von Mises, yield stress, etc.). These criteria will be reviewed in sequence.

2.6.3 FRACTURE CRITERIA IN CONTINUOUS CASTING OF STEEL

Fracture criteria are not precisely defined for steel near the solidus temperature. It has been reported in the literature⁽¹¹⁷⁾ that most steel grades exhibit a temperature zone just below the solidus, where the ductility reaches its low. Although the extent of this low ductility range depends on certain variables, such as the steel grade, it is generally considered that the zone is comprised between the solidus temperature and 40-70° C below the solidus.

Using this information, most authors^(110,115,120,124) identified the regions susceptible to cracks as the ones located within this temperature range and where the tensile principal stresses exceeded the yield stress of the material simultaneously.

Others⁽¹³⁸⁾ used the ratio of the calculated von Mises stress to the calculated yield stress or determined the yield point by the von Mises criterion^(116,129). Brimacombe et al^(116,117) adopted the criterion of a critical strain of 0.3 %, within the low ductility zone. However, Thomas⁽¹¹⁰⁾ reported that critical strains measured during bending of solidifying steel ingots were found to be in the range of 1.0-3.8%. Earlier, Vom Ende and Vogt⁽¹⁴⁵⁾ had said that tensile strains of 0.2-0.3%

were sufficient to cause cracks.

In general, the Von Mises effective stress parameter has been considered a reasonably good fracture criterion for materials that yield, flow plastically and subsequently fail in a ductile manner.

2.7 CONCLUDING REMARKS

The literature review is very clear concerning the need for deepening our current understanding of the promising single-belt near net shape casting process. Either by mathematical modeling or physical modeling, it is necessary that more insight be gained into the complicated phenomena involved.

Current delivery systems developed so far have drawbacks, though. As a consequence, major quality problems in the final product have been reported. Dimensional non-uniformity, cracks, etc., have been found on strips. Extensive research is needed to prevent such defects, and to guarantee a minimum level of reproducibility of the process and homogeneity of the product for a successful commercial operation.

In this regard, studying the stability of the meniscus zone is of great importance. It is accepted that the formation of dead zones or recirculating zones has a negative influence on evening flow onto the cooling belt. A mathematical model taking into account the turbulence effects more precisely, and the three-dimensional aspects, would be very useful in predicting and correcting the flow by a flow modifier. Also, a model capable to predict where the higher stresses are

located and what the nature of those stresses is, would help to prevent some of the defects that are currently presented. Some aspects related to the productivity of such a machine, such as mass flowrate, velocity of the belt and cooling efficiency, may also be discussed as an additional effort to give useful insight on this promising technology.

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MATHEMATICAL MODEL CONSTRUCTION

3.1 INTRODUCTION

Mathematical modelling has been an important tool for the development of near-net-shape casting processes. New designs could be tested without the need of building expensive pilot plants and the results of the models can be used as a starting point. Furthermore, once the pilot plant is built, the model can minimize the number of experiments necessary to better understand the process and to make the necessary adjustments.

Therefore, a mathematical model was envisaged to clarify some of the aspects inherent to the novel single-belt caster configuration proposed by Herbertson & Guthrie⁽¹⁾, for strip casting of steel. As mentioned in the literature review, Jefferies⁽²⁾ has presented earlier a fully-coupled two-dimensional model for this particular configuration. Nevertheless, the use of an artificially enhanced ad-hoc viscosity to account for the turbulence effects and the suppression of three-dimensional effects in her model are points that need to be improved, for a better understanding of the phenomena in this process.

In particular, the adoption of an homogeneously enhanced molecular viscosity and, by extension, an homogeneous effective thermal conductivity, may lead the modeller to considerable errors, especially on the temperature and solidification results. This could happen if the computational domain comprised regions of substantially different levels of turbulence, as was suspected to be the case for the problem under consideration. Also, for the flow modifier region, a special procedure is necessary for simulating turbulence and this will be discussed in more detail later.

3.2 MATHEMATICAL MODEL DEVELOPMENT

The mathematical model obeyed some predetermined design criteria, with respect to caster productivity and geometrical aspects. Conditions were also superimposed, such as the reduction of the likelihood of any hydraulic jump at the nozzle exit. For that, the kinetic energy of the flow in the exit gap ($mv^2/2$) was matched to the potential energy of the inlet (mgh). The losses due to friction in the reservoir were accounted for in the coefficient of discharge used in the model.

Figure 3-1 shows the schematic of the computational domain (symmetry axis) while Table 3-1 gives the range of variation in the geometric parameters studied. The following general procedure was adopted to determine the primary input data of the model:

- i) For a given belt velocity and coefficient of discharge, the metal head was fixed, and the mass flow rate was calculated. Therefore, for the same belt speed, different exit gaps yielded different productivities.

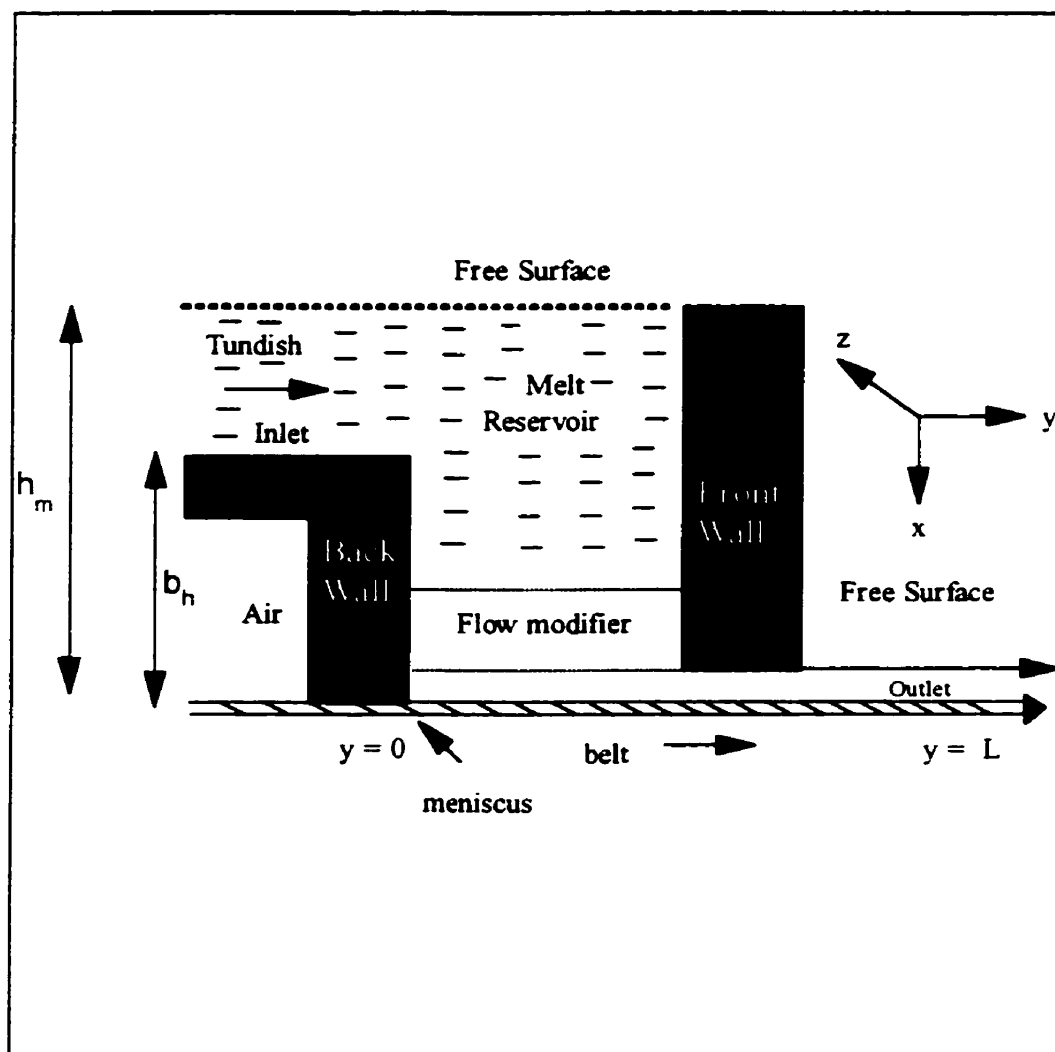


Figure 3-1. Schematic of the proposed configuration for the delivery system.

In addition, for the same exit gaps, the belt speed was varied, again causing changes in productivity. The metal height was kept as a standard parameter;

- ii) For those cases where the metal height was varied, the belt speed was kept constant, resulting in a different inlet velocity. In such cases, there was a mismatch between the kinetic and potential energy of the flow and its consequences are discussed.

Table 3-1 Geometric parameters.

Symbol	Description	Range of Variation
d_e	Exit gap, strip thickness	4-10 mm
h_m	Metal head	7-14 cm
l	Reservoir length	10-30 cm
L_d	Computational domain length	0.4-1.5 m
b_w	Back wall height	4-7 cm
d_p	Flow modifier thickness	2.5-5 cm
nz	Front wall thickness	2.25-8.25 cm
W	Width of the strip	25-45 cm
v_b	Belt velocity	0.5-1 m/s
S_b	Initial superheat	0-30° C
m'	Inlet flowrate	45-114 tph

In the simulations, the caster reached productivity levels similar to the

conventional continuous casting machines. Other parameters varied in the model were: inlet superheat, pool length, width of the strip and thickness of the front wall. Such variations were applied basically for the standard case, without the inclusion of the flow modifier. Different coefficients of discharge were adopted for the standard case and for the porous medium case. These coefficients were measured by Jefferies⁽²⁾ in a water model prototype.

3.3 FUNDAMENTALS

When one develops a complex mathematical model, some assumptions and simplifications have to be made, so as to be able to obtain reliable results coupled with efficiency of the code. Below is a list of the most important assumptions adopted in the model:

- i) The turbulent fluid flow and heat transfer are fully coupled;
- ii) The phenomena are essentially three-dimensional and a steady-state was assumed;
- iii) The metal flow was considered incompressible and the fluid, Newtonian;
- iv) A columnar dendritic solidification was assumed;
- v) A Low-Reynolds number κ - ϵ model was used to account for turbulence in the fluid flow;
- vi) The Darcy-Forchheimer-Brinkman equation was initially solved in the porous medium region;
- vii) The thermophysical properties in the liquid, mushy and solid zones were considered constant;

- viii) The latent heat was uniformly released between the liquidus and solidus temperature;
- ix) Natural convection effects were not included in the model, since the inertial effects are dominant;
- x) The metal was assumed to be laid down on the belt in an isokinetic manner, i.e., no tangential shear stress and no surface tension were considered.

The conservation of mass, momentum and energy are represented by differential equations, the general form being given by Patankar⁽³⁾, for steady-state flows:

$$\frac{\partial}{\partial x_i} (\rho u_i \phi) = \frac{\partial}{\partial x_i} \left(\Gamma_{eff} \frac{\partial \phi}{\partial x_i} \right) + S_\phi \quad (3-1)$$

where:

- ϕ = general dependent variable;
- u_i = velocities **u**, **v** and **w** in directions **x**, **y** and **z**, respectively;
- x_i = coordinate in the Cartesian system;
- Γ_{eff} = effective diffusion coefficient for variable ϕ ;
- S_ϕ = source term for dependent variable ϕ .

In equation 3-1, the first term, from left to right, is the convection term, the second term is the diffusion term and the third is the source term. The variable ϕ stands for the different quantities involved in the fluid flow/heat transfer model: velocities **u**, **v** and **w** and enthalpy **h**.

3.3.1 SOLIDIFICATION SCHEME

In order to take advantage of the benefits of the fixed grid techniques

applied to solidification problems, the continuum model advocated by Bennon and Incropera⁽⁴⁾ was used in this work. This enthalpy-porosity scheme is briefly mentioned in the literature review and its main assumptions are⁽⁴⁾:

- i) The mixture components may be viewed as isolated subsystems, if interactions with other mixture components are properly treated. However, the application of this principle to multiphase, multiconstituent mixtures must take into account that while the phases can be generally considered as isolated, their constituents are often inseparable due to intimate bonding on an atomic level;
- ii) All properties of the mixture are mathematical consequences of the component properties. However, for non-inert systems, the thermophysical phase properties need to be determined from phase diagrams or empirical equations;
- iii) The mean collective mixture behavior is governed by equations similar to those governing the individual components.

The values of ϕ , Γ_{ϕ} and S_ϕ are given in Table 3-2, for the conservation of mass (continuity equation), momentum and energy. The complete derivation of the equations is given in Reference 4. The second constituent of the source term in the momentum equations is derived from Darcy's law of porous media that accounts for the effect of the solid phase on the fluid flow in the mushy zone:

$$U_i = - \left(\frac{\kappa_p}{\mu} \right) \nabla P \quad (3-2)$$

where κ_p is the permeability of the porous media and U_i is the superficial velocity in the direction i .

Table 3-2 Values of dependent variable, diffusion coefficient and source terms for the conservation equations.

Conservation equation	Variable	Diffusion coefficient	Source term S_s
Continuity	1	0	0
Momentum x direction U velocity	U	μ_{eff}	$\frac{\partial}{\partial x_i} \left(\Gamma_\phi \frac{\partial u_i}{\partial x} \right) - \frac{C(1 - f_L^2)}{f_L + Q^3} (u - u_s)$ $- \frac{\partial P}{\partial x}$
Momentum y direction V velocity	V	μ_{eff}	$\frac{\partial}{\partial x_i} \left(\Gamma_\phi \frac{\partial u_i}{\partial y} \right) - \frac{C(1 - f_L^2)}{f_L + Q^3} (v - v_s)$ $- \frac{\partial P}{\partial y}$
Momentum z direction W velocity	W	μ_{eff}	$\frac{\partial}{\partial x_i} \left(\Gamma_\phi \frac{\partial u_i}{\partial z} \right) - \frac{C(1 - f_L^2)}{f_L + Q^3} (w - w_s)$ $- \frac{\partial P}{\partial z}$
Energy	H	Γ_{eff}	$\frac{\partial}{\partial x_i} \left[\Gamma_\phi \frac{\partial}{\partial x_i} (H_s - H) \right]$ $- \frac{\partial}{\partial x_i} [\rho f_s (H_L - H_s) (u_i - u_{is})]$

Darcy's law usually applies for columnar dendritic structure⁽⁵⁾ and the Carman-Kozeny equation (2-8), derived on extension from Darcy's law, was employed. According to Carman-Kozeny's the permeability κ_p is:

$$\kappa_p = \frac{f_L^3}{C(1-f_L^2)} \quad (3-3)$$

The most significant effect of these permeability source terms will be that of making the v -velocity (direction of movement) of the solidifying shell approach the belt velocity, with the completion of solidification. For the two other directions, the velocity component will approach zero. The source term becomes zero in the liquid zone and therefore has no influence on the fluid flow and heat transfer. A small value is assigned to the term "q". Its inclusion in the permeability source terms is justified to avoid division by zero in the solid region, i.e., where the liquid fraction is equal to zero.

The constant C (morphology constant) defines the morphology and effectively controls the degree of convection field into the mushy zone⁽⁶⁾. Its value should be big enough to force the velocity in the solid region to be equal to the belt speed in the direction of the movement and to zero in the two other directions. Although it is known⁽⁷⁾ that this coefficient needs to be determined according to the material solidification structure, values in the range of 10^5 - 1.6×10^7 have been reported in the literature. Minakawa et al⁽⁸⁾ have proposed a correlation between C and the secondary dendrite arm spacing (SDAS):

$$C = \frac{180}{d^2} \quad (3-4)$$

In Equation 3-4, d is the secondary dendrite arm spacing and should be given in **mm**.

In another reference⁽⁹⁾, the resistance to liquid steel flows by the solidifying dendrites was accounted for by the increase of the effective viscosity twenty times in the mushy zone.

In the present work, for the expected cooling rates, the secondary dendrite arm spacing is estimated to be in the neighbourhood of 10-20 μm . Thus, the application of Minakawa's formula would yield results of C between 4.5×10^5 (for SDAS = 20 μ) to 1.8×10^6 (for SDAS = 10 μ). However, a constant value equal to 10^7 was chosen in this work for the morphology constant, which is closer to the value used by Jefferies⁽²⁾ in her work. Nevertheless, some simulations were carried out with variations in this coefficient, to clarify its influence on the morphology of the mushy zone, for horizontal strip casting.

In the mathematical model, the latent heat was uniformly released between the liquidus and solidus temperatures. It means that the enthalpy behaves linearly in the solidification interval. The total enthalpy/per unit mass, i.e., specific enthalpy is calculated by:

$$H = \int_{T_{\text{ref}}}^T C_p dT + L_f f_L \quad (3-5)$$

where the liquid fraction f_L is given by:

$$f_L = \frac{H - H_s}{H_L - H_s} \quad (3-6)$$

In equations 3-5 and 3-6, T_{ref} is the reference temperature, C_p is the specific heat, L_f is the latent heat of fusion, H_L and H_s are the specific enthalpy at the liquidus and solidus temperature, respectively.

The liquid fraction may be a non-linear function of temperature^(10,11), the most common being the lever rule^(4,12). This rule applies when equilibrium is considered in the solidification. In fact, a combined micro-macroscopic approach would be recommended, since it is known that the grain formation occurs much faster near the end of the solidification than near its start⁽¹²⁾. Furthermore, some microscopic phenomena such as microsegregation cannot be included, when using this simple linear approach. Nevertheless, taking into account the sophisticated treatment that would be needed in order to include microscopic phenomena, the computational demands would be multiplied many times. Furthermore, previous work⁽¹³⁾ has shown that the slope of the liquid fraction evolution does not significantly affect the results in the solid shell in continuous casting simulation. As such, the simple linear model for updating the liquid fraction was maintained.

3.3.2 TURBULENCE MODEL

As mentioned before, turbulence is considered relevant for this particular system. Therefore, a search was made for the most suitable approach to deal with turbulence effects on fluid flow and heat transfer. Due to the reasons detailed in the literature review, a two-equation κ - ϵ model was chosen as the best approach to this problem. Moreover, the fact that some previous works⁽¹⁴⁻¹⁷⁾ have successfully employed such solutions to metallurgical problems involving turbulent

flows also helped in this choice.

Modeling turbulent flows with the help of a κ - ϵ model is not a simple task. Beyond the computational cost, there is also a lack of reliable experimental data that make it difficult to validate the results of the model⁽¹⁸⁾. Furthermore, it is necessary to apply wall functions in order to represent damping effects near solid boundaries. Worse indeed, the application of these functions cannot be extended to all types of flows. They are currently used for high Reynolds number shearing flows. In addition, the implementation of near wall functions to phase change processes is very difficult, since the position of the interface liquid-solid is unknown *a priori*⁽¹⁴⁾.

In this work, a Low-Reynolds number model was used to account for the turbulence near the walls (front, back and lateral). This type of model has been developed recently, in the past 25 years, to fill the vacuum created by the inapplicability of the wall functions methods to "Low-Reynolds number" flows. Of the low-Re number κ - ϵ models presented in the literature, the model proposed by Launder and Sharma⁽¹⁹⁾ was employed in this work. This model is easy to implement in solidification problems⁽¹⁴⁾, and has often been used for metallurgical applications⁽¹⁴⁻¹⁶⁾.

Table 3-3 shows the diffusion coefficients and the source terms for the turbulence equations employed in this work. With respect to the κ - ϵ turbulence models, one of the most important aspects is the development of an adequate expression for the turbulent viscosity. In turbulence-dominated flows, the value of the molecular viscosity becomes secondary, since the turbulent viscosity may reach much higher values (i.e. 10^2 to 10^5).

Table 3-3 Values of dependent variables, diffusion coefficients and source terms for the turbulence equations.

Conservation Equation	Variable	Diffusion coefficient	Source term S_ϕ
Turbulent kinetic energy	κ	Γ_{κ}	$G_\kappa - \rho\epsilon + D_\kappa$
Rate of dissipation of turbulent kinetic energy	ϵ	Γ_ϵ	$f_1 C_1 G_\kappa \frac{\epsilon}{\kappa} - f_2 C_2 \rho \frac{\epsilon^2}{\kappa} + E_\epsilon$
$G_\kappa = \mu_t \left(\frac{\partial u_i}{\partial x_j} + \frac{\partial u_j}{\partial x_i} \right) \frac{\partial u_i}{\partial x_j}$ $D_\kappa = -2\mu_t \left(\frac{\partial \sqrt{\kappa}}{\partial x_i} \right)^2$ $E_\epsilon = \frac{2\mu_t \mu_\epsilon}{\rho} \left(\frac{\partial^2 u_i}{\partial x_j \partial x_k} \right)^2$ $f_1 = 1 ; C_1 = 1.44 ; C_2 = 1.92$ $f_2 = 1 - 0.3 \exp(-R_T^2)$ $R_T = \frac{\rho \kappa^2}{\mu \epsilon}$ $\Gamma_{\kappa, \epsilon} = \mu + \frac{\mu_t}{Pr_{\kappa, \epsilon}}$ $Pr_\kappa = 1.0 ; Pr_\epsilon = 1.3$ $\mu_t = C_\mu f_\mu \rho \frac{\kappa^2}{\epsilon}$ $f_\mu = \exp \left[\frac{-3.4}{\left(1 + \frac{R_T}{50} \right)^2} \right]$ $C_\mu = 0.09$			

In Table 3-3, f_μ represents the generalized damping mechanism for the turbulent transport, in both phases, liquid and mushy⁽⁷⁾. The modification proposed by Shyy et al⁽⁷⁾ was also included to damp turbulence in the mushy zone:

$$f_{\mu 1} = \sqrt{F_L} \cdot f_\mu \quad (3-7)$$

3.3.3 POROUS MEDIUM MODELING

One of the most important objectives of this mathematical model is to simulate the modifications resulting from the inclusion of a flow modifier in the proposed metal delivery system. In this regard, the first important point is to determine the nature of the flow. Since the Re number in the filter is not very low and the Pr value of the metal is low (~ 0.17 for steel), the inertial effects may be important. Therefore, a prior investigation of the magnitude of these effects was performed. Furthermore, as suggested by Beckermann et al⁽²⁰⁾, the wall (viscous) effects should also be included in cases of high Da number. For this model, the Da number can be calculated by:

$$Da = \frac{\kappa_p}{d_p^2}, \text{ where: } \kappa_p = \frac{\epsilon_p D^2}{32} \quad (3-8)$$

In the equation above, d_p is the thickness of the filter and D is the filter average pore diameter. Since the permeability of the filters suitable to this kind of application (zirconia or alumina) is in the range of 10^{-8} to 10^{-9} m² and the filter thickness is typically 1 or 2 inches, the Da number will range in the

neighbourhood of 10^{-5} - 10^{-4} , which is fairly high.

Another reference⁽²¹⁾ suggests that Darcy's law should be selected when the volume of solid particles is larger than the volume occupied by the fluid, or in other words, Darcy's law is applicable to low-porosity media. In the present case, the porosities of the filters range from 0.84 to 0.88. Therefore, the three flow resistances were initially taken into consideration:

- i) The bulk damping resistance due to the porous structure (Darcy's law);
- ii) The resistance due to the inertial effects (Forchheimer's extension);
- iii) The viscous resistance due to the boundary walls (Brinkman's extension).

The original forms of those contributions to flow modification are given in the literature review. The general form of the modified momentum equations for porous media is⁽²²⁾:

$$0 = - \frac{\partial P}{\partial x_i} + \nabla \cdot (\mu_{eff,p} \nabla u_i) - \left(\frac{\mu_{eff,p}}{\kappa} + \frac{\rho \gamma |vel_p|}{\sqrt{\kappa}} \right) u_i \quad (3-9)$$

$$|vel_p| = \sqrt{u_p^2 + v_p^2 + w_p^2} \quad (3-10)$$

$$\kappa_p = \frac{\epsilon_p D^2}{32} \quad (3-11)$$

where:

u_i = velocities u , v and w , in directions x , y and z , respectively;

x_i = rectangular coordinates x , y and z ;

κ_p = permeability of the porous medium in m^2 ;

ϵ_p = overall porosity of the porous medium;

D = average porous media pore size in m ;

Υ = inertia coefficient of Forchheimer's extension = $0.55^{(20)}$.

The first results showed, however, that while increasing the computational complexity, the Forchheimer term didn't yield appreciable change in the flow results. Therefore, for the sake of simplicity and computational savings, this term was not employed for the parametric studies. Indeed, as McDonald et al⁽²³⁾ had concluded, for Re less than 300, the flow is laminar inside the porous medium and thus the inertial terms should not be significant. In the specific problem, Re , defined based on the pore diameter, ranged typically from 40 to 100.

To assess the effect of the permeability formulation to the results, the permeability was also calculated by Carman-Kozeny's equation:

$$\kappa_p = \frac{\epsilon_p^3}{5S_o^2(1 - \epsilon_p)^2} \quad (3-12)$$

where S_o is the Carman's specific surface exposed to the fluid per specific unit volume of solid (mm^{-1}) and its values were estimated from measurements carried out by Tian⁽²⁴⁾ on similar filters. Strictly speaking, Carman-Kozeny equation should be applied only for media with porosities up to 0.8. Nevertheless, it had been

found earlier⁽²⁵⁾ that no serious error results from the use of this equation for cases where the porosity is below 0.90. The differences in the fluid flow results for the two methodologies were considered not relevant for this problem, as will be discussed in Chapter 5.

3.3.3.1 General assumptions for the porous medium

The adoption of the Darcy-Brinkman formulation comprises the following assumptions:

- i) The flow modifier is treated macroscopically as a porous medium. The pore configuration has a small scale length compared to a macroscopic level. The material is fine-grained and may be considered homogeneous at a microscopic level. Therefore, the fluid and solid matrix are treated as a single medium, where the conservation equations will be solved;
- ii) The properties of the fluid are considered constant within the porous medium;
- iii) Thermal equilibrium is assumed between the fluid and the solid. This assumption is generally valid, except for a highly conducting filter material or very high fluid velocities;
- iv) Thermal losses by friction are neglected;
- v) The viscosity of the fluid is corrected within the porous medium. Same for the thermal conductivity:

$$\mu_{eff,p} = \mu_f \cdot \epsilon_p ; \quad k_{eff,p} = \epsilon_p k_{eff,f} + (1 - \epsilon_p) k_s \quad (3-13)$$

- vi) No phase change, chemical reactions, nor interfacial effects were

incorporated.

3.3.3.2 Turbulence modeling for flow in porous media

There is no tested and validated model for turbulence within a porous medium, as discussed in the literature review. Therefore, a basic approach was followed in this work:

- Apply the κ - ϵ equations to the whole domain, including the porous medium. To damp turbulence in the porous medium, the turbulent viscosity was multiplied by the porosity of the medium, similarly to the approach used for the mushy zone.

Since, as mentioned earlier, the flow was found to be essentially laminar in the porous medium region, the solution of κ - ϵ equations in this region may be optional.

3.4 BOUNDARY CONDITIONS AND THERMOPHYSICAL PROPERTIES

The initial conditions and the boundary conditions represent one of the most important aspects in any kind of modelling. A proper definition of the boundary conditions is mandatory for the model to be useful in practical applications. Usually, the boundary conditions are originated from in-plant measurements of the relevant variables, such as temperature and flow velocity. In addition, correlations and data existent in the literature are used with reasonable accuracy.

In the present case, since the model is supposed to simulate a new design

of a hypothetical caster, such information is not available. Therefore, the relevant thermal boundary conditions will be approximated according to data published in the literature. Nevertheless, the primary objective of the model was to study the influence of the most important process variables and the changes in the fluid flow and heat transfer resulting from the insertion of a porous medium in the proposed delivery system and for that, such approximations are not critical.

The most important thermal boundary condition in the present configuration is the overall heat transfer coefficient between the substrate (belt) and the solidifying shell. This coefficient was considered constant for most of the simulations, but in some cases a stepwise variation was modelled. In addition to these, in one of the simulations its transient behavior was expressed in terms of the transient heat transfer coefficient measured in a similar experimental apparatus constructed at McGill and described in Chapter 8.

The following standard boundary conditions were adopted for the turbulent fluid flow:

- * No slip on the solid walls, which means that the velocity components in the x , y and z surfaces and also κ - ϵ are zero. At the moving belt, the velocity in the direction of the movement, v , will be equal to the belt velocity and the two other components plus the κ - ϵ will be zero;
- * At the free surfaces of the melt (both within the reservoir and the exiting strip), the gradients of all variables with respect to x will be zero and the velocity perpendicular to the free surface will be zero, as well;

- * At the symmetry plane (direction z), the gradient of all variables with respect to z will be zero as well as the corresponding perpendicular velocity;
- * At the outlet, fully-developed flow was considered. This means that the normal gradient of the velocity v and of the turbulence variables will be set to zero along with the other velocities. In addition, the outflow is related to the inlet and the exit gap;
- * At the inlet, the velocity v is specified and linked to the outflow to respect continuity. Velocities u and w are set to zero. The inlet conditions for the turbulence variables were taken from the literature⁽²⁶⁾, for extended nozzles:

$$\kappa_{it} = 0.005 v_{it}^2 ; \quad \epsilon_{it} = 0.09 \frac{\kappa_{it}^{1.5}}{0.03 (h_m - b_w)} \quad (3-14)$$

where v_{it} is the inlet velocity and $(h_m - b_w)$ represents the height of the inlet stream.

The energy boundary conditions were, in general:

- * The metal loses no heat to the back and lateral walls, i.e., adiabatic conditions were assumed on these walls. In some specific cases, uniform heat fluxes were imposed on both walls;
- * An initial superheat was specified in the inlet;
- * The metal at the free surfaces of the reservoir and exit nozzle gap

loses heat to the environment by radiation and convection;

- * Heat is exchanged by conduction with the side and the bottom of the front wall. A uniform heat flux condition was applied to both sites, equal to 50 KW/m^2 ;
- * The metal mainly loses heat by convective heat exchange with the substrate (moving belt). In general, the heat transfer coefficient was considered constant and equal to $5000 \text{ W/m}^2\text{.K}$, with variations in specified cases;
- * On the outlet, a fully-developed profile was considered for the enthalpy.

The thermophysical properties of the low-carbon steel and porous media are presented in Table 3-4.

Temperature dependency of the thermophysical properties of steel was tested, but found to have small effect on the results, for the range of temperatures within the computational domain. The error that may occur due to uncertainty in such properties has been discussed in the literature^(27,28) and will be treated later in this thesis. The density was considered constant, which means that shrinkage during solidification was neglected. Since the finite-difference model employed a fixed grid, the assumption of constant density was necessary to ensure that mass is conserved.

Table 3-4 Thermophysical properties of low-C steel and porous media⁽²⁾.

Low Carbon Steel	Porous media - 92% Al₂O₃ foam filters 45 ppi
Density, $\rho = 7020 \text{ kg/m}^3$	Pore size, $D = 420 \text{ }\mu\text{m}$
Thermal conductivity, $k = 32.6 \text{ W/m.K}$	Overall porosity, $\epsilon_p = 0.84$
Specific heat, $C_p = 680 \text{ J/kg.K}$	Thermal conductivity, $k_p = 1.4 \text{ W/m.K}$
Heat of fusion, $L_f = 270 \text{ KJ/kg}$	Porous media Zirconia ZTA 10 ppi
Solidus temperature, $T_s = 1492^\circ\text{C}$	Pore size, $D = 1443 \text{ }\mu\text{m}$
Liquidus temperature, $T_l = 1535^\circ\text{C}$	Overall porosity, $\epsilon_p = 0.87$
Molecular viscosity, $\mu = 0.0068 \text{ kg/m.s}$	Thermal conductivity, $k_p = 2.1 \text{ W/m.K}$
Emissivity of liquid steel, $\epsilon_l = 0.28$	

3.5 NUMERICAL MODEL

The fully-coupled, nonlinear, partial differential equations (conservation of mass and energy and momentum equations) cannot be solved analytically. Therefore, a control-volume finite difference scheme based on the SIMPLE algorithm (Semi-Implicit Method for Pressure Linked Equations) was employed. The detailed description of this algorithm is given in Patankar⁽³⁾ and will not be repeated here.

The basic point of a control-volume scheme is that the domain is divided into a number of small control volumes, which surround the corresponding grid points. The number of control volumes are adjusted according to a compromise between accuracy and computational speed. A staggered grid, type B (see Patankar⁽³⁾) was employed for the velocities. In this practice, the control-volume boundaries are drawn first and the grid points are then placed at the geometric center of each control volume, i.e., the grid point do not coincide with the boundaries of the computational domain.

The partial differential equations are discretized into algebraic equations of the following form:

$$a_P \phi_P = a_E \phi_E + a_W \phi_W + a_N \phi_N + a_S \phi_S + a_T \phi_T + a_B \phi_B + S_P \quad (3-15)$$

where the coefficients a_E , a_W , a_N , a_S , a_T and a_B represent the neighbours of point P, for the three-dimensional case. Term S_P includes the remaining constant terms (source terms). Convective and diffusive terms are included in the coefficients a_i , while the source term S_P represents the additional terms, for solidification, turbulence, and modifications for flow through a porous medium.

An upwind scheme was employed for the convective terms. The choice of this scheme depends on the magnitude of the Peclet number (Pe). When Pe is high, as in a solid-liquid system, the upwind scheme is recommended⁽²⁹⁾. Furthermore, the upwind scheme has been reported to aid convergence in domains with high cell Reynolds number⁽³⁰⁾. To ensure stability of the solution, the source terms were linearized.

In the SIMPLE-algorithm, the pressure field is first guessed with estimates

of the velocities. Then, initial values of velocities are obtained from the solution of the discretized momentum equations. In the sequence, pressure and velocity corrections are calculated and the pressures and velocity fields updated. Also, in the SIMPLE-algorithm a rapid variation in the pressure field usually results in a very slow rate of convergence⁽³¹⁾. This is because the global mass conservation is obtained by the local mass conservation through the pressure correction equation and this equation is not very sensitive to rapid changes in fluid velocities, caused by discontinuities in geometry, such as sudden compression. In this model, this problem was hardly observed, using under-relaxation factors between 0.3 and 0.5.

An iterative, line-by-line, Tri-Diagonal Matrix Algorithm (TDMA) was employed to solve the system of equations. The highly nonlinear nature of the conservation equations requires under-relaxation factors, in order to achieve convergence. The initial values chosen for the under-relaxation factors were 0.4 for velocities, 0.2 for κ - ϵ , and 0.35 for the enthalpy. These values were progressively increased as the convergence process (number of iterations) evolved, achieving peaks of 0.7 for velocities and κ - ϵ values and 0.6 for enthalpies. For the cases where the filter was modelled, the initial under-relaxation factors were considerably lower, usually 0.2 for velocities, while source terms related to the porous medium also required under-relaxation. This requirement can be explained in terms of the dramatic change in the flow pattern in the region of the filter.

On average, 5000 iterations were needed to achieve convergence in a Silicon Graphics Challenge L 100 MHz, every iteration lasting approximately 35 s of CPU time. The following convergence criteria were chosen:

- For the mass balance (Continuity):

$$\frac{\Sigma Res_{mass}}{m^*} \leq 5 \times 10^{-4} \quad (3-16)$$

- For velocities (force balance):

$$\frac{\Sigma Res_{vel}}{m^* \times v_{it}} \leq 5 \times 10^{-4} \quad (3-17)$$

- For the enthalpy (energy equation):

$$\frac{\Sigma Res_h}{m^* \times H_{it}} \leq 5 \times 10^{-4} \quad (3-18)$$

- For the turbulence quantities κ - ϵ :

$$\frac{\Sigma Res_{\kappa, \epsilon}}{m^* \times Max_{\kappa, \epsilon}} \leq 5 \times 10^{-4} \quad (3-19)$$

where:

m^* = initial mass flowrate;

ΣRes_i = sum of residuals of variable i ;

v_{it} = inlet velocity;

$Max_{\kappa, \epsilon}$ = maximum value of turbulence variables in the domain;

H_{it} = inlet enthalpy.

When all these criteria were achieved simultaneously, the simulation was considered complete.

3.5.1 GRID DEPLOYMENT

Figure 3-2 shows the system of Cartesian coordinates used in the model and a typical grid (symmetry axis). The grid was drawn in such a way that fine gridding was adopted near the regions where the largest discontinuities were expected. These regions were, in particular, in the neighbourhood of the three walls, the exit gap and the whole region corresponding to the solidifying strip, as well as the flow modifier domain. In the z direction, the grid is uniform up to the neighbourhood of the sidewalls, where refining is required.

The grid dependency was investigated for the standard cases, with and without the filter. For the case without the filter, a 72x56x23 grid was chosen, after calculations showed that finer grids yield refinements that were not worth the extra computational time. Figures 3-3 and 3-4 show the grid dependency of some typical results of the model. These simulations were carried out according to the standard conditions given in Table 3-5, with the exception of belt velocity and corresponding inlet flowrate. A value of 0.5 m/s was chosen for the belt velocity, in order to provide a thicker final solidified shell. As it is seen in Figure 3-3, refining the grid from 72x56x23 nodes to 92x61x26 produced little variation in the solidified shell thickness, as opposed to the first refinement of the initial 42x56x23 grid. On the other hand, Figure 3-4 shows that the velocity v above the belt was nearly unchanged with further refinement in the initial grid. This result may

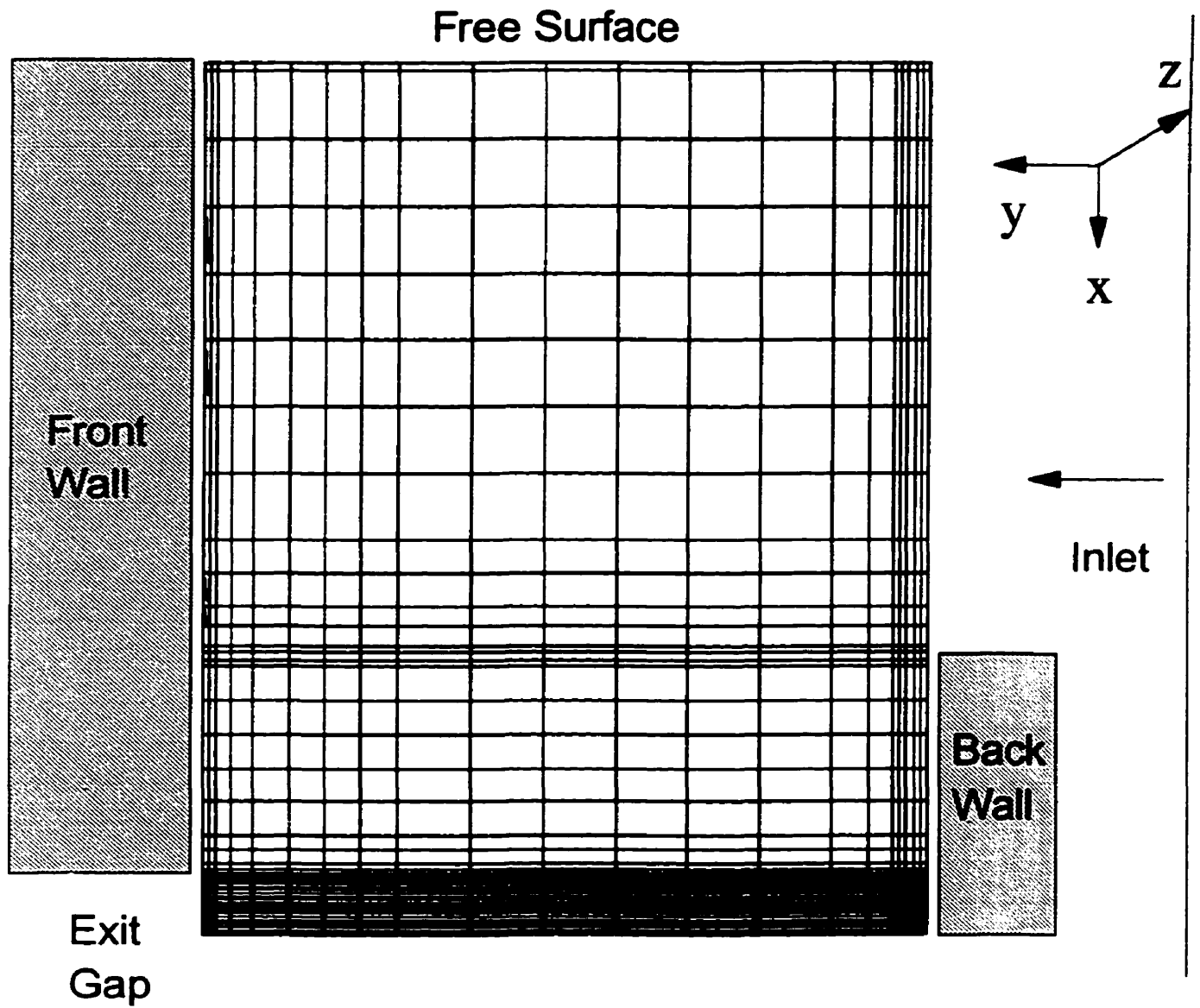


Figure 3-2. System of Cartesian coordinates used in the model and the origin of the axes.

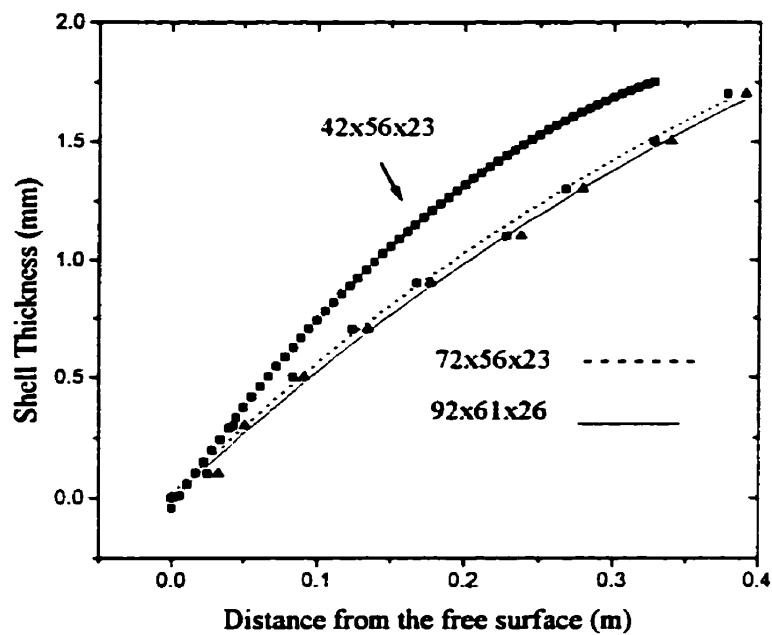


Figure 3-3. Grid dependence of the solidified shell thickness.

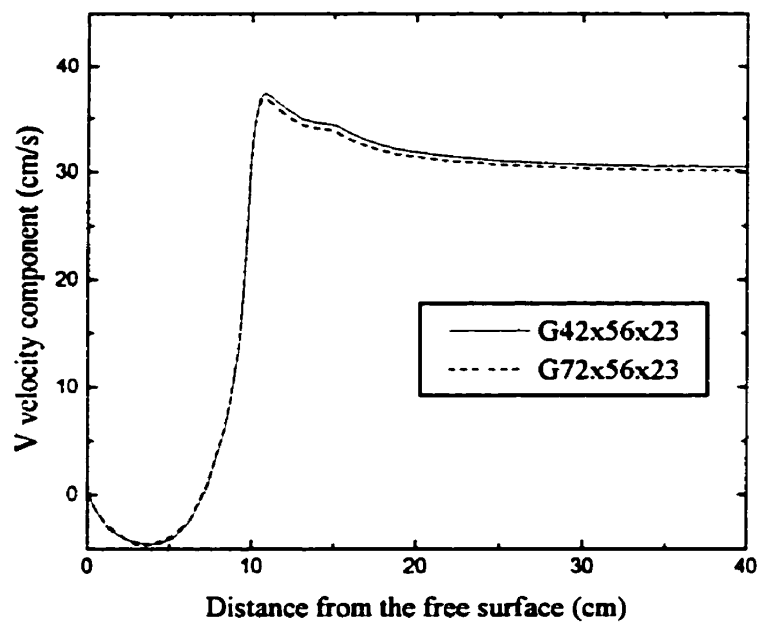


Figure 3-4. Grid dependence of velocity v , 5 mm above the substrate.

suggest that if one is interested in running only the fluid flow part of the model, a coarser grid (42x56x23) will give reliable results, with significant computational savings. The values of the velocities were checked in other positions of the computational domain, as well, confirming these results.

Table 3-5 Standard conditions for the simulations of low-carbon steel strip casting.

Symbol	Description	Standard Value
d_e	Exit gap, strip thickness	10 mm
h_m	Metal head	7 cm ; 14 cm (filter)
l	Reservoir length	10 cm
L_d	Computational domain length	40 cm
b_w	Back wall height	4 cm
d_p	Flow modifier thickness	2.5 cm
n_z	Front wall thickness	2.25 cm
W	Width of the strip	45 cm
v_b	Belt velocity	1 m/s
S_s	Initial superheat	10° C
m'	Inlet flowrate	114 tph

For the parametric study, diverse grid variations were adopted, according to the new geometrical aspects of the computational domain.

Of particular importance is the presence of the front wall in the computational domain. Usually, walls are treated as boundaries in metallurgical processes. The existence of such an internal solid body requires special care from the modeller. The velocities were then driven to zero by a technique that consists in making the coefficients $a_N, \dots, a_B = 0$ and in assigning values to the coefficients S_C and S_P of the source term that set the velocities in the wall region to zero. This way, the energy equation reduces to the Laplace equation for three-dimensional heat conduction, at the bounding solid surfaces of the metal delivery system.

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FLUID FLOW AND HEAT TRANSFER RESULTS

The mathematical model was used for parametric studies, to help the modeller achieve his main task: the determination of the most suitable design for the proposed delivery system. The study focused on the hypothetical casting of 10-mm, 8-mm and 4-mm strips on the single-belt caster. Emphasis was given to the homogenization of temperatures along the strip's width, to avoid defects common to this process, as reported by Spitzer & Schwerdtfeger⁽¹⁾. To achieve such homogenization, the distribution of the flow along the width of the reservoir in the exit zone is crucial. The existence of very low velocities near the lateral or side-walls will lead to earlier solidification in these regions and contribute to the occurrence of defects. Therefore, the most important parameters of the process were studied and their relative influence in the fluid flow/heat transfer evaluated.

4.1 EFFECT OF FLOWRATE AND BELT SPEED

The standard conditions for the parametric studies are given in Table 4-1. These conditions were determined from earlier studies⁽²⁾.

The standard flowrate considered was 252 Tph/m width or 113,7 Tph for the standard width of 0.45 m. This level of productivity would match the current levels in the conventional continuous casting machines.

To have the metal laid down onto the belt in an isokinetic manner, as described in the literature survey, a belt speed of 1 m/s is necessary for this standard case, and therefore, an inlet velocity of 0.333 m/s was applied uniformly throughout the width of the inlet zone.

Table 4-1 Standard casting conditions for the model simulations.

Symbol	Description	Standard Value
d_e	Exit gap	10 mm
h_m	Metal head	7 cm
l	Reservoir length	10 cm
W	Reservoir width	45 cm
L_d	Computational domain length	40 cm
b_w	Back wall height	4 cm
S_h	Inlet superheat	10° C
nz	Front wall thickness	2.5 cm
v_b	Belt speed	1 m/s

The flowrate, belt speed and inlet velocity were modified accordingly, for the belt speeds of 0.8 and 0.5 m/s. The reason for choosing such values was that 1 m/s would be the maximum operational speed envisaged for the horizontal belt caster currently being developed at Lulea, Sweden. Also, the use of lower belt speeds will reduce the total necessary length of the cooling belt, since the residence time of the metal on the belt will increase.

4.1.1 FLUID FLOW RESULTS

Figures 4-1 to 4-3 show a transverse cut of the fluid flow predicted in the delivery system. It is seen, for all three cases, that a recirculation zone extends from the back wall to around 5.5 cm from the meniscus. The cuts were made on the symmetry axis. For belt speeds of 1 and 0.8 m/s (cases 1 and 2, respectively), almost no visible difference was observed. For case 3, the recirculation zone is a little larger (belt speed = 0.5 m/s), owing to the lower inlet momentum. Also, from the recirculated mass, a bigger portion is carried up in case 3, because of the weaker drag force of the belt. That could contribute to freezing on the region next to the meniscus.

Another region of interest is in the neighbourhood of the front wall. As expected, the velocities in that region are low for cases 1 and 2 and very low, for case 3. This leads to the formation of a dead zone, which would significantly affect the temperature profile, as will be discussed later.

At the exit gap entrance, less turbulence was observed for case 3, due to the reduced metal velocity. The same is true for the region of the strip in the delivery system. Figures 4-4 to 4-6 show a cut of the turbulent kinetic energy at the

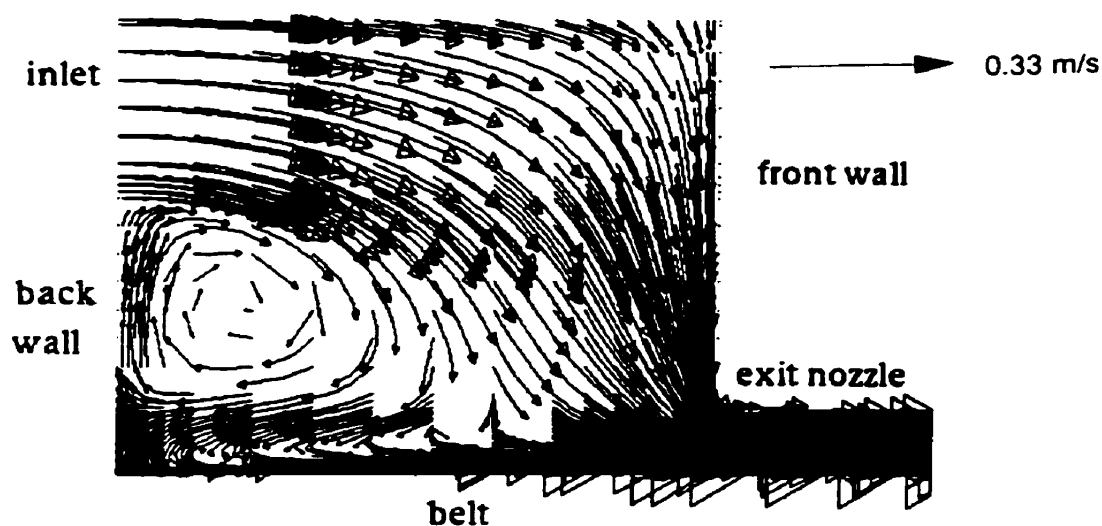


Figure 4-1. Vertical cut along the symmetry axis - Belt speed = 1 m/s ($d_g = 10$ mm, $h_m = 7$ cm, $l = 10$ cm, $Q = 4.5 \times 10^{-3}$ m³/s).

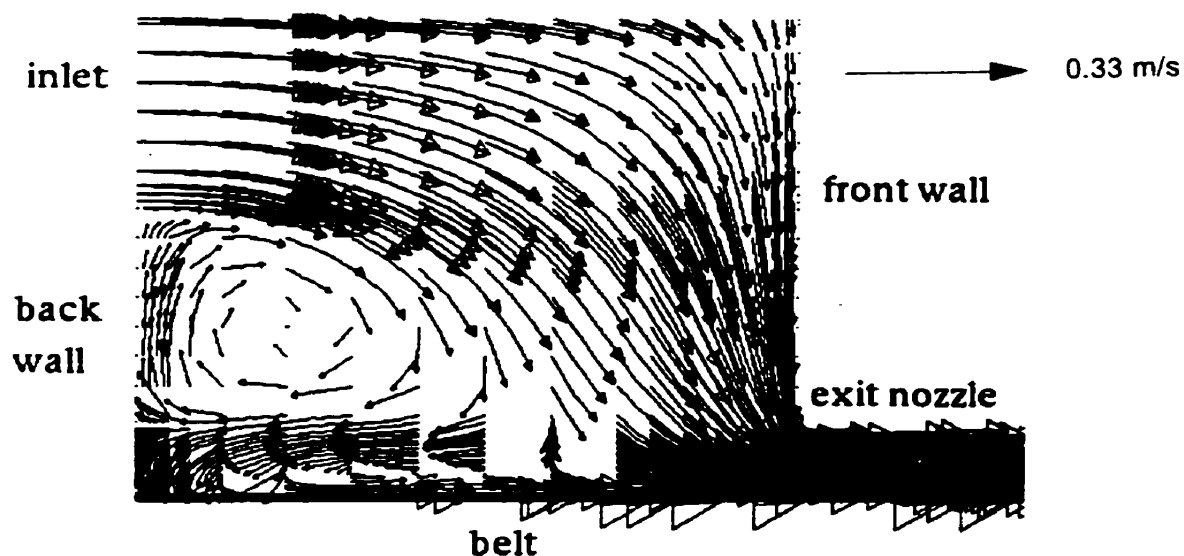


Figure 4-2. Vertical cut along the symmetry axis - Belt speed = 0.8 m/s ($d_g = 10$ mm, $h_m = 7$ cm, $l = 10$ cm, $Q = 3.6 \times 10^{-3}$ m³/s).

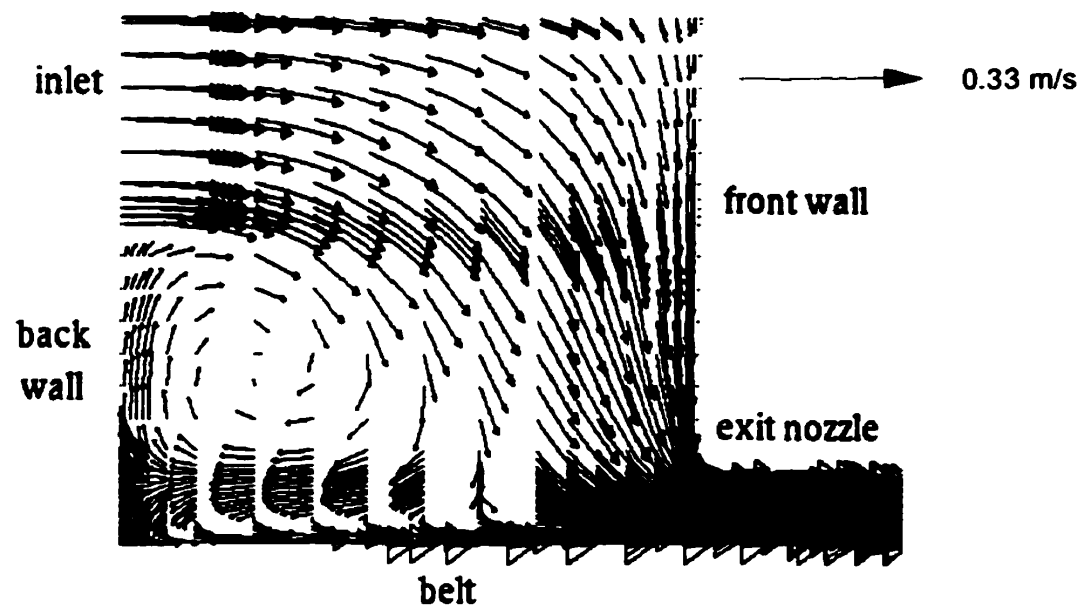


Figure 4-3. Vertical cut along the symmetry axis - Belt speed = 0.5 m/s ($d_g = 10$ mm, $h_m = 7$ cm, $l = 10$ cm, $Q = 2.25 \times 10^{-3}$ m³/s).

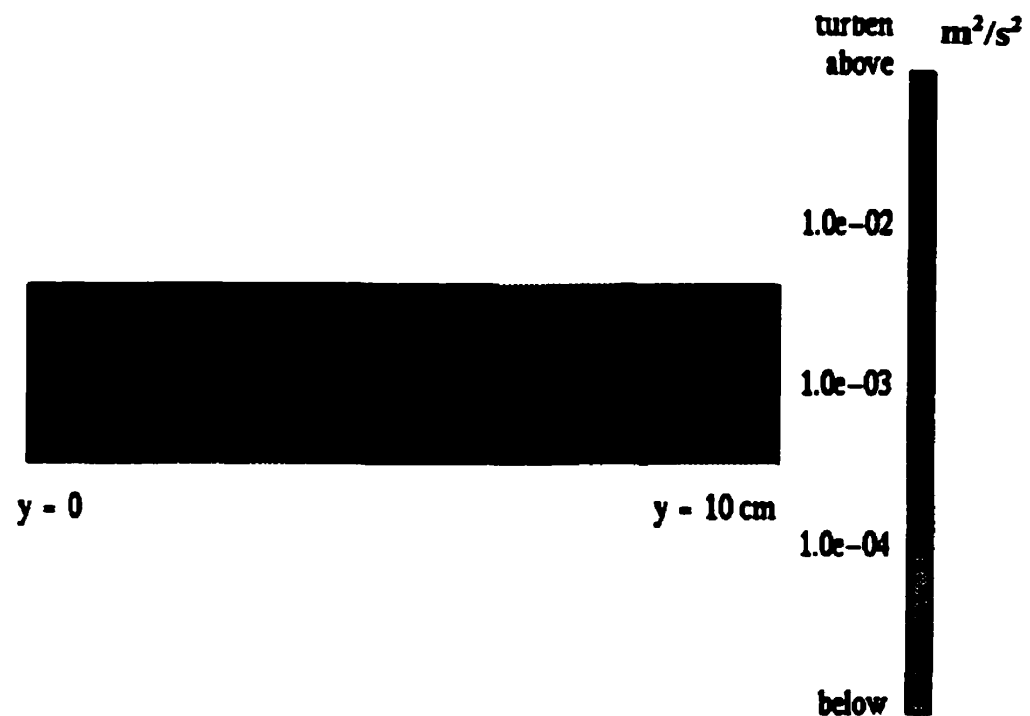


Figure 4-4. Turbulent kinetic energy at the symmetry axis - Belt speed = 1 m/s ($d_g = 10$ mm, $h_m = 7$ cm, $l = 10$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

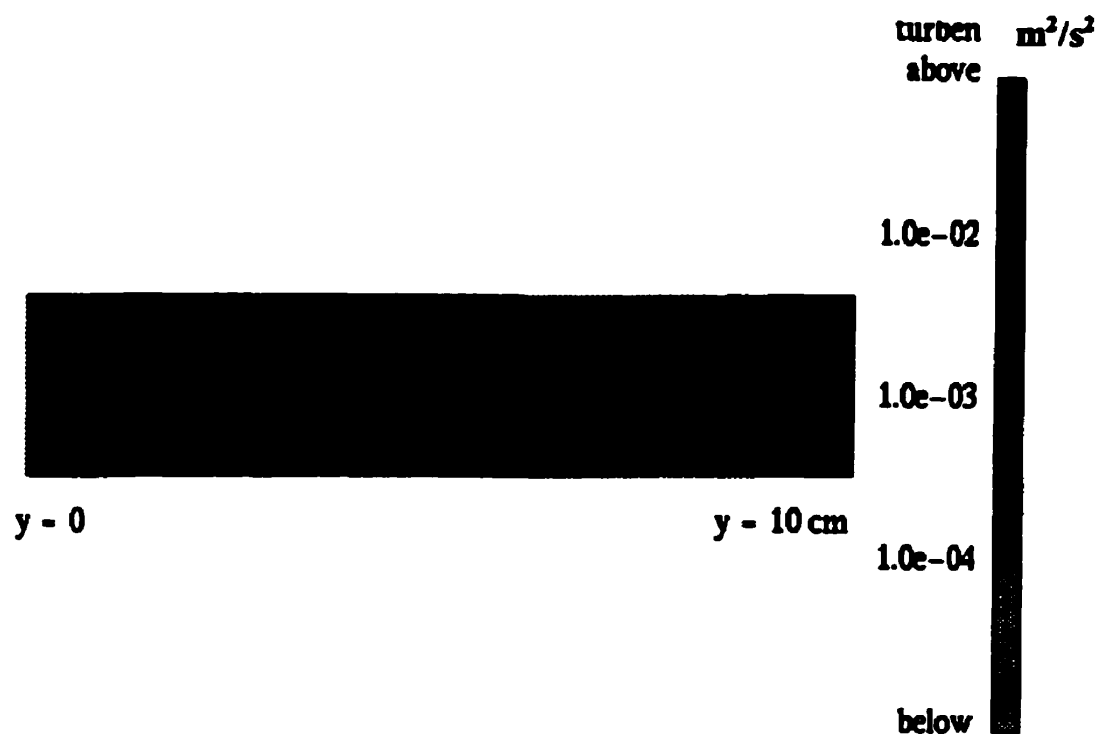


Figure 4-5. Turbulent kinetic energy at the symmetry axis - Belt speed = 0.8 m/s ($d_g = 10$ mm, $h_m = 7$ cm, $l = 10$ cm, $Q = 3.6 \times 10^{-3} \text{ m}^3/\text{s}$)

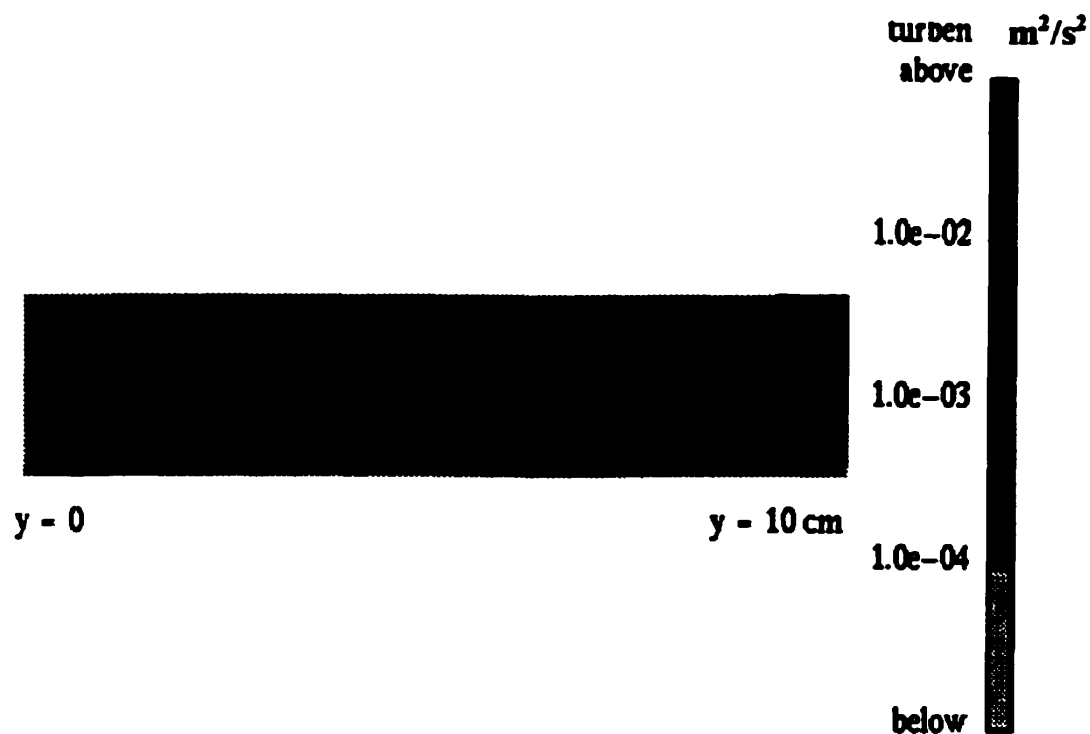


Figure 4-6. Turbulent kinetic energy at the symmetry axis - Belt speed = 0.5 m/s ($d_g = 10$ mm, $h_m = 7$ cm, $l = 10$ cm, $Q = 2.25 \times 10^{-3} \text{ m}^3/\text{s}$)

symmetry axis, in the region of the reservoir corresponding to the thickness of the strip. An increase in turbulence in the region of the strip close to the nozzle entrance was also observed, due to significant flow contraction.

In addition, case 2 (belt speed = 0.8 m/s) shows an increased homogeneity in the turbulent energy throughout the region of the strip in the delivery system. In case 1, the velocity of the belt is very high, creating a region of high turbulence, between 1 and 6 cm from the meniscus. On the other hand, in case 3, the metal arrives at the belt with low momentum, causing a situation where turbulence is high in the metal just above the belt and low on the top of the strip. Uniformity in the turbulent flow is believed important in determining the quality of the strips produced in this kind of delivery configuration⁽³⁾. Strips originated from flows with lower and more homogeneous levels of turbulence are expected to have better surface quality and present fewer defects⁽⁴⁾.

Finally, it is clearly seen that the drag force due to the belt's movement is responsible for increasing the turbulence level in the region of the strip. To confirm this finding, a simulation was run with no belt movement and the same inlet flow. Figure 4-7 shows the kinetic energy associated with the flow. As suspected, much lower levels of turbulence were found for the case of the static belt.

4.1.2 HEAT TRANSFER AND SOLIDIFICATION RESULTS

Figures 4-8 to 4-10 show the existence of a cold region near the front wall. This cold region results from the very low velocities of the fluid, as shown before. It is shown that, as the inlet velocity is reduced, the extension and intensity of the

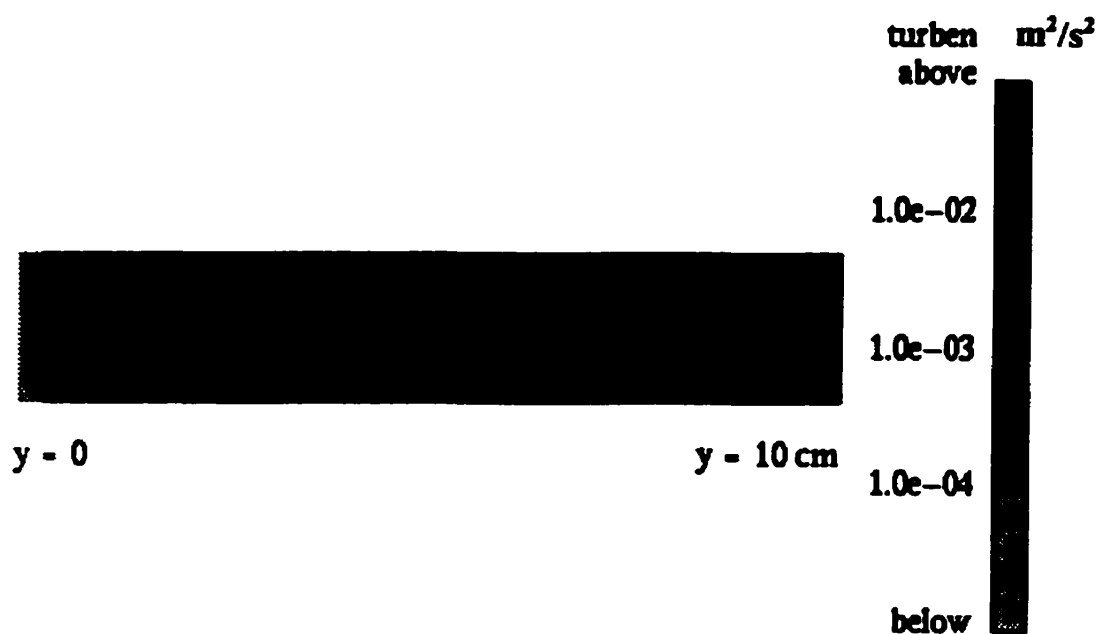


Figure 4-7. Turbulent kinetic energy at the symmetry axis - Static belt ($d_s = 10$ mm, $h_m = 7$ cm, $l = 10$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

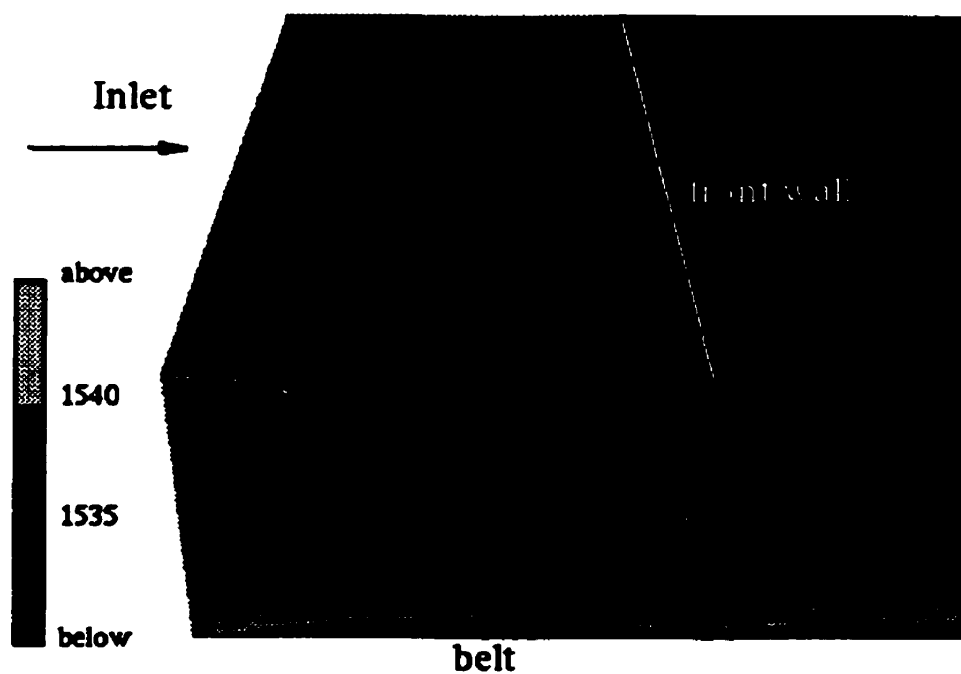


Figure 4-8. Temperature profile in the reservoir - Detail of the top edge - Belt speed = 1 m/s ($d_s = 10$ mm, $h_m = 7$ cm, $l = 10$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

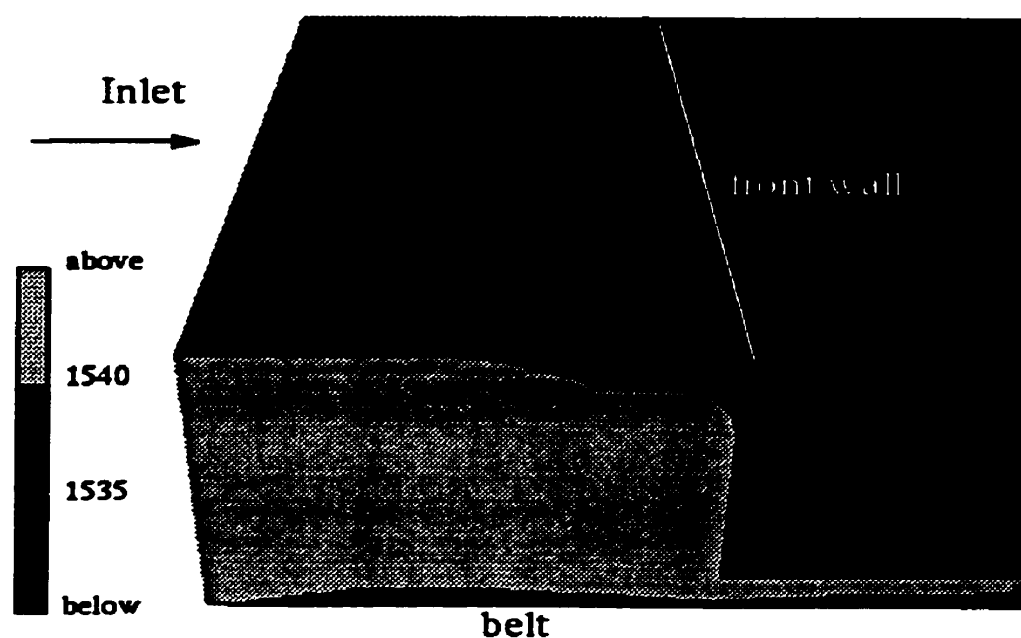


Figure 4-9. Temperature profile in the reservoir - Detail of the top edge - Belt speed = 0.8 m/s ($d_g = 10$ mm, $h_m = 7$ cm, $l = 10$ cm, $Q = 3.6 \times 10^{-3}$ m³/s)

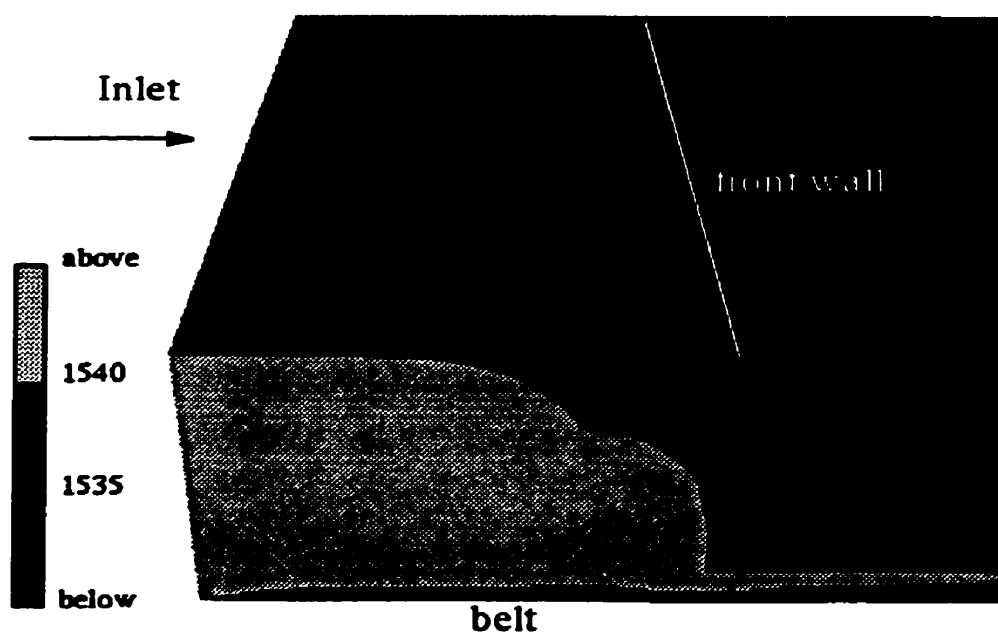


Figure 4-10. Temperature profile in the reservoir - Detail of the top edge - Belt speed = 0.5 m/s ($d_g = 10$ mm, $h_m = 7$ cm, $l = 10$ cm, $Q = 2.25 \times 10^{-3}$ m³/s)

cold zone increases. For case 1, the cold zone (between 1535 and 1540°C) occupies a region up to 1 cm from the front wall. For case 3, there is even the occurrence of a mushy region ($T < 1535^{\circ}\text{C}$) adjacent to the front wall.

As can be observed from Figures 4-11 to 4-13, quite different temperature profiles along the symmetry axis, corresponding to the thickness of the strip, are predicted for the three cases considered. Due to the lower momentum and turbulence, the upper half of the strip in case 3 is completely hot: all temperatures in the region are above 1540°C. On the other hand, a little solidified shell is already seen, due to the increased residence time on the belt.

Because of the high belt speed in case 1, no solidified shell is visible up to the nozzle gap. However, the cold zone extends from the belt up to almost half the strip height in the reservoir, due to the higher turbulence. A comparison between the turbulent energy graphs and the temperature profiles shows a correspondence between them.

Near the nozzle gap, a reheating and remelting of the metal is always observed, caused by the strong momentum of the incoming flow in that direction.

A higher speed will increase the necessary length of the belt. Figures 4-14 to 4-16 present the temperature profile for the final 10 cm of the domain. It is seen that for case 3, the lowest belt velocity, there is very little liquid beyond about 40 cm of the belt, while for the other cases, at least the upper half of the strip is still liquid after 40 cm. Furthermore, the solidified shell thickness is considerably higher in case 3, as will be discussed below.

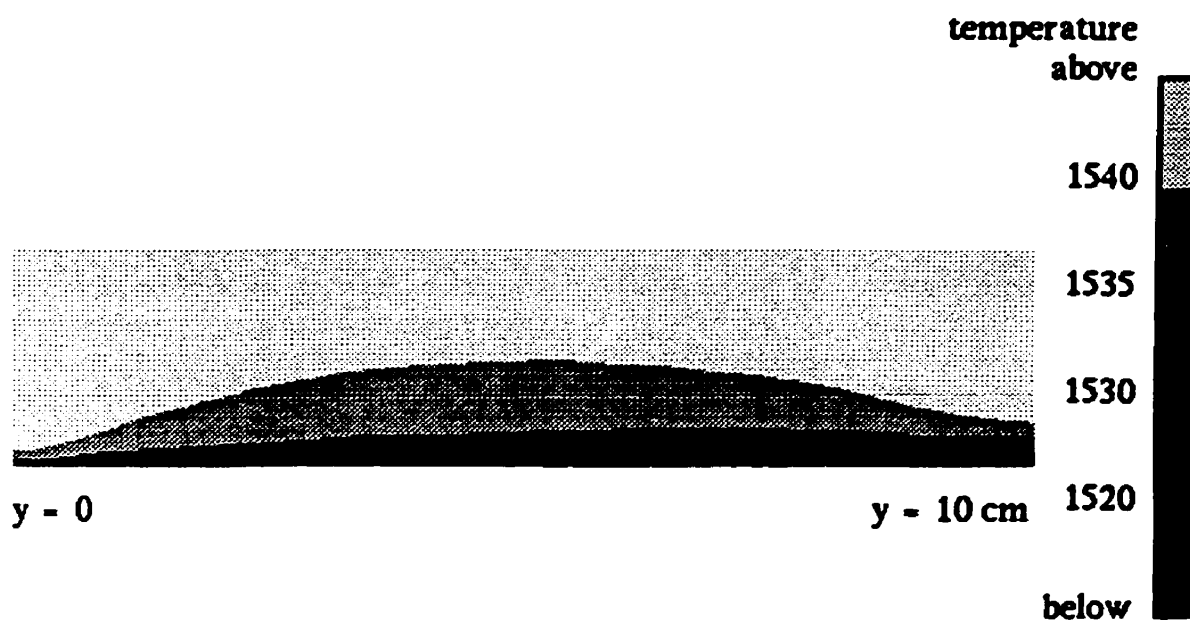


Figure 4-11. Temperature profile at the symmetry axis in the region of the strip inside the reservoir - Belt speed = 1 m/s ($d_g = 10$ mm, $h_m = 7$ cm, $l = 10$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

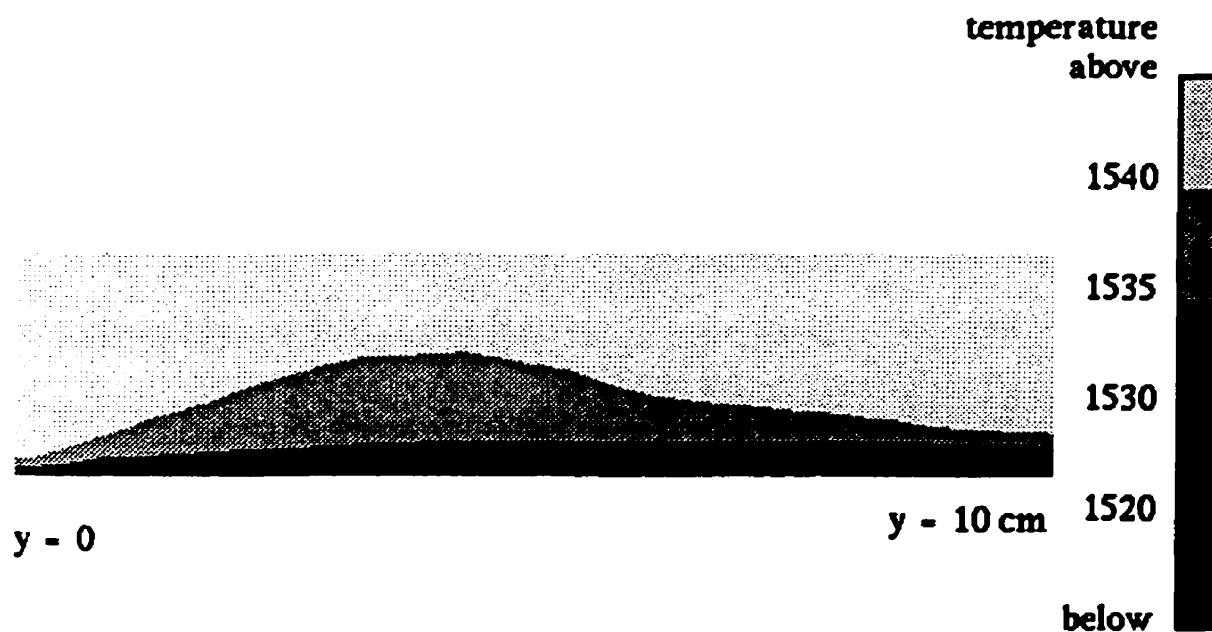


Figure 4-12. Temperature profile at the symmetry axis in the region of the strip inside the reservoir - Belt speed = 0.8 m/s ($d_g = 10$ mm, $h_m = 7$ cm, $l = 10$ cm, $Q = 3.6 \times 10^{-3}$ m³/s)

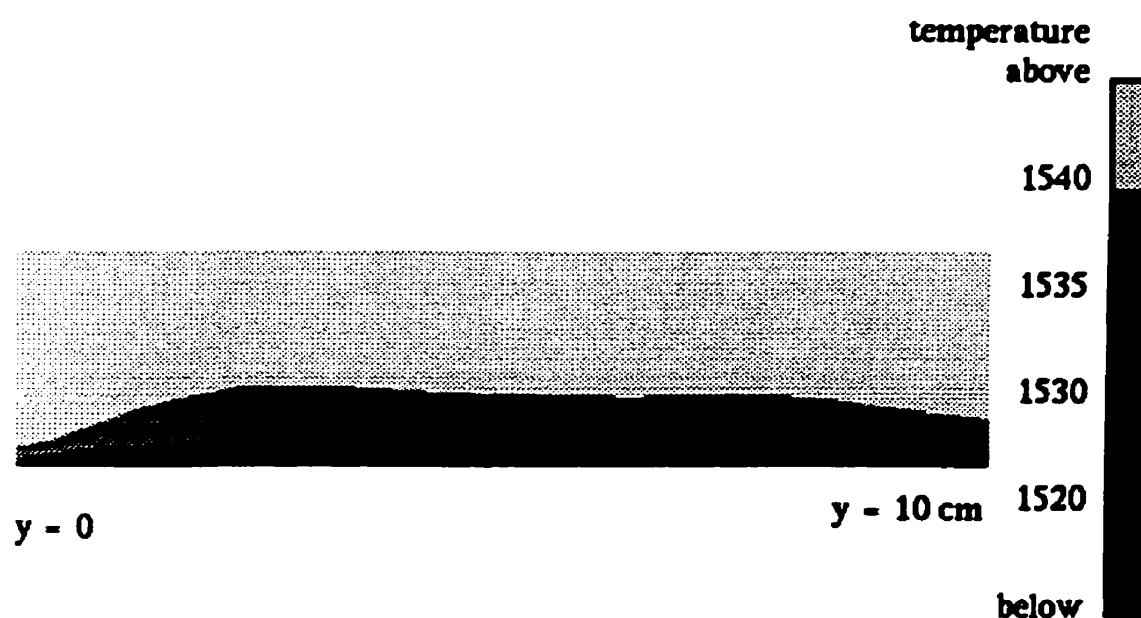


Figure 4-13. Temperature profile at the symmetry axis in the region of the strip inside the reservoir - Belt speed = 0.5 m/s ($l = 10$ cm, $d_g = 10$ mm, $h_m = 7$ cm, $Q = 2.25 \times 10^{-3}$ m³/s)

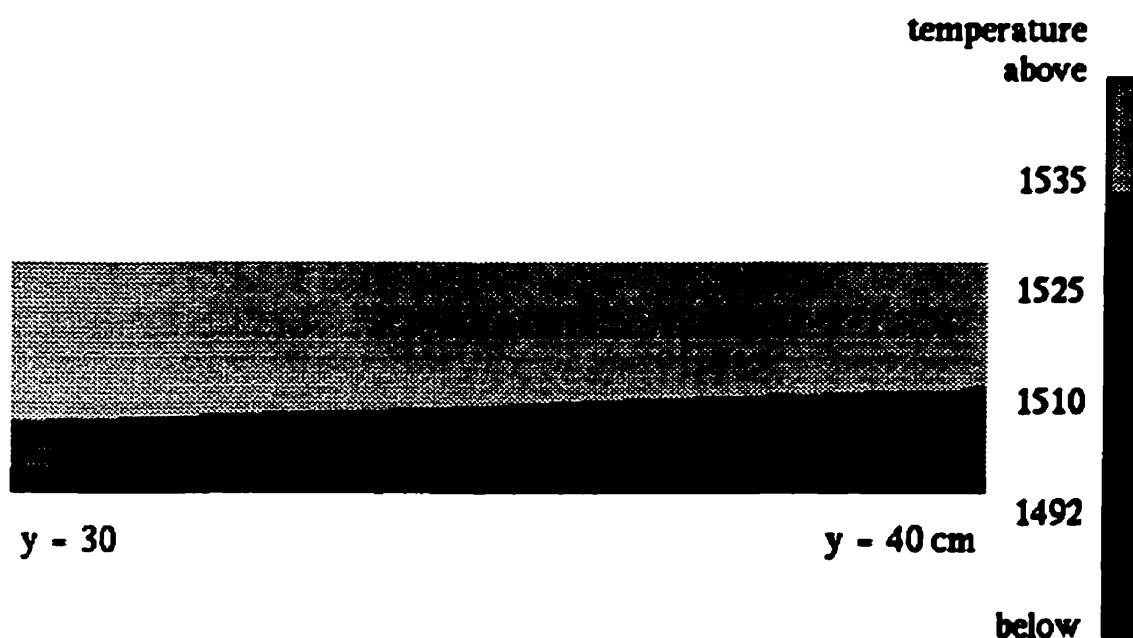


Figure 4-14. Temperature profile at the symmetry axis in the final 10 cm of the domain - Belt speed = 1 m/s ($l = 10$ cm, $d_g = 10$ mm, $h_m = 7$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

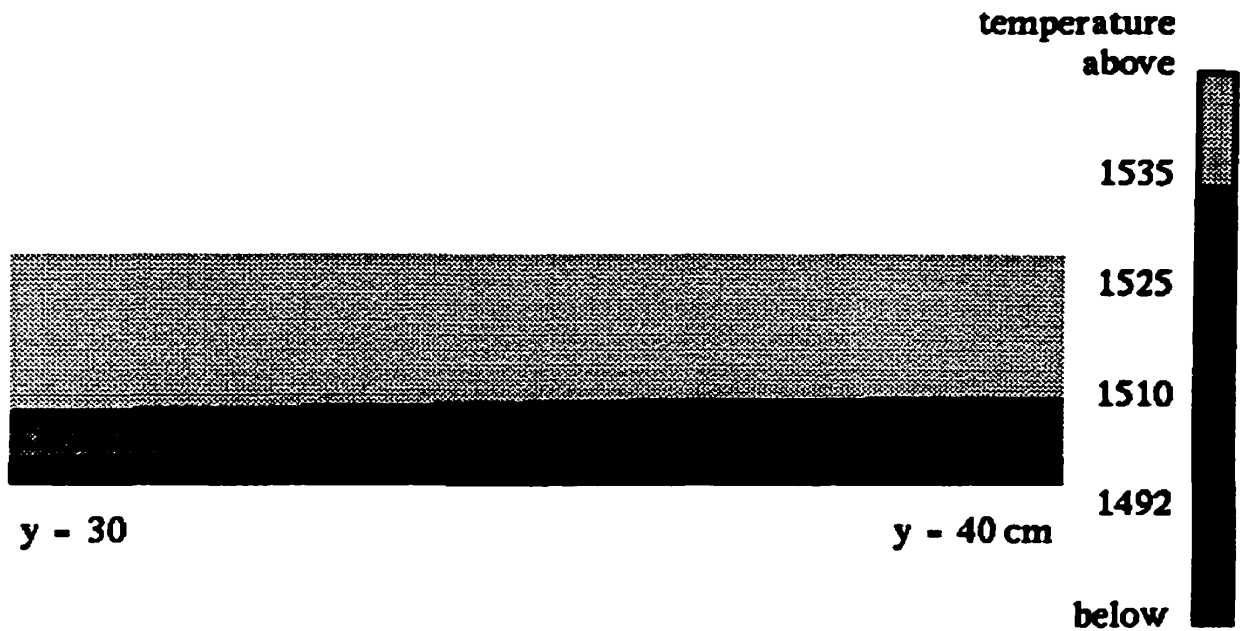


Figure 4-15. Temperature profile at the symmetry axis in the final 10 cm of the domain - Belt speed = 0.8 m/s ($d_g = 10$ mm, $h_m = 7$ cm, $l = 10$ cm, $Q = 3.6 \times 10^{-3}$ m³/s)

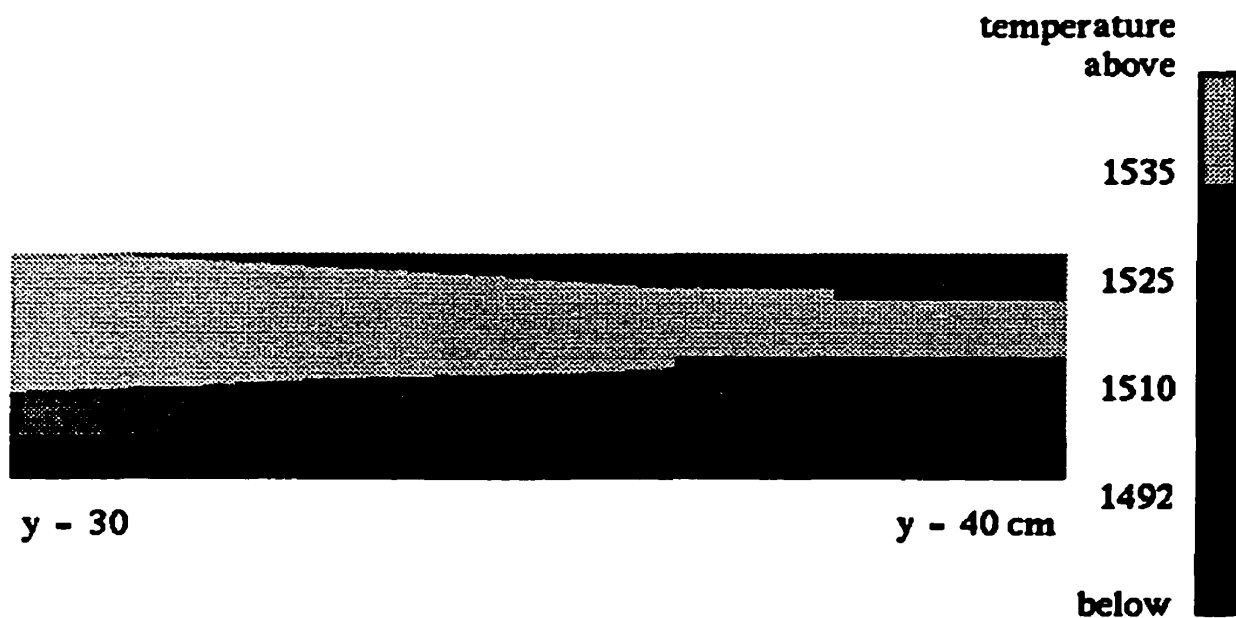


Figure 4-16. Temperature profile at the symmetry axis in the final 10 cm of the domain - Belt speed = 0.5 m/s ($d_g = 10$ mm, $h_m = 7$ cm, $l = 10$ cm, $Q = 2.25 \times 10^{-3}$ m³/s)

It is clearly shown in Figures 4-17 to 4-19 that the solidified fraction increases with the decrease in belt speed. After 40 cm of the belt, there was about 1.3 mm of the strip with a solid fraction, f_s , higher than 0.4 for case 1, around 1.5 mm for case 2 and approximately 2 mm for case 3. The differences in the solidified shell were: 1.1, 1.3 and 1.7 mm, respectively for the three cases. The thin solidified shells anticipate the need for relatively lengthy belts, in the range of approximately 15-30 m, for these levels of velocity. In this rough calculation, cooling from the top is not considered, so these values will be higher than actual ones. Furthermore, the calculation is based on a constant value for the heat transfer coefficient between the belt and the strip. It will also be seen later that the imposition of a transient evolution for h may significantly change the temperature profile and, consequently, the thickness of the solid shell.

4.2 EFFECT OF THE EXIT GAP

Following the standard conditions given in Table 4-1, the thickness of the strip was varied, from 10 mm, to 8 and 4 mm. The idea behind this study was not only to investigate the effects of the exit gap reduction in the fluid flow/heat transfer, but to also set a starting point from which the eventual necessary improvements would be suggested. For cases 4 and 5, corresponding to the thinner 8 and 4 mm strips, the inlet flowrate was reduced accordingly.

4.2.1 FLUID FLOW RESULTS

It is seen in Figures 4-20 e 4-21 that the flowrate reductions corresponding to thinner strips have a similar influence on the flow pattern as for cases 2 and 3. Little change, if any, is observed between cases 1 and 4. For case 5, the slight

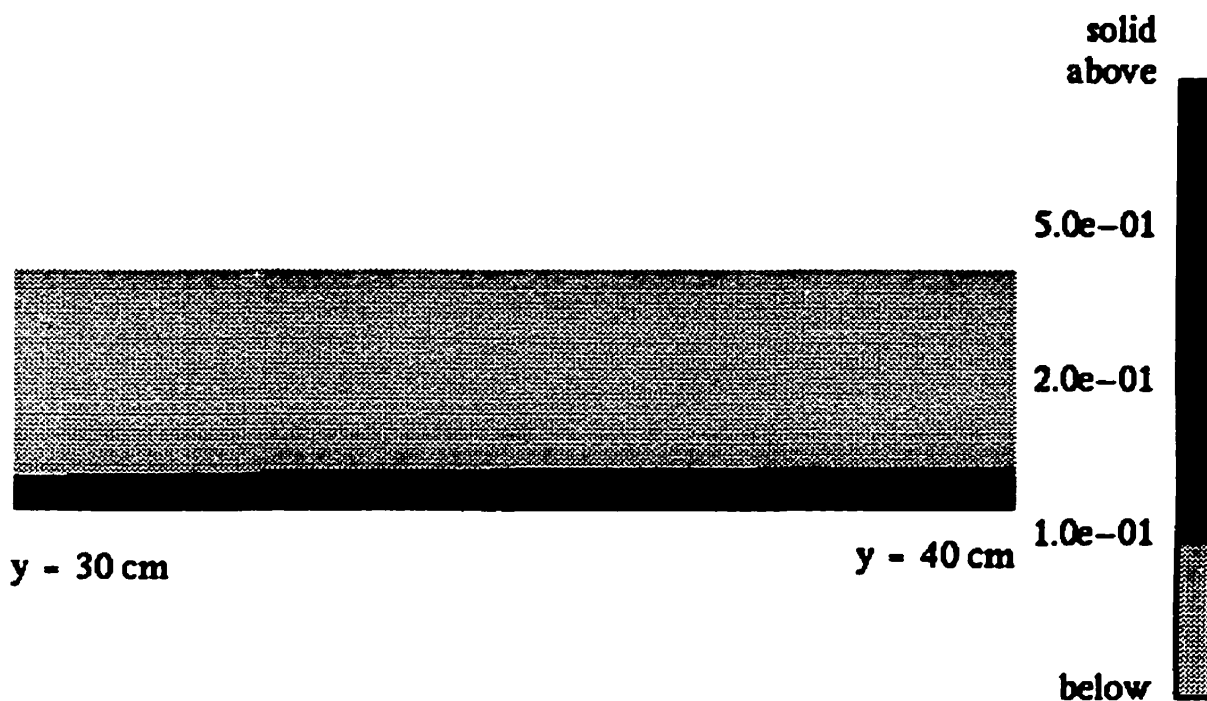


Figure 4-17. Solid fraction at the symmetry axis in the final 10 cm of the domain
 Belt speed = 1 m/s ($d_g = 10$ mm, $h_m = 7$ cm, $l = 10$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

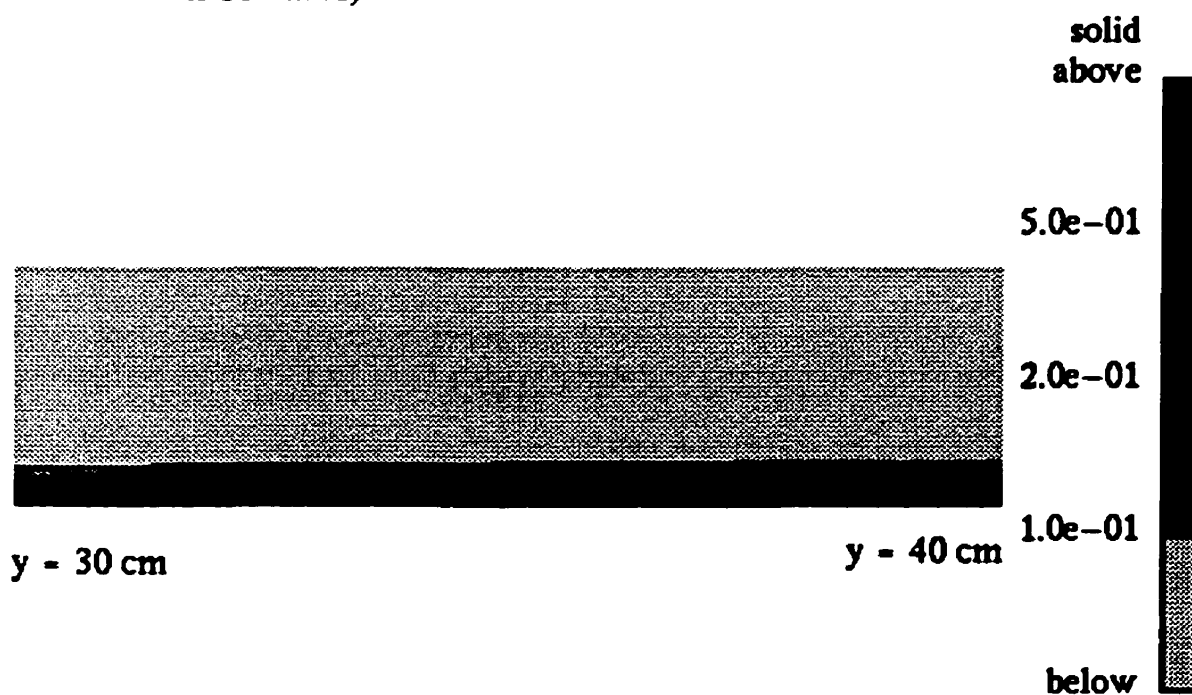


Figure 4-18. Solid fraction at the symmetry axis in the final 10 cm of the domain
 Belt speed = 0.8 m/s ($d_g = 10$ mm, $h_m = 7$ cm, $l = 10$ cm, $Q = 3.6 \times 10^{-3}$ m³/s)

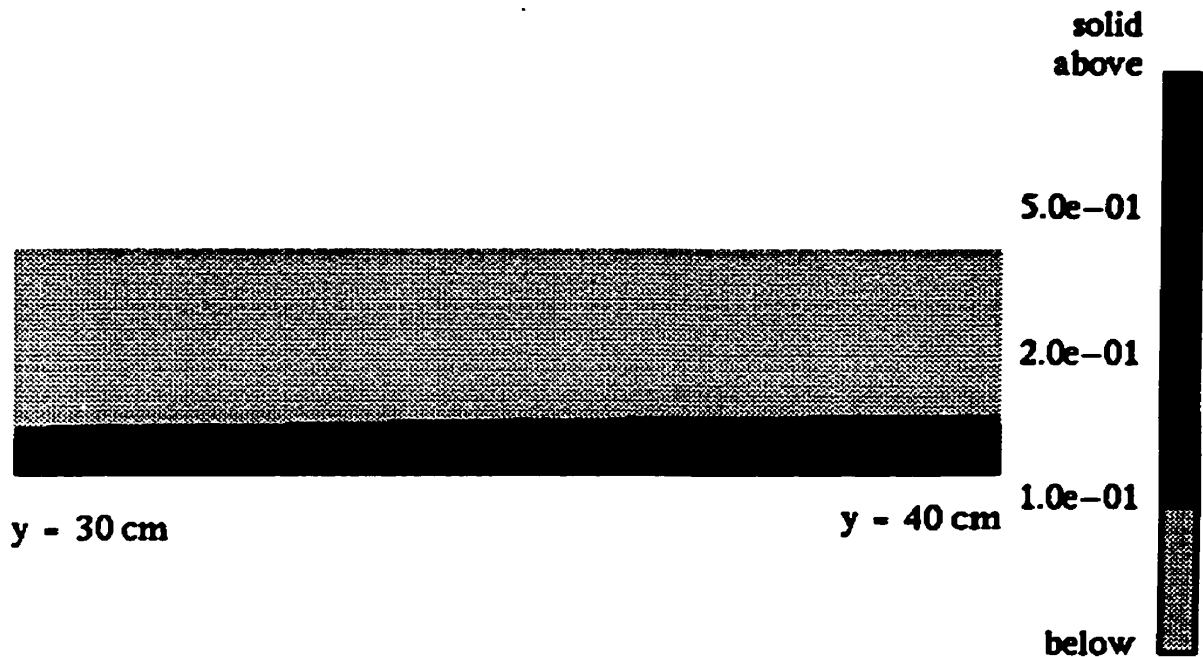


Figure 4-19. Solid fraction at the symmetry axis in the final 10 cm of the domain
 Belt speed = 0.5 m/s ($d_g = 10$ mm, $h_m = 7$ cm, $l = 10$ cm, $Q = 2.25 \times 10^{-3}$ m³/s)

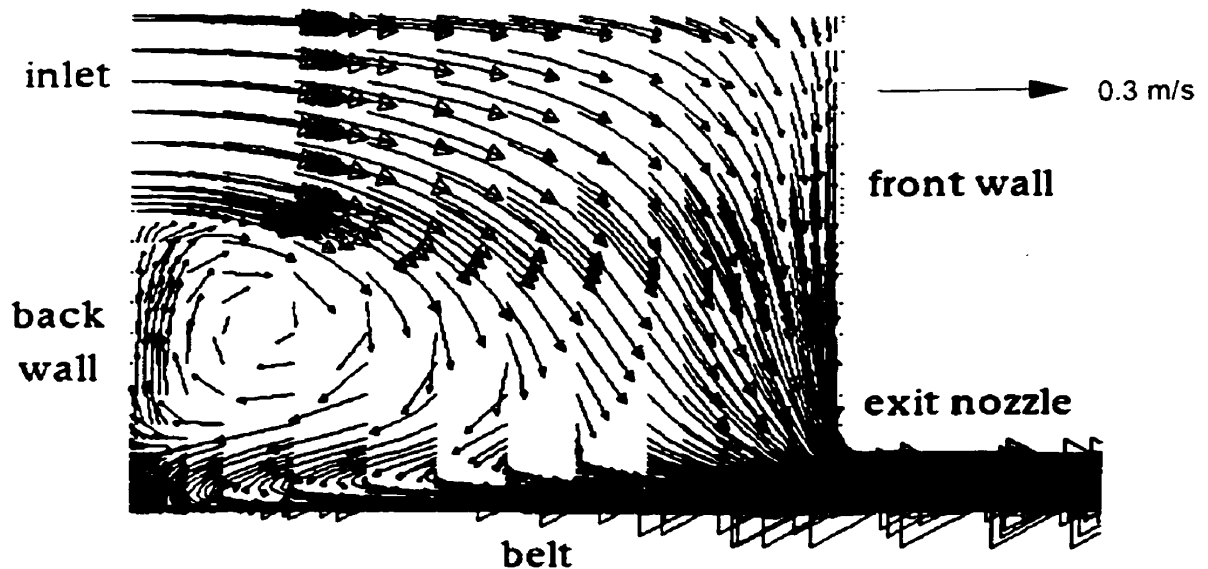


Figure 4-20. Vertical cut along the symmetry axis - Exit gap = 8 mm ($v_b = 1$ m/s, $h_m = 7$ cm, $l = 10$ cm, $Q = 3.6 \times 10^{-3}$ m³/s)

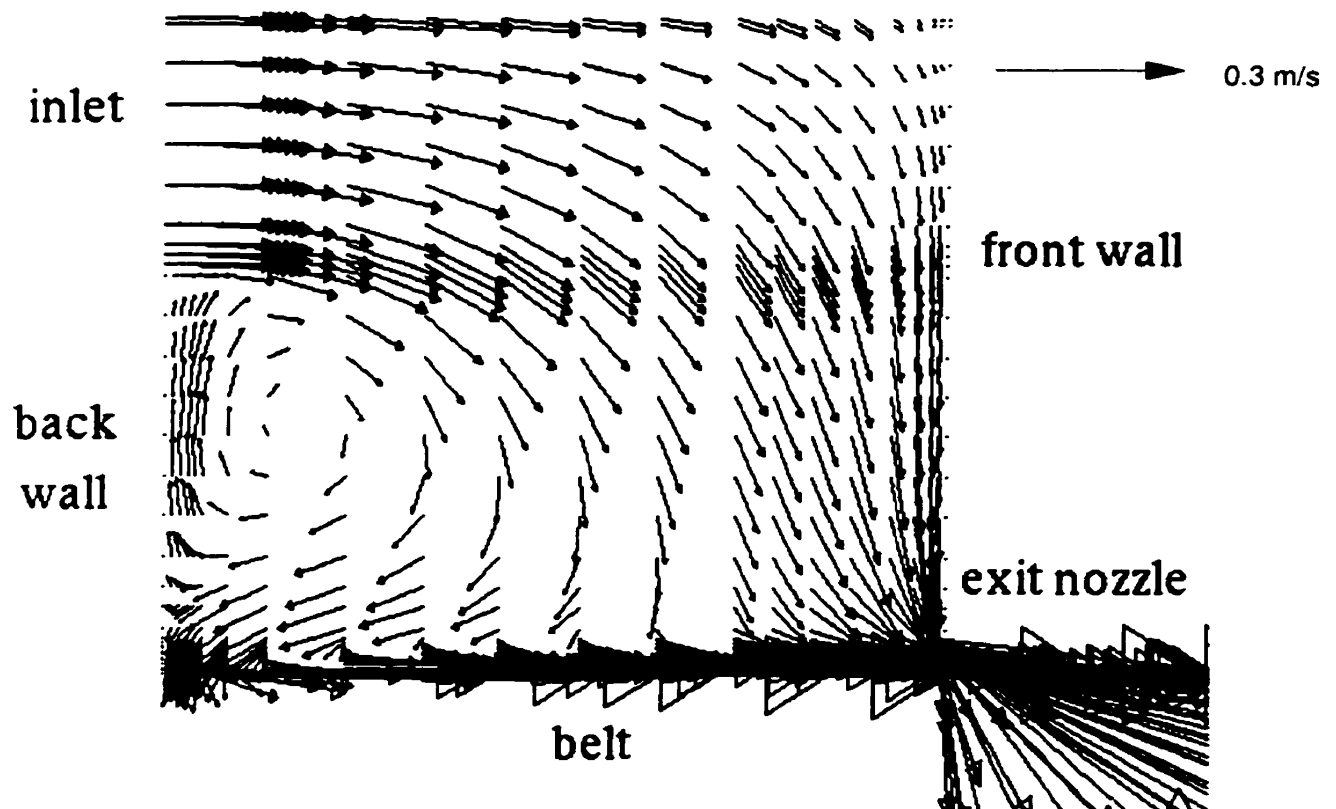


Figure 4-21. Vertical cut along the symmetry axis - Exit gap = 4 mm ($v_b = 1$ m/s , $h_m = 7$ cm, $l = 10$ cm, $Q = 1.8 \times 10^{-3}$ m³/s)

increase in the recirculation zone is again observed, due to the weaker inlet momentum. However, the recirculatory flow zone next to the meniscus is the same as cases 1 and 5. This is due to the adoption of the same belt speed for all cases.

Figures 4-22 and 4-23 confirm the importance of belt speed on turbulence levels, at the region of the forming strip. The pattern is not very different for case 5. When the flow reaches the belt, its weaker momentum causes a weaker reaction from the belt drag force. Comparing Figure 4-23 with Figure 4-6, representing a higher flowrate, with a lower belt speed, one can readily observe that the inlet flowrate does not determine turbulence levels in the strip region as much as the belt speed. It is also noticed that at entrance of the exit nozzle, the turbulent kinetic energy is higher for the 4-mm gap, owing to the more abrupt contraction in the flow. This increase in κ is relevant, since the flow passage through the exit nozzle will ultimately determine how even the thickness of the strip will be, as will be seen later.

4.2.2 ROLE OF THE BELT IN THE TURBULENCE PROFILE

The following conclusions were drawn from the simulation of the metal fluid flow onto a static belt:

- i) A recirculation zone is present for static belts, and is larger than those for moving belts, since the metal is not drawn into the developing boundary layer of the moving belt. The flow then separates around 7 cm from the meniscus;
- ii) For most of the strip region within the reservoir, especially the region in contact with the belt, the turbulent kinetic energy for a static belt condition is roughly one order of magnitude less than in the previous moving belt case (see Figure 4-7).

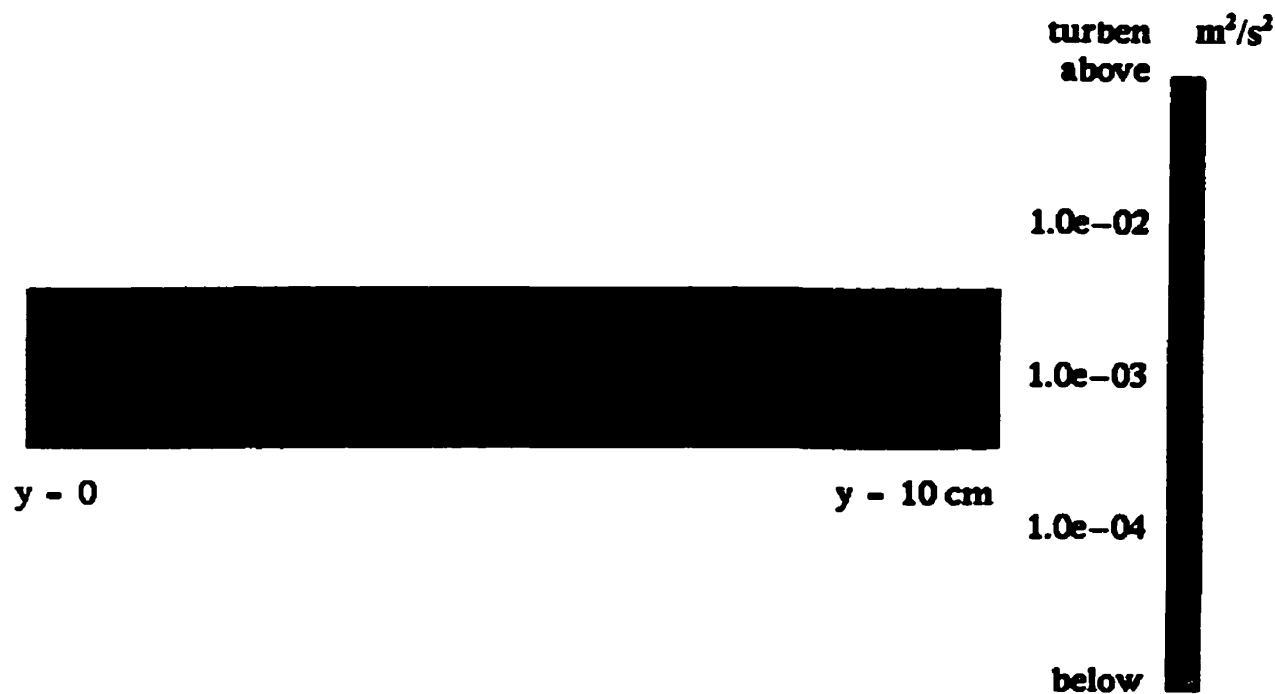


Figure 4-22. Turbulent kinetic energy at the symmetry axis - Exit gap = 8 mm
($v_b = 1$ m/s, $h_m = 7$ cm, $l = 10$ cm, $Q = 3.6 \times 10^{-3}$ m^3/s)

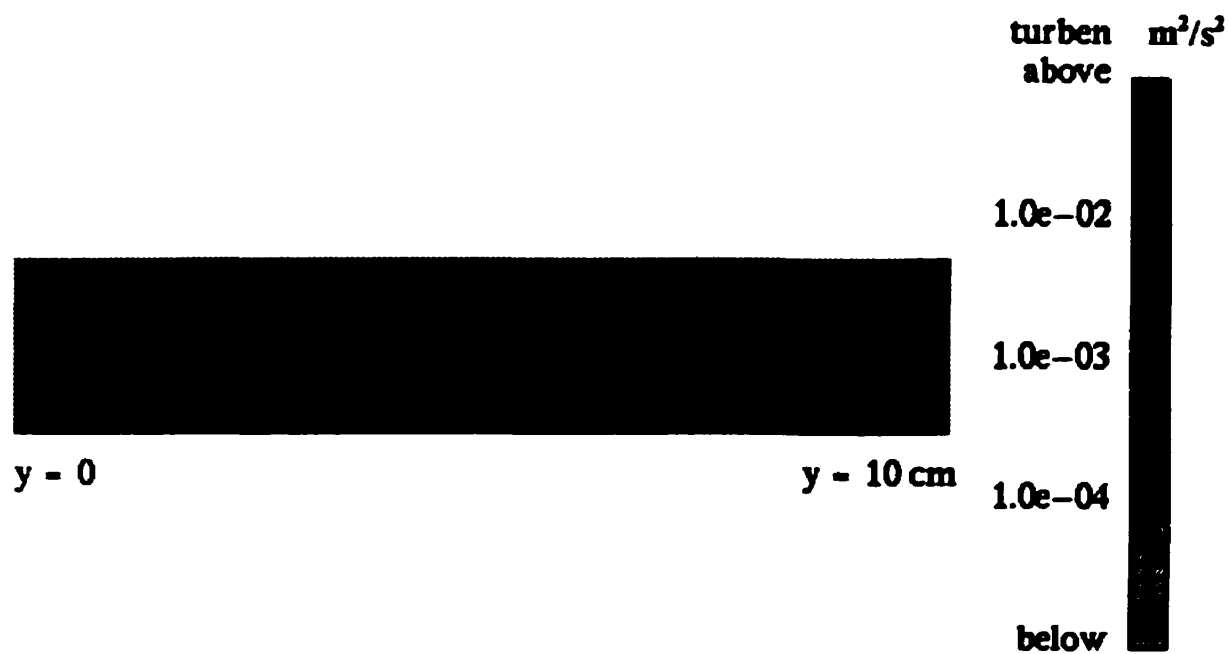


Figure 4-23. Turbulent kinetic energy at the symmetry axis - Exit gap = 4 mm
($v_b = 1$ m/s, $h_m = 7$ cm, $l = 10$ cm, $Q = 1.8 \times 10^{-3}$ m^3/s)

Therefore, the importance of the belt movement in the turbulence is once again highlighted.

4.2.3 HEAT TRANSFER AND SOLIDIFICATION RESULTS

The temperature profiles in the reservoir are shown in Figures 4-8, 4-24 and 4-25, for strip thicknesses of 10, 8 and 4 mm, respectively. Here, similarly to the case of low belt speed (case 3), a relatively large region of mushy and even solid material appears near the front wall, for an exit gap of 4 mm. For 4-mm thickness, there is a relatively cold region ($T < 1537^{\circ}\text{C}$) extending up to the nozzle entrance, which could create interference with the incoming flow, and cause sticking and skulls at the triple point entry meniscus. At this point, one should recall that these simulations assume adiabatic conditions at the back and lateral walls. Therefore, in reality, the situation would be even worse than shown.

To attribute accurate values for the heat losses to the walls, in-plant measurements are necessary, but such data are currently unavailable. The type of refractory brick and its thermal properties will determine the total heat losses. Some simulations taking into account approximate heat losses to the wall will be presented later.

Figures 4-8, 4-24 and 4-25 show that while turbulence levels in the region of the strip thickness are mainly determined by belt speeds, the thermal profile in the delivery system is mostly influenced by entry flowrates.

The lateral temperature profiles in the symmetry axis, corresponding to the thickness of the strip in the reservoir, are given by Figures 4-26 and 4-27. No important qualitative changes are reported for case 4, whereas for the 4-mm gap,

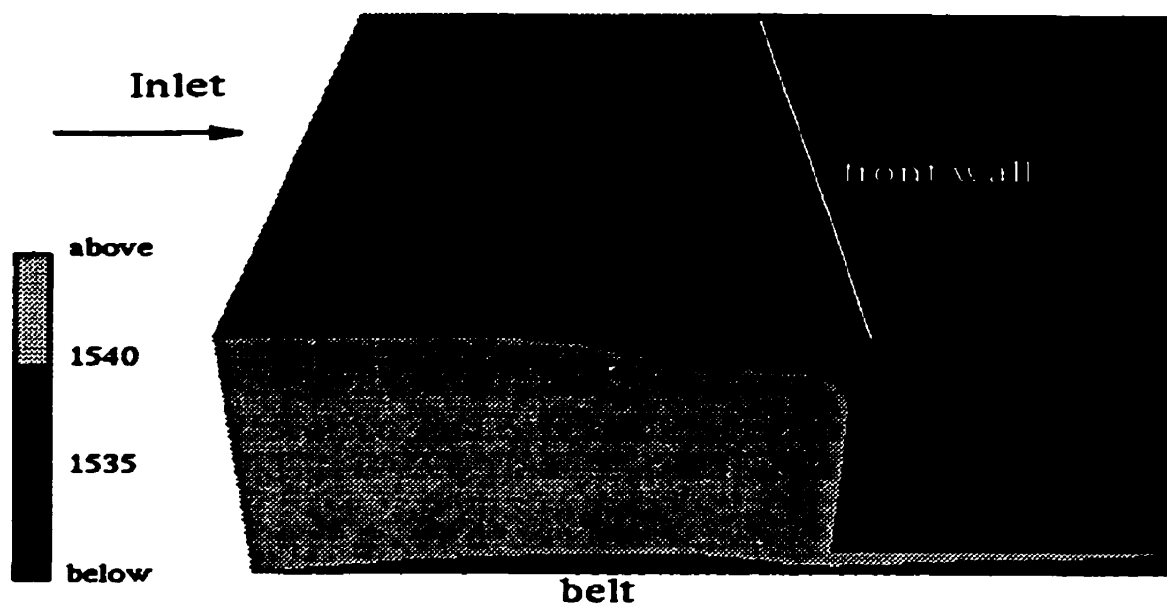


Figure 4-24. Temperature profile in the reservoir - Detail of the top edge - Exit gap = 8 mm ($v_b = 1$ m/s, $h_m = 7$ cm, $l = 10$ cm, $Q = 3.6 \times 10^{-3}$ m³/s)

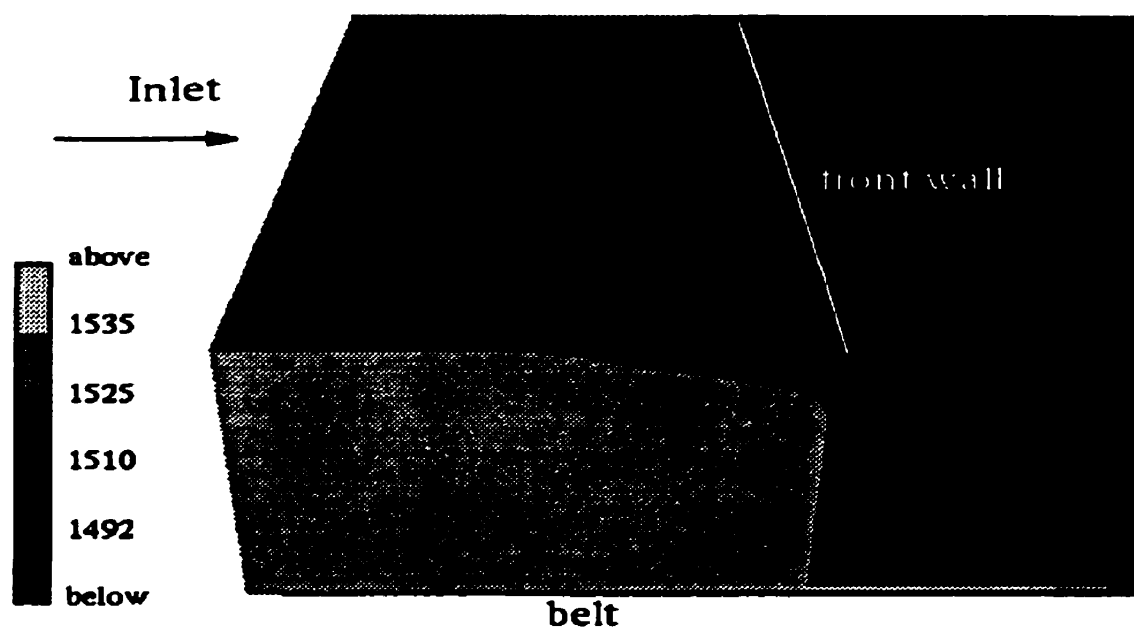


Figure 4-25. Temperature profile in the reservoir - Detail of the top edge - Exit gap = 4 mm ($v_b = 1$ m/s, $h_m = 7$ cm, $l = 10$ cm, $Q = 1.8 \times 10^{-3}$ m³/s)

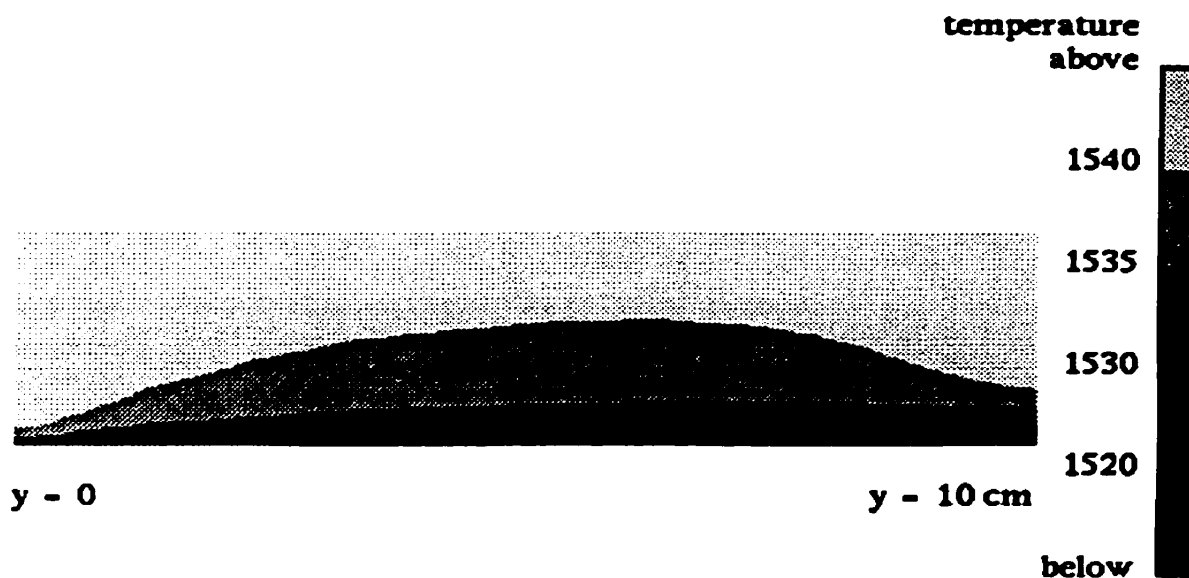


Figure 4-26. Temperature profile at the symmetry axis in the region of the strip inside the reservoir - Exit gap = 8 mm ($v_b = 1$ m/s, $h_m = 7$ cm, $l = 10$ cm, $Q = 3.6 \times 10^{-3}$ m³/s)

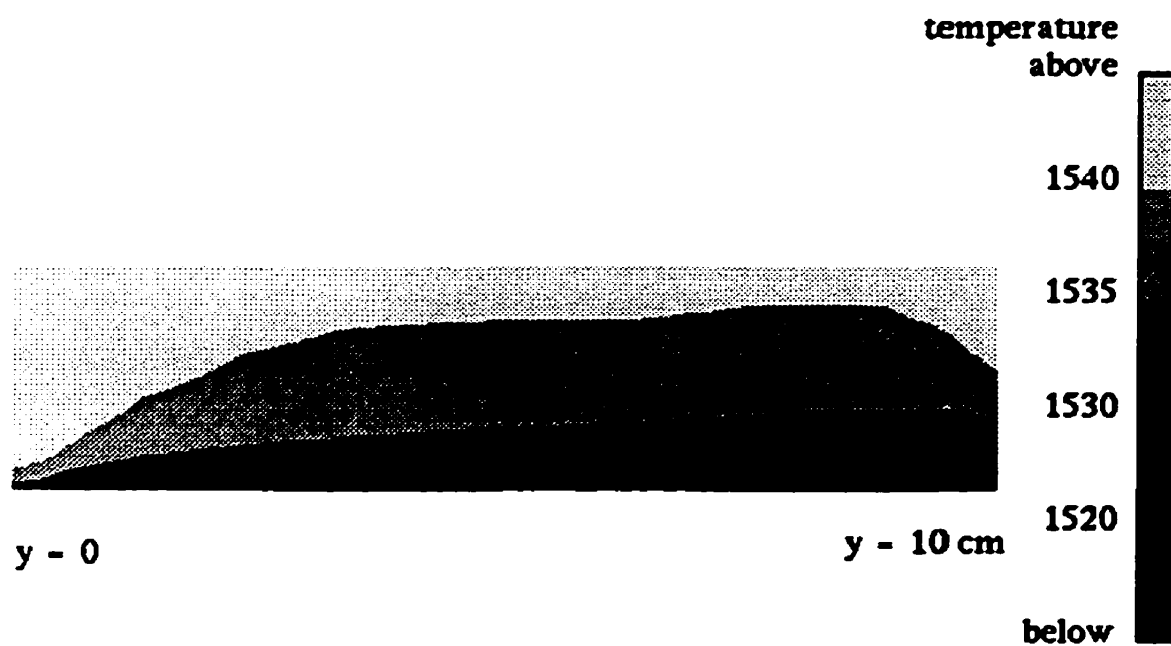


Figure 4-27. Temperature profile at the symmetry axis in the region of the strip inside the reservoir- Exit gap = 4 mm ($v_b = 1$ m/s, $h_m = 7$ cm, $l = 10$ cm, $Q = 1.8 \times 10^{-3}$ m³/s)

almost the whole strip thickness reaches temperatures below 1540°C and, just before the nozzle exit, the upper half is reheated to above 1540°C . Compared with the other cases, it is seen that in case 5 the reheating occurs later, about 1 cm before the exit gap. For case 4, the reheating starts at about 2.5 cm from the gap, and for case 1, in the middle of the pool length. This is a consequence of the incoming flow impacting the solidifying metal at different positions along the pool length, for the different exit gap thicknesses.

In Figure 4-28, there is little liquid metal at 40 cm from the meniscus (origin). On the edge of the 8-mm strip, the liquid vanished entirely. Also, Figure 4-29 shows that no more liquid metal exists over the last 10 cm of the domain, for the 4-mm gap. These are considerable changes from the standard conditions of 10-mm thickness, where more than half of the strip remains liquid after 40 cm.

As in all previous cases, it is observed that the last metal to leave the liquid state is situated closer to the top surface than to the bottom of the strip. This is explained by the application of different thermal boundary conditions to the two surfaces: on the bottom, there is a very high rate of initial heat extraction by sprays cooling the belt, whereas on the top surface the metal loses heat to the environment only by radiation and natural convection.

Similarly to cases 1 to 3, it is observed in Figures 4-30 and 4-31 that the solid fraction increases with flowrate decrease. Thus, for a strip thickness of 4 mm, the overall solid fraction is higher than for other cases, as would be expected. However, the solidified shell is the same (1.1 mm after 40 cm of belt). This can be explained by the low thermal conductivity of steel, which retards heat transfer to the mushy zone. Different solidified shells are expected for longer residence

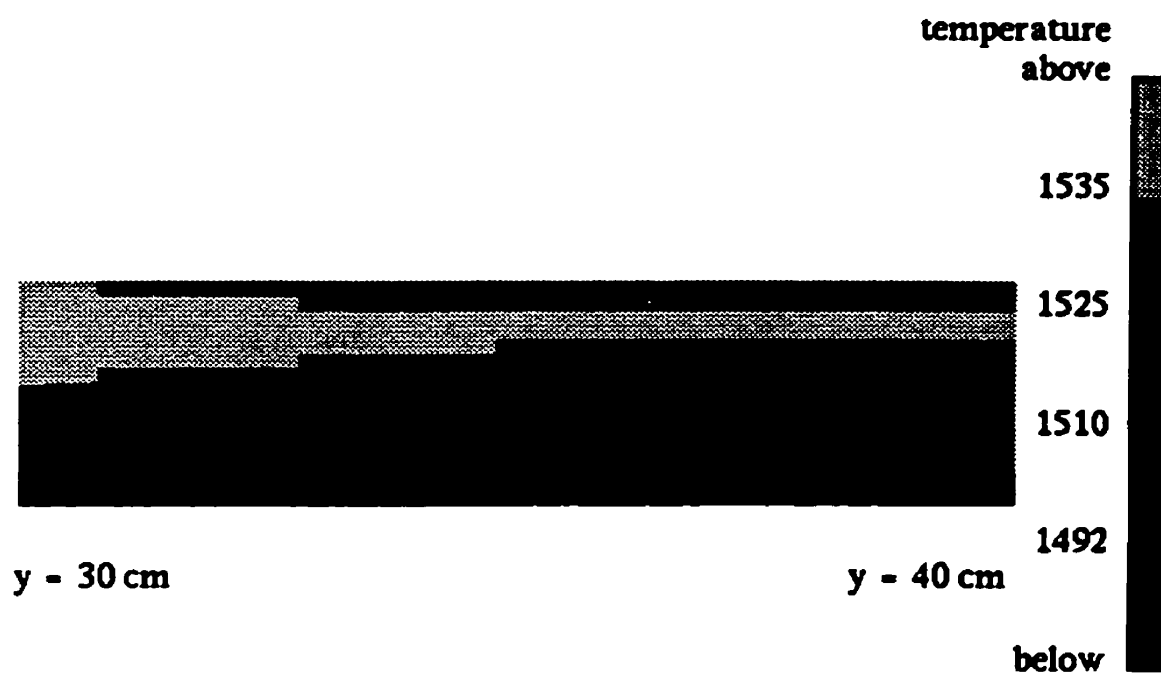


Figure 4-28. Temperature profile at the symmetry axis in the final 10 cm of the domain - Exit gap = 8 mm ($v_b = 1$ m/s, $h_m = 7$ cm, $l = 10$ cm, $Q = 3.6 \times 10^{-3}$ m³/s)



Figure 4-29. Temperature profile at the symmetry axis in the final 10 cm of the domain - Exit gap = 4 mm ($v_b = 1$ m/s, $h_m = 7$ cm, $l = 10$ cm, $Q = 1.8 \times 10^{-3}$ m³/s)

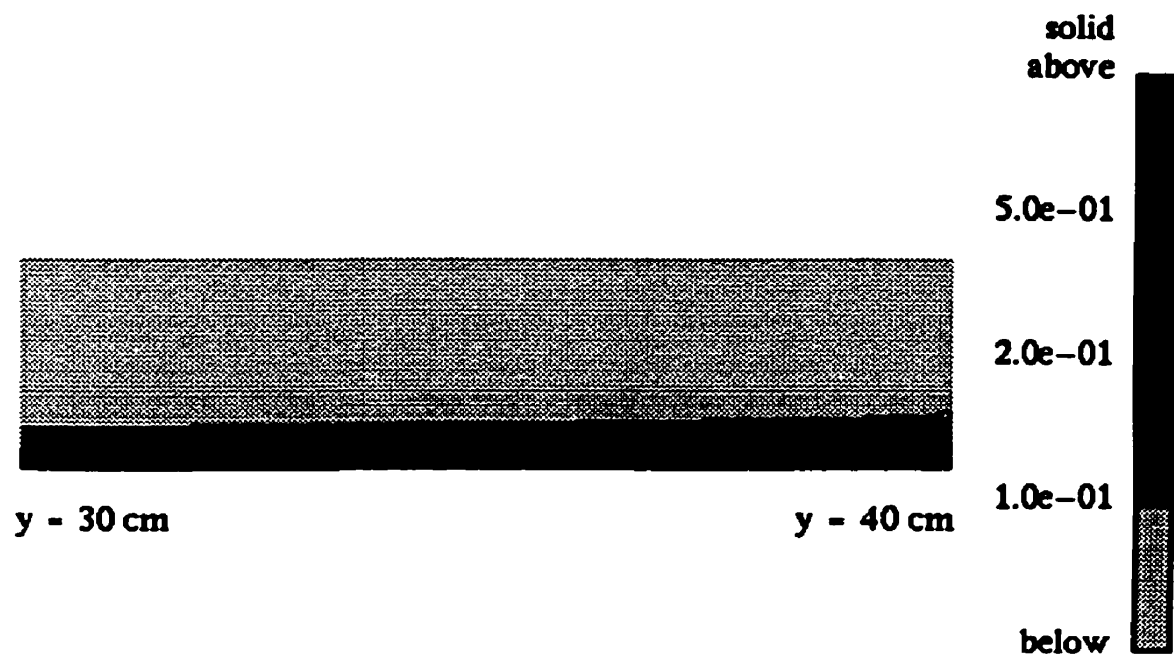


Figure 4-30 Solid fraction at the symmetry axis in the final 10 cm of the domain
Exit gap = 8 mm ($v_b = 1$ m/s, $h_m = 7$ cm, $l = 10$ cm, $Q = 3.6 \times 10^{-3}$ m³/s)

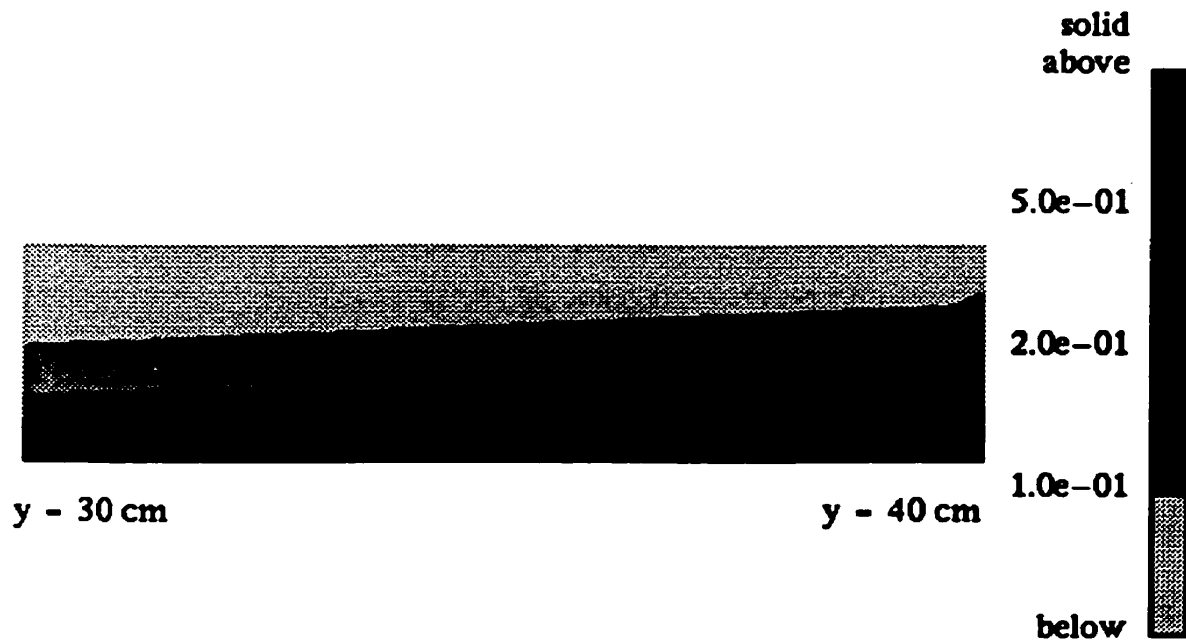


Figure 4-31. Solid fraction at the symmetry axis in the final 10 cm of the domain
Exit gap = 4 mm ($v_b = 1$ m/s, $h_m = 7$ cm, $l = 10$ cm, $Q = 1.8 \times 10^{-3}$ m³/s)

times, due to the lower thermal load requirements associated with cooling a thinner strip. Indeed, Figure 4-29 reminds us that the whole 4-mm strip is already in the mushy state after 40 cm, as opposed to the 10-mm strip.

4.3 SOLIDIFICATION PROFILE ALONG THE STRIP WIDTH

One of the purposes of this model is to investigate the solidification profile along the width of the strip. Premature, or earlier solidification on the lateral of the caster walls is believed to cause defects and to reduce the overall quality of the strip⁽¹⁾, due to an uneven contraction of the shell. In this study, the three-dimensional model is able to predict the extent of such phenomena. In addition, the stress model presented in Chapter 7 will extend this discussion with more details.

Table 4-2 gives predicted values of the average solid fractions at the end of the 40 cm long domain, both for the symmetry axis and the region next to the lateral wall, for the various conditions studied so far. It can be concluded that thinner strips yield less homogeneous solidification. However, the absolute differences in solid fractions between the center and edge of the strip actually increase as the solidification evolves. This may explain the results in Table 4-2. An interesting observation from this Table is that solidification for the belt speed of 0.8 m/s was the most homogeneous of the three investigated, and the Δ for the 0.5 m/s speed was nearly the same as that for the standard 1 m/s case, although the extent of solidification was more advanced. This may be a consequence of the diminished turbulence caused by smaller belt speeds.

Table 4-2 Solid fractions along the width for cases 1 to 5 ($y = 40$ cm).

Description	f_c (center)	f_e (edge)	Δ
Standard	0.157	0.189	0.032
Belt speed = 0.8 m/s	0.176	0.196	0.020
Belt speed = 0.5 m/s	0.240	0.273	0.033
Exit gap = 8 mm	0.195	0.235	0.040
Exit gap = 4 mm	0.356	0.414	0.058

4.4 EFFECT OF GEOMETRICAL PARAMETERS

The geometrical aspects of the proposed delivery system may have a crucial role in the optimization of the caster design. Since the proposed system has not been tested in a pilot plant, nor even at the laboratory scale to date, the mathematical model may be helpful in predicting the response of the fluid flow and heat transfer/solidification to changes in some parameters. The variables chosen for investigation were pool height and length, thickness of the front wall and width of reservoir.

4.4.1 POOL HEIGHT

The idea behind an increase in pool height was to reduce the recirculation zone next to the back wall and consequently to reach a better flow distribution

within the reservoir. An added benefit would be improved metal-belt contact over the extended delivery system, leading to increased heat extraction and thicker shells.

On the other hand, a negative consequence of increasing the pool height for the same belt speed is the increase in the likelihood of a hydraulic jump, given the same belt velocity, as discussed in Chapter 3. However, some measures may be taken to increase the energy losses in the channel below the front wall, to counterbalance this effect. This matter will be a subject of discussion later, in Appendix A.

i) **Fluid Flow Results**

Figures 4-32 to 4-34 show the flow pattern for pool heights of 9, 11 and 14 cm. Since the flowrate is kept the same, the inlet velocity is progressively reduced from 0.333 m/s to 0.1 m/s. This translates into a smoother flow towards the front wall and to an increasingly reduced recirculation zone next to the back wall: extending approximately 4.5, 4.1 and 3.5 cm, respectively, for pool heights of 9, 11 and 14 cm, as opposed to 5.5 cm for the standard pool of 7 cm.

On the other hand, the lower inlet velocity will represent lower velocities in the vicinity of the front wall, which may generate "cold zones." This possibility will be investigated in the next section.

The effects of the reduced inlet velocity and of the more homogeneous flow in the turbulence levels at the strip thickness are shown in Figures 4-35 to 4-37. It is observed that a higher pool corresponds to less turbulence in the region of the strip, inside the reservoir. On the other hand, a higher pool increases the

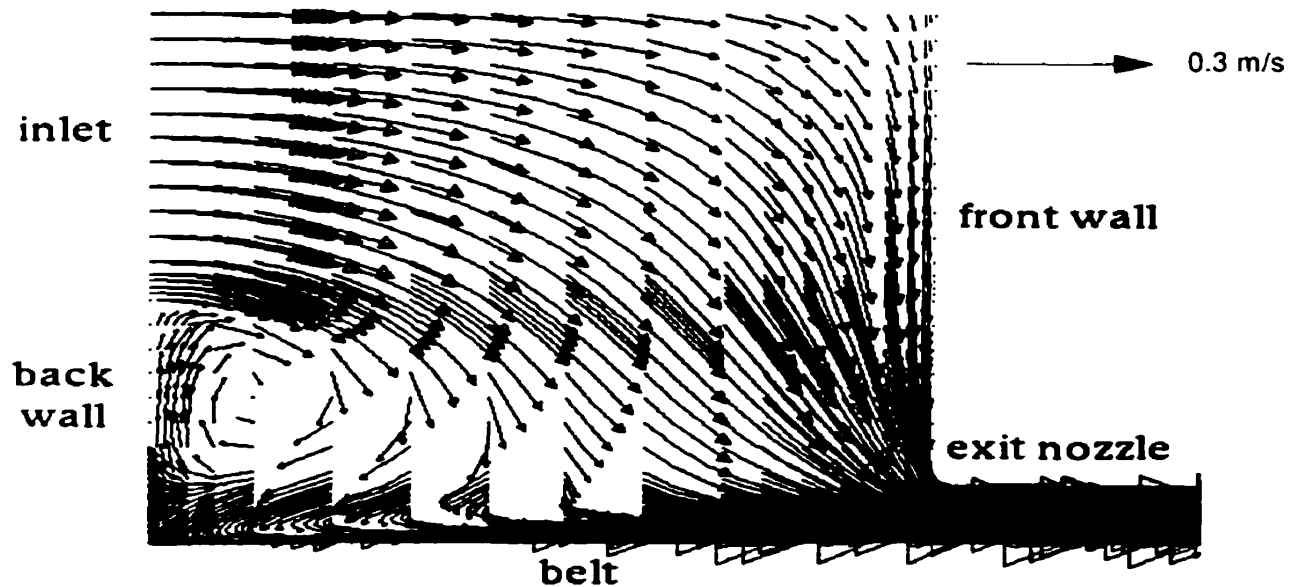


Figure 4-32. Vertical cut along the symmetry axis - Pool height = 9 cm ($v_b = 1$ m/s, $d_g = 10$ mm, $l = 10$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

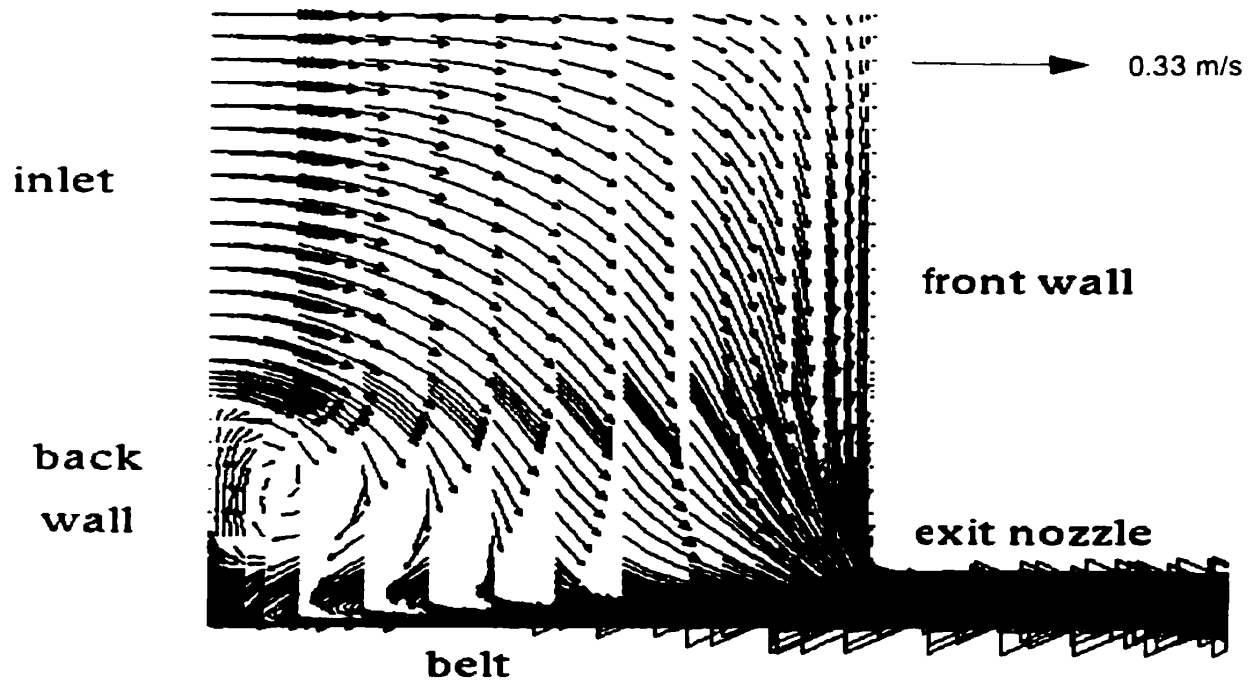


Figure 4-33. Vertical cut along the symmetry axis - Pool height = 11 cm ($v_b = 1$ m/s, $d_g = 10$ mm, $l = 10$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

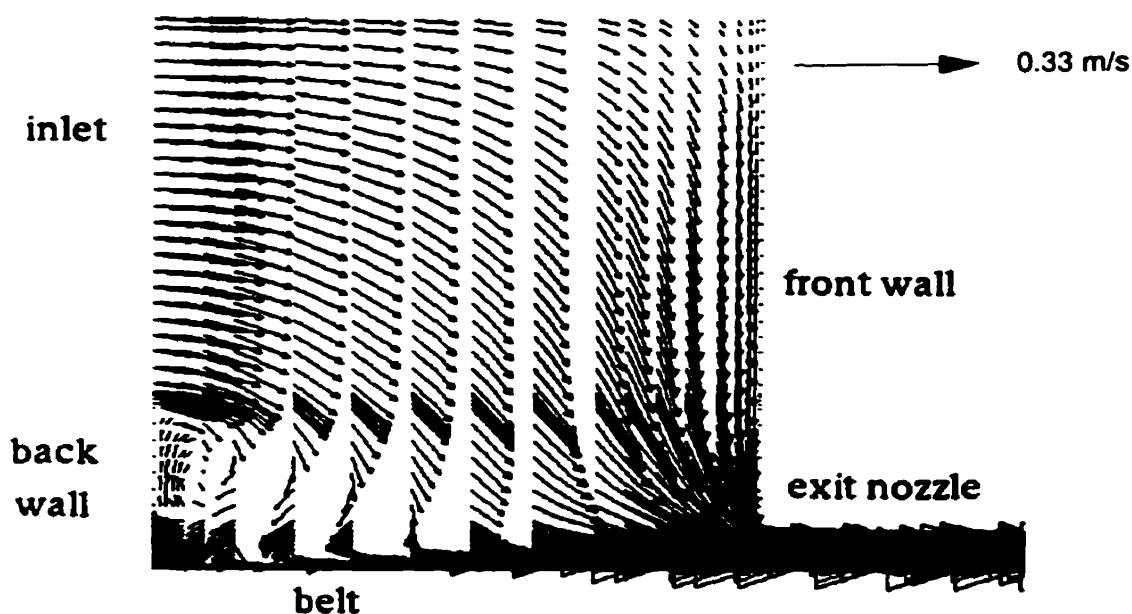


Figure 4-34. Vertical cut along the symmetry axis - Pool height = 14 cm ($v_b = 1$ m/s, $d_g = 10$ mm, $l = 10$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

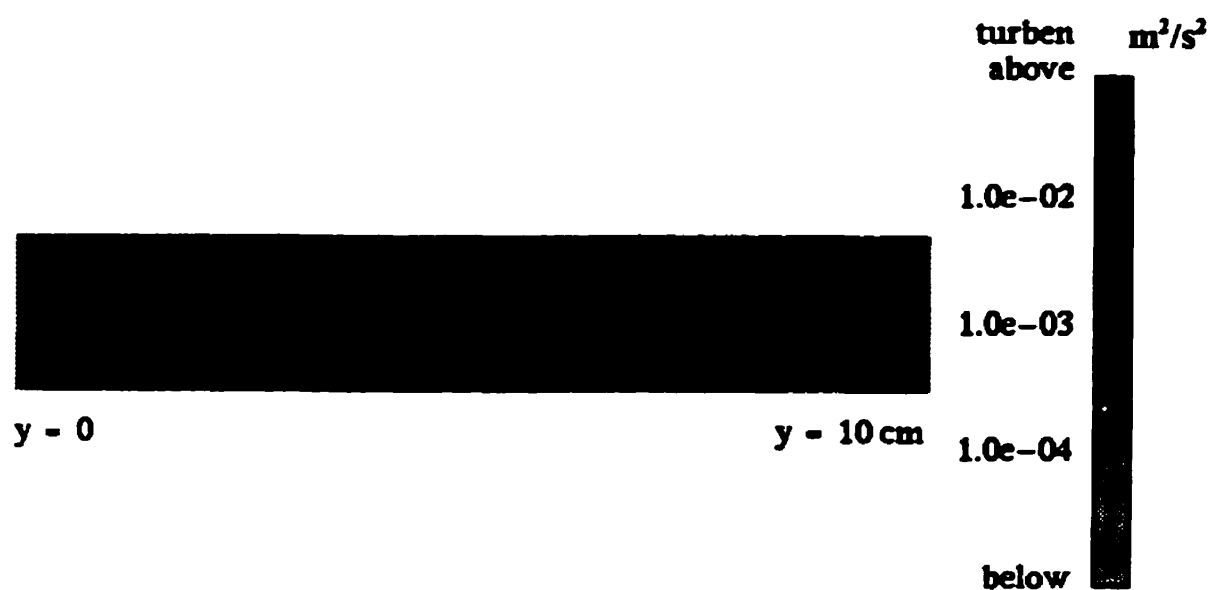


Figure 4-35. Turbulent kinetic energy at the symmetry axis - Pool height = 9 cm ($v_b = 1$ m/s, $d_g = 10$ mm, $l = 10$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

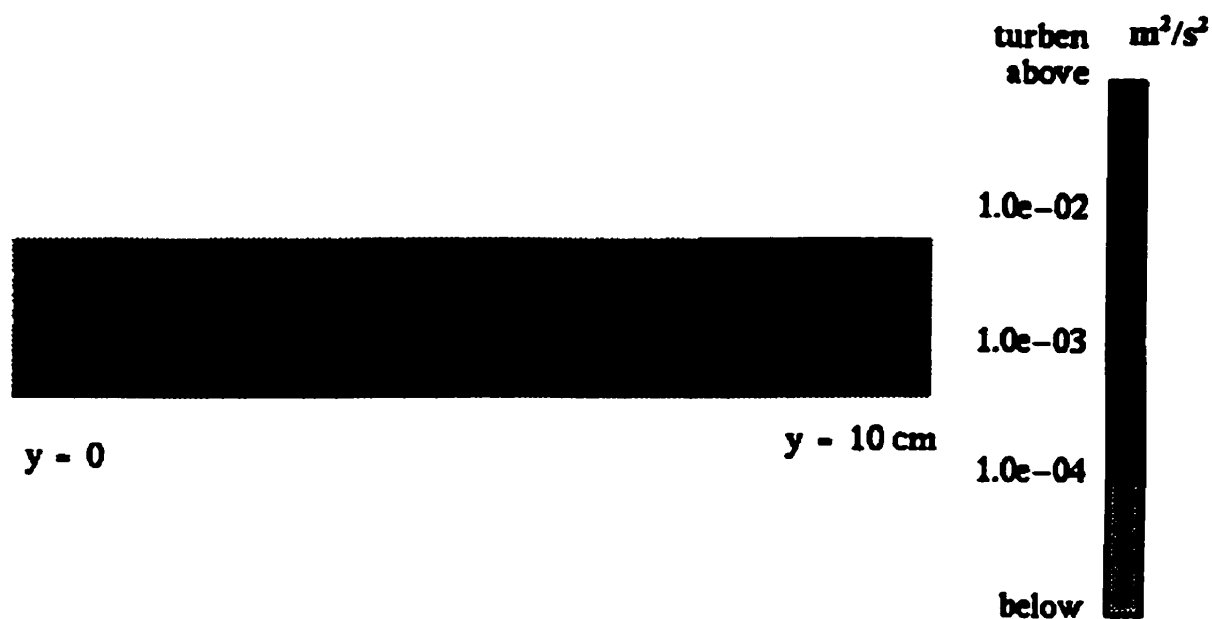


Figure 4-36. Turbulent kinetic energy at the symmetry axis - Pool height = 11 cm ($v_b = 1 \text{ m/s}$, $d_g = 10 \text{ mm}$, $l = 10 \text{ cm}$, $Q = 4.5 \times 10^{-3} \text{ m}^3/\text{s}$)

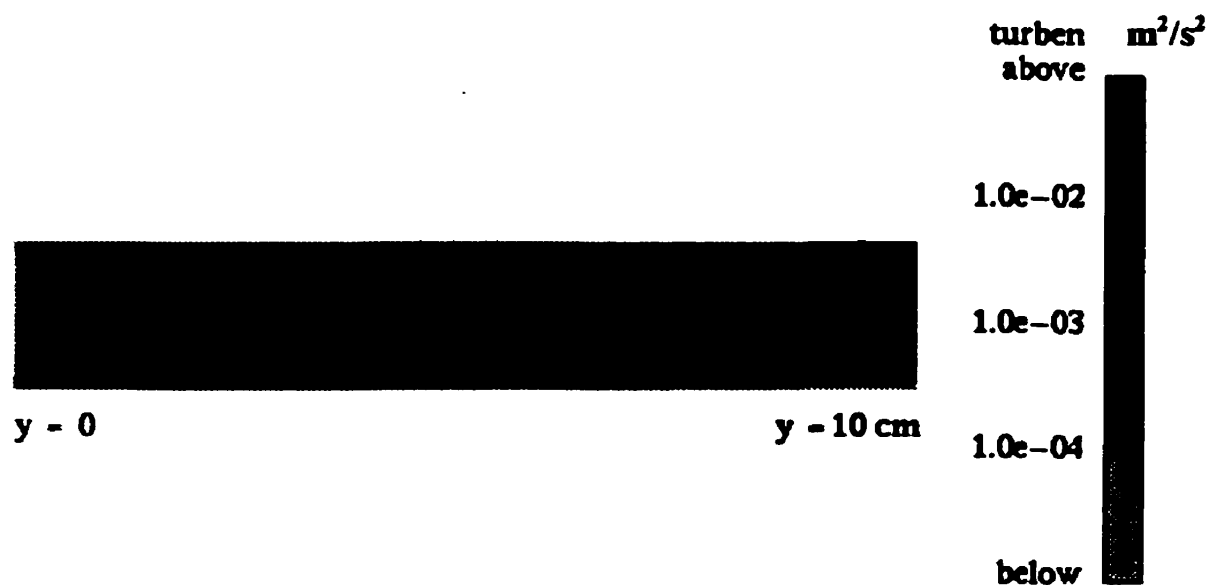


Figure 4-37. Turbulent kinetic energy at the symmetry axis - Pool height = 14 cm ($v_b = 1 \text{ m/s}$, $d_g = 10 \text{ mm}$, $l = 10 \text{ cm}$, $Q = 4.5 \times 10^{-3} \text{ m}^3/\text{s}$)

turbulence at the entrance to the exit nozzle.

ii) **Heat Transfer/Solidification Results**

As expected, the increase in the pool height originated a cold zone in the vicinity of the front wall, as seen in Figure 4-38, representing the 14-cm pool. For the pool height of 14 cm, the mushy zone covers approximately 1 cm in the center of the reservoir and 5 cm next to the lateral wall. This is due to the poor alimentation of metal to this region.

As for the solid fractions at the end of the domain, Table 4-3 shows that the 9-cm pool gives the most homogeneous solidification. This was achieved mainly by a remarkable reduction in the solid fraction at the lateral wall region, since the values in the center are nearly unchanged. For all heights simulated, the homogeneity along the strip was higher than in the standard conditions. However, no definite tendency was observed, since the highest Δ were found for the shallowest and deepest pools. A more detailed study showed that, at the standard condition (pool height = 7 cm), the turbulent kinetic energy on the top of the strip is higher on the edge than in the center, which is the opposite of the expected behavior. This observation may explain the non-alignment of the standard condition to the other three cases, where Δ increases with the pool height. The reason for this deviant behavior in the standard case was not well understood, but it might be related to the spreading of the high-momentum incoming flow out to the sides, at the nozzle entrance. As for the solidified shell thickness, no changes were observed for the various pool heights used, and a constant value of 1.1 mm was found at 40 cm from the meniscus.

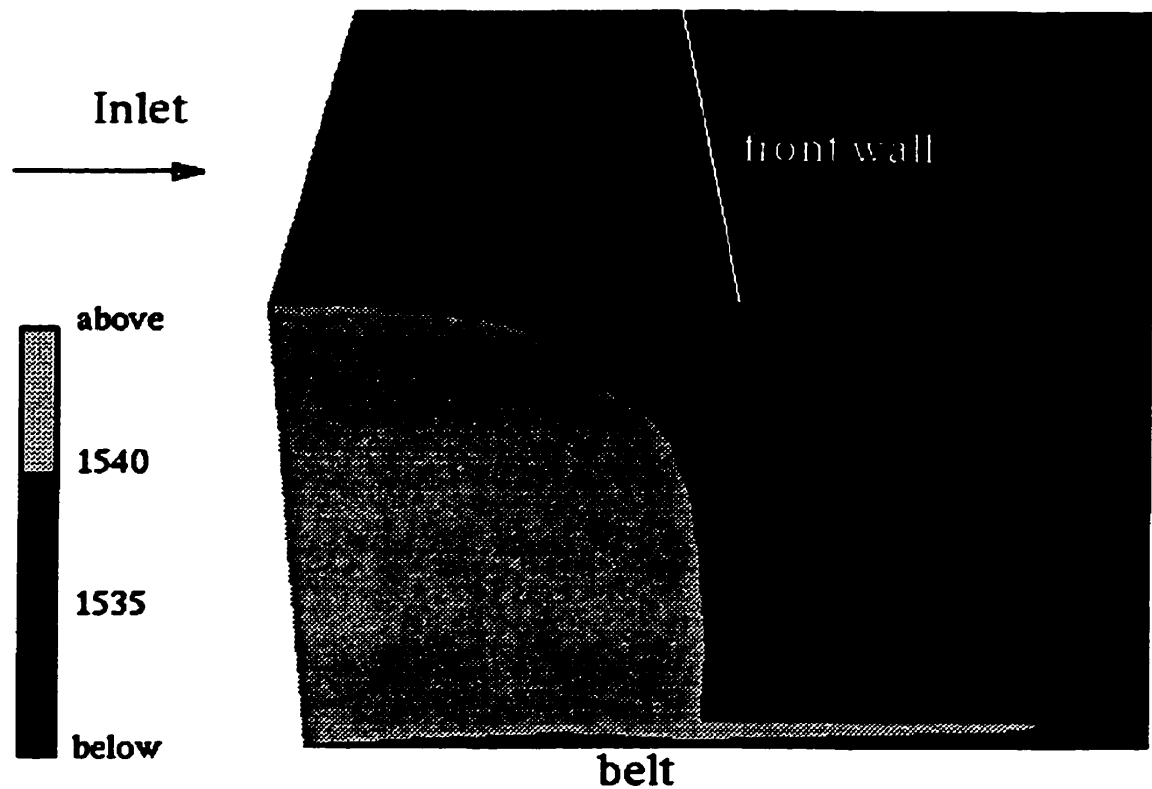


Figure 4-38. Temperature profile in the reservoir - Detail of the top edge - Pool height = 14 cm ($v_b = 1$ m/s , $d_g = 10$ mm, $l = 10$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

Table 4-3 Average solid fraction at $y = 40$ cm, for various pool heights.

Pool height	f_s (center)	f_s (edge)	Δ
7 cm	0.157	0.189	0.032
9 cm	0.153	0.176	0.022
11 cm	0.155	0.181	0.026
14 cm	0.156	0.186	0.030

4.4.2 POOL LENGTH

Another geometrical aspect that can be changed in the pursuit of improvements to the fluid flow within the reservoir is pool length. Increasing the pool length should give more time for the flow to develop within the reservoir and, therefore, to establish a "better organized", not so concentrated flow towards the nozzle gap, thereby reducing the turbulence at the entrance.

i) Fluid Flow Results

The flow patterns predicted for extended pool lengths of 20 and 30 cm are given in Figures 4-39 and 4-40. One can clearly distinguish the two most visible changes in the flow patterns: 1) the "horizontalization" of the flow beyond the first half of the reservoir, starting from the inlet and 2) the change in size and morphology of the recirculation zone next to the back wall.

The "horizontalization" of the flow has a major effect in lowering the

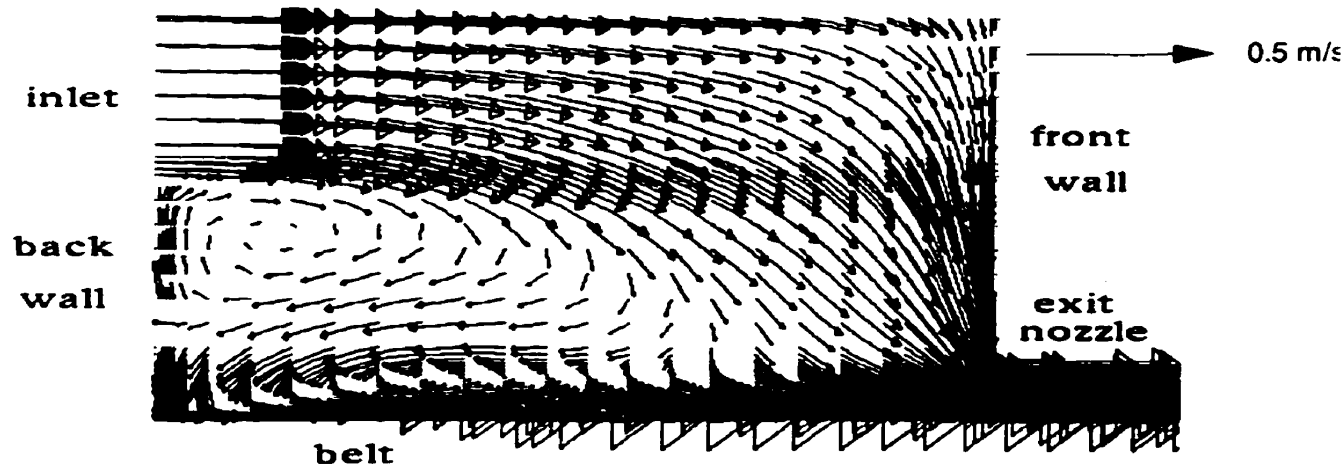


Figure 4-39. Vertical cut along the symmetry axis - Pool length = 20 cm ($v_b = 1$ m/s, $d_g = 10$ mm, $h_m = 7$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

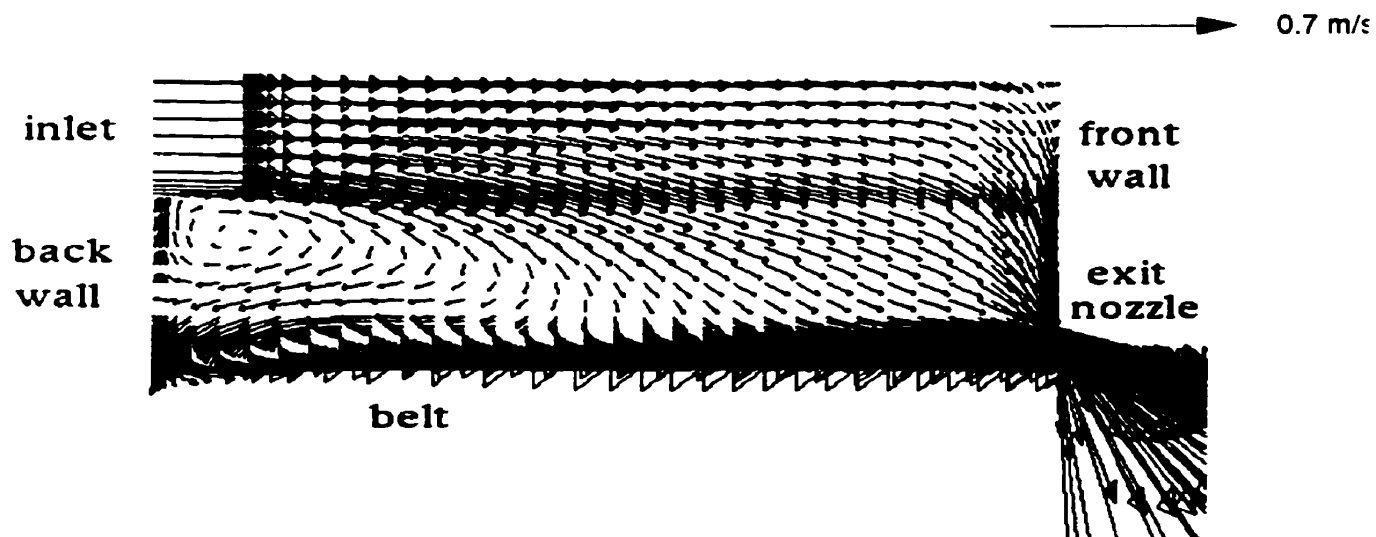


Figure 4-40. Vertical cut along the symmetry axis - Pool length = 30 cm ($v_b = 1$ m/s, $d_g = 10$ mm, $h_m = 7$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

velocity for the metal reaching the exit gap. For the "standard" conditions, the metal flow towards the gap is roughly divided into two components: one goes straight to the exit gap, while the other is recirculated and then dragged along by the belt. It is noticed that, for larger pools, an intermediate zone exists between the original two. This phenomenon is best seen in Figure 4-40, where part of the metal that is not recirculated reaches the belt at about half the reservoir extended nozzle's length (or 15 cm). It is believed that in such cases the metal will have more time to accommodate the drag force exerted by the belt, and when it finally reaches the exit gap, turbulence levels will be relatively reduced. On the other hand, the interaction of this part of the flow with the flow advancing towards the nozzle exit could generate more turbulence.

The recirculation shown in Figure 4-39 was calculated to be 61% of the reservoir length, or 12.6 cm. This represents an increase in length and percentage compared to the standard case (5.5 cm and 55%). For the most extended nozzle (or largest pool), the recirculation zone covers "only" 50% of the reservoir, or 15 cm. The modifications in the recirculation zone, however, are not restricted to just its length. It is observed that the "whorl" shrinks for largest pools, reducing the height of the recirculation zone. For the 20 and 30-cm lengths, the metal is drawn a long way back towards the back wall, where most of it is then directed down towards and dragged along by the belt. The height of the recirculation zone is reduced from around 3 cm for the standard case to approximately 1.5 cm for the 30 cm-pool.

As for the turbulent kinetic energy for the 30-cm pool, shown in Figure 4-41, it follows qualitatively the same profile as the standard conditions: an initial zone of high turbulence, where the recirculation flow meets the high-speed belt

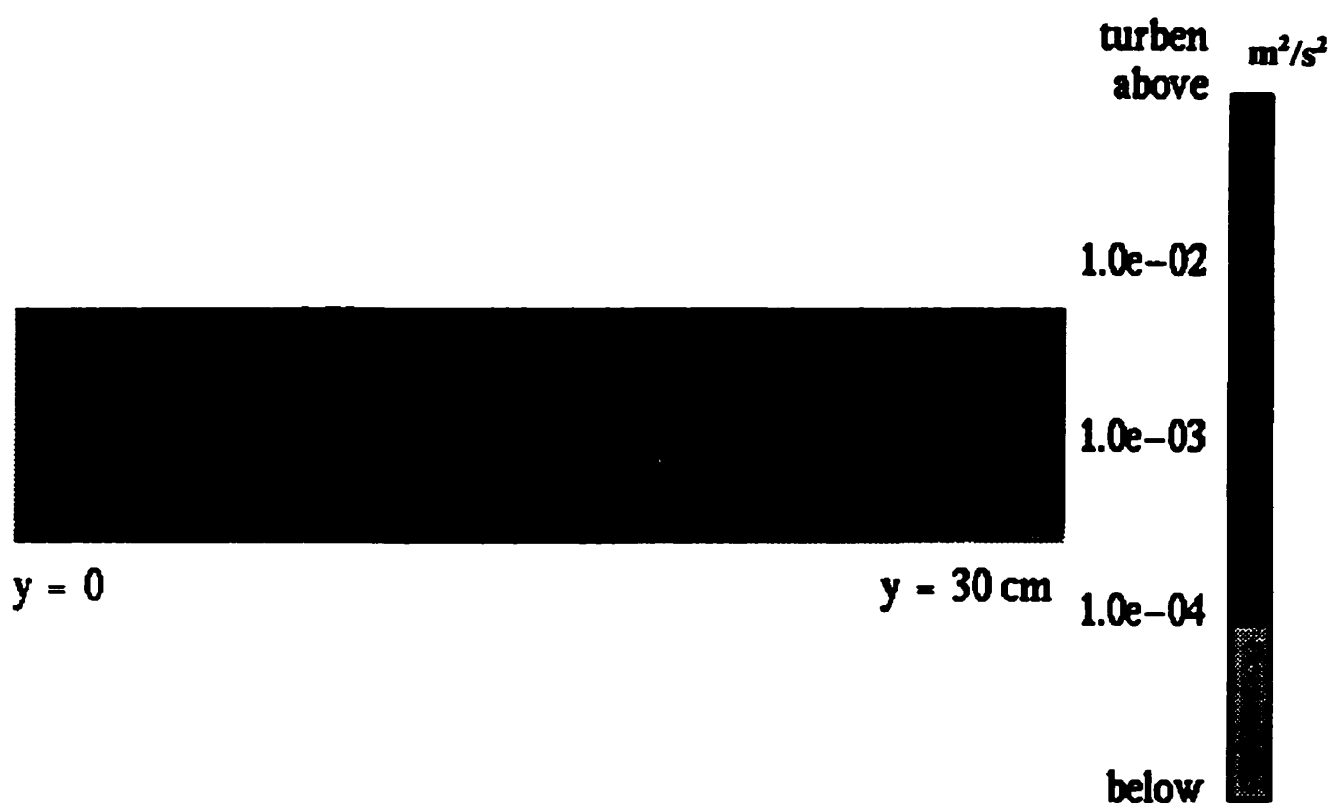


Figure 4-41. Turbulent kinetic energy at the symmetry axis - Pool length = 30 cm ($v_b = 1 \text{ m/s}$, $d_g = 10 \text{ mm}$, $h_m = 7 \text{ cm}$, $Q = 4.5 \times 10^{-3} \text{ m}^3/\text{s}$)

and an increase in turbulence towards the nozzle entrance, due to the flow contraction.

ii) **Heat Transfer/Solidification Results**

An increased pool length contributed to a reduced overall, but more homogeneous solidification (Table 4-4) compared with the standard condition of a 10-cm extended nozzle pool. An increase in the length beyond 20 cm indicated a reverse tendency, but for both the 20 and 30-cm pools, solidification was more homogeneous than for the "standard" condition. Therefore, in terms of heat transfer, the implications of altering the pool length were qualitatively similar to those given by variations in the pool height. An intermediate condition, (pool=20 cm), yielded the best results.

Table 4-4 Average solid fraction at $y = 40$ cm, for various pool lengths.

Pool length	f_s (center)	f_s (edge)	Δ
10 cm	0.157	0.189	0.032
20 cm	0.157	0.182	0.025
30 cm	0.156	0.184	0.028

Nevertheless, the most dramatic change brought about by the extended pool length may be the temperature profile in the region of the reservoir close to the front wall. Figures 4-42 and 4-43 show these profiles. Extended cold zones are predicted, especially for the case of a 30-cm pool, where the mushy zone would cover 5 cm of the pool, in the symmetry axis and approximately 17 cm near the

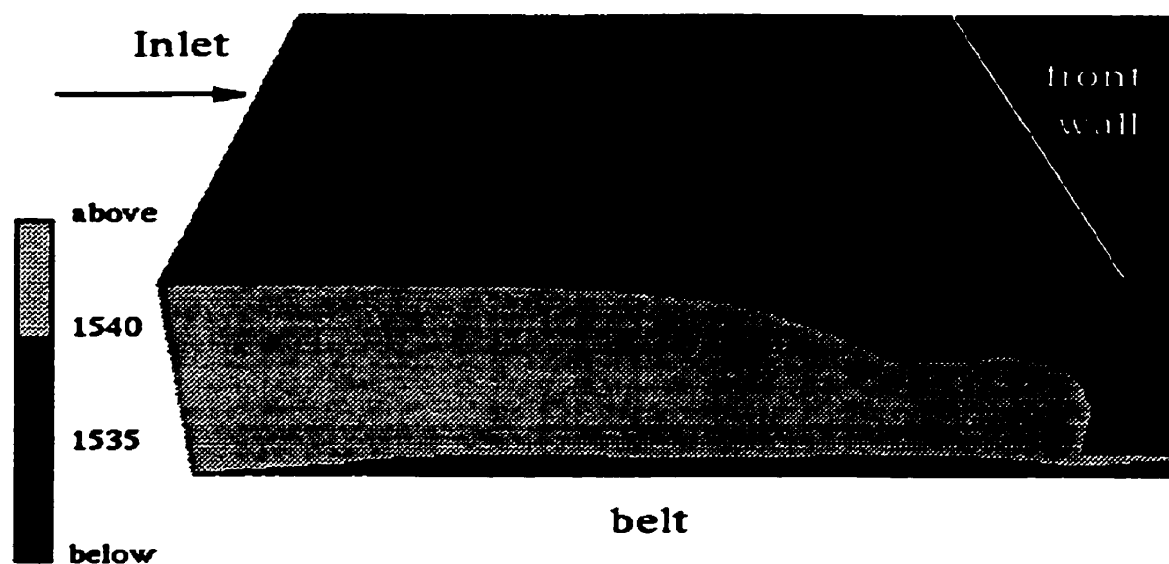


Figure 4-42. Temperature profile in the reservoir - Detail of the top edge - Pool length = 20 cm ($v_b = 1$ m/s, $d_g = 10$ mm, $h_m = 7$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

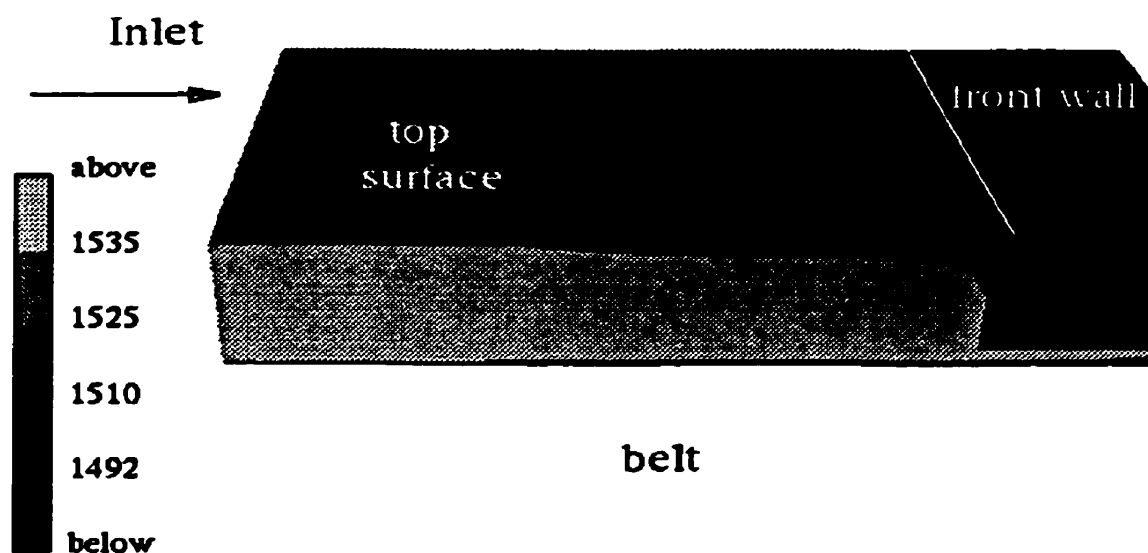


Figure 4-43. Temperature profile in the reservoir - Detail of the top edge - Pool length = 30 cm ($v_b = 1$ m/s, $d_g = 10$ mm, $h_m = 7$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

lateral wall. The extended cold zones are explained by the reduced velocities calculated for that region, where the metal has already lost most of its initial momentum. Such a problem could be minimized by the adoption of magnetic stirring, but would require an increase in the initial superheat of the metal or a higher inlet velocity. This would either imply retardation in the solidification, or a redesign of the delivery system.

4.4.3 RESERVOIR WIDTH

Reducing the width from the "standard" 45 cm to 25 cm, keeping other standard parameters, but with a flowrate reduced accordingly, did not cause any noticeable modification to the fluid flow patterns or heat transfer results. Therefore, to better quantify the modifications brought about by a change in the width of the strip, a hypothetical belt speed higher than 1 m/s was applied to the belt, with the aim of keeping the same flowrate and same productivity of the machine, per meter width of product. Two cases were considered: for widths of 35 and 25 cm, belt speeds of 1.29 and 1.8 m/s were applied to the belt. For the second case, the speed being much higher than that generated in the metal by the corresponding metal head, the likelihood of a hydraulic jump is considerable. Since the results for both cases were qualitatively coherent, for the sake of brevity only the case of a hypothetical 25 cm strip will be discussed in the sequence.

For the reduction to almost half the original width, a huge belt length would be predicted, since its speed was raised to 1.8 m/s. Thus, a dramatic change is expected in the fluid flow/heat transfer characteristics.

i) Fluid Flow Results

A downward deformed flow pattern results from the increased inlet velocity.

As opposed to the standard condition, the metal seems to have turbulent behavior, even in the region closer to the front wall (Figure 4-44). The recirculation zone is roughly the same size, but its morphology is altered, the flow being more vigorous in the recirculation circuit.

The very high speed of the cooling belt generates a fairly high amount of turbulence in the region of the strip inside the reservoir. Figure 4-45 presents the turbulence energy profile for the strip. The values of κ are always very high in the neighbourhood of the belt, from the meniscus point to the exit gap. These results confirm once again the primary effect of belt speed on flow turbulence.

ii) **Heat Transfer/Solidification Results**

The total elapsed time for the belt to move 40 cm drops from 0.4 to 0.25s. Thus, a much hotter strip is expected at the end of the domain. Indeed, a very thin solidified shell (0.4 mm) is found, and only 25 % of the strip reached the mushy state.

These simulations involving high belt speeds to maintain the same levels of productivity for narrower strips are actually more of a theoretical exercise, since the wisest decision to be made in such case would be to lower the levels of productivity, keeping the same operational conditions. As already mentioned, casting narrower strips with proportionally reduced initial flowrates yielded no noticeable changes, according to the mathematical model. The profit in doing such a study is present in the light it sheds on the flexibility of the proposed horizontal single-belt caster for thin strip casting: reducing the width by almost half does not

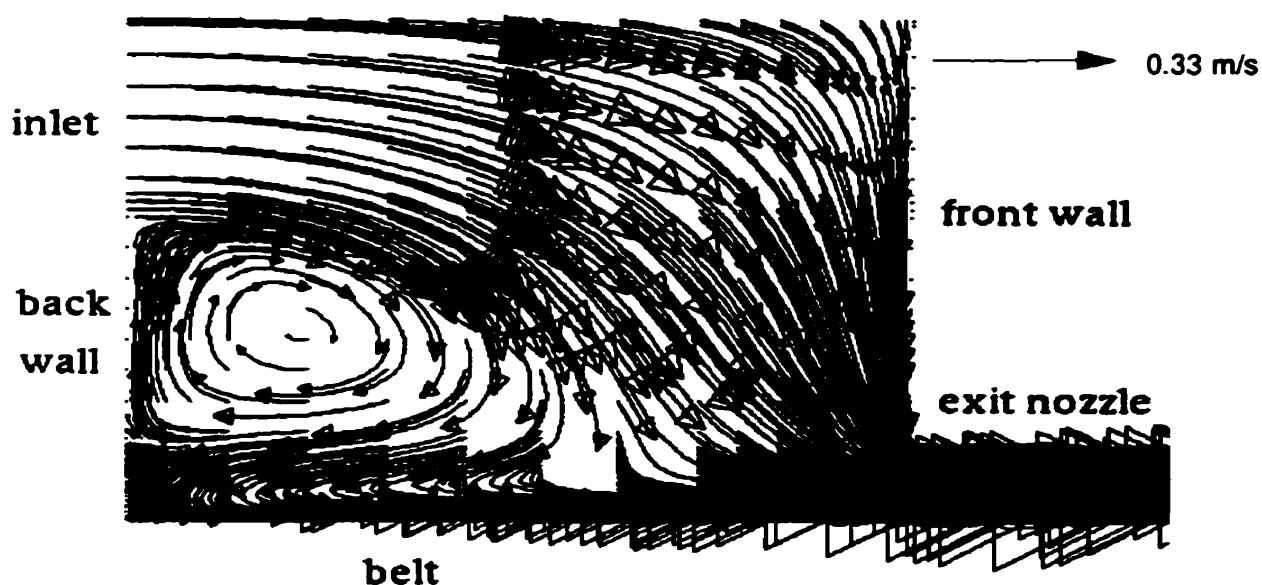


Figure 4-44. Vertical cut along the symmetry axis - Belt speed = 1.8 m/s ($l = 10$ cm, $d_g = 10$ mm, $h_m = 7$ cm, $Q = 4.5 \times 10^{-3}$ m³/s, $W = 22.5$ cm)

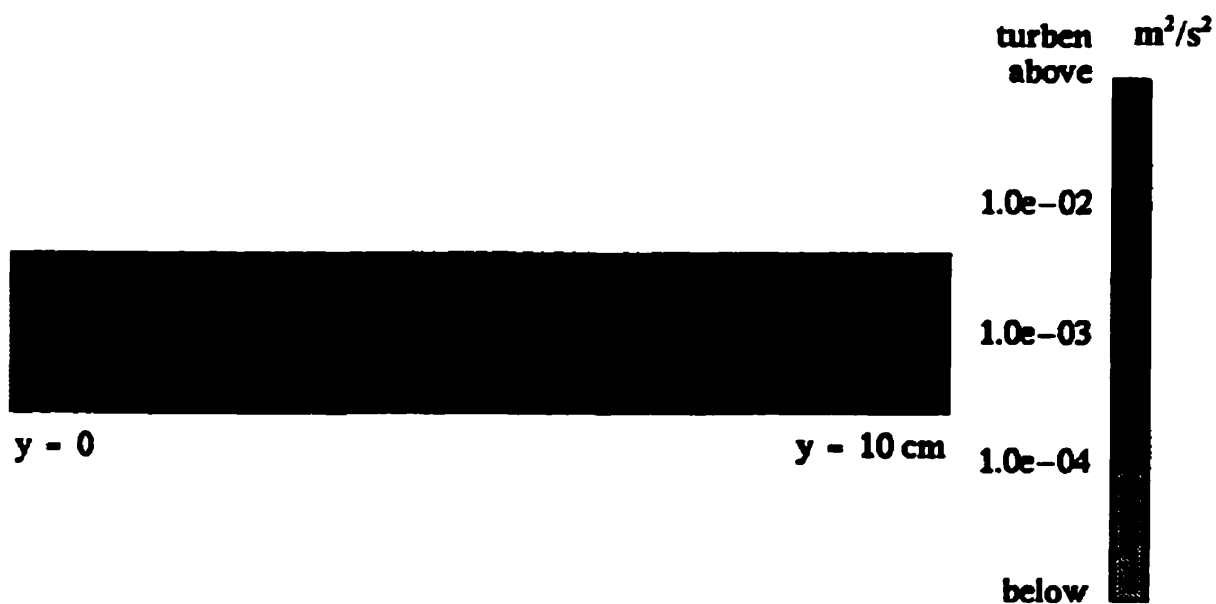


Figure 4-45. Turbulent kinetic energy at the symmetry axis - Belt speed = 1.8 m/s ($l = 10$ cm, $d_g = 10$ mm, $h_m = 7$ cm, $Q = 4.5 \times 10^{-3}$ m³/s, $W = 22.5$ cm)

require any major changes in the process, other than changing the width of the reservoir zone, by moving the lateral walls closer to one another and reducing the belt width's accordingly. In addition, such simulations brought valuable information on the behavior of the single-belt caster running at speeds above 1 m/s.

4.4.4 FRONT WALL THICKNESS

Since the heat losses were represented only by conduction between the metal and the wall, and thus, no frictional energy dissipation was included, the changes brought about by an the extension of the front wall were not important.

Figure 4-46 shows the temperature profile at the symmetry axis at the end of the domain for a front wall thickness of 2.25 cm. Compared with Figure 4-47 (thickness of 8.25 cm), only the morphology of the remaining liquid zone suggests change. On the lateral edge, the liquid phase vanishes at 39 cm from the meniscus, for both cases. For the center of the strip, the results are nearly identical. The replacement of the convection/radiation thermal boundary condition with conduction has a residual effect, especially when compared with the strong forced convection applied to the bottom of the strip.

A theoretical study of the effect of frictional energy dissipation by the wall on the liquid velocities is presented in Appendix A.

4.5 EFFECT OF INITIAL SUPERHEAT

The initial superheat is an important operational variable of the casting process. A low superheat is generally desired to reduce the solidification time and

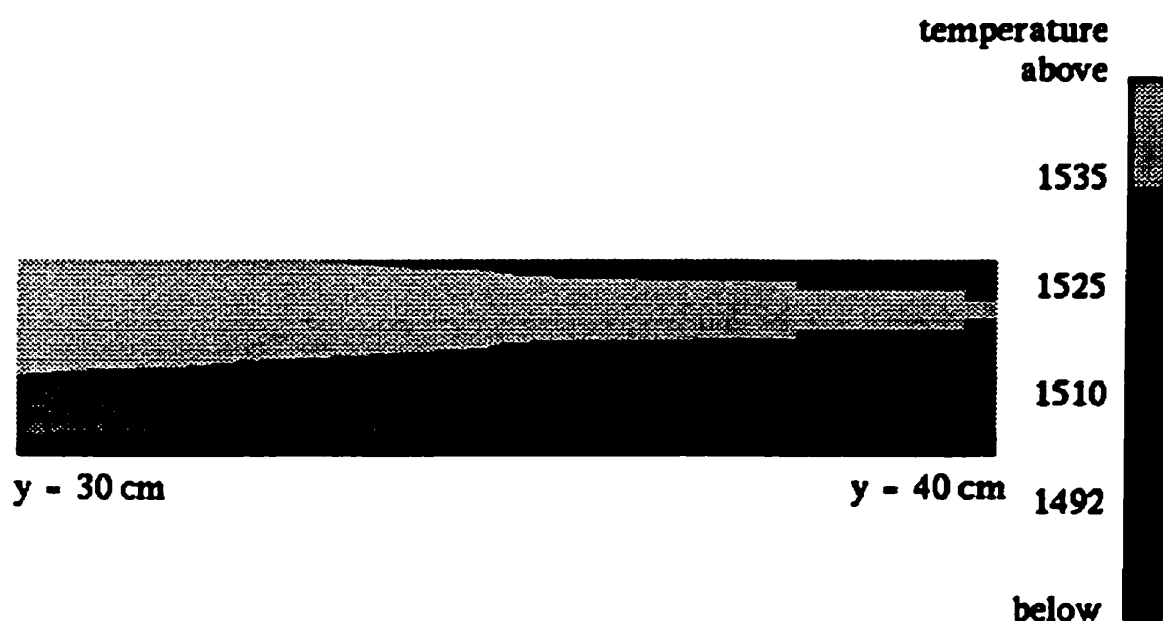


Figure 4-46. Temperature profile at the symmetry axis in the final 10 cm of the domain - Front wall thickness = 2.25 cm ($v_b = 1$ m/s, $l = 10$ cm, $d_g = 10$ mm, $h_m = 7$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

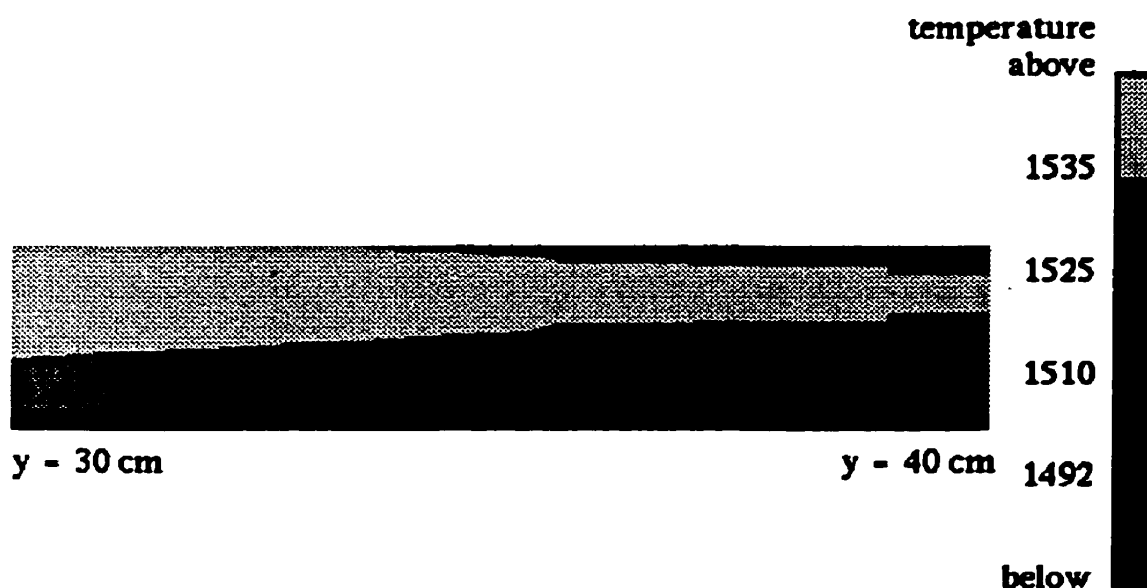


Figure 4-47. Temperature profile at the symmetry axis in the final 10 cm of the domain - Front wall thickness = 8.25 cm ($v_b = 1$ m/s, $l = 10$ cm, $d_g = 10$ mm, $h_m = 7$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

to generate a finer microstructure, which is known to yield better mechanical properties⁽⁵⁾. However, a lower superheat can create operational problems and a "not so good" wettability of the cooling belt. As described in Chapter 8, the higher the initial superheat of the metal, the higher is its fluidity and, in consequence, the better is the wettability of the substrate. On the other hand, a relatively high superheat could cause spreading of the metal out to the edges⁽⁶⁾ and slow solidification. Therefore, a compromise needs to be found, and an optimal initial superheat determined, for each operational condition.

In this work, levels of initial superheat ranging from 0 to 30°C were investigated. These values are within the industrial range for continuous casting. For these levels of superheat, the most relevant observations regarding fluid flow and, especially heat transfer and solidification, will now be presented.

i) **Fluid Flow Results**

For the zero superheat condition, a triangular-shaped zone of virtually zero velocities is seen in the top left of Figure 4-48. This zero-velocity zone is a result of the coupling between low velocities near the wall and low initial superheat. The dead zone will influence the flow in the reservoir. It is observed that the metal flow is pushed downwards and concentrates in a smaller area. The recirculation zone shrinks a little, due to this movement, in a similar manner of the flow in higher pools. Here, the flow is even less developed than in the standard condition.

For superheats of 5, 20 and 30°C: no noticeable changes were identified in the fluid flow for these levels of superheat. Compared with the 10°C superheat, the flow patterns are identical.

As for the turbulent energy, an increased superheat contributes towards extending the zone of high turbulence right above the belt. For the 30°C superheat, this zone extends up to almost the nozzle's entrance.

ii) Heat Transfer/Solidification Results

The triangular-shaped zone shown in Figure 4-48 for the zero superheat case has its temperature profile presented in Figure 4-49. The whole reservoir is clearly mushy and a 3 cm solid layer appears next to the front wall. Although the model damps the velocities in the mushy zone, according to Darcy's law, the metal flow is still regular towards the exit gap, as shown in Figure 4-48, since in most of the reservoir the temperature is still no more than 3°C below the liquidus temperature. Here, it is important to remind that adiabatic conditions were considered for the lateral and back walls, whereas a uniform heat flux was applied to the front wall. Heat is lost by radiation and convection on the top of the reservoir.

For the reduced initial superheat of 5°C, the temperature profile in the reservoir is similar to standard conditions, except for a small mushy zone developing in the vicinity of the wall.

Table 4-5 gives the overall solid fractions for the various superheats investigated. The thicknesses of the solid shells at the end of the domain did not vary, for the levels investigated. The relatively low thermal conductivity of the carbon steel explains this result, since the thermal resistance through the shell is much higher than through the mushy/liquid zone. Therefore, what happens is that as the initial superheat increases, the morphology throughout the strip becomes more complex, with solid, mushy and liquid still present at the end of the domain.

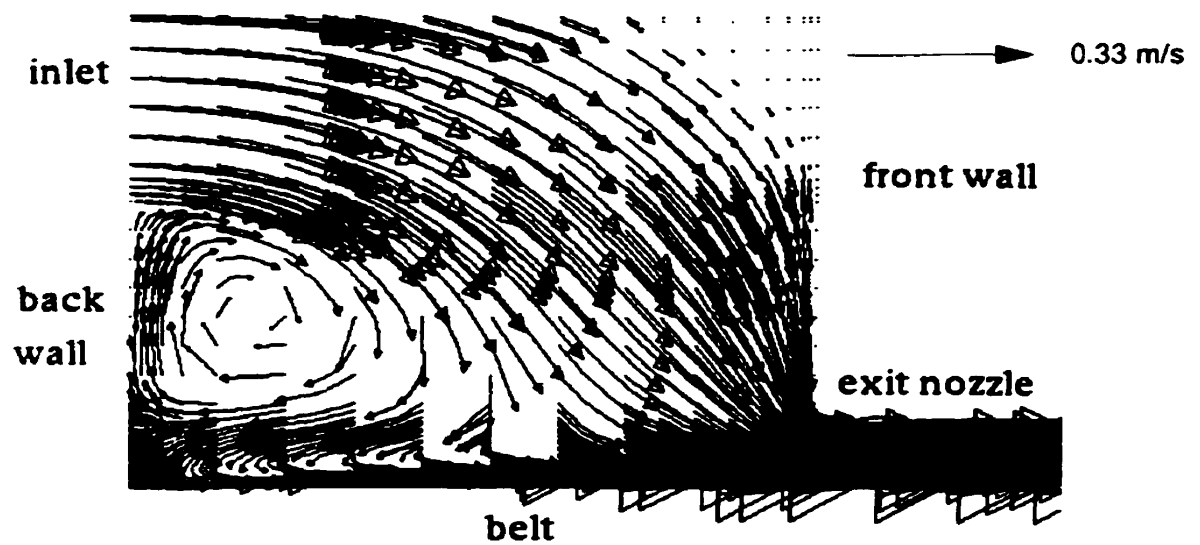


Figure 4-48. Vertical cut along the symmetry axis - Zero superheat ($v_b = 1$ m/s, $l = 10$ cm, $d_g = 10$ mm, $h_m = 7$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

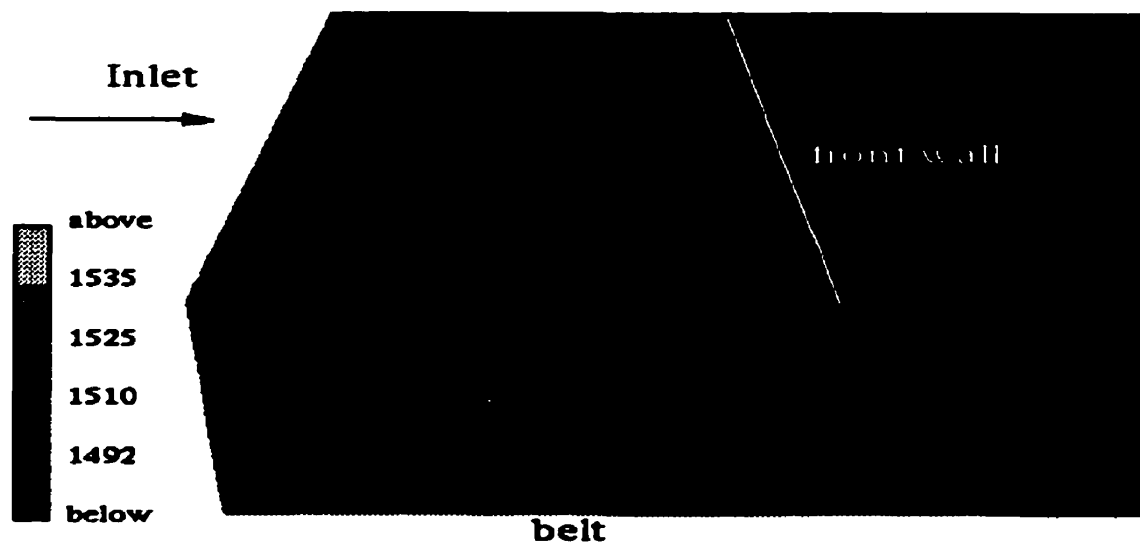


Figure 4-49. Temperature profile in the reservoir - Detail of the top edge - Zero superheat ($v_b = 1$ m/s, $l = 10$ cm, $d_g = 10$ mm, $h_m = 7$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

For example, for the initial superheat of 30°C, only 20% of the strip is mushy, at 40 cm from the meniscus, whereas the liquid metal vanishes at approximately 33 cm for the case of 5°C superheat.

Remarkable differences are again found between the temperature profiles in the symmetry axis and on the edge, which allow us to conclude that the initial superheat does not assume an important role in determining the homogeneity of solidification along the width of the strip. Any changes to this temperature profile should be brought by means of modifying the fluid flow in the reservoir zone.

Table 4-5 Average solid fraction at $y = 40$ cm, for various inlet superheats.

Inlet Superheat (°C)	f_s (center)	f_s (edge)	Δ
0	0.186	0.231	0.045
5	0.175	0.211	0.036
10	0.157	0.189	0.032
20	0.149	0.175	0.026
30	0.136	0.162	0.026

4.6 **FACTORS AFFECTING ACCURACY OF THE MODEL**

The results of a mathematical model reflect the quality and nature of the assumptions on which the model is based. Therefore, such assumptions must

represent the real system being studied, as closely as possible. In the study of near-net-shape casting processes, very little information is available concerning the morphology of the mushy zone. This difficulty is not actually a privilege of near-net-shape processes, since not much is known for the conventional continuous casting machines, although these processes have been around for some forty years.

Besides the lack of information on the morphology of the mushy zone, more accurate values for the overall heat transfer coefficient between the metal and the cooling belt are still in need. Usually, a constant averaged value for h is assigned for the whole length of the cooling belt, which is clearly an idealization.

The purpose of this study is to identify and to investigate the main factors affecting the accuracy of the mathematical model presented herein. To achieve that, the constant C , which defines the morphology of the mushy zone, will be varied in the next section, and different profiles for the overall heat transfer coefficient will also be tried. Uncertainties in the thermophysical properties of the metal and their influence on the fluid flow and heat transfer results will also be discussed.

4.6.1 EFFECT OF THE MORPHOLOGY CONSTANT C

As reported in Chapter 3, Minakawa's equation⁽⁷⁾ was taken as a first reference to calculate the morphology constant. For the SDAS values reported by Spitzer & Schwerdtfeger⁽¹⁾, for horizontal single-belt casting of stainless steel and from the values found in our experimental apparatus, an average SDAS of $15 \mu\text{m}$ was adopted in this work. This translates, according to Minakawa's equation, to a value of approximately 1×10^6 for C . Nevertheless, as already mentioned, a value of 1×10^7 was employed in this work, which is closer to the value chosen

by Jefferies⁽²⁾ for her two-dimensional model (1.6×10^7).

Thus, taking this value as the standard, the constant was varied by two orders of magnitude, up and down. As such, values of 10^5 and 10^9 were used for C , while all other conditions remained unchanged.

i) **Fluid Flow Results**

No significant changes were noticed on the fluid flow patterns, for the different values of C . This is due to the absence of any mushy zone in the reservoir, for the standard conditions, except for the region immediately over the cooling belt.

The turbulent kinetic energy profiles, however, show important changes in the region of the strip inside the reservoir. In Figure 4-50, representing a lower value of C , considerably more turbulence is seen in the region of the strip. This is a result of a less dampened velocity in the mushy zone, due to a lower Darcy term. On the other hand, for a higher value of C (Figure 4-51), the velocity dampening is very intense in the mushy region, thereby reducing the turbulent kinetic energy values.

The kinematic viscosities for the three cases are presented in Figures 4-52 to 4-54 to better illustrate the turbulence picture. As for the turbulent kinetic energy, the turbulent viscosity increases for lower values of C .

ii) **Heat Transfer/Solidification Results**

The temperature profile in the reservoir remained nearly unchanged, except

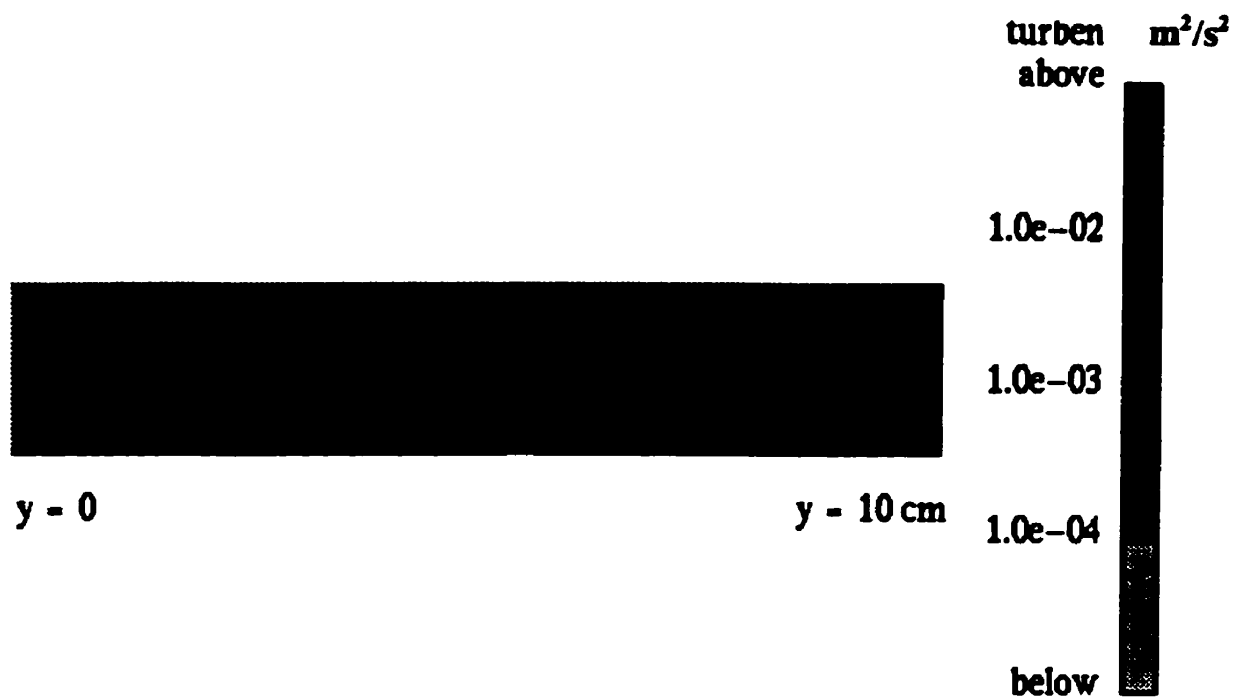


Figure 4-50. Turbulent kinetic energy at the symmetry axis - $C = 1 \times 10^5$ ($v_b = 1 \text{ m/s}$, $l = 10 \text{ cm}$, $d_g = 10 \text{ mm}$, $h_m = 7 \text{ cm}$, $Q = 4.5 \times 10^{-3} \text{ m}^3/\text{s}$)

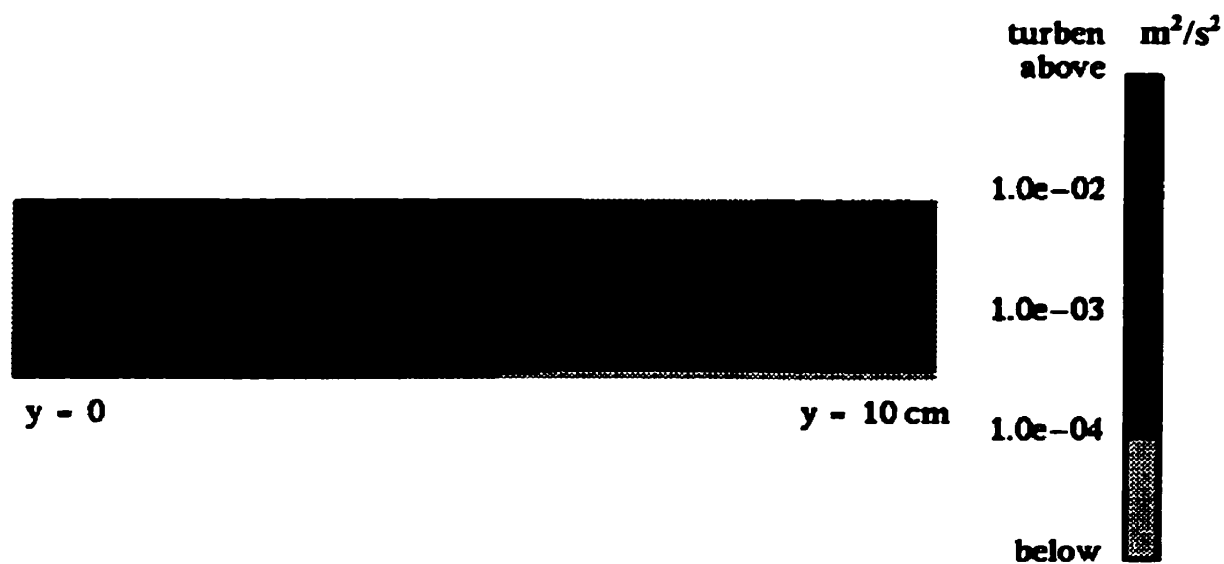


Figure 4-51. Turbulent kinetic energy at the symmetry axis - $C = 1 \times 10^9$ ($v_b = 1 \text{ m/s}$, $l = 10 \text{ cm}$, $d_g = 10 \text{ mm}$, $h_m = 7 \text{ cm}$, $Q = 4.5 \times 10^{-3} \text{ m}^3/\text{s}$)

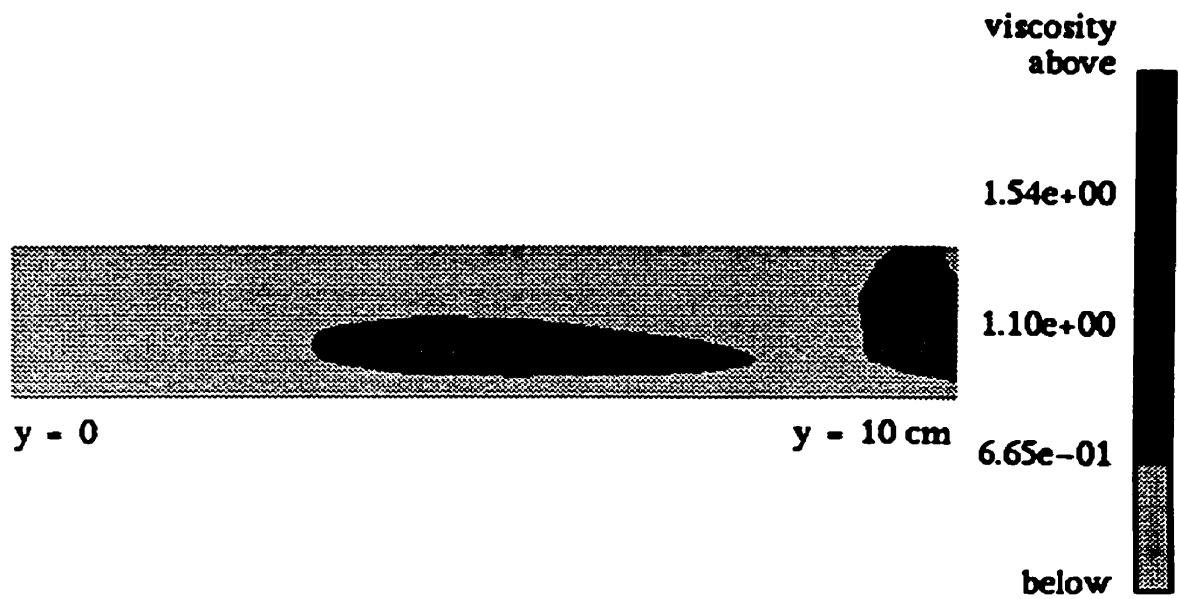


Figure 4-52. Turbulent viscosity at the symmetry axis - $C = 1 \times 10^5$ ($v_b = 1$ m/s, $l = 10$ cm, $d_g = 10$ mm, $h_m = 7$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

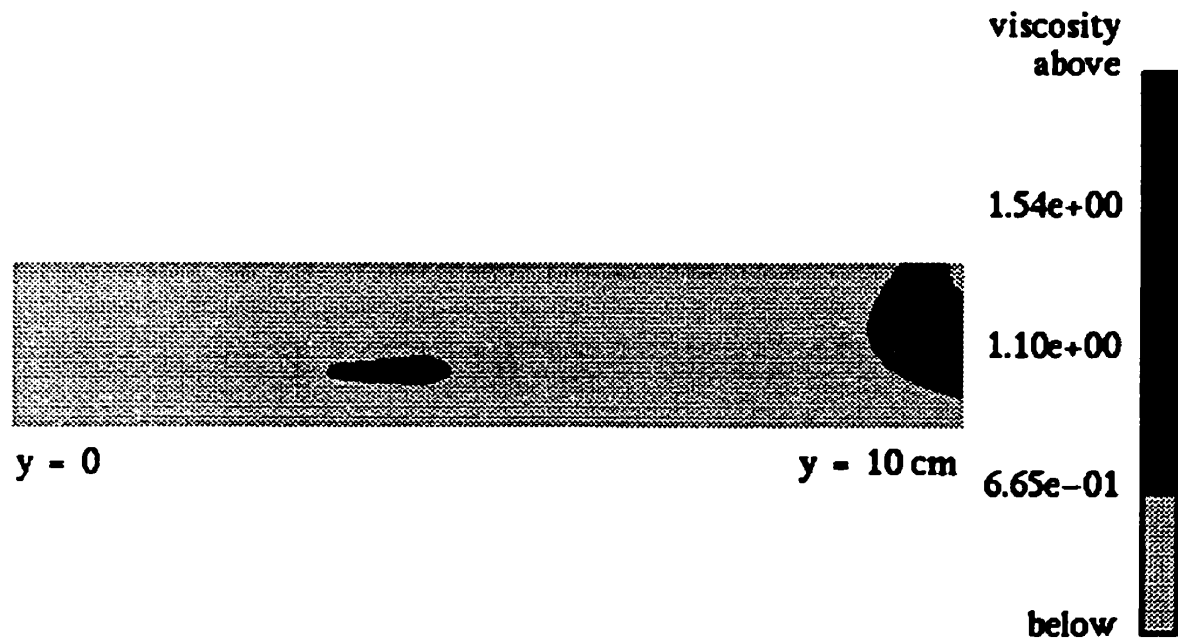


Figure 4-53. Turbulent viscosity at the symmetry axis - $C = 1 \times 10^7$ ($v_b = 1$ m/s, $l = 10$ cm, $d_g = 10$ mm, $h_m = 7$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

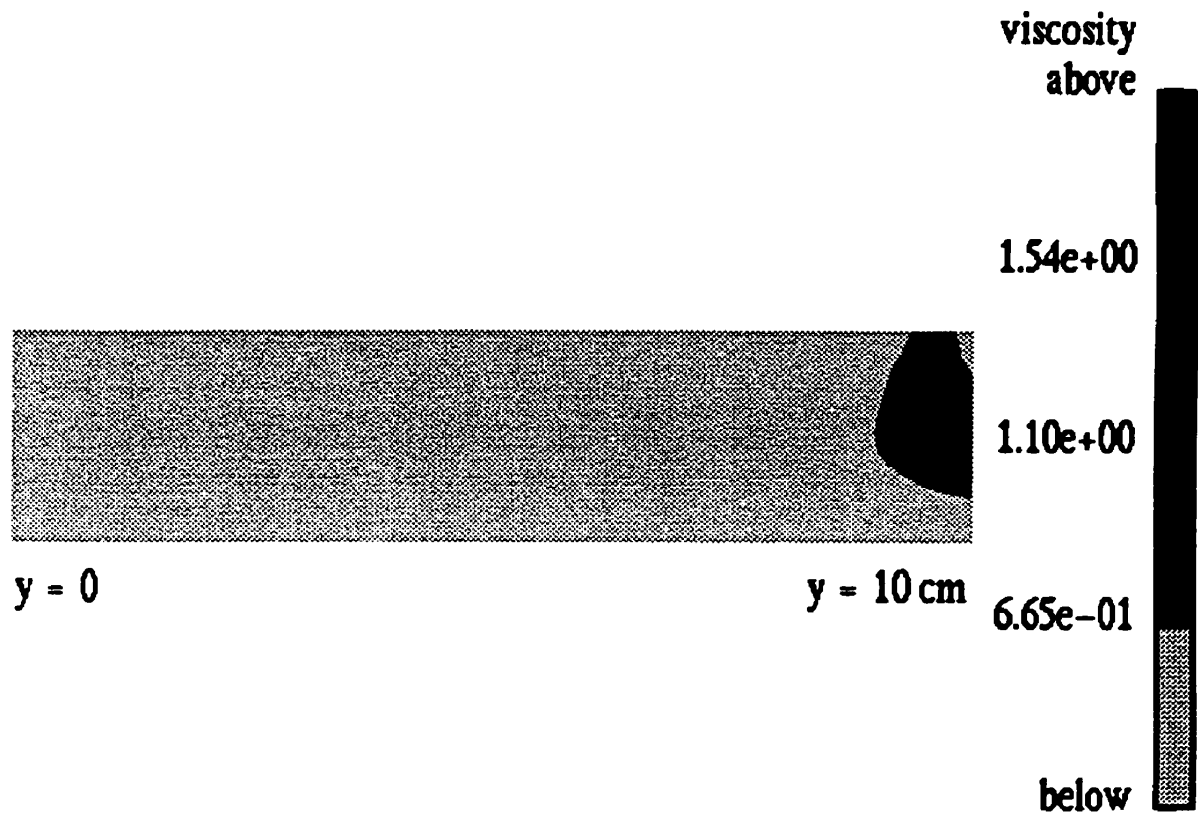


Figure 4-54. Turbulent viscosity at the symmetry axis - $C = 1 \times 10^9$ ($v_b = 1 \text{ m/s}$, $l = 10 \text{ cm}$, $d_g = 10 \text{ mm}$, $h_m = 7 \text{ cm}$, $Q = 4.5 \times 10^{-3} \text{ m}^3/\text{s}$)

for the region above the belt, where the metal is already mushy. These changes are shown in Figures 4-55 and 4-56, for the smaller and bigger values of C . For the smaller C , the difference is best noticed near the nozzle entrance, where the mushy zone is enlarged and the reheating is not as strong as in the standard case. For the bigger C , the modifications happened throughout the strip's length: the strip is hotter and the mushy zone is minimal at the exit gap. Almost the whole strip is above 1540°C at this point, which will eventually retard the solidification.

At 40 cm from the meniscus, the temperature profiles are more representative of the mushy zone shape. Figures 4-57 and 4-58 show very clearly the effect of the Darcy coefficient. A small value for C maximizes the mushy zone: here, almost no liquid is seen in the center of the strip after 40 cm. On the contrary, an exaggeratedly high value for C would reduce considerably the mushy region, confining it to a narrow region of approximately 1 mm. To make up for this diminished mushy zone, the solidified shell is thicker: 1.3 mm. This behavior would portray a "banded" solidification, which is in disagreement with the current experience and knowledge in carbon steel solidification.

Table 4-6 shows that the divergence in solid fraction is still present, for the symmetry axis and for the edge of the strip, though it would be well reduced in a hypothetical "banded" solidification.

The simulations involving the Darcy coefficient C showed the relevance of its accurate determination in the pursuit of precise temperature profiles and mushy zone morphology. Therefore, efforts to determine this parameter for the specific process should be encouraged.

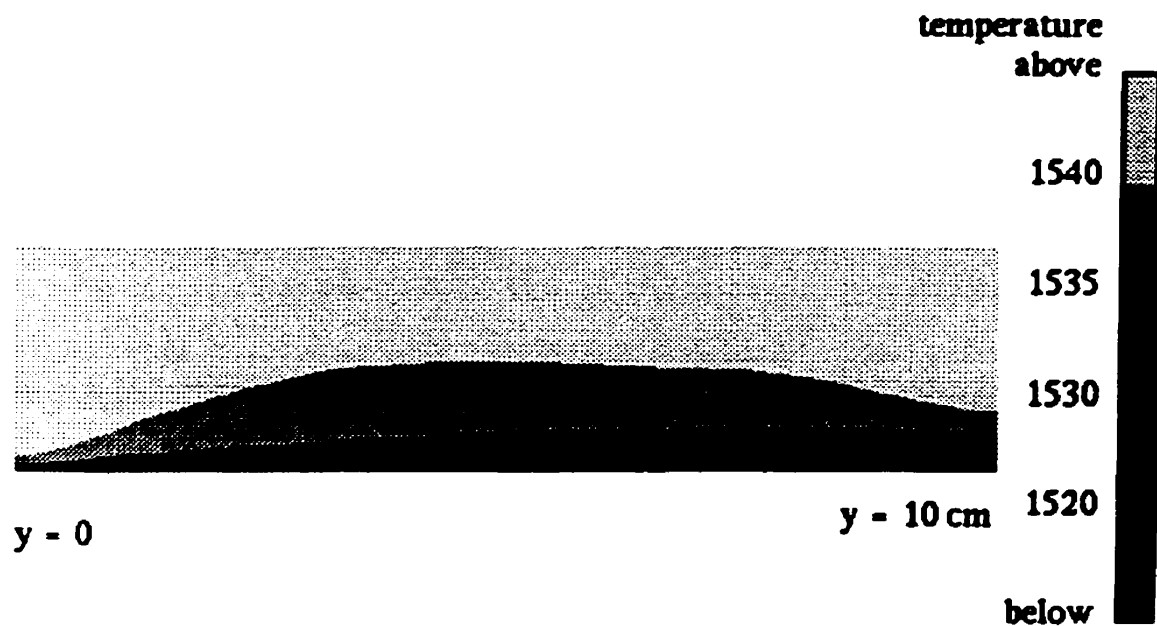


Figure 4-55. Temperature profile at the symmetry axis in the region of the strip inside the reservoir - $C = 1 \times 10^5$ ($v_b = 1$ m/s, $l = 10$ cm, $d_g = 10$ mm, $h_m = 7$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

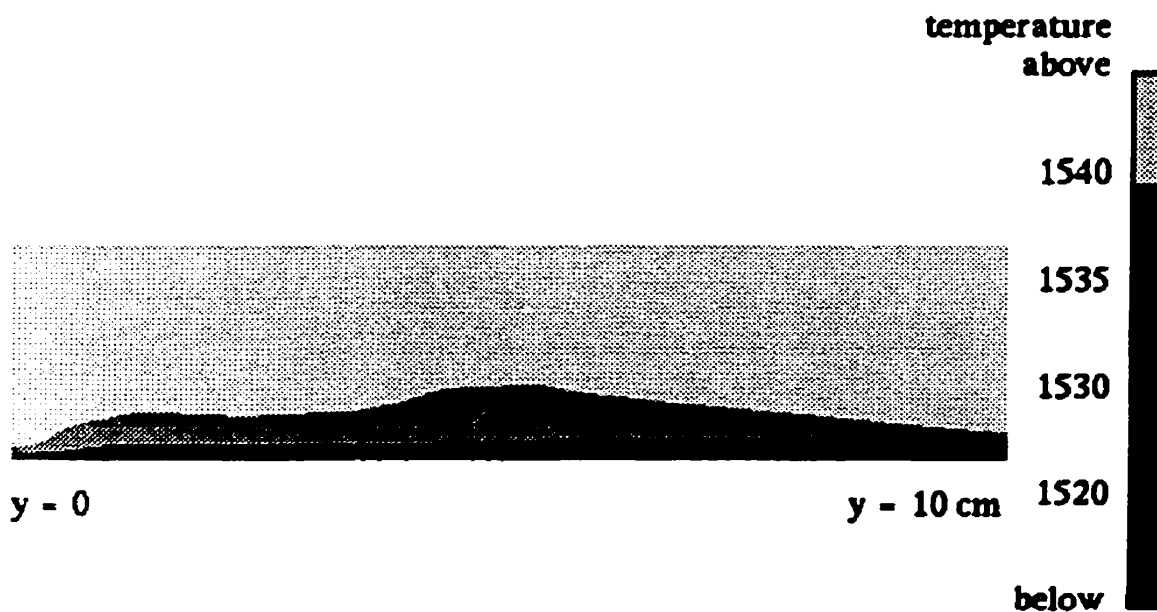


Figure 4-56. Temperature profile at the symmetry axis in the region of the strip inside the reservoir - $C = 1 \times 10^9$ ($v_b = 1$ m/s, $l = 10$ cm, $d_g = 10$ mm, $h_m = 7$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

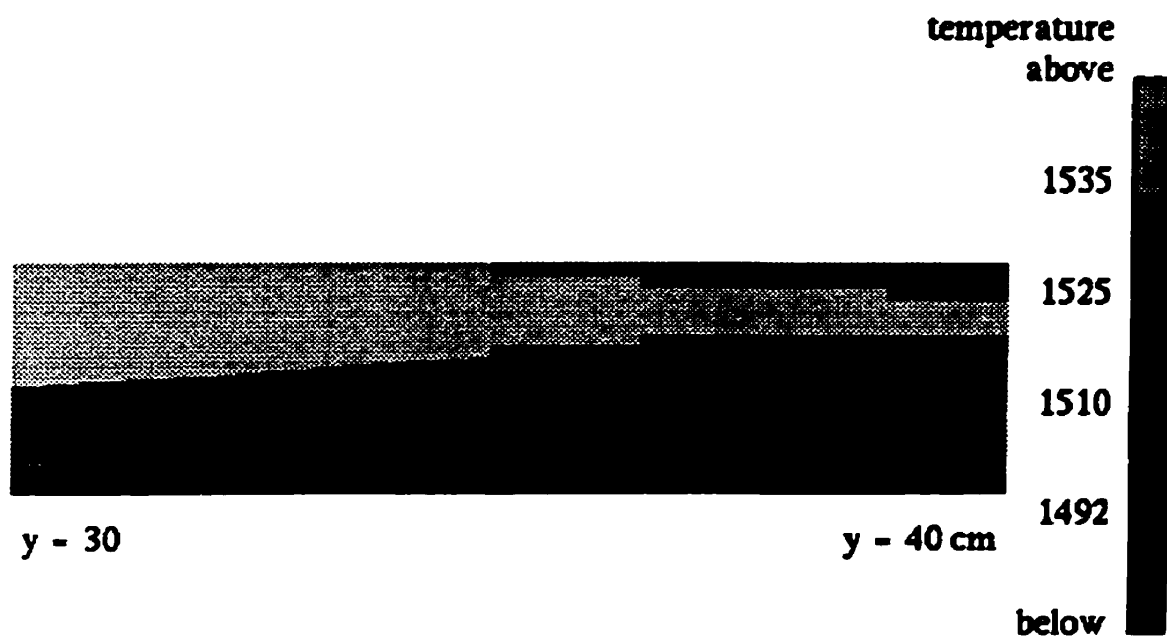


Figure 4-57. Temperature profile at the symmetry axis in the final 10 cm of the domain - $C = 1 \times 10^5$ ($v_b = 1$ m/s, $l = 10$ cm, $d_g = 10$ mm, $h_m = 7$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

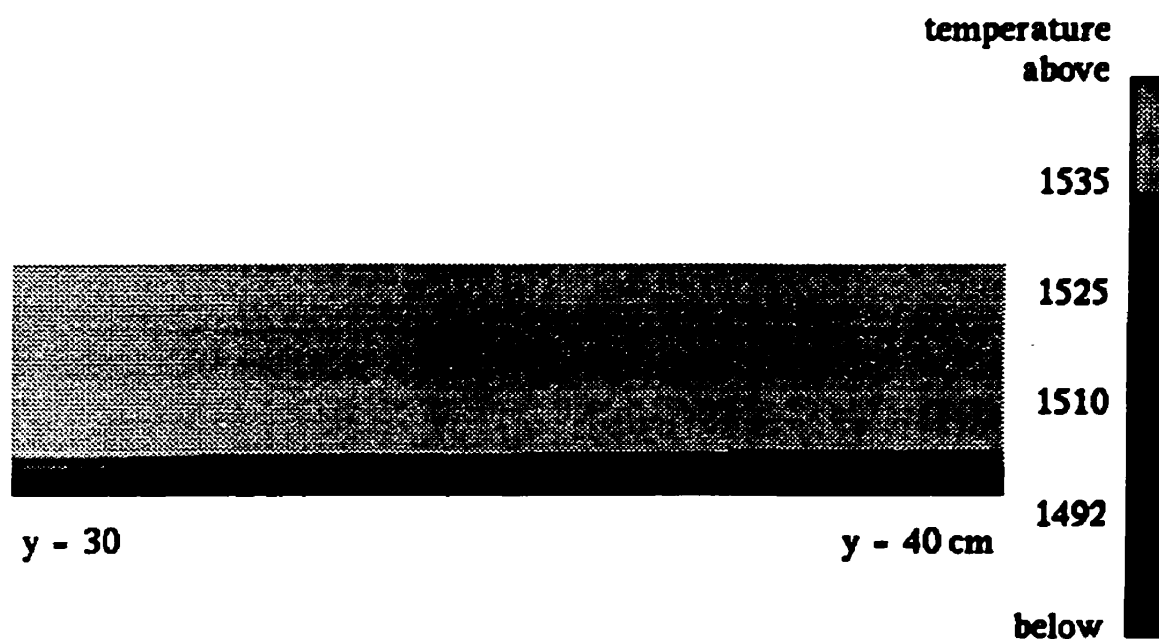


Figure 4-58. Temperature profile at the symmetry axis in the final 10 cm of the domain - $C = 1 \times 10^9$ ($v_b = 1$ m/s, $l = 10$ cm, $d_g = 10$ mm, $h_m = 7$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

Table 4-6 Average solid fraction at $y = 40$ cm, for various values of C .

C	f_s (center)	f_s (edge)	Δ
1×10^5	0.175	0.280	0.105
1×10^7	0.157	0.189	0.032
1×10^9	0.163	0.167	0.004

4.6.2 EFFECT OF THE OVERALL HEAT TRANSFER COEFFICIENT h

Perhaps the most important boundary condition in a horizontal single-belt caster is the overall heat transfer coefficient h applied to the metal interface. As will be discussed in Chapter 8, the thermal boundary conditions for near-net-shape casting processes vary markedly among the various processes. Values of h up to $1 \text{ MW/m}^2 \cdot ^\circ\text{C}$ have been reported⁽⁶⁾ for splat cooling processes, and as low as $700 \text{ W/m}^2 \cdot ^\circ\text{C}$ for slab casting in a twin-belt machine⁽⁹⁾. Therefore, each type of contact between the molten metal and the cooling substrate corresponds to distinct ranges of h .

Measuring the heat transfer coefficient in an actual horizontal belt machine is not an easy task. The moving belt restricts the positioning of thermocouples. Measurements of the thickness of the solidified shell and of microscopic parameters, such as the SDAS, are broadly used and its values are correlated with associated heat fluxes and heat transfer coefficients. In addition, laboratory scale trials have been attempted to determine q and h for horizontal casting of aluminum

and steel^(9,10). These earlier efforts, though relevant, failed to reproduce more closely the industrial conditions of wettability and relative velocity between the liquid metal and the substrate, since the laboratory trials were carried out using either static apparatus⁽⁹⁾ or employing speeds much lower than the ones intended for the single-belt caster configuration⁽¹⁰⁾.

The purpose of this section is to determine how critical the precise determination of the thermal boundary conditions at the interface metal-cooling belt is for the first stages of solidification. For that, the initial approach of a constant value of h throughout the length of the belt was replaced with two alternative strategies:

a) Stepwise variation of h , for the various regions of the caster.

A three-stage solidification process was considered for the first 40 cm of the belt. This assumption was based on the general knowledge that heat transfer conditions are nearly ideal during the first moments of casting and that the contact between the metal and the belt deteriorates as casting evolves, due to metal contraction and formation of an air gap. The scheme was as follows: for the part of the belt inside the reservoir, a maximum heat transfer coefficient of 10,000 W/m².°C was applied. Between the exit gap and the end of the front wall, an intermediate value of 5000 W/m².°C was considered and from this point up to the end of the domain, an h of 3000 W/m².°C was adopted.

i) Fluid Flow

The most noticeable change in fluid flow in the reservoir is the

"horizontalization" of the recirculating flow right above the cooling belt up to half the reservoir length, as shown in Figure 4-59. The reason for that might have been the reduced effect of the drag force in the recirculating flow, due to a quicker formation of a solid and mushy zone. However, this alteration does not seem relevant, since most of the recirculated flow is dragged down in the region next to the back wall, as in the standard condition.

In the other regions of the reservoir, the flow patterns are nearly identical to the standard ones. The turbulence profiles also indicate that the effect is limited to the region immediately above the cooling belt.

ii) Heat Transfer/Solidification Results

The doubled heat transfer coefficient in the reservoir zone is responsible for the formation of a thin solidified shell within the reservoir. Another interesting finding is that the temperature profile is suddenly changed at approximately 3 cm from the meniscus ($y = 3$ cm, Figure 4-60), switching from cooling to reheating. This reversion may not be credited to the effect of the incoming flow, but rather to the changes in the flow pattern mentioned above. The premature solidification contributes to damp turbulence right above the belt, and the liquid region above the solid/mushy zone has its cooling retarded.

Table 4-7 gives the solid fractions compared with the case of constant h . Despite the higher initial solidification for the varying h , the results after 40 cm are nearly the same, with f_s still slightly higher than the original case. This is explained by the subsequent reduction of h to $3000 \text{ W/m}^2\cdot^\circ\text{C}$. However, this reduction will make a considerable difference if the domain is extended to 1.5 m.

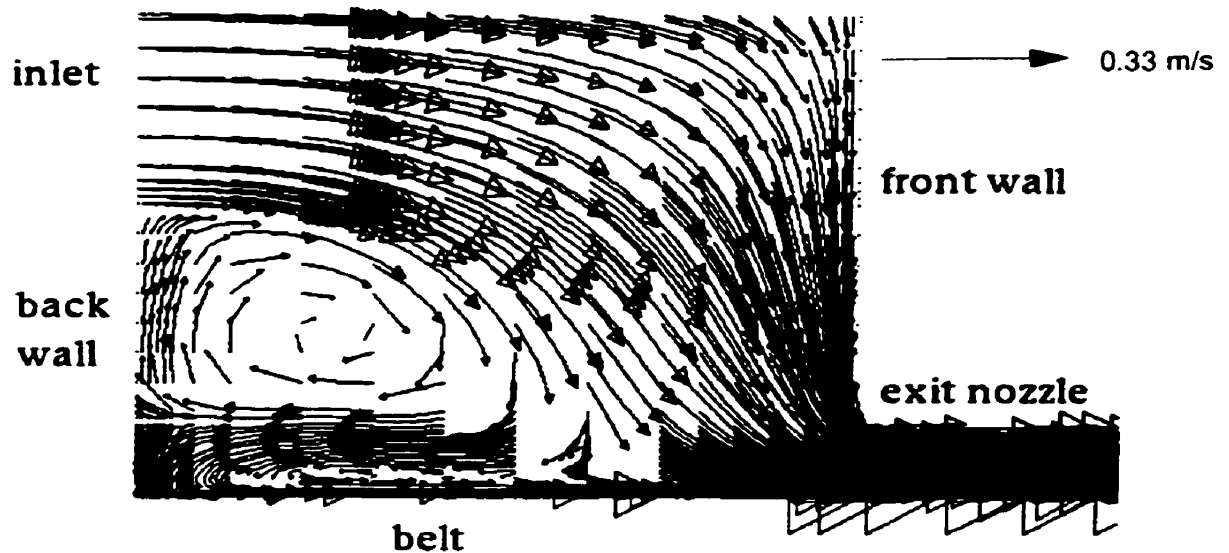


Figure 4-59. Vertical cut along the symmetry axis - Stepwise variation of h ($v_b = 1$ m/s, $l = 10$ cm, $d_s = 10$ mm, $h_m = 7$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

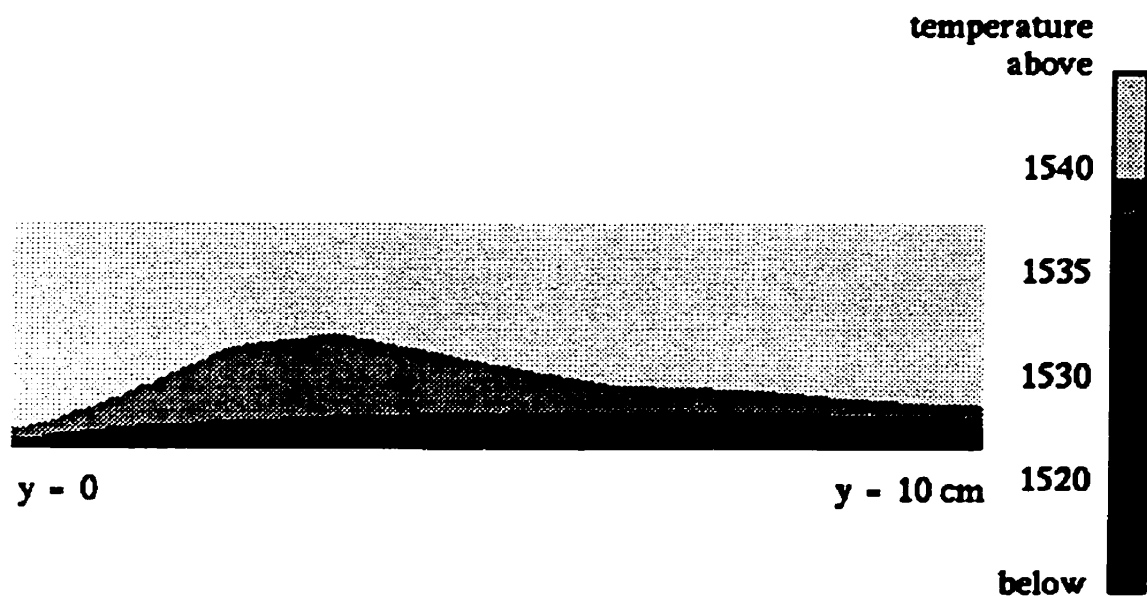


Figure 4-60. Temperature profile at the symmetry axis in the region of the strip inside the reservoir - Stepwise variation of h ($v_b = 1$ m/s, $l = 10$ cm, $d_s = 10$ mm, $h_m = 7$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

At this point, the solid shell will be 18% less than in the original case.

Table 4-7 Average solid fraction at $y = 40$ cm, considering various schemes for the evolution of h .

Description	f_s (center)	f_s (edge)	Δ
Constant h	0.157	0.189	0.032
Stepwise variation	0.167	0.201	0.034

b) Transient evolution of h , as given by the experiments carried out with the apparatus described in Chapter 8.

Figure 4-61 shows the evolution of the heat transfer coefficient in a typical experiment carried out in an apparatus where the substrate is moving at speeds in the same order as those intended industrial speeds for the single-belt caster. To input the variation of h in the code in a simplified manner, two stages were considered: the first is a linear variation of h from time zero up to the peak and the second is the decay of h according to the formula:

$$\frac{q}{q_p} = 1.04 - 0.275t + 0.00144t^2 + 5 \times 10^{-4}t^3 \quad (4-1)$$

Figure 4-62 shows the excellent fit given by this correlation to the experimental data obtained in the apparatus. For $t=0$ and very small times, a restriction was imposed on the code, to avoid a value of h higher than the peak.

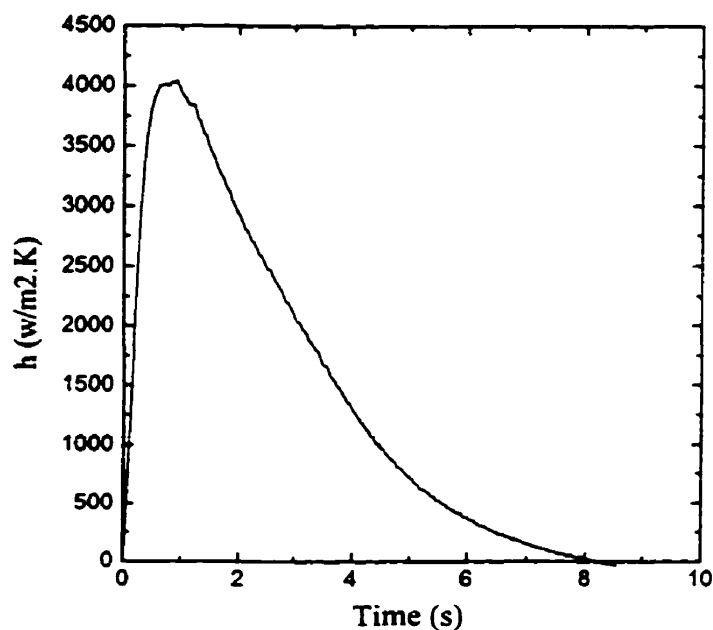


Figure 4-61. Calculated interfacial heat transfer coefficients for the casting of steel in the experimental apparatus.

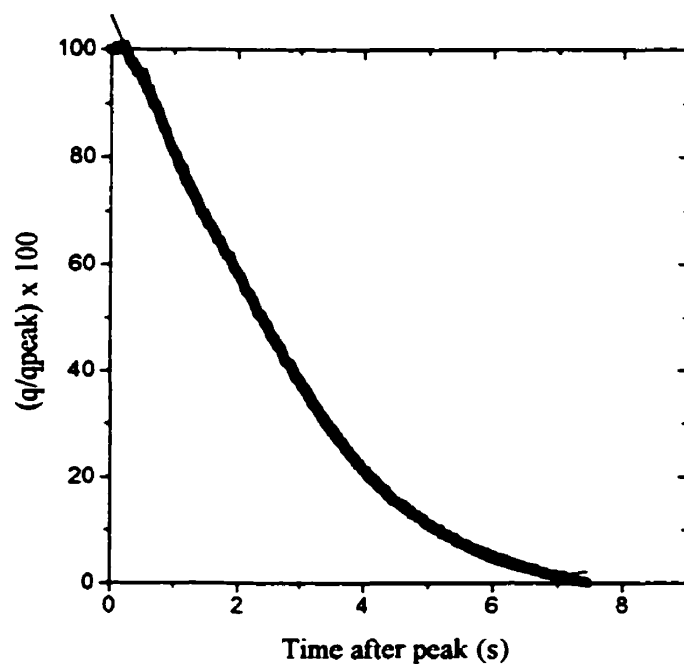


Figure 4-62. Variation in normalized interfacial heat fluxes versus contact times following the peak in interfacial heat flux, for the casting of steel in the experimental apparatus.

The most important advantage in using this data is that they were obtained under conditions fairly similar to actual ones. One may find different values for different belt substrates and coatings, but the shape of the curve is believed to remain approximately the same.

To summarize the analysis, Table 4-8 provides a comparison between the average solid fractions and thicknesses of the solid shell at 1.5 m from the meniscus, for the three cases considered for h : uniform value, stepwise variation and transient evolution. The results stress the importance of an accurate determination of the evolution of h with time.

Table 4-8 Average solid fractions and solid shell thicknesses at 1.5 m from the meniscus (entry wall), for different schemes of h .

Description	f_s (center)	f_s (edge)	X (cm)
Constant h	0.393	0.436	2.60
Stepwise variation	0.335	0.386	2.20
Transient evolution	0.278	0.342	1.80

4.6.3 THERMAL BOUNDARY CONDITIONS ON THE WALLS

In the standard condition, no conduction is considered either to the lateral wall or back walls, since they are not internal boundaries. Only on the front wall was a heat flux of 50 KW/m² applied. This value represents an estimate of the

actual heat flux into the refractory bricks. Applying a constant heat flux as a boundary condition simplifies the calculation at the wall, since the Laplace equation for heat conduction does not need to be solved in that part of the domain. Moreover, heat fluxes are employed more often in the industrial environment, for representing the heat losses at the wall.

The relevant question, however, is the right value for the heat fluxes that should be applied at the walls. To evaluate the sensitivity of the model to this parameter, heat fluxes of 50 and 100 KW/m² were adopted for all walls. This range is believed to cover the industrial values for the thickness and type of refractory (alumina) intended to be used.

i) Fluid Flow Results

In both cases, no modifications were observed in the flow pattern and turbulence profile in the reservoir zone.

ii) Heat Transfer/Solidification Results

In Figure 4-63, it is seen that the region of temperatures below 1542°C is larger in the region next to the front wall for the 50 KW/m² flux and a tiny mushy region is predicted at the meeting point of the two walls, on the top of the reservoir, for the case of a 100 KW/m² flux (Figure 4-64).

Another interesting observation in Figures 4-63 and 4-64 is the occurrence of "cold zones" next to the back wall, especially where the walls meet. This is not likely to become a problem, since the temperature decreases by a maximum of 3°C in the first case and, for a more intense heat flux, the minimum temperature in the

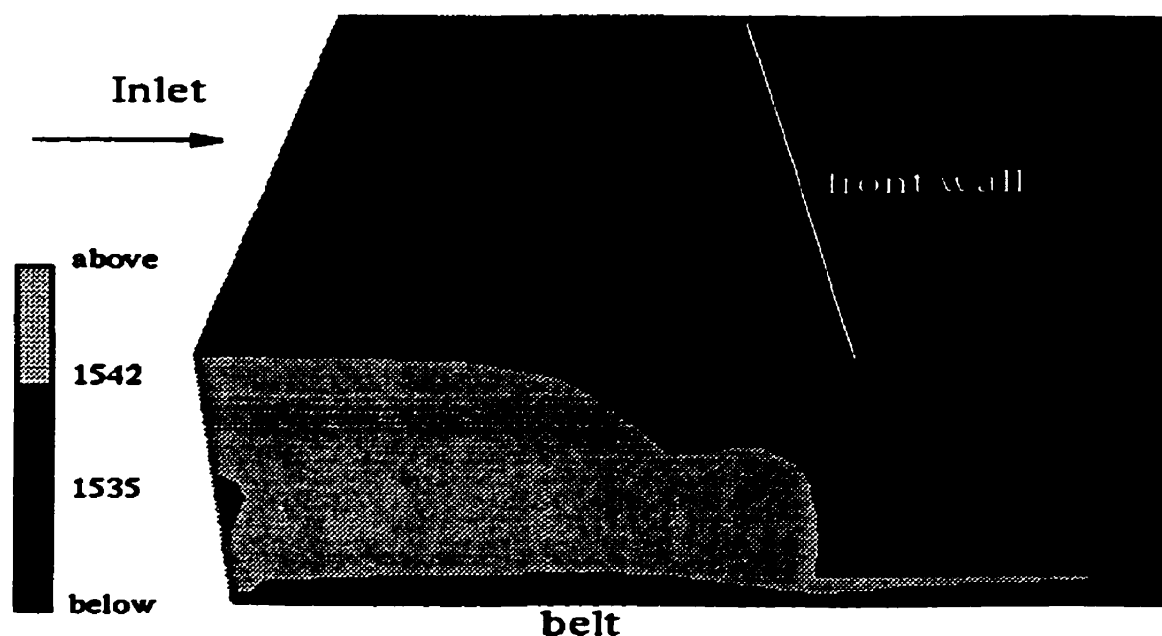


Figure 4-63. Temperature profile in the reservoir - Detail of the top edge - Heat loss to the walls = 50 KW/m^2 ($v_b = 1 \text{ m/s}$, $l = 10 \text{ cm}$, $d_g = 10 \text{ mm}$, $h_m = 7 \text{ cm}$, $Q = 4.5 \times 10^{-3} \text{ m}^3/\text{s}$)

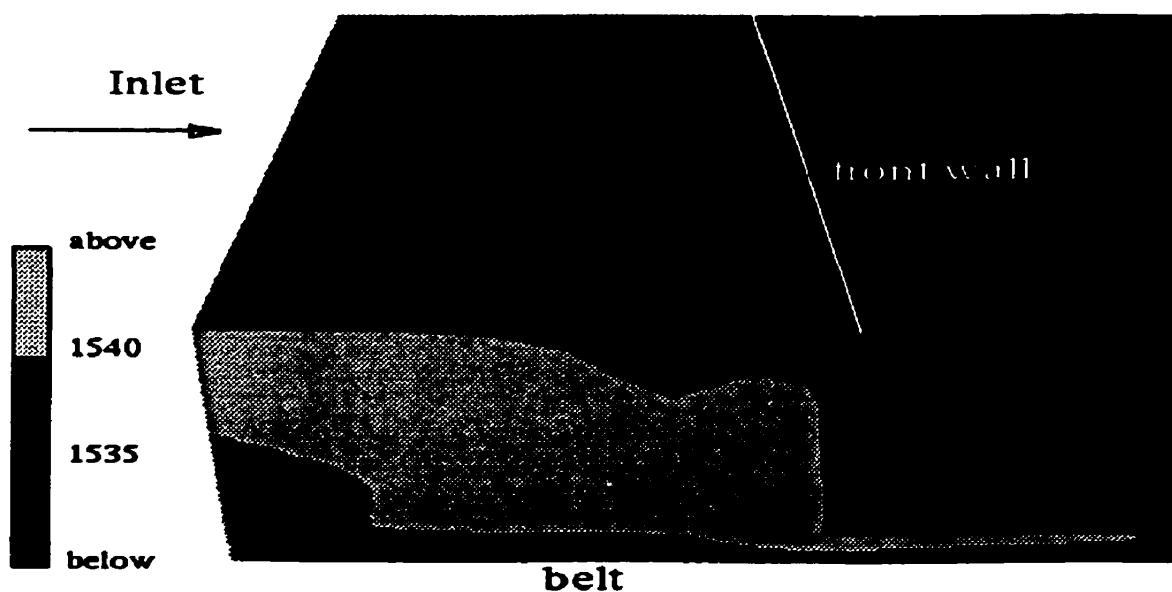


Figure 4-64. Temperature profile in the reservoir - Detail of the top edge - Heat loss to the walls = 100 KW/m^2 ($v_b = 1 \text{ m/s}$, $l = 10 \text{ cm}$, $d_g = 10 \text{ mm}$, $h_m = 7 \text{ cm}$, $Q = 4.5 \times 10^{-3} \text{ m}^3/\text{s}$)

neighbourhood of the back wall is still 4°C above the freezing point. Therefore, no skull formation is expected at the back wall.

At the end of the domain, as one would expect, the overall solid fractions were higher, considering heat losses to the walls, as given by Table 4-9. It is readily concluded that the divergence at the center-wall increased, due to a considerable increase in the solid fraction at the edge, whereas in the center, f_s was nearly the same. The solidified shell is the same in all cases (1.1 mm), but for the 100 KW/m², a new solid layer of 0.2 mm is predicted to form on the sidewall region, at $y = 41$ cm, whereas for the standard condition, it is predicted at $y = 44$ cm.

Table 4-9 Average solid fraction at $y = 40$ cm, considering heat losses to the lateral and back walls.

Heat fluxes on the walls (KW/m ²)	f_s (center)	f_s (edge)	Δ
0	0.157	0.189	0.032
50	0.157	0.195	0.038
100	0.157	0.210	0.053

From the simulations that included heat fluxes at the walls, instead of adiabatic conditions, the following conclusions can be drawn:

- The average solid fractions at the edges increased, as expected, by

approximately 3% and 10% for heat fluxes of 50 KW/m² and 100 KW/m², respectively, whereas the solid fractions in the center remained nearly stable. This increased the deviation between the center and the edges;

- In the reservoir zone, no freezing was observed in the vicinity of the back wall and only a tiny mushy region is seen on the top of the reservoir. Therefore, an initial superheat of 10°C seems reasonable for safe operation of the delivery system;

- The model proved to be able to handle any thermal boundary conditions required by the operational conditions. An experimental determination of the heat losses on the edges is necessary, to supply the model with more realistic boundary conditions.

-

4.6.4 EFFECT OF THERMOPHYSICAL PROPERTIES

The uncertainty in thermophysical properties of the metal, in particular steel, is a source of error in a mathematical model for fluid flow/heat transfer and solidification. Some properties have a smaller margin of error in its determination, some larger. In addition, the assumptions usually made in this kind of model, of constant values for a relatively wide temperature range, could also lead to considerable errors in the results.

With respect to the thermophysical properties of steel, the main assumptions made in this work were:

- i) **Constant density.** This is a requirement of the numerical method, as discussed earlier, and has little influence on the results;

- ii) Constant value for the thermal conductivity. Since the diffusion coefficient Γ_b will assume much higher values in the turbulent liquid, the influence of k will be limited to the solidified shell. Considering that the lower temperature at the end of the domain is usually above 1200°C, the assumption of constant k seems reasonable. The same for the specific heat C_p ;
- iii) A value of 6.85×10^{-3} kg/m.s was used for the molecular viscosity. However, some discrepancies exist in the literature concerning the value of μ . Thomas⁽¹¹⁾, for example, adopted a value of 5.5×10^{-3} kg/m.s for the viscosity of a low carbon steel.

In this section, the importance of the value assigned to the molecular viscosity in the turbulence κ - ϵ model will be evaluated. For that, a simulation was run using μ of 5.5×10^{-3} kg/m.s as given by Thomas⁽¹¹⁾.

i) **Fluid Flow Results**

The flow pattern is nearly identical to the standard case. Any changes were imperceptible. The same applies to the turbulent energy profile inside the reservoir.

ii) **Heat Transfer/Solidification Results**

The only discernible change was that the overall solidification at the end of the domain was reduced, especially on the wall, in comparison to the original case, as seen in Table 4-10. The mushy zone has shrunk a little, as well.

Table 4-10 Average solid fraction at $y = 40$ cm, for different values of μ .

Viscosity (kg/m.s)	f_s (center)	f_s (edge)	Δ
6.85×10^{-3}	0.157	0.189	0.032
5.50×10^{-3}	0.154	0.177	0.023

From this simulation, it was concluded that the new value of the molecular viscosity did not alter significantly the fluid flow patterns and the heat transfer results, for the particular problem. A 2% decrease in the solid fraction in the center of the strip and a 6% decrease on the wall region, resulted from such change. Since this property of the liquid metal is not easily determined, the results obtained are believed to be in the range of experimental error.

4.7 IMPORTANCE OF TURBULENCE MODELLING

One of the main purposes of this mathematical model was the inclusion of turbulent flow modelling in the delivery system. As discussed in Chapter 3, the issue of turbulent fluid flow has inspired various ways to deal with it.

The easiest and most immediate approach is to assign an ad-hoc factor to boost the molecular viscosity throughout the domain. This way, the kinematic viscosity will assume a constant value equal to the molecular viscosity times the ad-hoc factor in all regions of the liquid metal. In the mushy zone the viscosity, and by consequence the thermal conductivity, will be corrected according to its porosity (liquid fraction), following the same approach as in a κ - ϵ model. Using

an ad-hoc viscosity could work well for cases where the geometry is not very intricate and no major heterogeneities exist within the domain. When the flow follows a smooth path, as for example, in a twin-roll caster, turbulence modelling by κ - ϵ equations is not very crucial⁽¹²⁾.

For horizontal belt casters, Jefferies⁽²⁾ has presented a 2D model, with artificially boosted viscosity. Despite the usefulness of her results, the use of ad-hoc viscosity limits the applicability of her model. As discussed before, the fluid flow in this configuration changes dramatically due to the compression to which it is submitted at the entrance to the nozzle exit. The existence of a recirculation zone and the so-called "dead zone" near the front wall are also issues of particular relevance. Therefore, the assumption of ad-hoc viscosity was replaced with a more realistic one, which in the present case was represented by the Launder and Sharma κ - ϵ model.

To assess the actual implications of this change in strategy, simulations were run for the model, using a constant value of $6.85 \times 10^{-1} \text{ kg/m.s}$ ($100 \times \mu$) for the viscosity throughout the domain. The results were then tabulated and compared with those given by the Launder and Sharma κ - ϵ model.

4.7.1 FLUID FLOW RESULTS

The fluid flow pattern for the case of the ad-hoc viscosity model is shown in Figure 4-65. The general aspect is the same as that for the κ - ϵ model. However, some differences are noticeable: the recirculation zone is pushed a little backwards and shrinks. The metal in the external part of the recirculatory circuit (outside the core) is all drawn in by the cooling belt.

4.7.2 HEAT TRANSFER/SOLIDIFICATION RESULTS

The ad-hoc viscosity implications on the temperature profiles may be seen in Figures 4-66 and 4-67. These figures give the temperature profile in the center of the strip for the first 40 cm of the belt, for the κ - ϵ model and ad-hoc assumption, respectively. Because of the damped turbulence in the ad-hoc case, the temperatures are somewhat higher than in the standard case. Almost the entire strip is above 1540°C up to the nozzle exit (10 cm from the meniscus). At the end of the domain, the dissimilarity is even more evident: on one hand, the solidified shell is thicker (1.3 cm); on the other hand, the liquid remainder of the strip is much hotter than in the results given by the κ - ϵ model, as seen in Figure 4-67. This result is explained by the underprediction of turbulence by the ad-hoc model, which reduces the heat exchange throughout the mushy and liquid regions and contributes to the thicker shell. Indeed, Jefferies⁽²⁾ reported values for the solidified shell that exceeded those calculated with the analytical solutions. In Chapter 6, the results of this model are compared with Jefferies' and the analytical solutions.

Another interesting observation from Figure 4-67 was that no mushy zone was found on the top surface of the strip at the end of the domain. That would underscore the mechanism of preferential solidification, from the bottom to the top of the strip. Indeed, at the end of the domain only 18% of the strip is mushy, against 40% for the κ - ϵ solution. In the ad-hoc solution, the flow is not sufficiently turbulent to transmit the heat losses at the interface shell-belt throughout the thickness of the strip.

For the region next to the front wall (Figure 4-68), the velocities calculated by the ad-hoc model are lower and therefore a considerably larger mushy zone

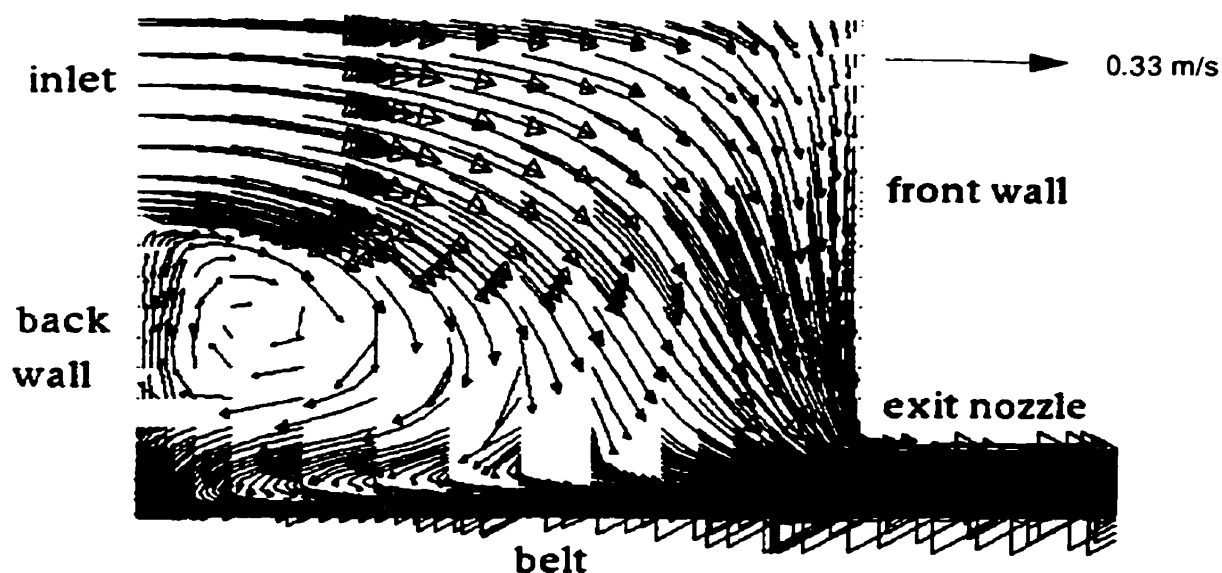


Figure 4-65. Vertical cut along the symmetry axis - Ad-hoc viscosity ($v_b = 1$ m/s, $l = 10$ cm, $d_g = 10$ mm, $h_m = 7$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

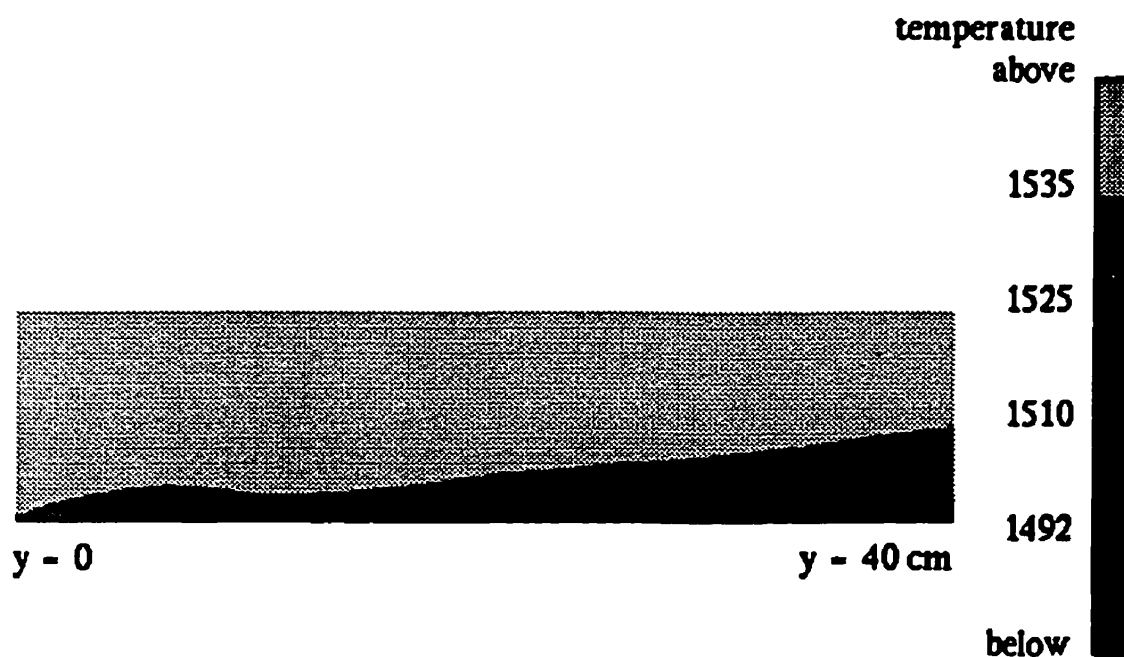


Figure 4-66. Temperature profile in the center of the strip, for the first 40 cm of the belt, using κ - ϵ model ($v_b = 1$ m/s, $l = 10$ cm, $d_g = 10$ mm, $h_m = 7$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

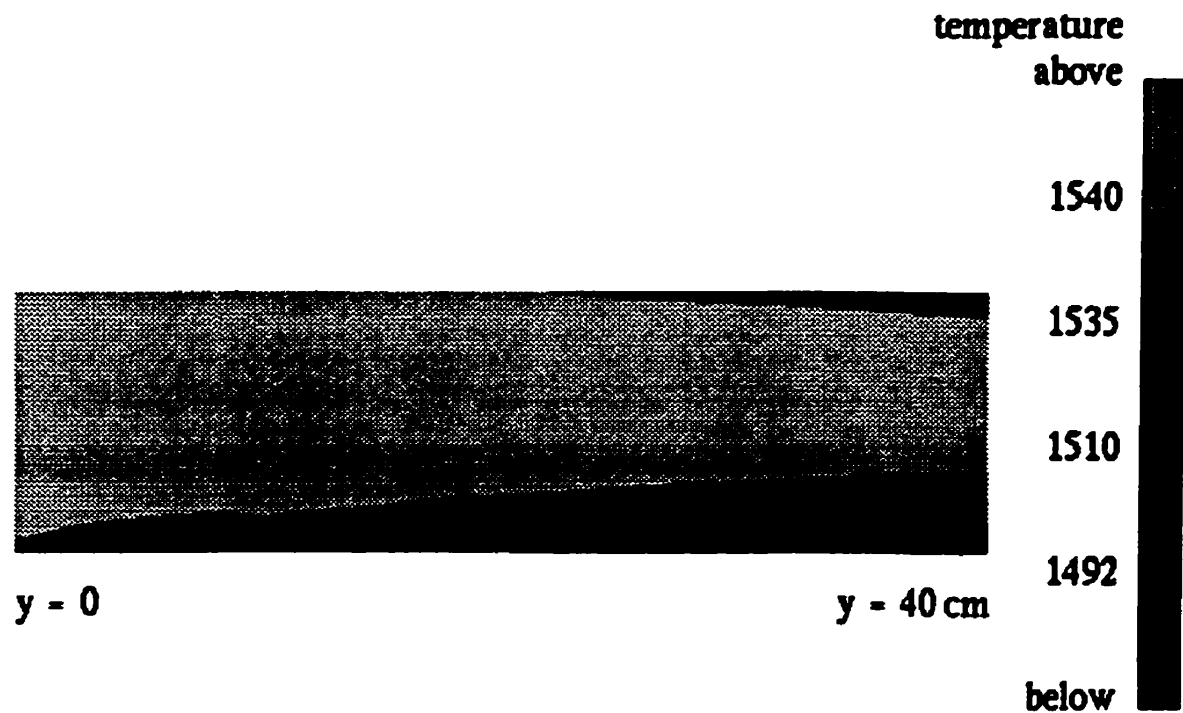


Figure 4-67. Temperature profile in the center of the strip, for the first 40 cm of the belt, using ad-hoc viscosity ($v_b = 1$ m/s, $l = 10$ cm, $d_g = 10$ mm, $h_m = 7$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

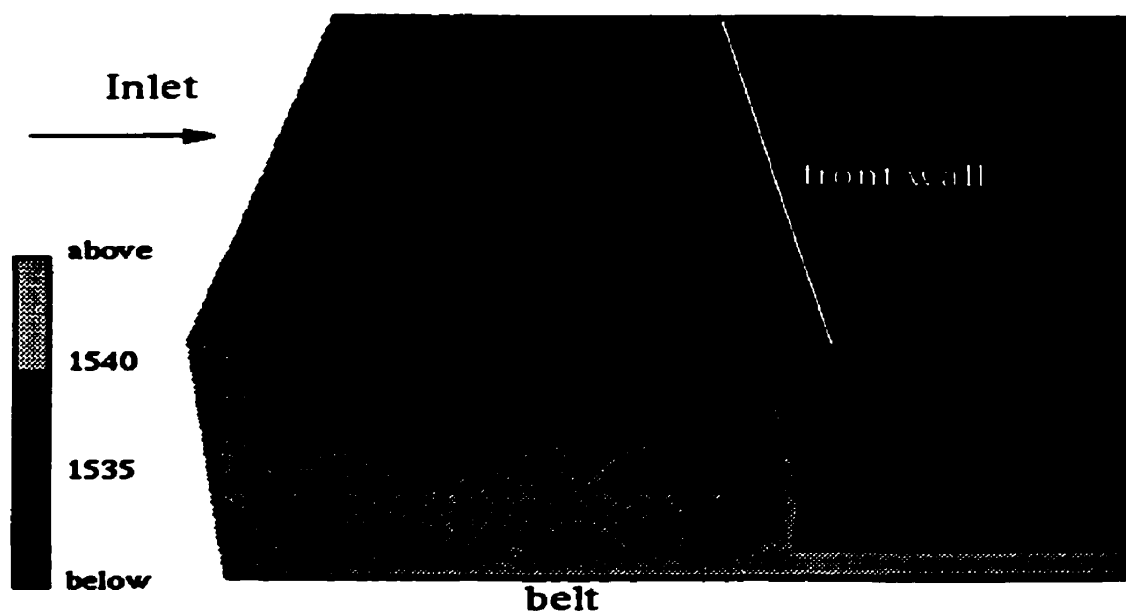


Figure 4-68. Temperature profile in the reservoir - Detail of the top edge - Ad-hoc viscosity ($v_b = 1$ m/s, $l = 10$ cm, $d_g = 10$ mm, $h_m = 7$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

appears as a result. In this region, the temperatures are 5°C lower, in the average, than the results given by the κ - ϵ model.

From the results presented above and previously in this Chapter, as well, one can readily conclude that the ad-hoc viscosity scheme is inappropriate for this particular configuration. The existence of a front wall within the computational domain, the change in flow direction and intensity of the sudden contraction at the nozzle gap, constitute limitations to such an approach. Evidently, these heterogeneities within the fluid flow could not be satisfactorily represented by the constant viscosity scheme assumed throughout the domain. Accordingly, overprediction of the solidified shell thickness and underprediction of the mushy zone size are expected with the use of the ad-hoc viscosity scheme.

4.8 EXTENDED POOLS FOR THINNER STRIPS

The improvements brought by larger pools to the casting of a 10-mm strip were encouraging and thus the modification was tested on the casting of thinner strips.

4.8.1 FLUID FLOW RESULTS FOR 8-mm THICK STRIP

It is seen in Figure 4-69 that the fluid flow modifications brought by the extended pool of 20 cm were quite distinct for the 8-mm gap. The smaller velocities made the flow right above the back wall fall down quickly and, as a consequence, the recirculation zone was smaller. The core of the whorl is more circular-shaped. With the shrinking of the recirculation zone, the flow assumes a

quasi-rectilinear trajectory towards the exit gap.

For the 30-cm pool, Figure 4-70 shows an even more distinct flow pattern. The recirculation whorl shrinks even more, to approximately 9 cm. This is due to the lower inlet flowrate (compared with the 10-mm gap) and the consequent loss of momentum by the fluid. On the other hand, the metal reaches the front wall at much lower velocities, which leads to the formation of a triangular-shaped "dead zone."

4.8.2 HEAT TRANSFER RESULTS FOR 8-mm STRIP

A main drawback of such modification is the cold zone formed near the front wall, as given by Figure 4-71, for the 20-cm pool. Temperatures down to 1515°C were found in that region. On the lateral wall region, the mushy zone extends up to the middle of the reservoir, as opposed to no mushy for the 10-cm pool.

If the 20-cm pool presented freezing near the front wall, what would be expected from the 30-cm pool? Figure 4-72 gives the temperature profile on the top of the reservoir and on the lateral wall. A 6 to 8-cm thick solid shell is predicted next to the back wall, in addition to a mushy zone extending up to almost half the reservoir in the center and for more than 20 cm on the edge. That would create obvious operational problems to the proposed delivery system.

The average solid shell for both cases was the same as in the 10-cm pool. However, the average solid fraction at the end of the domain varied, as shown in Table 4-11. Once again, f_s varied more on the edges than in the center of the strip,

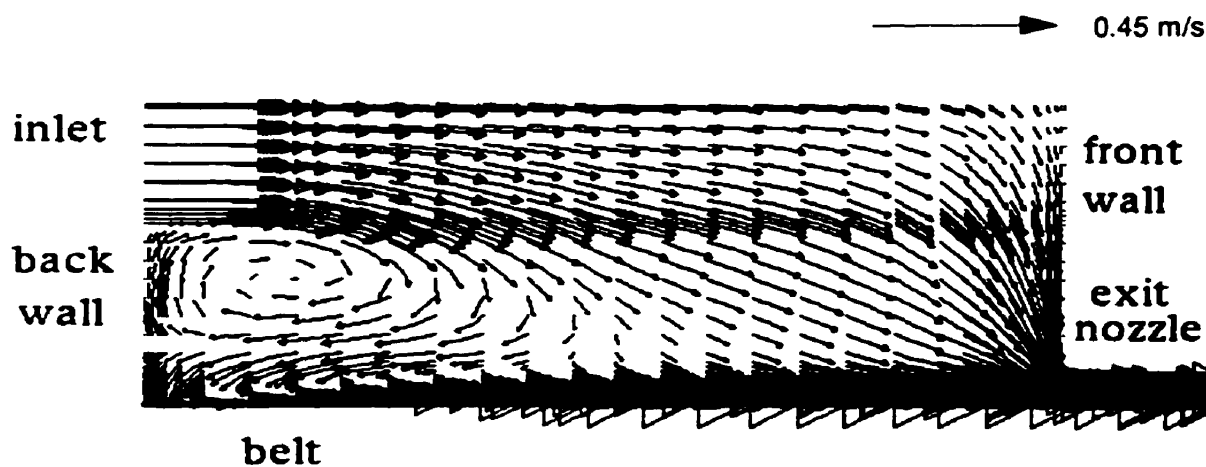


Figure 4-69. Vertical cut along the symmetry axis - 20-cm pool and 8-mm gap
 $(v_b = 1 \text{ m/s}, h_m = 7 \text{ cm}, Q = 3.6 \times 10^{-3} \text{ m}^3/\text{s})$

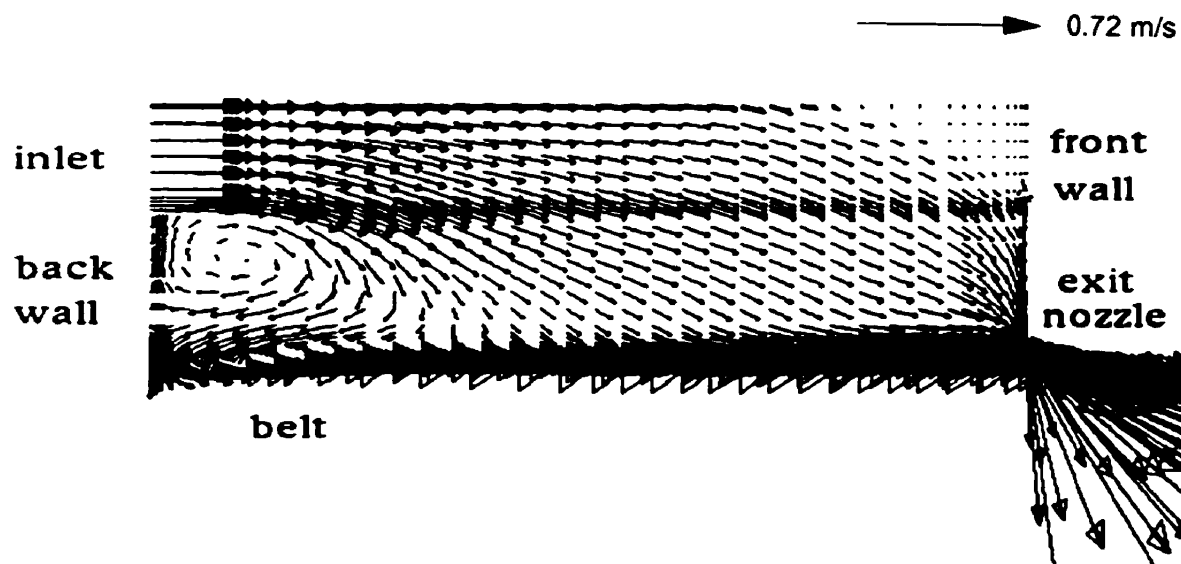


Figure 4-70. Vertical cut along the symmetry axis - 30-cm pool and 8-mm gap
 $(v_b = 1 \text{ m/s}, h_m = 7 \text{ cm}, Q = 3.6 \times 10^{-3} \text{ m}^3/\text{s})$

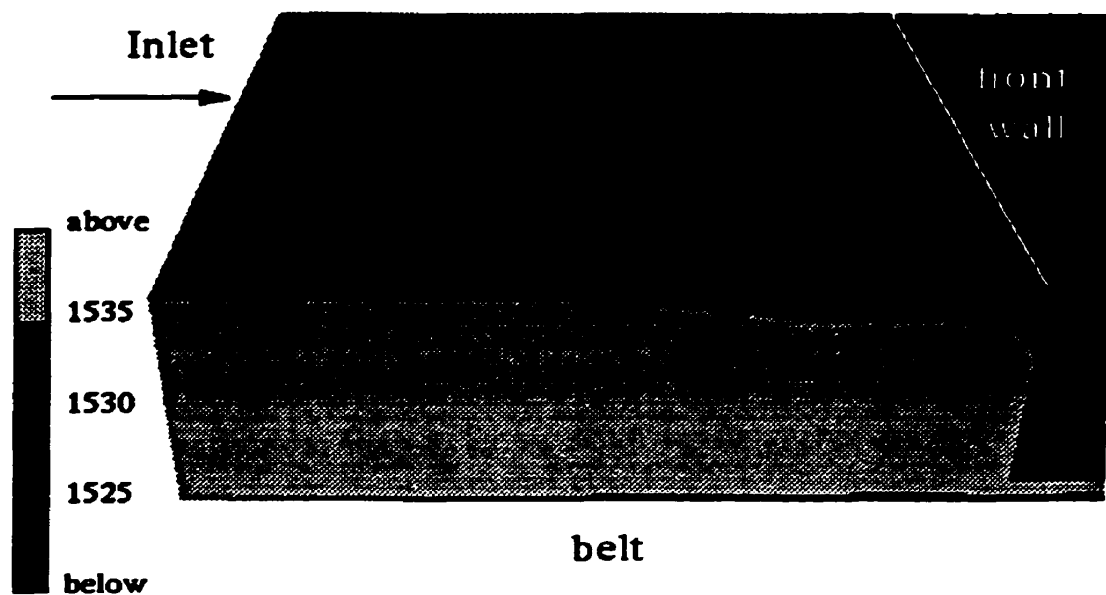


Figure 4-71. Temperature profile in the reservoir - Detail of the top edge - 20-cm pool and 8-mm gap ($v_b = 1$ m/s, $h_m = 7$ cm, $Q = 3.6 \times 10^{-3}$ m³/s)

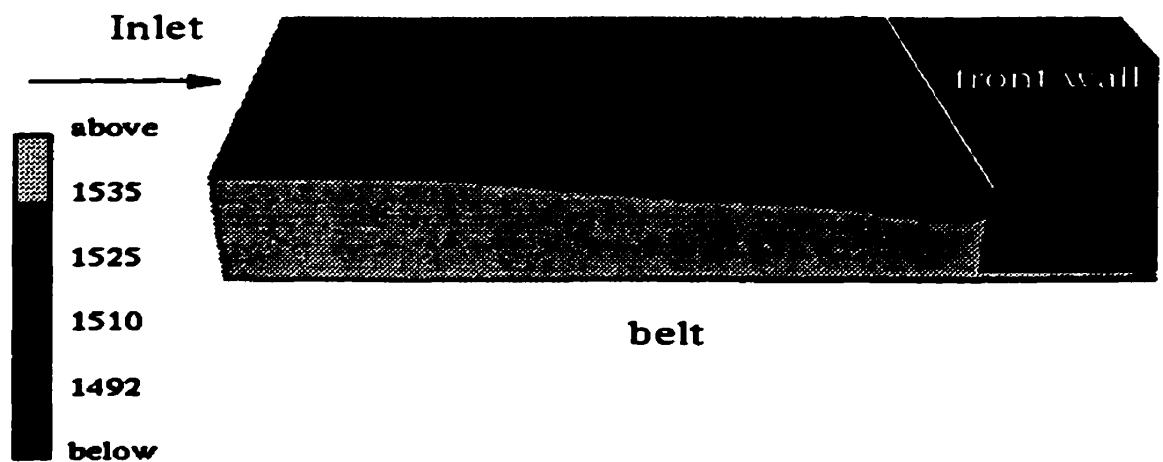


Figure 4-72. Temperature profile in the reservoir - Detail of the top edge - 30-cm pool and 8-mm gap ($v_b = 1$ m/s, $h_m = 7$ cm, $Q = 3.6 \times 10^{-3}$ m³/s)

which means that any attempt to make the solidification along the width more homogeneous passes through the reduction of the solid fractions on the edges, and this is obtained with a better flow distribution. The following observations can be made from these results:

1) The deviation in the solid fraction between center/wall increased slightly for the extended pool of 20 cm, but was considerably reduced for the 30-cm pool. Such reduction was due to the smoother flow, as mentioned above;

2) The most important drawback of the extended delivery zone is the cold zone created next to the back wall, which was caused by the low-momentum flow. This deficiency will require some modifications either in the geometry or in the initial superheat, for the extended pool length to be adopted.

Table 4-11 Average solid fraction at $y = 40$ cm, for various pool lengths and 8-mm strip thickness.

Pool length	f_s (center)	f_s (edge)	Δ
10 cm	0.195	0.235	0.040
20 cm	0.198	0.240	0.042
30 cm	0.196	0.230	0.034

4.8.3 FLUID FLOW RESULTS FOR 4-mm STRIP

The inlet flowrate is reduced by half, for the same belt speed, in comparison to the 8-mm strip. That would anticipate a weak inlet momentum, and the impossibility of the liquid metal to reach the front wall. A shrinking recirculation whorl is seen in Figure 4-73, for the 20-cm pool. Due to the weaker momentum, the flow is pushed downwards between the triangular-shaped dead zone near the front wall and the recirculation whorl next to the back wall. It is also seen for the first time a little recirculation whorl at the bottom of the dead zone, just above the exit gap. This whorl was caused by both the weak velocities of the metal in that region and the narrower exit gap. Therefore, the part of the flow coming from the inlet that has not been drawn in by the belt has to bypass this little whorl to reach the gap. As a consequence of the dead zone and the whorl, the flow becomes nearly horizontal just above the belt, in the second half of the reservoir.

For the 30-cm pool, a large dead zone is formed, as expected, extending from the front wall to 10 cm from the inlet, on the top of the reservoir (Figure 4-74). This zone is followed by a recirculation whorl that is formed right above the nozzle entrance. Hence, the predicted flow pattern in the middle of the reservoir is odd: a dead zone of zero velocity is flanked by the whorl on the left and the incoming flow, on the right. Because of this behavior, the recirculation zone next to the back wall nearly vanishes.

4.8.4 HEAT TRANSFER RESULTS FOR 4-mm STRIP

Table 4-12 shows that the average solid fraction is considerably reduced on the edges, for the extended pools of 20 and 30 cm, as in the other cases. The

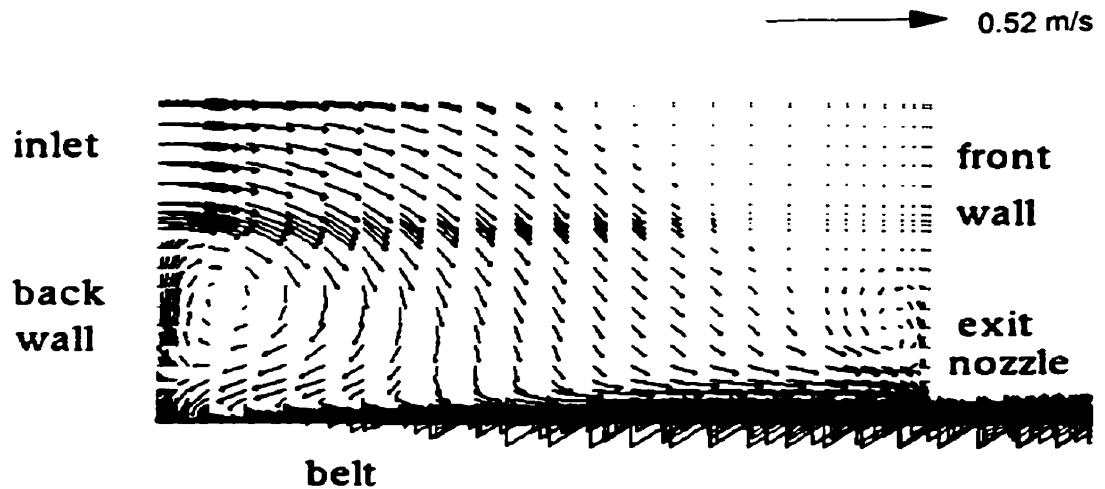


Figure 4-73. Vertical cut along the symmetry axis - 20-cm pool and 4-mm gap
 $(v_b = 1 \text{ m/s}, h_m = 7 \text{ cm}, Q = 1.8 \times 10^{-3} \text{ m}^3/\text{s})$

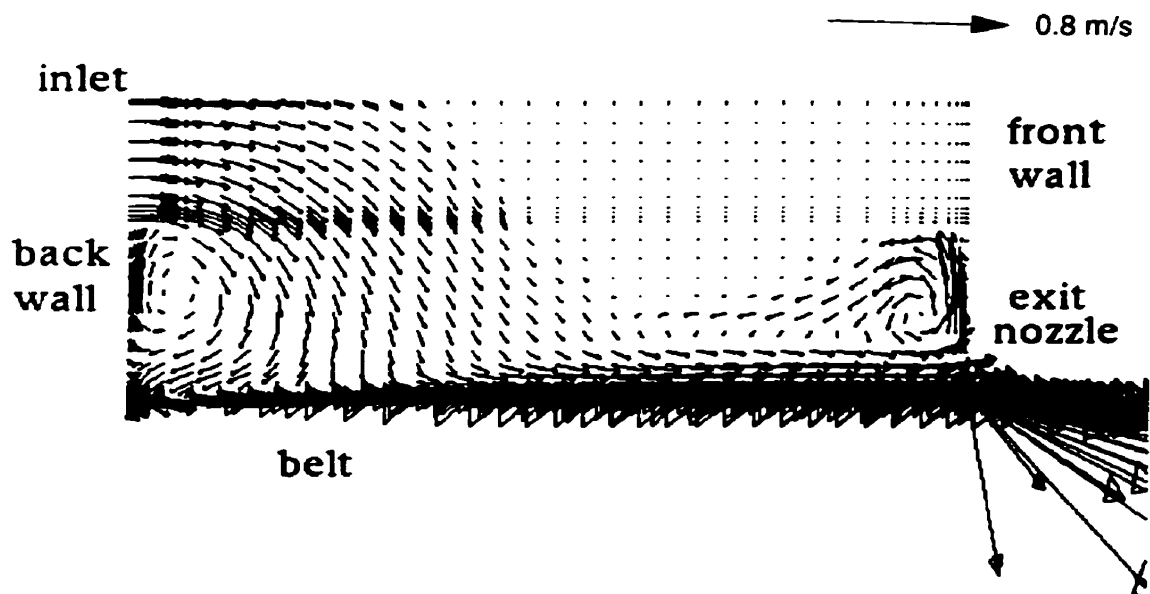


Figure 4-74. Vertical cut along the symmetry axis - 30-cm pool and 4-mm gap
 $(v_b = 1 \text{ m/s}, h_m = 7 \text{ cm}, Q = 1.8 \times 10^{-3} \text{ m}^3/\text{s})$

solidified shell, however, was the same.

Table 4-12 Average solid fraction at $y = 40$ cm, for various pool lengths and 4-mm strip thickness.

Pool length	f_s (center)	f_s (edge)	Δ
10 cm	0.356	0.414	0.058
20 cm	0.355	0.410	0.055
30 cm	0.352	0.401	0.049

As shown in Figures 4-73 and 4-74, the predicted dead zone near the front wall is much more pronounced than in the 10-cm pool length, for both the 20 and 30-cm pools. For the 20-cm pool, the top view of the reservoir (Figure 4-75) shows an enormous solid zone in the neighbourhood of the front wall, which is up to 10 cm long and 2 cm deep. Figure 4-76 shows the predicted temperature profile in the 30-cm pool. Two thirds of the reservoir would be either solid or mushy. It is seen that the metal reaches the nozzle completely on the mushy state, at temperatures between 1530°C and 1535°C. This means that, even for a very thin strip, the thermal load brought about by the turbulent incoming flow is not able to counterbalance the strong cooling caused by the thermal boundary conditions imposed on the bottom of the belt (constant heat transfer coefficient). The correction of the turbulent viscosity by the liquid fraction and consequently, the correction of the diffusivity term Γ_b , helps isolate the solid shell and the mushy zone from the thermal load of the incoming flow. For the diminutive exit gap, this could ultimately indicate freezing of steel within the nozzle .

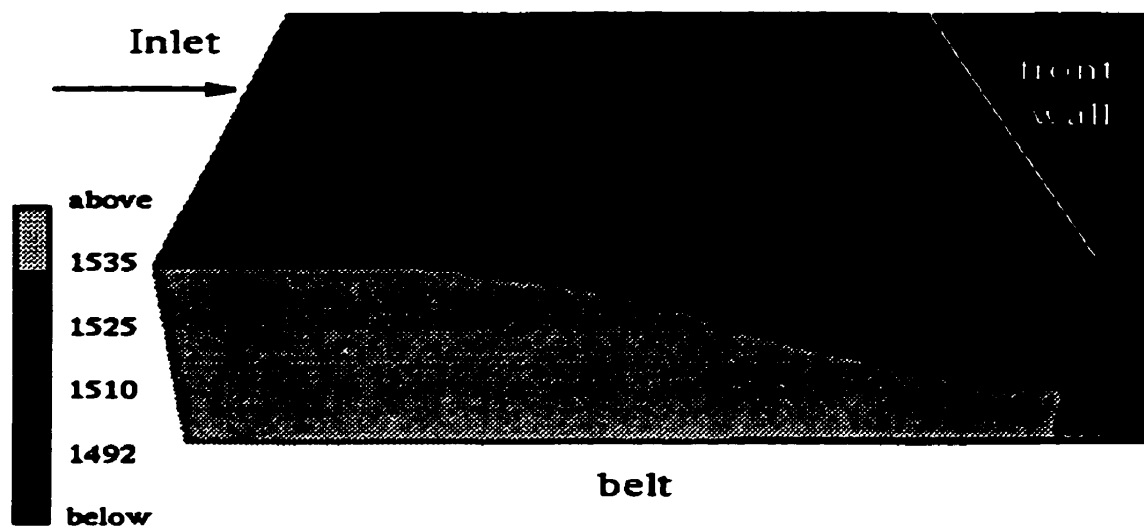


Figure 4-75. Temperature profile in the reservoir - Detail of the top edge - 20-cm pool and 4-mm gap ($v_b = 1$ m/s, $h_m = 7$ cm, $Q = 1.8 \times 10^{-3}$ m³/s)

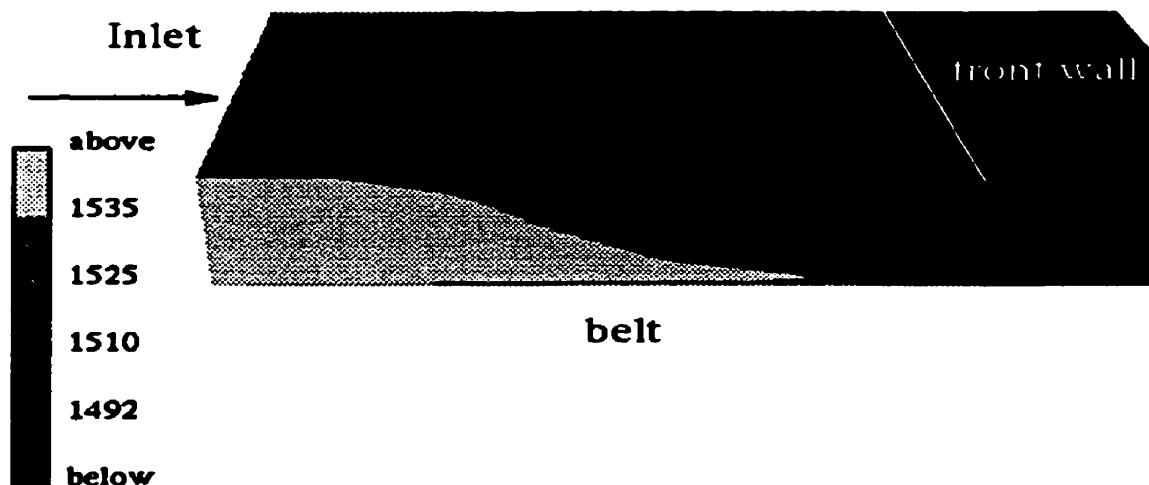


Figure 4-76. Temperature profile in the reservoir - Detail of the top edge - 30-cm pool and 4-mm gap ($v_b = 1$ m/s, $h_m = 7$ cm, $Q = 1.8 \times 10^{-3}$ m³/s)

4.9 SUMMARY AND CONCLUSIONS

The simulations carried out for the diverse geometrical configurations and for the other important parameters, such as the belt speed, initial superheat and strip thickness, gave important insight into the process operation and design of the delivery system. Some of the main conclusions that could be drawn were:

- 1) The turbulence levels in the region of the strip, inside the reservoir, are mainly affected by the belt speed and, to a lesser extent, by the inlet flowrate;
- 2) On the other hand, inlet flowrates determine the temperature profile within the extended reservoir and the occurrence, or not, of a cold region in the vicinity of the front wall;
- 3) For the 10-mm strip, the most homogeneous solidification was predicted using a belt speed of 0.8 m/s, a pool height of 9 cm, and a pool length of 20 cm. For the 8-mm and 4-mm strips, the extended pool length created a cold zone near the front wall that generally affected the viability of the process;
- 4) It was observed that the solidification progress is increasingly heterogeneous, i.e., the deviation in solid fraction between the center and the edge of the strip increases with time and for thinner strips;
- 5) Recirculation zones were formed near the back wall in all cases,

although some variables may be changed to reduce its extent. In this regard, the pool height was the most important variable;

- 6) The pool height and length modifications had little effect on the average solid fractions in the center of the strip. Mostly, the changes affected the solid fraction on the edges. Therefore, in those cases where the divergence center/edge was reduced, this happened mainly through the retardation of solidification near, and on, the edge;
- 7) The initial superheat was not found to affect the homogeneity of solidification across the width of the strip.

The main factors influencing the accuracy of the model were identified and its importance investigated. From this study, the most important conclusions were:

- 1) The morphology constant C was found to have a strong influence on the mushy zone morphology, when varied by two orders of magnitude from the original value. Therefore, its accurate determination for the specific process in study is of foremost importance;
- 2) The overall heat transfer coefficient between the water sprays and the bottom of the strip also has an obvious influence on temperature and solidification profiles. A stepwise variation of h can reproduce its dynamic behavior for a short domain, i.e., a short time, but its transient evolution should be determined so as to have more accurate

model outputs for the entire belt;

- 3) Applying heat fluxes of up to 100 KW/m^2 on the back and lateral walls brought no dramatic modifications for the proposed process, other than the obvious increase in the solid fraction at the edges of the strip. For an initial superheat of 10°C , no freezing was observed on the back wall region;
- 4) The natural discrepancies in the values of the thermophysical properties of the metal are not expected to cause major errors on the model predictions.

Finally, modelling turbulence with a $\kappa\text{-}\epsilon$ model, proved to be of importance, for this particular configuration. The existence of a solid body in the domain and the sudden contraction experienced by the liquid metal constitute serious limitations to the applicability of the ad-hoc viscosity approach. As a result, overpredictions of the solid shell and underpredictions of the mushy zone are to be expected.

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POROUS MEDIUM MODIFICATIONS

5.1 INTRODUCTION

The previous model simulations showed that while some parametric optimization is possible, some points still need major improvements in the proposed delivery system. One of them is the metal delivery to the cooling belt. In all simulations, it was shown that a smoother flow was needed in the neighbourhood of the belt. Recirculation zones and cross flows were always present in the previous simulations, providing heterogeneous metal delivery throughout the belt. The flow is concentrated in the neighbourhood of the exit gap, which creates a high-turbulence zone.

The way the metal is delivered to the belt is considered crucial in ensuring homogeneous properties and good quality of the strips produced in the horizontal strip caster⁽¹⁾. As mentioned in the literature review, the ideal interaction between the metal and the belt is isokinetic deposition, or in other words, "laying" the metal on the belt, with no shear stresses, nor relative velocity⁽²⁾.

For the proposed geometry, the metal flow is largely bidimensional, with

strong components in the x and y directions. The calculated velocities w were, in general, one or two orders of magnitude lower than the others.

With the aim of smoothing the flow in the reservoir while keeping it bidimensional, a flow modifier was adopted in the delivery system. The modifier consisted of a ceramic filter, as described in Chapter 3. Its physical properties, such as porosity and pore diameter were varied and different thicknesses were employed. Considering that the coefficient of discharge for the filter case is lower, as experimentally determined by Jefferies⁽³⁾, a constant metal head of 14 cm was employed in the simulations with the filter. Since the back wall height was kept the same, the inlet stream was 10 cm high, which made inlet velocities lower for the same flowrate. The other geometrical parameters were kept the same of the standard conditions adopted in Chapter 4.

Initially, a simulation with the 45 ppi alumina filter was carried out and the results were compared with those given by the 10 ppi alumina filter. Since both filters proved to modify the flow positively, the 45 ppi alumina filter was chosen for the subsequent simulations, because of its lower permeability. A simulation was run with the 10 ppi zirconia filter, as well, and the results were compared. The thermophysical properties of both filters are given in Table 3-3.

The results of the initial simulation with a 2.5 cm thick alumina filter are shown below. The filter was positioned right above the exit gap and extended up to almost the end of the back wall. Its length was the same of the reservoir, which means that it covered the entire flow between the back and front walls.

5.2 FLUID FLOW CHARACTERISTICS

Figure 5-1 gives the flow in the reservoir zone. One can clearly see that a quite different flow pattern is established, especially next to the back wall. The recirculation whorl, which stubbornly resisted any changes in the standard configuration, has disappeared. The flow is delivered more smoothly to the belt and no back-flows are observed.

One could argue that the changes were due to the increased pool height, but as seen in Figure 4-34, a higher pool reduces, but does not completely eliminate the recirculation zone. Furthermore, the benefits brought about by the filter go beyond the elimination of the recirculation whorl, as discussed in the following.

Near the nozzle entrance, the influence of the filter is as accentuated as in the first half of the reservoir. As opposed to the other cases, little flow concentration occurs in the last 2 cm of the reservoir's length, which was responsible for a high-turbulence region on the entrance to the exit nozzle, in the no-filter case.

The kinetic energy profile confirms the fluid flow observations. In Figure 5-2, it is seen that the turbulent kinetic energy is, in average, one order of magnitude lower immediately over the belt and near the nozzle entrance. As in the no-filter case, turbulence reaches a maximum in the region right above the belt, due to the drag force exerted by the latter, on the liquid.

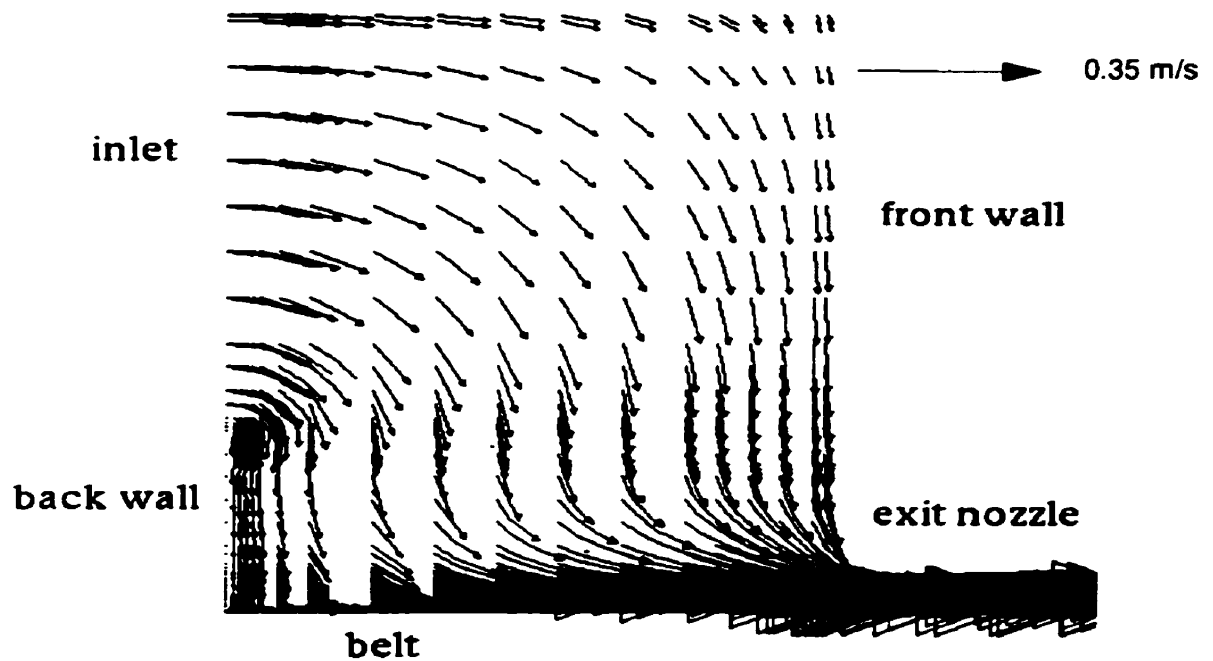


Figure 5-1. Vertical cut along the symmetry axis - 45 ppi alumina filter ($v_b = 1$ m/s, $d_g = 10$ mm, $l = 10$ cm, $h_m = 14$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

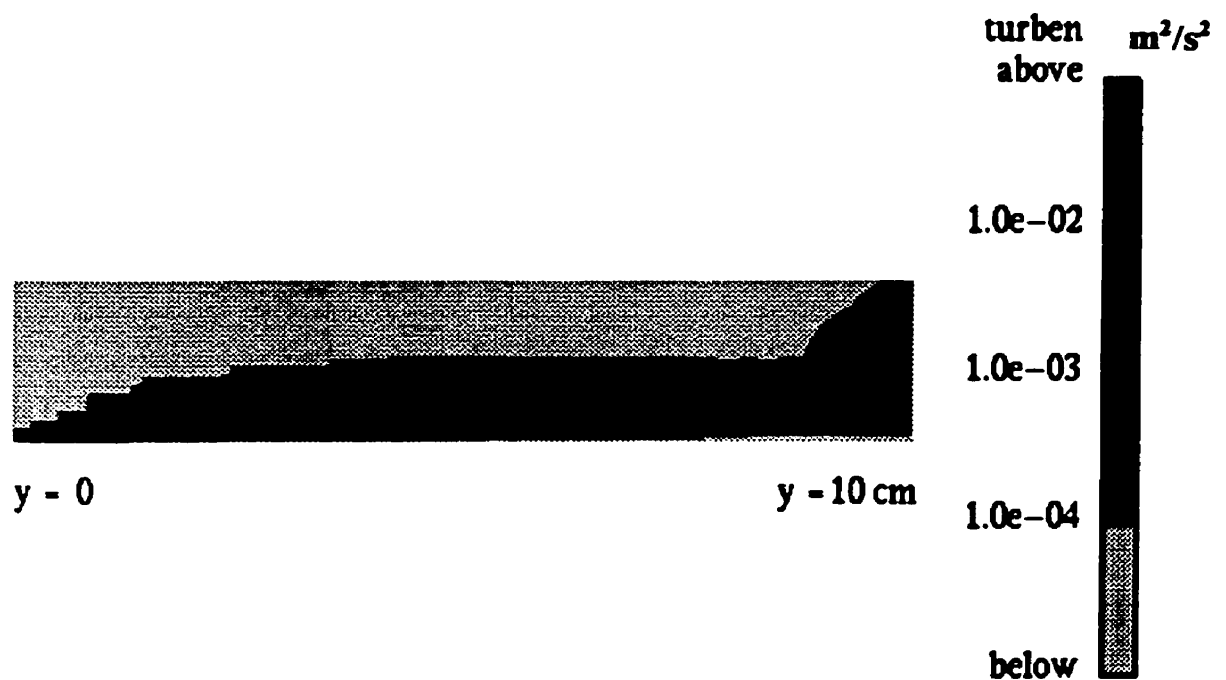


Figure 5-2. Turbulent kinetic energy at the symmetry axis - 45 ppi alumina filter ($v_b = 1$ m/s, $d_g = 10$ mm, $l = 10$ cm, $h_m = 14$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

Another important point to be considered is the three-dimensional aspects of the flow. Figure 5-3 depicts the fluid flow 0.5 cm from the lateral wall, for both cases (conventional and filter). For the no-filter case (Figure 5-3 a), the flow is divided approximately 2 cm from the front wall. The reason for this profile might be the spreading of metal onto the edges, after reaching the front wall (jet impingement). For the filter case, this does not occur at the same position (Figure 5-3 b), but closer to the lateral wall. A more homogeneous flow was also found at the exit gap, for the filter case.

5.3 HEAT TRANSFER RESULTS

As in the previous cases, reheating happens in the neighbourhood of the front wall. However, the position and the extent of the reheating are different. The temperature profile of the strip in the reservoir (Figure 5-4) looks smoother, since there is cooling up to about 8 cm from the meniscus, as opposed to the no-filter case, where the reheating starts 5 cm from the meniscus and is much stronger.

The average solid fractions at 8 cm from the meniscus were 0.0732 on the edge and 0.070 in the center, as opposed to 0.0737 and 0.0689, respectively for the no-filter case. Therefore, the solidification is more uniform along the width for the filter case. Although the difference between the two cases seems small, it is actually relevant, especially considering that only 2 mm of the strip are already mushy at that position. At 0.5 mm from the belt, for example, the use of the filter reduces exactly by half the deviation edge-center of the solid fraction.

At the end of the domain, the results were confirmed. The average solid

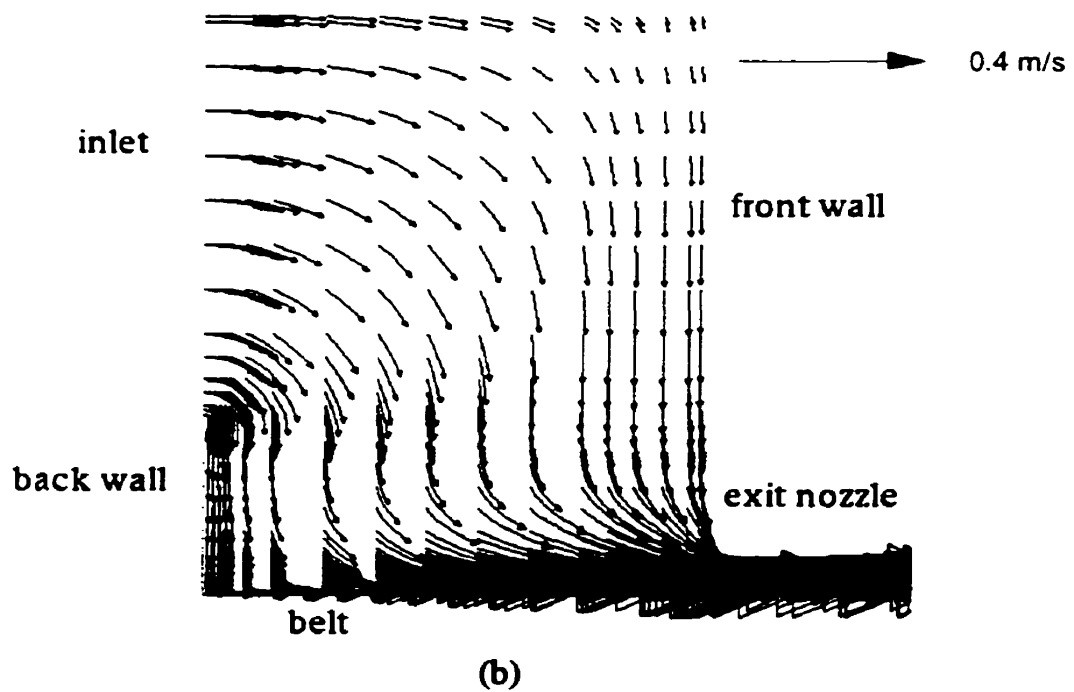
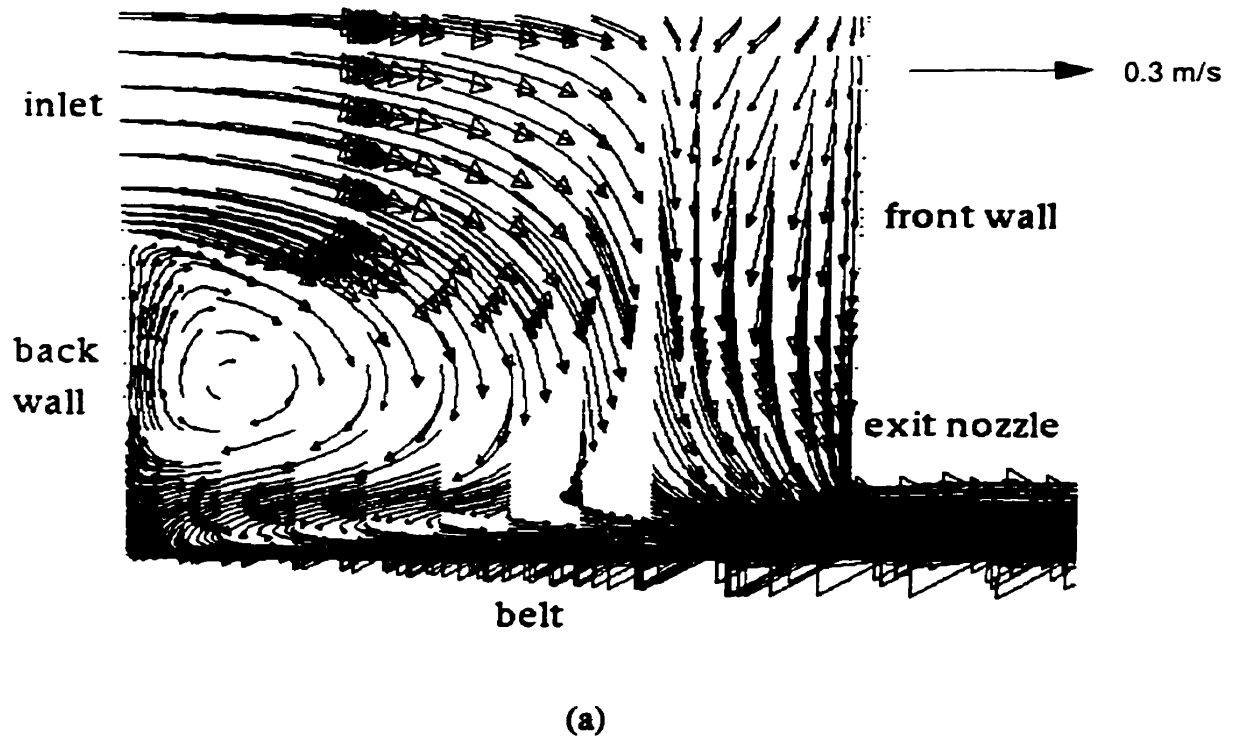


Figure 5-3. Vertical cut at 0.5 cm from the lateral wall: (a) no-filter case; (b) filter case.

fraction on the edge is significantly lower than in the no-filter case, whereas f_c in the center remains roughly the same (Table 5-1). The reason for this result seems to be the better flow distribution along the width of the strip and this may be linked to the reduced turbulence in the gap entrance. To optimize the performance of the flow modifier, the challenge will be of increasing the solid fraction in the center of the strip, approximating it to the enhanced value obtained next to the lateral wall or to damp solidification on the edges, by means of an even better flow distribution. That would add to the already improved flow in the reservoir, which is of fundamental significance in the early stages of solidification.

Three main paths were followed in order to investigate improvements in the performance of the flow modifier. The first was varying its physical properties and thickness, the second was changing its positioning and the third was modifying the geometry of the reservoir, especially the metal head.

Table 5-1 Average solid fraction at $y = 40$ cm, for the filter case, as opposed to the no-filter case.

Pool height	Filter	f_c (center)	f_e (edge)	Δ
3 cm	N	0.157	0.189	0.032
10 cm	Y	0.159	0.178	0.019

To finish this preliminary analysis about the modifications imposed on the fluid flow and heat transfer by the filter, having a look at the temperature profile in the reservoir is necessary. Figure 5-5 shows that although a thin mushy zone is

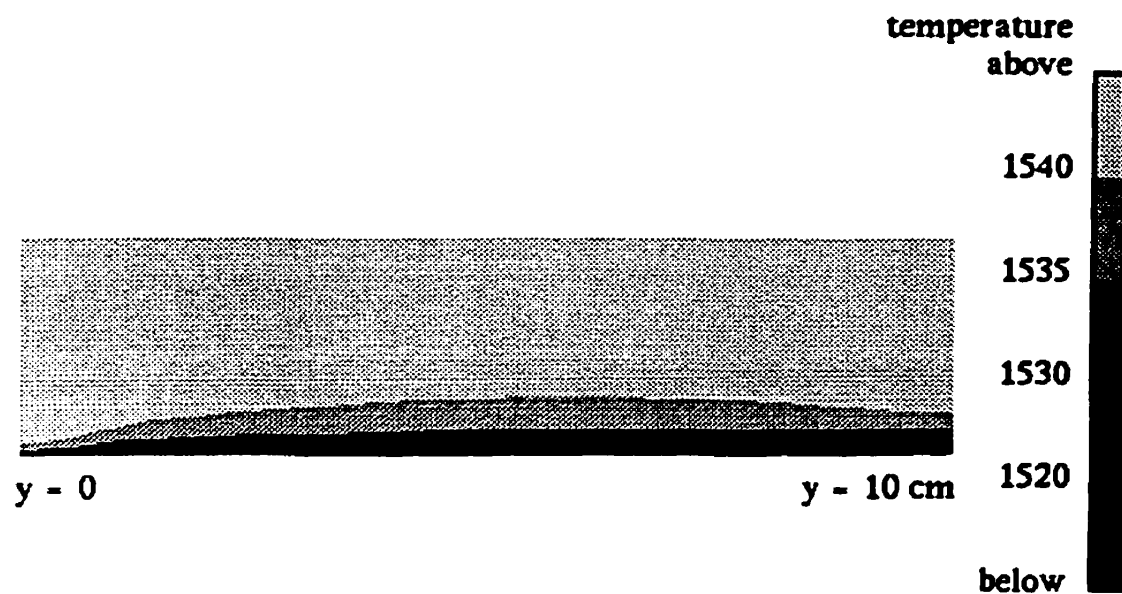


Figure 5-4. Temperature profile at the symmetry axis in the region of the strip inside the reservoir - 45 ppi alumina filter ($v_b = 1$ m/s, $d_g = 10$ mm, $l = 10$ cm, $h_m = 14$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

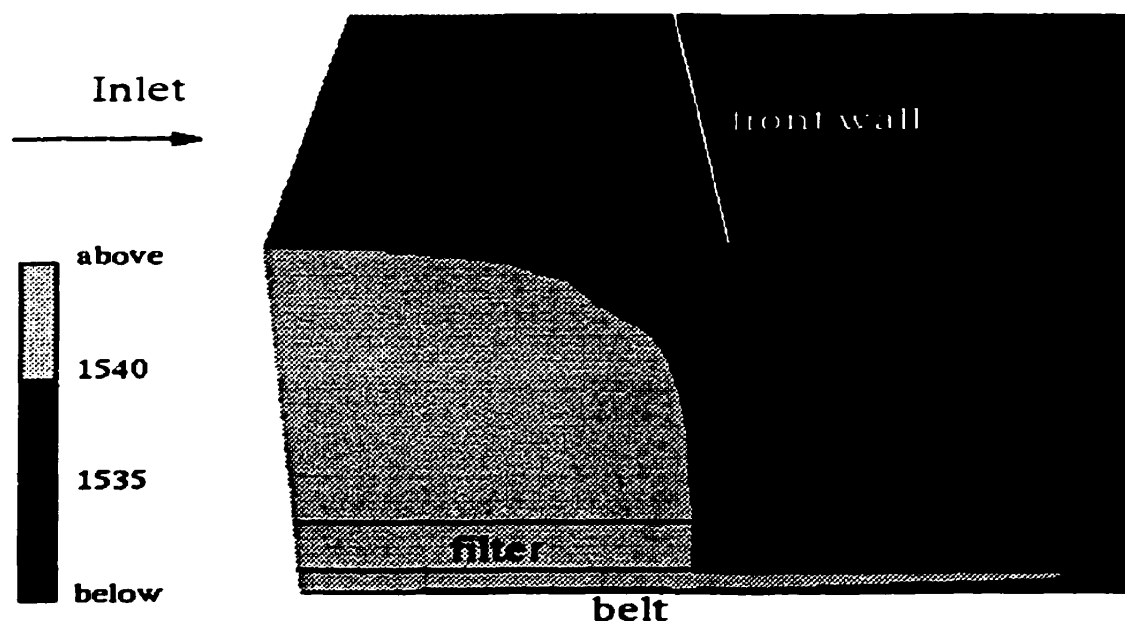


Figure 5-5. Temperature profile in the reservoir - Detail of the top edge - 45 ppi alumina filter ($v_b = 1$ m/s, $d_g = 10$ mm, $l = 10$ cm, $h_m = 14$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

formed next to the front wall, it should not create a big problem, because the temperature in that region is never below 1520°C and thus simple measures could prevent the metal from freezing.

5.4 PHYSICAL PROPERTIES OF THE FILTER

5.4.1 PERMEABILITY

The most relevant physical properties of the filter are its bulk porosity and the average pore diameter. Usually, ceramic filters are also characterized by their ppi (pores per inch), which gives a measure of pore distribution within the filter domain. In this work, the filter porosity was varied from 0.84 to 0.87 and the pore diameter, accordingly, from 420 to 1443 μm . These values were supplied by the filter's manufacturer, Hi-Tech Ceramics⁽⁴⁾.

The results obtained after these changes in the physical properties were similar to the first ones. However, it can be seen in Figure 5-6 that the flow is a little weaker near the back wall and more concentrated near the nozzle entrance. This is due to the approximately one order of magnitude higher permeability, which approximates the flow profile to the no-filter case. The incoming flow is not damped as strongly as in the previous case (45 ppi). Still, the improvement in comparison to the no-filter case is remarkable.

As for the kinetic energy profile in the region of the strip, this is similar to the 45 ppi case. However, a slight increase in κ is observed above the belt and near the entrance to the exit nozzle of the extended reservoir.

With respect to the heat transfer characteristics, it is seen that the reheating is stronger in the reservoir, and starts earlier (Figure 5-7). This approximates, as well, the thermal profile in the reservoir to the no-filter case. Accordingly, the solid fractions were 0.158 and 0.182 in the center and on the edges, respectively.

5.4.2 FILTER THICKNESS

In the sequence, different thicknesses of the filter were tried, in an attempt to smooth the flow downstream. The thickness was varied from 1" (default) to 1.5 and 2". In both cases, the back wall height was increased, to accommodate the filter. So, for the 1.5" thickness, the back wall height was 5 cm and for the 2" thickness, 6 cm.

Figure 5-8 gives the fluid flow pattern for the 2" thick filter. Due to the increased thickness, the flow becomes smoother earlier. Also, the flow becomes completely vertical before it exits the filter.

With respect to the heat transfer results, the temperature profile of the strip inside the reservoir is similar to the standard case. However, the temperature profile on the top of the reservoir is distinct, since the mushy zone next to the front wall has shrunk. This is explained by the higher inlet velocity of the metal, for the higher back wall and the same metal head.

At the end of the domain, the results varied very little in comparison to the 2.5-cm filter. The solidified shell did not change, and the average solid fraction is almost identical.

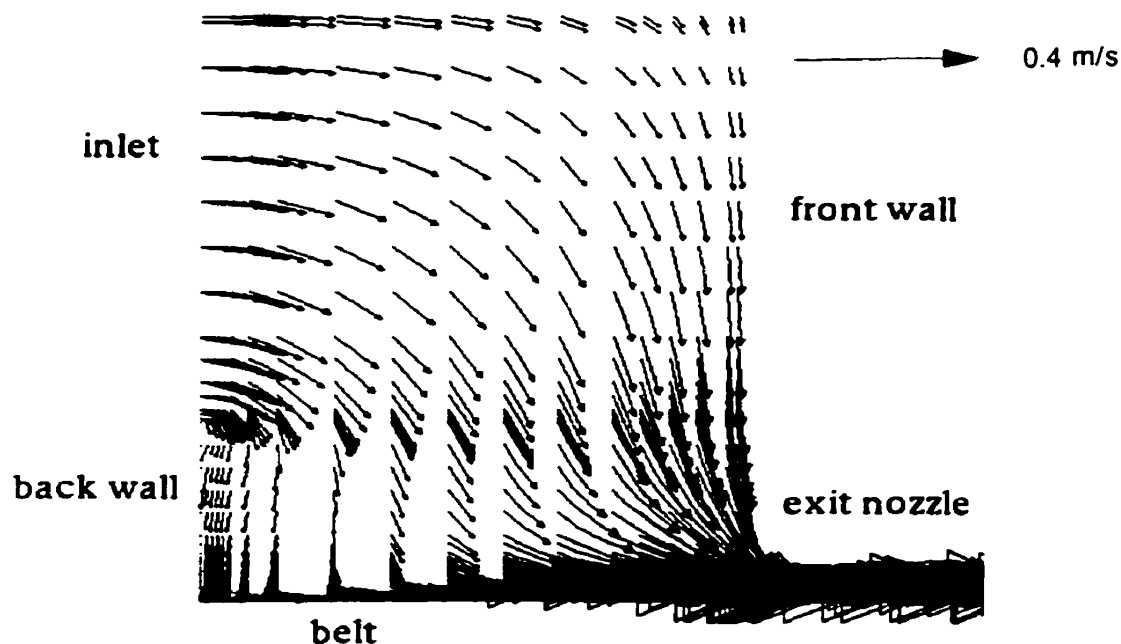


Figure 5-6. Vertical cut along the symmetry axis - 10 ppi zirconia filter ($v_b = 1$ m/s, $d_g = 10$ mm, $l = 10$ cm, $h_m = 14$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

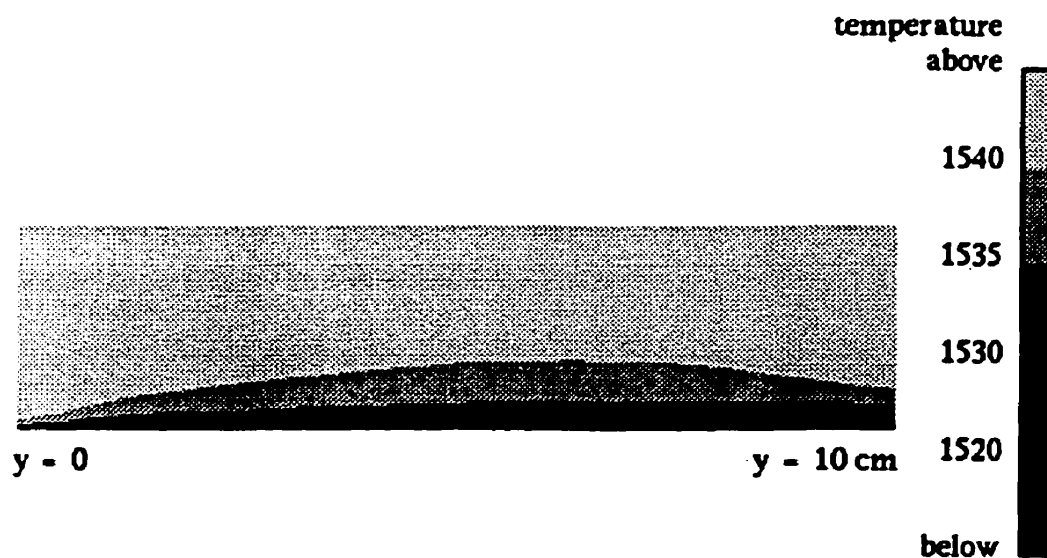


Figure 5-7. Temperature profile at the symmetry axis in the region of the strip inside the reservoir - 10 ppi zirconia filter ($v_b = 1$ m/s, $d_g = 10$ mm, $l = 10$ cm, $h_m = 14$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

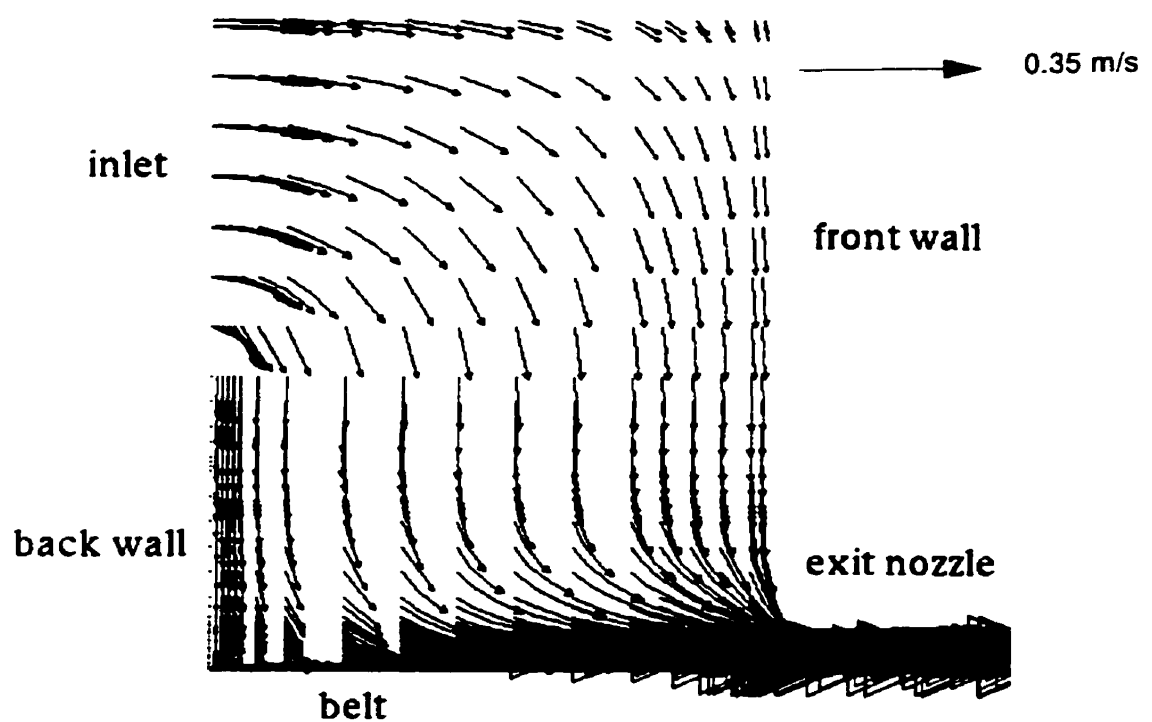


Figure 5-8. Vertical cut along the symmetry axis - 5-cm thick alumina filter ($v_b = 1$ m/s, $d_s = 10$ mm, $l = 10$ cm, $h_m = 14$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

Therefore, the thicker filter just contributed to reduce the mushy zone in the neighbourhood of the front wall, due to the increase in the inlet velocity that results from the requirement of a higher back wall. Negligible changes were observed in the fluid flow distribution along the width of the reservoir and in the heat transfer profile, as well. Thus, using a thicker filter and, correspondingly, a higher back wall, would contribute to overcome the drawback represented by the cold zone next to the front wall.

5.5 FILTER POSITIONING

With the objective to investigate the influence of the filter positioning, a simulation was run with the filter ending 2.5 cm above the exit gap, instead of right above the belt. In such a case, the back wall had its height increased by 2 cm, to allow for installation of the filter. All the other geometrical and flow parameters were kept the same.

The fluid flow pattern, seen in Figure 5-9, indicates a little concentration of the flow in the region of the exit gap, due to either the cessation of the flow modification or the higher inlet velocity. The flow concentration was helped by the higher inlet velocity for the reduced pool above the back wall, because it pushed the flow a little far towards the front wall.

Figure 5-10 confirms the tendency for flow concentration near the exit gap. It is seen that the kinetic energy is higher near the nozzle entrance and next to the back wall, with a profile closer to the no-filter case.

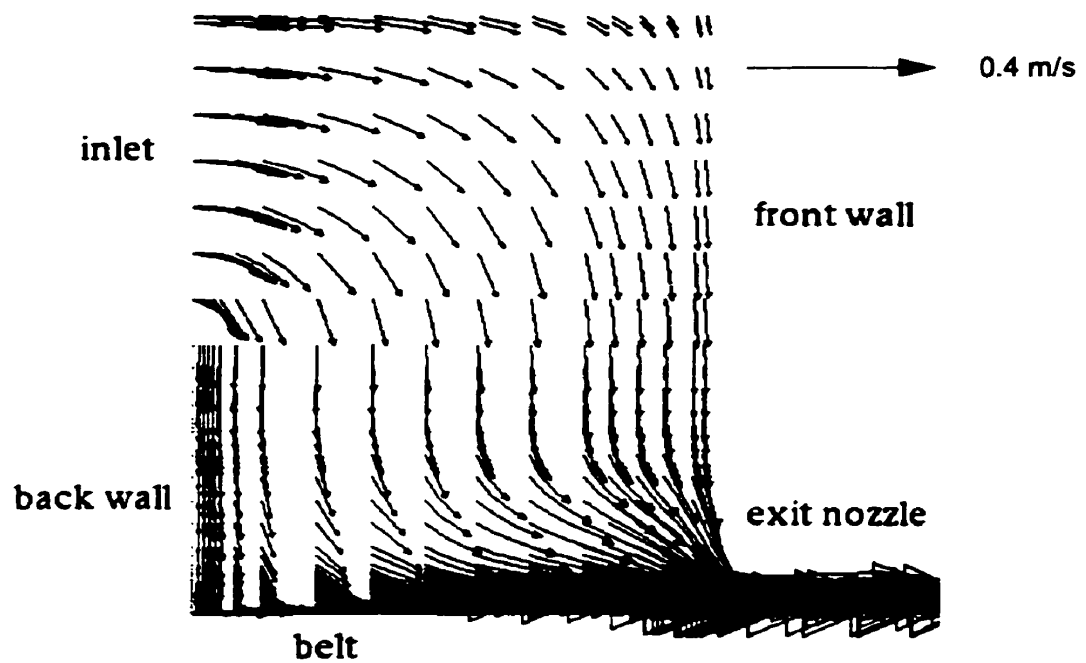


Figure 5-9. Vertical cut along the symmetry axis - Filter ending 2.5 cm above the exit gap ($v_b = 1$ m/s, $d_g = 10$ mm, $l = 10$ cm, $h_m = 14$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

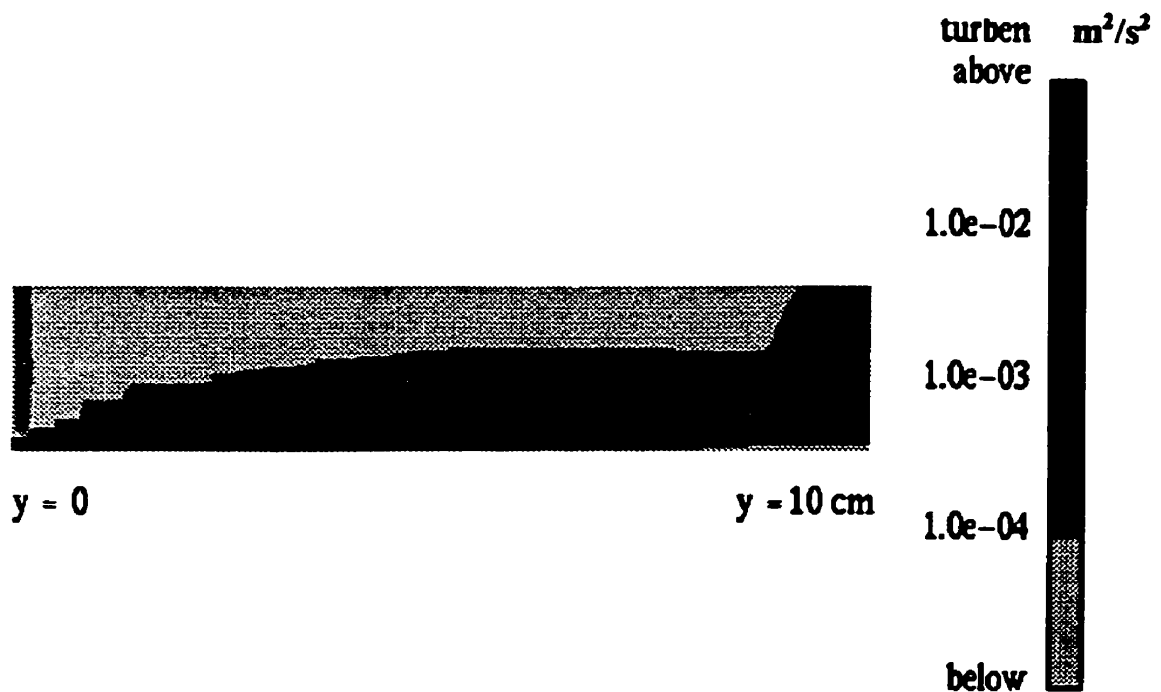


Figure 5-10. Turbulent kinetic energy at the symmetry axis - Filter ending 2.5 cm above the exit gap ($v_b = 1$ m/s, $d_g = 10$ mm, $l = 10$ cm, $h_m = 14$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

These flow modifications are reflected in the temperature profile inside the reservoir. The reheating of the metal occurs later and it is more pronounced than in the standard filter case (Figure 5-11). Finally, the enhanced solidification on the edges is confirmed by the average solid fractions shown in Table 5-2. At a first glance, the average solid fraction on the edges would be expected fairly higher than in the standard case, but the increases in f_s were minimum, because the overall enhanced turbulence raised the solid fractions in the liquid, whereas it decreased them near the solid shell.

Table 5-2 Average solid fraction at $y = 40$ cm, for different filter positioning.

Positioning	f_s (center)	f_s (edge)	Δ
Standard	0.159	0.178	0.019
2.5 cm above the gap	0.158	0.183	0.025

5.6 EXTENDED POOL COMBINED WITH FILTER

The extension of the pool to 20 and 30 cm was tried for the filter case, as well. The idea was to try to combine the obvious advantages of the inclusion of a filter with the ones brought about by the extended pool, such as more homogeneous solidification throughout the strip, as discovered in Chapter 4. In other words, are the findings of Chapter 4 applicable to the filter case?

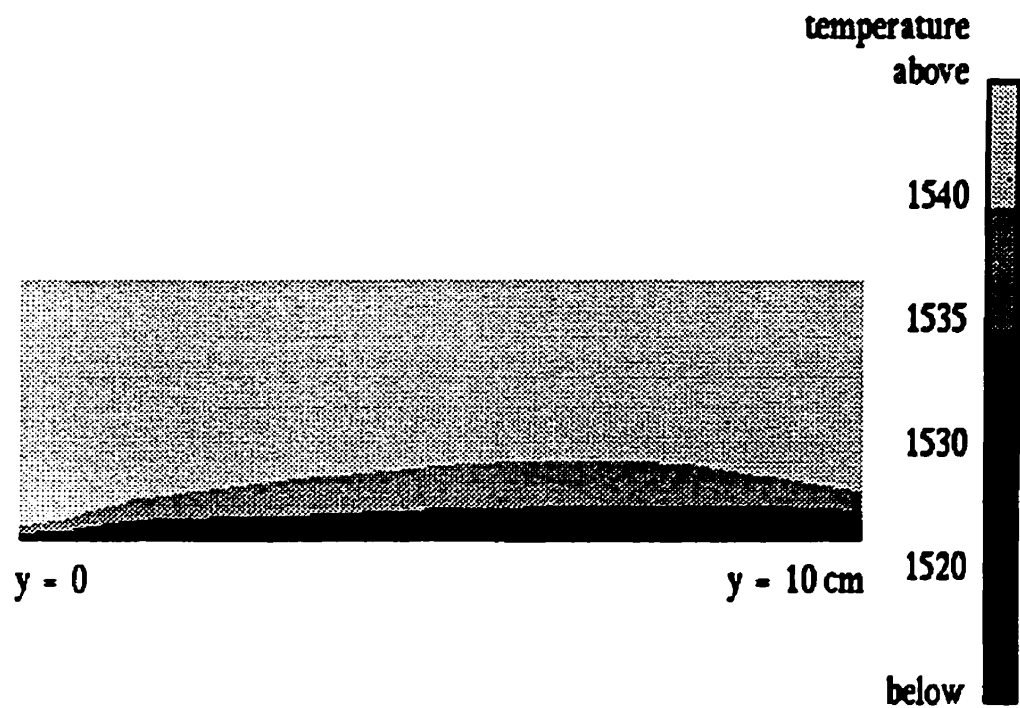


Figure 5-11. Temperature profile at the symmetry axis in the region of the strip inside the reservoir - Filter ending 2.5 cm above the exit gap ($v_b = 1 \text{ m/s}$, $d_g = 10 \text{ mm}$, $l = 10 \text{ cm}$, $h_m = 14 \text{ cm}$, $Q = 4.5 \times 10^{-3} \text{ m}^3/\text{s}$)

Figures 5-12 and 5-13 give the flow patterns in the delivery system. Two main conclusions can be drawn from the Figures:

1) For the 30-cm pool, the use of the filter creates a back flow zone from the back wall up to half the reservoir length. For the 20-cm pool, at one-third of the length, the flow becomes increasingly horizontal in direction of the gap.

2) The concentration of the flow in the region of the exit gap is progressively reduced for the larger pools, partially due to the occurrence of a triangular-shaped dead zone next to the front wall. Indeed, the turbulent energy undergoes a reduction next to the gap, for the larger pools.

However, no changes in the heat transfer homogeneity were noticed, for a larger pool of 20 cm (Table 5-3). This finding suggests that the smoothing effect of the filter does not depend on the length of the metal pool, and it was fully present already at the 10-cm pool. Table 5-3 shows that the 30-cm pool presented the most homogeneous solidification, with a deviation of only 0.010 on the edges. Although this seems to be a significant improvement, it is mostly due to the nonoccurrence of solidification on the edges, on the top surface of the strip (as opposed to the 10-cm pool), since the heat loss by radiation and convection occurs only after approximately 32 cm (as opposed to 12 cm for the 10-cm pool).

One advantage of the extended pools is that the reheating of the strip is less pronounced, due to the distribution of the flow inside the reservoir and its lower concentration at the exit gap. Reheating may originate stresses on the solidified shell and cause defects.

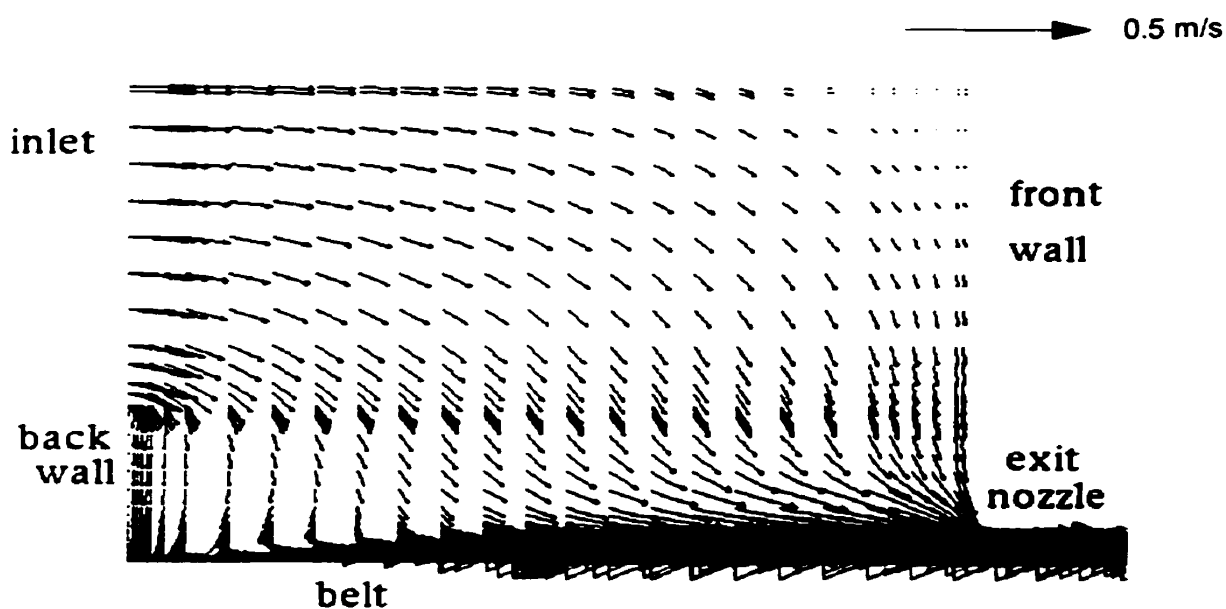


Figure 5-12. Vertical cut along the symmetry axis - 20-cm pool length and filter
($v_b = 1 \text{ m/s}$, $d_g = 10 \text{ mm}$, $h_m = 14 \text{ cm}$, $Q = 4.5 \times 10^{-3} \text{ m}^3/\text{s}$)

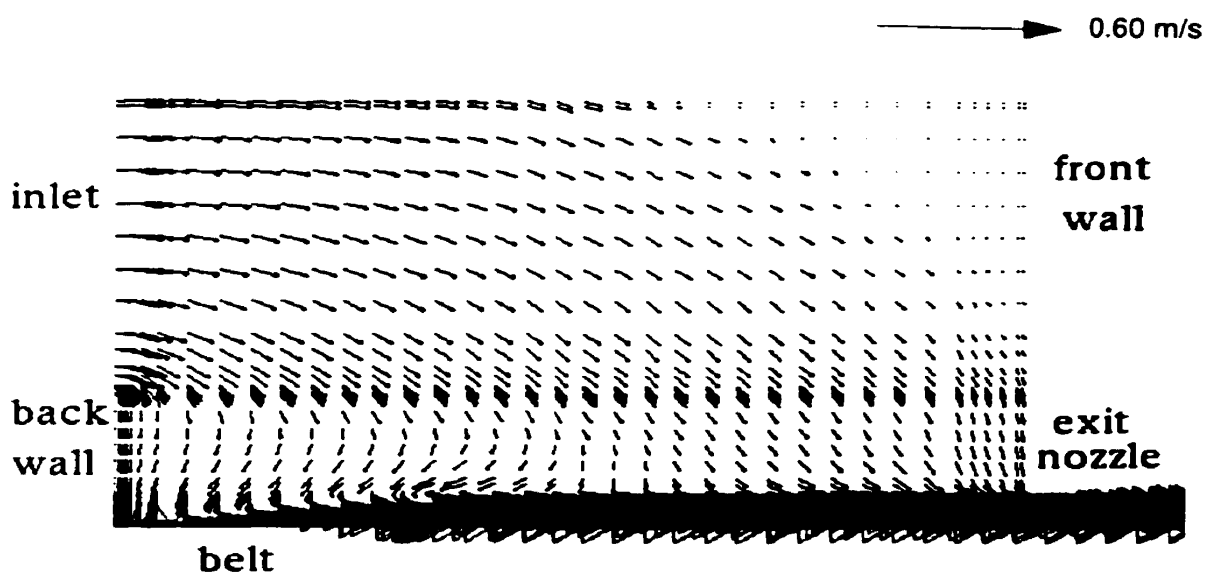


Figure 5-13. Vertical cut along the symmetry axis - 30-cm pool length and filter
($v_b = 1 \text{ m/s}$, $d_g = 10 \text{ mm}$, $h_m = 14 \text{ cm}$, $Q = 4.5 \times 10^{-3} \text{ m}^3/\text{s}$)

Table 5-3 Average solid fraction at $y = 40$ cm, for various pool lengths, with the inclusion of the filter.

Pool length	f_s (center)	f_s (edge)	Δ
10 cm	0.159	0.178	0.019
20 cm	0.159	0.177	0.018
30 cm	0.161	0.171	0.010

As in the no-filter case, the issue of freezing next to the front wall arises again. Figures 5-14 and 5-15 show the temperature profiles in the reservoir for the 20-cm and 30-cm pools respectively. The cold zone is especially accentuated in the second case. To overcome such a problem, there are two main alternatives: raising the initial superheat and increasing the initial inlet velocity, by means of changes in the geometrical parameters. The second alternative was first considered and, therefore, some changes were made on the geometrical aspects of the reservoir. The most relevant results of such simulations will be revealed in the following.

Based on the conclusions obtained in Chapter 4, the pool height was altered to 9 cm. This way, the inlet velocity would be raised and the cold zone would be narrowed. Simultaneously, it was expected that the shorter pool height would yield more homogeneous solidification, as in the no-filter case.

Obviously, the temperature profile in the reservoir was affected, due to the improved fluid flow conditions. The solid zone in the vicinity of the front wall was significantly reduced and the mushy region remaining was predicted to be tolerable (Figure 5-16).

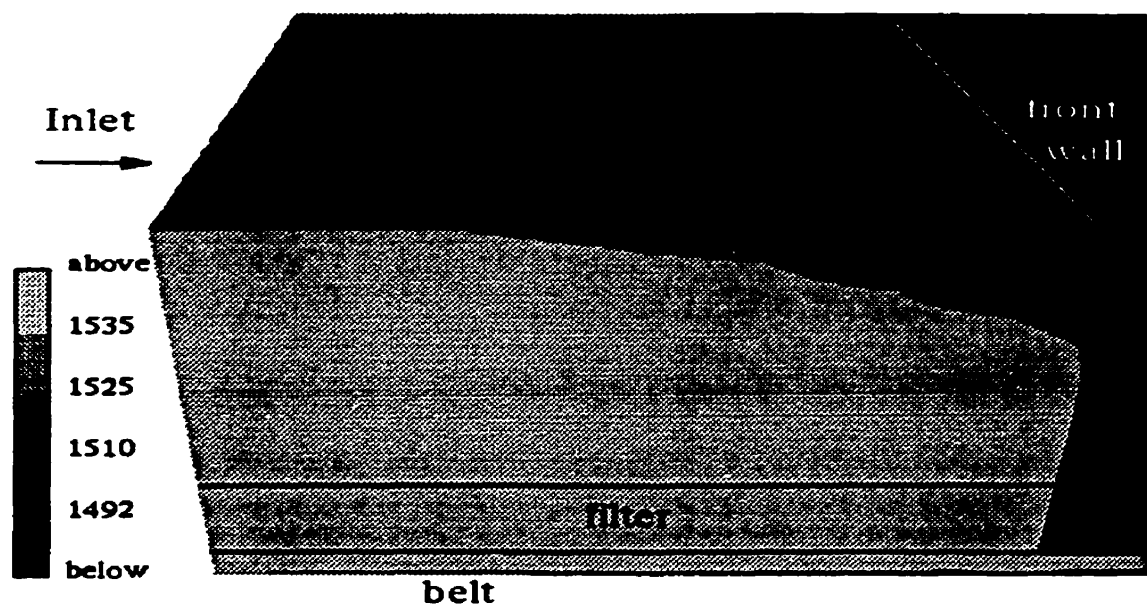


Figure 5-14. Temperature profile in the reservoir - Detail of the top edge - 20-cm pool length and filter ($v_b = 1$ m/s, $d_g = 10$ mm, $h_m = 14$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

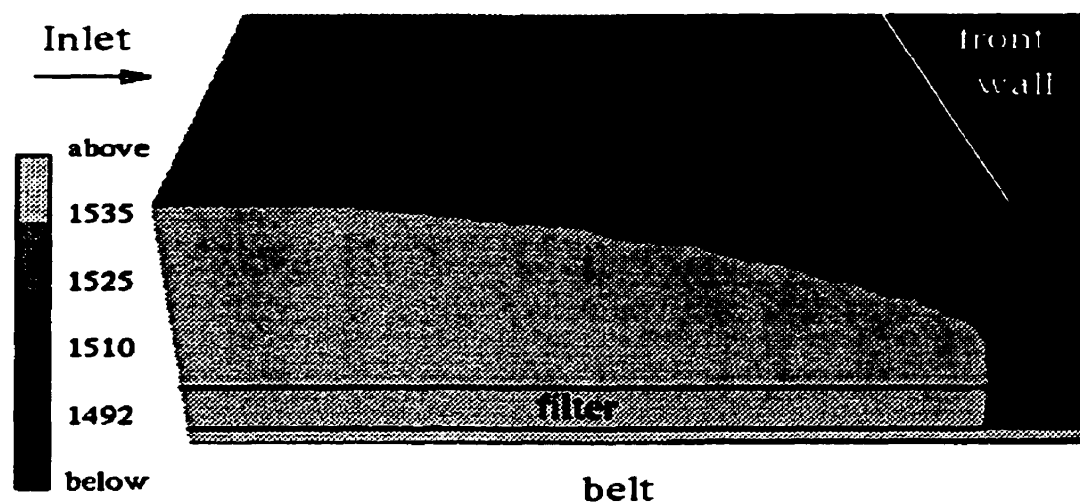


Figure 5-15. Temperature profile in the reservoir - Detail of the top edge - 30-cm pool length and filter ($v_b = 1$ m/s, $d_g = 10$ mm, $h_m = 14$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

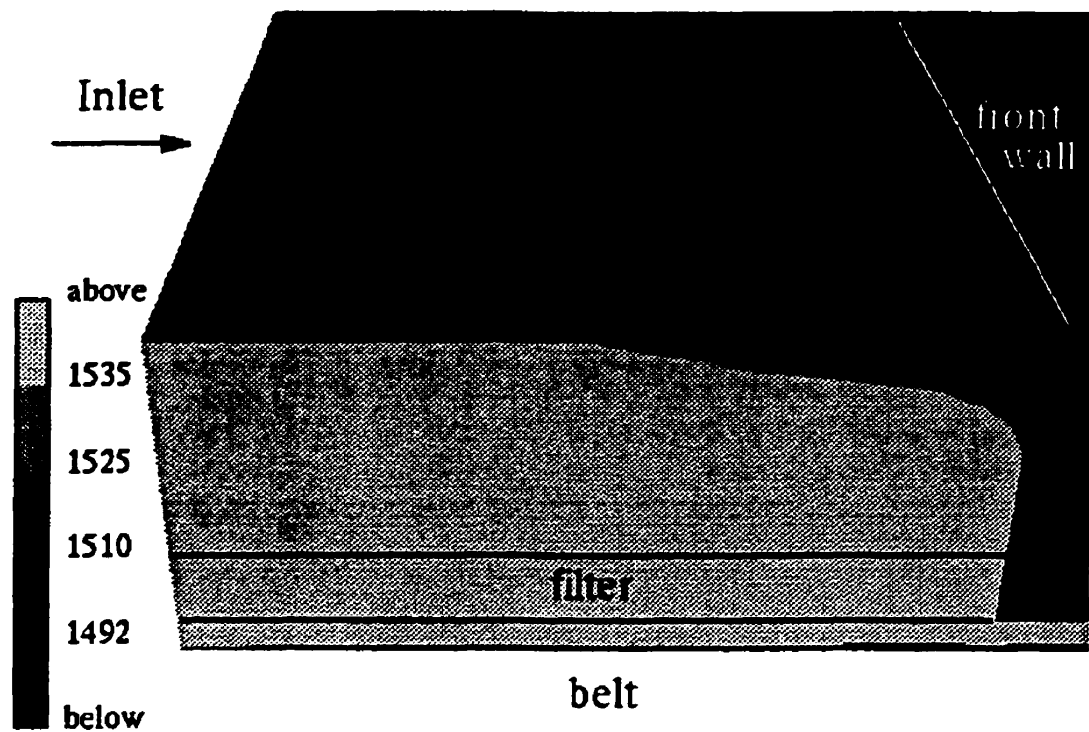


Figure 5-16. Temperature profile in the reservoir - Detail of the top edge - 9-cm pool height and filter ($v_b = 1$ m/s, $d_g = 10$ mm, $l = 20$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

As for the temperatures beyond the exit gap, little differences were noticed for the new pool height. The average solid fractions were nearly the same as in the previous case.

In conclusion, changing the geometry of the reservoir yielded irrelevant changes to the fluid flow and heat transfer uniformity. The adoption of pools lower than the standard (14 cm) could contribute to the reduction of the cold zone next to the front wall. However, this modification would increase the likelihood of a hydraulic jump beyond the exit gap, since in this case there would be a mismatch between the velocity head in the nozzle gap and the potential head in the caster nozzle.

5.7 INCLUSION OF THE FILTER FOR THINNER STRIPS

After simulating different configurations for the 10-mm strip, the filter insertion was extended to the other thicknesses intended in this study, so that the most appropriate casting conditions could be determined. For the initial simulations, the default length of the pool was 10 cm and the height was 14 cm.

5.7.1 STRIP THICKNESS OF 8 mm

Virtually no modifications were observed in the fluid flow patterns for the 8-mm gap, compared with the 10-mm one, as in the no-filter case.

The average solid fractions were 0.199 in the center and 0.233 on the edges, which is slightly higher in the center and slightly lower at the edges compared with the case without the filter (0.195 and 0.235). This means that the inclusion of the filter would enhance the solidification in the center of the 8-mm

strip.

With respect to the temperature in the reservoir, a small solid region ($T < 1492^{\circ}\text{C}$) was found on the corner formed by the front and lateral walls (Figure 5-17).

5.7.2 STRIP THICKNESS OF 4 mm

The flow modifier proves also to be very efficient in smoothing the flow for thinner strips (see Figure 5-18). Due to the lack of momentum, the flow in the region close to the back wall moves backwards when the liquid metal passes through the filter. As expected, the dead zone is enlarged near the wall.

As for the heat transfer aspects, it was observed that the reheating of the strip occurred much earlier than in the no-filter case and it is virtually nonexistent at the entrance to the exit nozzle of the extended delivery system. This is due to the much lower turbulence levels in this region, in comparison to the no-filter case. The average solid fraction was increased in the center (0.362×0.356) and decreased on the edges (0.409×0.414) resulting in a smoother solidification in comparison to the standard no-filter case. However, as already anticipated, the drawback to the insertion of a flow modifying filter for the 4-mm strip is freezing near the wall, shown in Figure 5-19.

5.8 DETERMINATION OF THE CASTING CONDITIONS WITH THE FLOW MODIFIER

A brief study was conducted to find the most suitable operational conditions

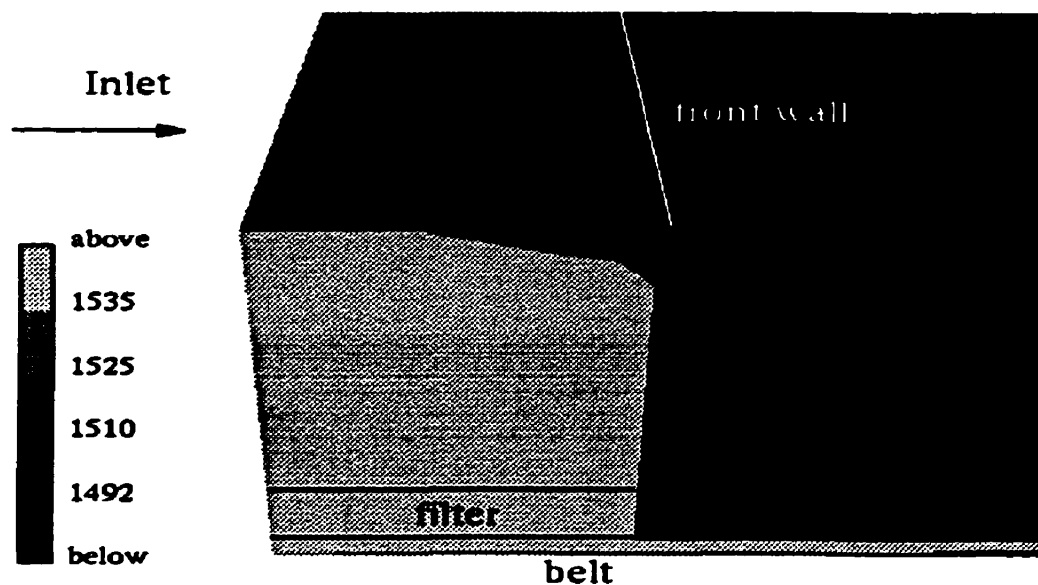


Figure 5-17. Temperature profile in the reservoir - Detail of the top edge -8-mm exit gap and filter ($v_b = 1$ m/s, $l = 10$ cm, $h_m = 14$ cm, $Q = 3.6 \times 10^{-3}$ m³/s)

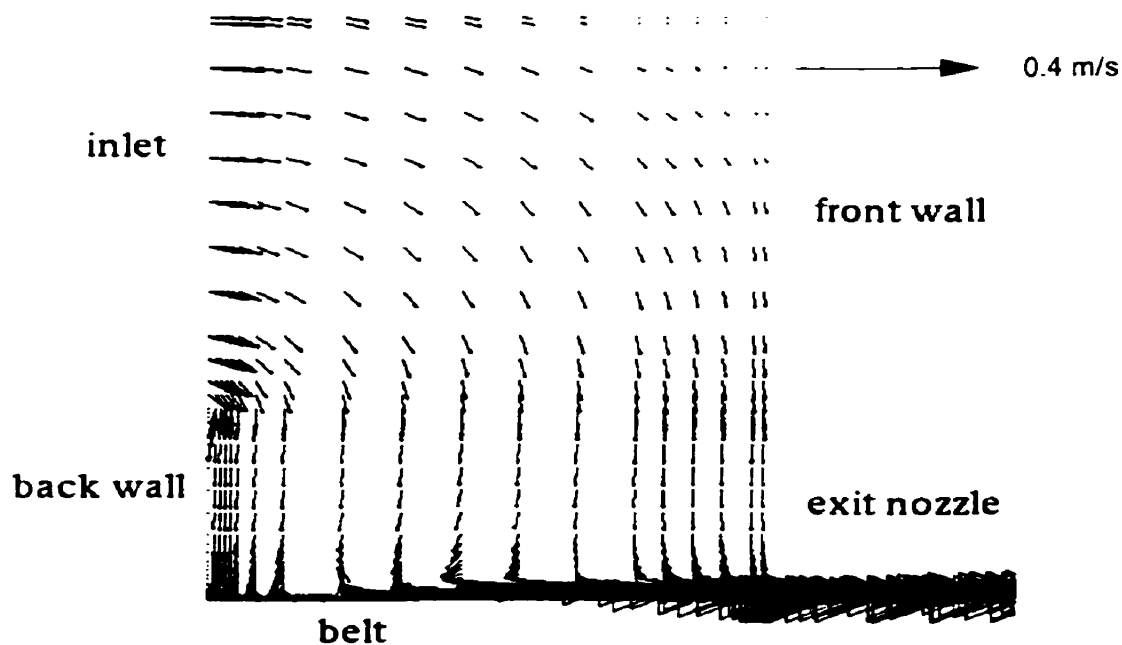


Figure 5-18. Vertical cut along the symmetry axis - 4-mm exit gap and filter ($v_b = 1$ m/s, $l = 10$ cm, $h_m = 14$ cm, $Q = 1.8 \times 10^{-3}$ m³/s)

for the casting of 10, 8 and 4-mm strips. As opposed to the no-filter case, it was found that extended pools have little influence on the uniformity of the solidification. Their effect was noticed only for the 30-cm pool, because the solidification was retarded on the top surface. Nevertheless, as already mentioned, extended pools may present less reheating and remelting of the incipient solid shell.

One conclusion from this study is that the height of the inlet stream should be greater than 3 cm, for a better distribution of the flow. On the other hand, pool heights more than 5 cm have minimum effect on the distribution of the flow across the width of the reservoir. Between 5 and 10 cm, the flow was steadily homogeneous and the kinetic energy results near the entrance to the exit nozzle were also more homogeneous. Therefore, more uniform solidification would be expected.

Another important conclusion was that a back wall higher than 6 cm caused a little recirculation whorl next to it, as seen in Figure 5-20. In this specific case, the back wall height was 7 cm and the inlet stream was 7 cm, as well. The filter was of little help in this configuration. Therefore, a standard height of 4 cm was initially considered for the back wall. The metal head on the inlet could vary in the range 5-10 cm. The higher the stream, the cooler the metal in the neighbourhood of the front wall. Therefore, an increase in the metal superheat would be mandatory. The lower the stream, the more likely the occurrence of a mismatch between the incoming metal and the belt speed. For the cases where a very high initial superheat is required, an increase in the back wall height up to 7 cm would reduce this requirement, and produce an acceptable thermal profile in the reservoir.

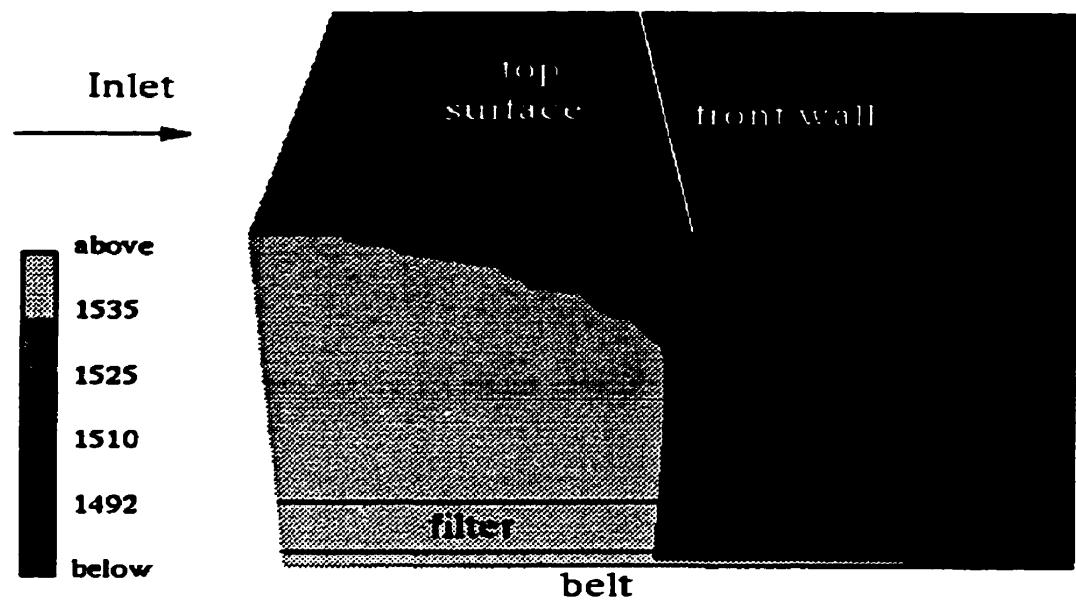


Figure 5-19. Temperature profile in the reservoir - Detail of the top edge -4-mm exit gap and filter ($v_b = 1$ m/s, $l = 10$ cm, $h_m = 14$ cm, $Q = 1.8 \times 10^{-3}$ m³/s)

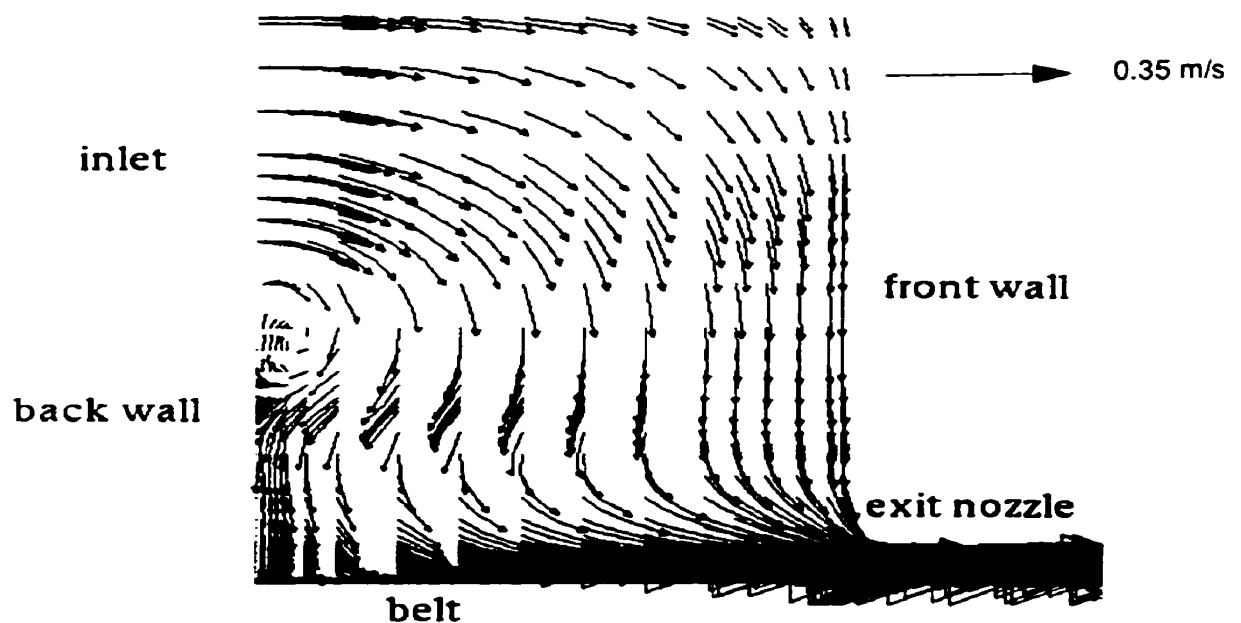


Figure 5-20. Vertical cut along the symmetry axis -Back wall height of 7 cm and filter ($v_b = 1$ m/s, $d_g = 10$ mm, $l = 10$ cm, $h_m = 14$ cm, $Q = 4.5 \times 10^{-3}$ m³/s)

Finally, the study carried out made it possible to suggest adequate operational conditions for the casting of 10, 8 and 4-mm strips, using the ceramic filter, as a flow modifier. These recommendations are given in Table 5-4.

Table 5-4 Recommended operational conditions for strip casting in the proposed delivery system, with the insertion of a ceramic filter.

Thickness (mm)	Pool height (cm)	Pool length (cm)	Superheat (°C)
10	10	10	10
10	10	20	10/15
10	10	30	20/25
10	14	10	10
10	14	20	25/30
10	14	30	35/40
8	same	same	+ 3/5
4	10	10	15/20
4	10	20	25/30
4	10	30	40/50

- Notes:
- 1) The pool height of 10 cm will correspond to a belt speed of 0.85-0.9 m/s. For the 14-cm pool, the intended 1 m/s belt speed could be employed;
 - 2) The values for the 8-mm strip are the same for the 10-mm

strip, with an increase in the inlet superheat of 3 and 5° C.

- 3) Back walls higher than 4 cm can be employed to reduce the initial superheat.

5.9 SUMMARY AND CONCLUSIONS

Simulations carried out for the standard configuration of the delivery system showed the need for an instrument to distribute the incoming flow better, either in the direction perpendicular to the flow or in the direction of the width. The nature of the flow in the standard conditions was mainly bidimensional, with the u and v velocities at least one order of magnitude larger than the w velocities.

The results for a standard pool height of 14 cm with a 2.5-cm thick filter confirmed the elimination of a recirculation whorl next to the back wall. In addition, the turbulent energy immediately above the cooling belt is lower than in the standard case. The reheating of the liquid and remelting of the shell still occurs in the reservoir zone, but a bit ahead and to a smaller extent. For instance, in the region of the strip inside the reservoir zone, the solidification along the width is smoother than in the no-filter case. This pattern prevails at, and after, the exit gap, yielding more uniform solidification at all times.

Nevertheless, the appearance of a "cold" zone in the neighbourhood of the front wall, due to the reduced inlet velocities, was an important drawback of the original configuration. A challenge was therefore launched: to find a way to increase the solid fraction f_s in the center of the strip or decrease f_s on the edges, for a configuration that would combine the benefits of the filter insert and an

adequate temperature profile in the reservoir. Three main paths were followed to attain this objective: varying the physical properties and the thickness of the ceramic filter, changing its positioning and modifying the geometry of the delivery system.

Modification in the filter permeability was of no help. Using a 10 ppi filter aggravated the divergence center-edge of f_s , as expected. The possibility of using a 80 ppi filter was raised, but its drawback would be the lower permeability, which might cause clogging of the filter in an industrial operation. However, increasing the thickness of the filter and, accordingly, the back wall height, maintained the performance of the filter, while considerably reducing the "cold" zone in the reservoir.

The geometry of the reservoir was modified, obeying the conclusions drawn from Chapter 4. First, the pool length was extended to 20 and 30 cm. Although the flow seems to be better distributed in the reservoir, no noticeable changes in f_s resulted from the extension of the pool to 20 cm. For the 30-cm pool, the average solid fraction in the center was closer to the edges than in the previous cases. However, this was due mainly to the retarded solidification on the top surface of the strip. The use of larger pools required changes in other parameters, to avoid very low velocities of the metal in the neighbourhood of the front wall. Such parameters would be: filter thickness, back wall height and superheat.

Further investigation showed that the inlet stream should be higher than 3 cm and that the back wall may be higher than the standard 4 cm. These measures will be relevant for the solidification homogeneity (first case) and for the reduction of the cold regions in the reservoir (second case).

Studies were conducted for the casting of 8-mm and 4-mm strips, as well. For the 8 mm strip, the flow is quite similar to the 10-mm one. However, the weak momentum of the inlet flow pushes the liquid metal backwards for the 4-mm strip.

It was found that the geometry of the reservoir and the inlet superheat are the main parameters in determining the most suitable conditions for the casting of strips, for the proposed delivery system. Configurations were then recommended for the casting of 10, 8 and 4-mm strips on the horizontal single-belt caster, with the insertion of the flow modifier in the proposed delivery system.

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MODEL VALIDATION

6.1 INTRODUCTION

To validate the mathematical model, some specific conditions were simulated with the model and their results compared with semi-analytical solutions and experimental results. For the particular configuration under study, velocity measurements for water flow were performed by Jefferies⁽¹⁾. With respect to the heat transfer between the metal and the substrate, applying analytical solutions to certain idealized conditions is possible. Besides, it is possible to compare the cooling rates obtained with the ones predicted according to relationships with the SDAS (Secondary Dendrite Arm Spacings) measured in strips cast at similar conditions.

6.2 FLUID FLOW VALIDATION

Jefferies⁽¹⁾ has presented some results of a water model developed at McGill. She measured the v -velocities at 8, 7.1 and 5.6 cm from the back wall and at 2 to 3 mm above the substrate, supposedly at the central region of the reservoir width (Figure 6-1). The author ran a number of simulations with her two-dimensional ad-hoc turbulence model and compared the results with the

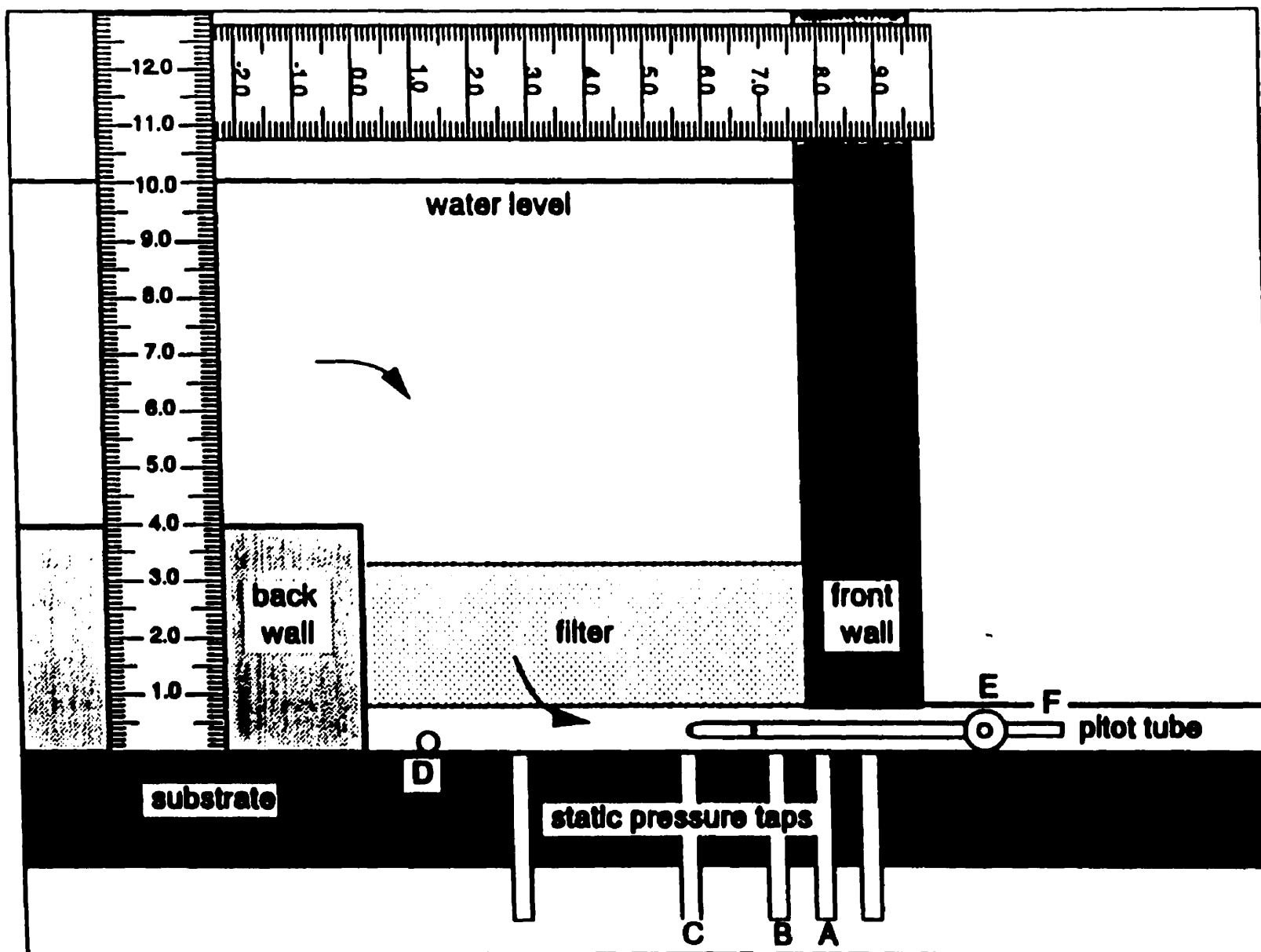


Figure 6-1. Details of the work zone and exit gap⁽¹⁾.

measurements carried out at the water model. Various water levels were simulated, signalling different flow rates and the exit gap was varied, as well. Figure 6-2 shows good correspondence between predicted and experimental velocities. Most predictions fell within $\pm 20\%$ correspondence, except for positions B (7.1 cm) and C (5.6 cm), for the cases without the filter, which presented an underestimation of the experimental velocities. Various hypotheses were raised by the author for this discrepancy, among them the possibility of errors in the experimental measurements.

In addition to hypothetical experimental errors, Jefferies⁽¹⁾ suggests that the ad-hoc viscosity treatment of turbulence might be the responsible for such discrepancies. Therefore, the comparison between her measurements and the predictions of a three-dimensional κ - ϵ turbulence model may shed some light into this matter. Table 6-1 shows the simulated parameters corresponding to Jefferies' water model experiments and which were applied to the three-dimensional κ - ϵ model. The fluid flow boundary conditions employed in these simulations were basically the same as the simulations with liquid steel: no slip on the walls, uniform inlet, fully developed flow at the outlet and stress free and no normal flow components at the free surface of the reservoir. However, at the substrate all velocities were set to zero, since the water model simulations were carried out with a static substrate.

A quick analysis of the results showed that the inclusion of three-dimensional effects and especially κ - ϵ turbulence did not improve fluid flow predictions to any great extent. In general, the predictions are now closer to the experimental measurements, but the divergence continues for the points B and C,

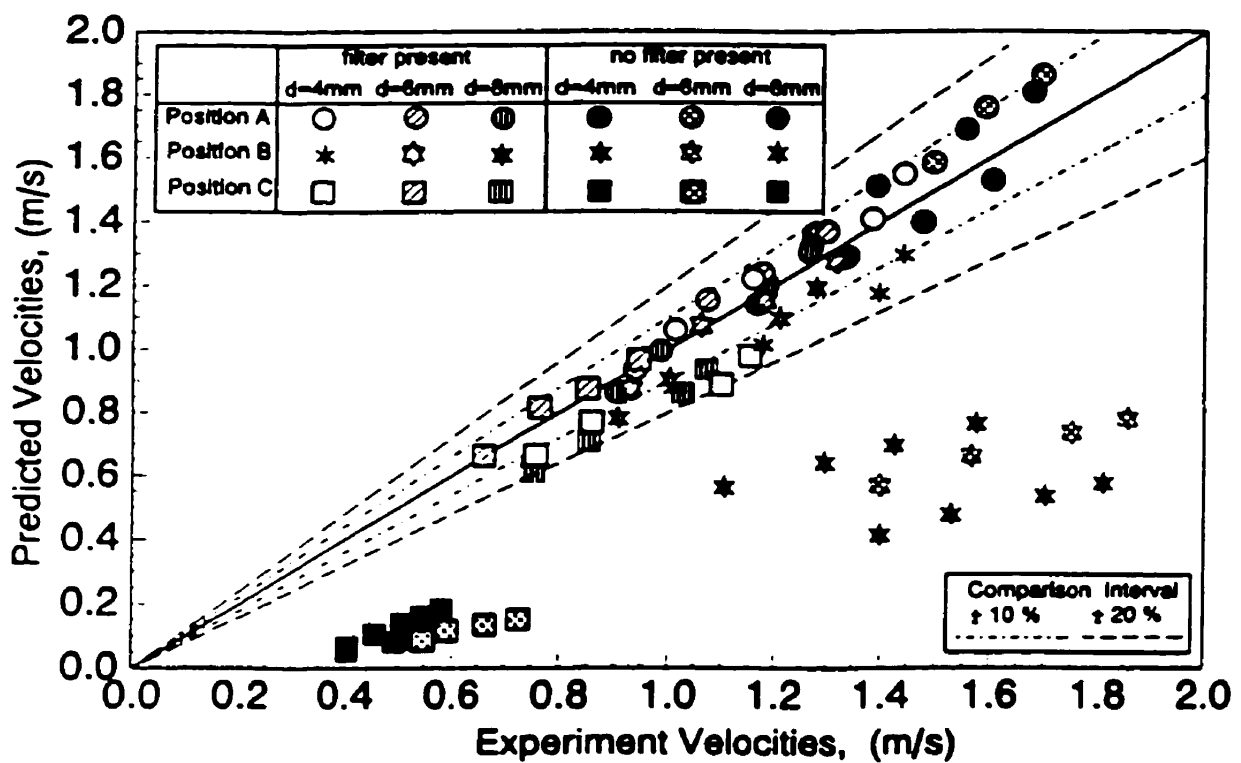


Figure 6-2. Correspondence between predicted velocities by the 2D ad-hoc viscosity and Jefferies' experimental measurements.

for the cases with no filter. After a more detailed analysis of the experimental points, it is possible to identify some conditions where the velocity at position B is even higher (!) than at the corresponding position A. This is very unlikely to occur, since the flow contracts after position B and the flow is being accelerated in that region. These observations suggest that the strong discrepancies reported should be due mainly to measurement errors on the water model experiments, rather than to imprecisions in the numerical simulations. Therefore, only the measurements performed at position A of Figure 6-1 (in the exit gap) were considered for the comparison with the predictions of the three-dimensional κ - ϵ model. In this position, the measured velocities were found always coherent with the different experimental conditions.

Table 6-1 Simulation parameters corresponding to the water model simulations

Simulation number	Exit gap (mm)	Length of domain (cm)	Water level (cm)	Flowrate (kg/min)
01/02*	4	14	8	16/12.4*
03/04*	4	14	14	22.2/18.2*
05/06*	6	15	8	24.3/17.8*
07/08*	6	15	14	33.4/25.6*
09/10*	8	14	8	27.8/21.4*
11/12*	8	14	14	37.4/31.6*

* Experiments with filter

It is seen in Figure 6-3 that the model predictions compared well to all measurements carried out in position A, for the cases listed in Table 6-1, falling within the range of $\pm 20\%$. Thus, the mathematical model proved successful in predicting the fluid flow in this particular configuration.

6.3 **SOLIDIFICATION COMPARISONS**

The validation of the heat transfer/solidification model may be partially accomplished with the use of analytical or semi-analytical solutions, for idealized cases of solid shell formation. Two main cases may be considered: the case where a constant temperature is imposed on the substrate and the case where a constant convective heat transfer coefficient is adopted between the melt and the substrate. Since the second case is more realistic for the process under study, some calculations were made for the thickness of the solidified shell, according to the following semi-analytical solution.

The governing equation for the solidified shell was solved for thermophysical properties invariant with temperature:

$$\nabla (k \nabla T) = \rho C_p \frac{\partial T}{\partial t} + \nabla (\rho C_p u_i T), \text{ where } u_i = u, v, w \quad (6-1)$$

A number of simplifications can be made to this equation. Considering that conduction in the axial and in the z directions can be neglected compared with the normal direction and also that the u and w velocities are zero in the solid shell, the original equation reduces to:

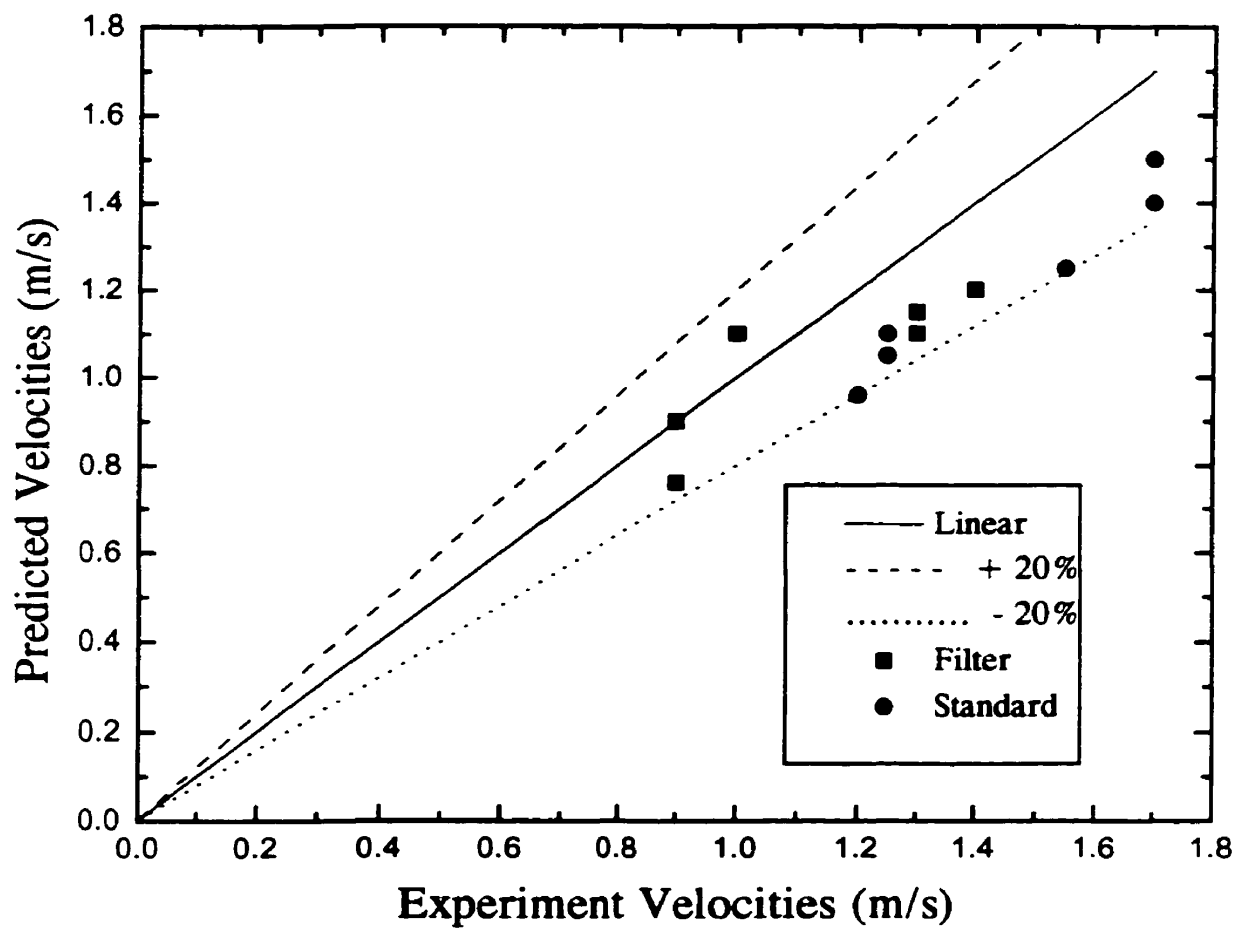


Figure 6-3. Correspondence between predicted velocities by the 3D, κ - ϵ model and Jefferies' experimental measurements.

$$k \frac{\partial^2 T}{\partial x^2} = \rho C_p v_b \frac{\partial T}{\partial y} + \rho C_p \frac{\partial T}{\partial t} \quad (6-2)$$

where v_b is the velocity of the substrate.

The mathematical model assumed steady-state conditions. So:

$$\frac{\partial T}{\partial t} = 0 \quad (6-3)$$

Then:

$$\alpha_s \frac{\partial^2 T}{\partial x^2} = v_b \frac{\partial T}{\partial y} \quad (6-4)$$

Equation 6-4, representing one-dimensional solidification, will give the heat transfer conditions for the solidification process. Adiabatic conditions were considered for all remaining regions of the caster, including the walls and free surfaces, except for the inlet temperature.

Following the approach suggested by Jefferies⁽¹⁾, the solid shell predictions of a zero-order model, an empirical model and the mathematical model were compared.

6.3.1 ZERO ORDER MODEL

To use the semi-analytical solution, a constant overall heat transfer coefficient is mandatory at the interface metal-substrate:

$$k \frac{\partial T}{\partial x} = h (T - T_{amb}) \quad (6-5)$$

The overall heat balance at the interface liquid-mushy can be represented by⁽²⁾:

$$k_s \frac{\partial T}{\partial x} - k_l \frac{\partial T}{\partial x} = \rho L_f v_b \frac{dX(y)}{dy}, \text{ at } x = X(y) \quad (6-6)$$

where L_f is the latent heat of solidification.

At the solidifying shell-liquid interface:

$$T(X(y), y) = T_{melt} \quad (6-7)$$

To calculate the temperature across the solidified shell, a linear temperature profile was assumed⁽¹⁾. So:

$$T(x, y) = a \left(\frac{y}{v_b} \right) x + b \left(\frac{y}{v_b} \right) \quad (6-8)$$

where v_b is the velocity of the substrate.

The parameters **a** and **b**, which are functions of (y/v_b) are determined using equations (6-5) and (6-7). Therefore, the temperature profile within the solid shell is given by⁽¹⁾:

$$T(x, y) = T_{melt} + \frac{h(T_{melt} - T_{amb})}{k_s + h X(y)} (x - X(y)) \quad (6-9)$$

The expression for the temperature in the solid shell is now substituted into equation (6-6), to give the expression for the rate of growth of the solid shell:

$$\rho L_f v_b \frac{dX(y)}{dy} = \frac{h(T_{melt} - T_{amb})}{1 + \frac{h}{k_s} X(y)} \quad (6-10)$$

Considering initial zero thickness for the solid shell, at $y=0$ and integrating equation (6-10), the final equation for the solid shell is⁽¹⁾:

$$X(y) = -\frac{k_s}{h} \pm \sqrt{\left(\frac{k_s}{h}\right)^2 + \frac{2k_s (T_{melt} - T_{amb}) y}{\rho L_f v_b}} \quad (6-11)$$

Clearly, the only physically relevant root of equation (6-11) is the positive one and then the solidified shell in the **x** direction can be calculated according to some parameters of the process and the substrate velocity in the **y** direction.

6.3.2 EMPIRICAL MODEL

A very simple and useful approximation for the evolution of the solidified shell is given by:

$$X(y) = K\sqrt{t}, \text{ where } t = \frac{y}{v_b} \quad (6-12)$$

This formula is commonly used in industry to roughly estimate the thickness of the solid shell. Equation (6-12) refers to the thickness of only one side in contact with the mold (symmetry). In this configuration for the single-belt caster, only one side (bottom) is in contact with the substrate. Therefore, there is no need to multiply the results of equation (6-12) by a factor of two. As for the free surface, the mathematical model results show that at $y=40$ cm it is only starting to leave the liquid phase. Thus, no solid shell is predicted on the top of the forming strip for this length of belt. In addition, for these particular solidification comparisons, no heat transfer was considered on the free surface (adiabatic condition), for the mathematical model simulations. Values of K ranging from 10 to 20 mm/min^{1/2} are commonly employed in this empirical equation⁽¹⁾.

6.3.3 MATHEMATICAL SIMULATIONS**a) Remelting of the shell**

In our particular geometrical configuration, it was seen that remelting of the incipient solid shell occurs immediately before the exit gap. The reheating

of the metal and partial remelting of the shell is due to the high-momentum incoming flow. To minimize the effect of remelting on the solidification comparisons, the thicker strip was chosen (10 mm) and some geometric parameters of the reservoir were altered. Although these changes reduced the remelting, they hardly changed the final thickness of the solid shell for the grid size employed in the model (50 nodes in the strip region). Therefore, it can be concluded that the partial remelting had little influence on the prediction of the solid shells at the end of the domain (40 cm). The aspect ratio of the grids, y/x , ranged from 30 to 60 in the solid shell region.

As mentioned before, adiabatic thermal boundary conditions were adopted, except for the inlet (uniform temperature), the substrate (uniform heat transfer coefficient) and the outlet (thermally fully-developed). For the inlet, a zero superheat was considered, the temperature therefore was 1535°C. At the substrate-metal interface, an overall heat transfer coefficient of 10000 W/m².K was applied and, as in Jefferies' ⁽¹⁾ simulations, the substrate speed was fixed at 0.9 m/s.

6.3.4 RESULTS AND DISCUSSION

Figure 6-4 shows the predicted thickness for the corresponding thermal boundary conditions, according to Jefferies' model and the semi-analytical and empirical solutions. In Figure 6-4, various values of the ad hoc multiplicative factor were tried by her. In fact, this factor corresponds to the ratio k_{eff}/k_l , which corresponds to the local Nu number at the interface solid-liquid. The two-dimensional ad-hoc viscosity model overpredicts the solid shell by as much as 60%, for the lowest values of Nu chosen. For Nu equal to 45, the result

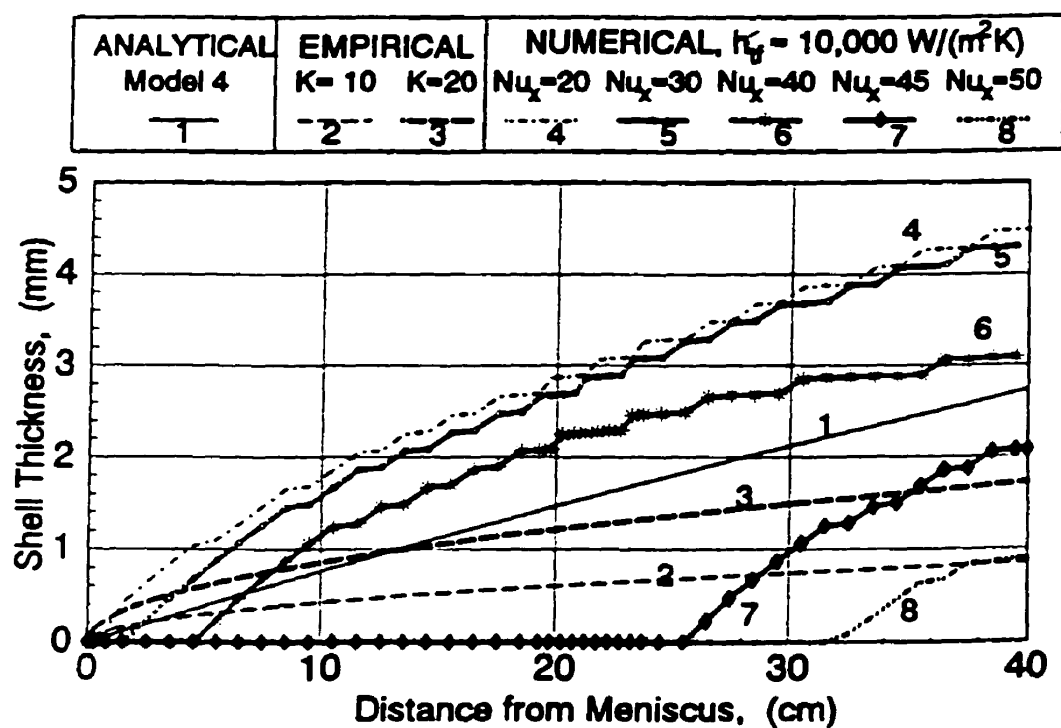


Figure 6-4. Predicted shell thickness for some heat flux boundary conditions - Jefferies' work.

approaches the analytical solution at the end of the domain; however, the solidified shell starts at approximately 25 cm from the meniscus and rises much more sharply than the analytical solution. The best results of the two-dimensional model were given by Nu equals to 40, where the solid shell starts at approximately 5 cm from the meniscus, surpasses the analytical solution at approximately 8 cm and reaches a value approximately 10% higher than the analytical solution at the end of the computational domain. Despite the partial success obtained with the use of an appropriate Nu number, the results of this model are clearly dependent on the right choice of the ad-hoc factor and this parameter, being dependent on the local Nusselt number, has to be determined for different relative velocities between the solidifying shell and the molten metal close to it.

As shown in Figure 6-5, the predictions of the three-dimensional, κ - ϵ turbulence model are much closer to the analytical solution than those of Jefferies. In Figure 6-5, not only is the value at the end of the domain closer, but also the evolution of the solid shell with time is more harmonious for the three-dimensional model. Moreover, considering that the mathematical model solution is not continuous as is the semi-analytical one, due to the use of finite grid sizes, the agreement between the three dimensional κ - ϵ model and the semi-analytical solution (which itself is not exact) is good.

6.4 SDAS MEASUREMENTS

One of the possibilities to validate the heat transfer/solidification predictions is the comparison of predicted process parameters, such as cooling rates or local solidification times, with correlations based on the SDAS (Secondary Dendrite

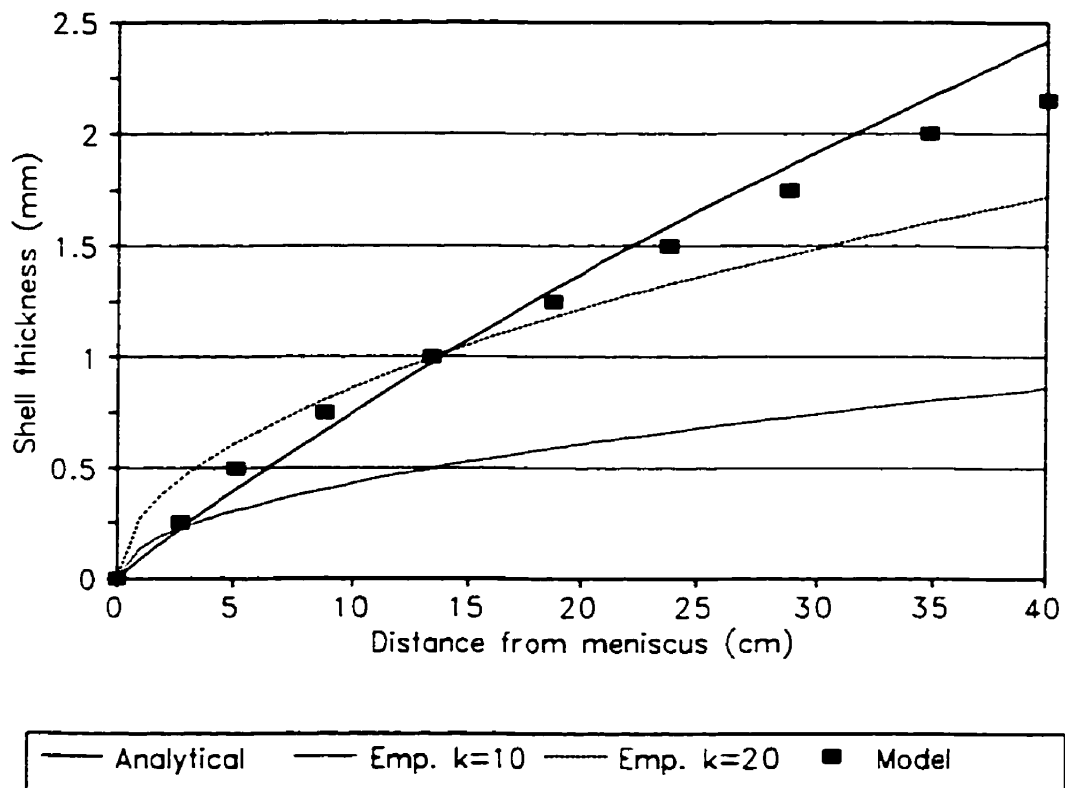


Figure 6-5. Comparison of the shell thickness predicted by the 3D, κ - ϵ model and other models.

Arm Spacing) measured in actual castings. Empirical relationships between the SDAS and such parameters are available in the literature for diverse materials. The crucial question here is the range of validity of such correlations. Wang and Matthys⁽³⁾ argued that these relationships are well documented in the literature for low cooling rates only. However, Couture et al⁽⁴⁾ sustained that the correlation developed by Suzuki et al⁽⁵⁾, which was established for steel with carbon contents ranging from 0.1 to 0.88% and for cooling rates varying from 1 and 500° C/min, holds for cooling rates within the range 10^{-2} to 10^8 °C/s, making their use very attractive, especially for fast-cooling processes, where direct cooling rate measurements are not easy.

In this work, some of the many relationships given in the literature were used to predict the SDAS, according to calculated local cooling rates. These values were then compared with the ones given in the literature, preferably for measured SDAS under similar conditions. Table 6-2 shows the constants for some of the correlations given in the literature employed in the validation of the model. All relationships were based on the following formulation:

$$\lambda = B \cdot \left(\frac{\partial T}{\partial t} \right)^{-n} \quad (\mu.m) \quad (6-13)$$

where:

$$\left(\frac{\partial T}{\partial t} \right) = \frac{(T_L - T_S)}{\Delta t_s} \quad (6-14)$$

and Δt_s is the local solidification time.

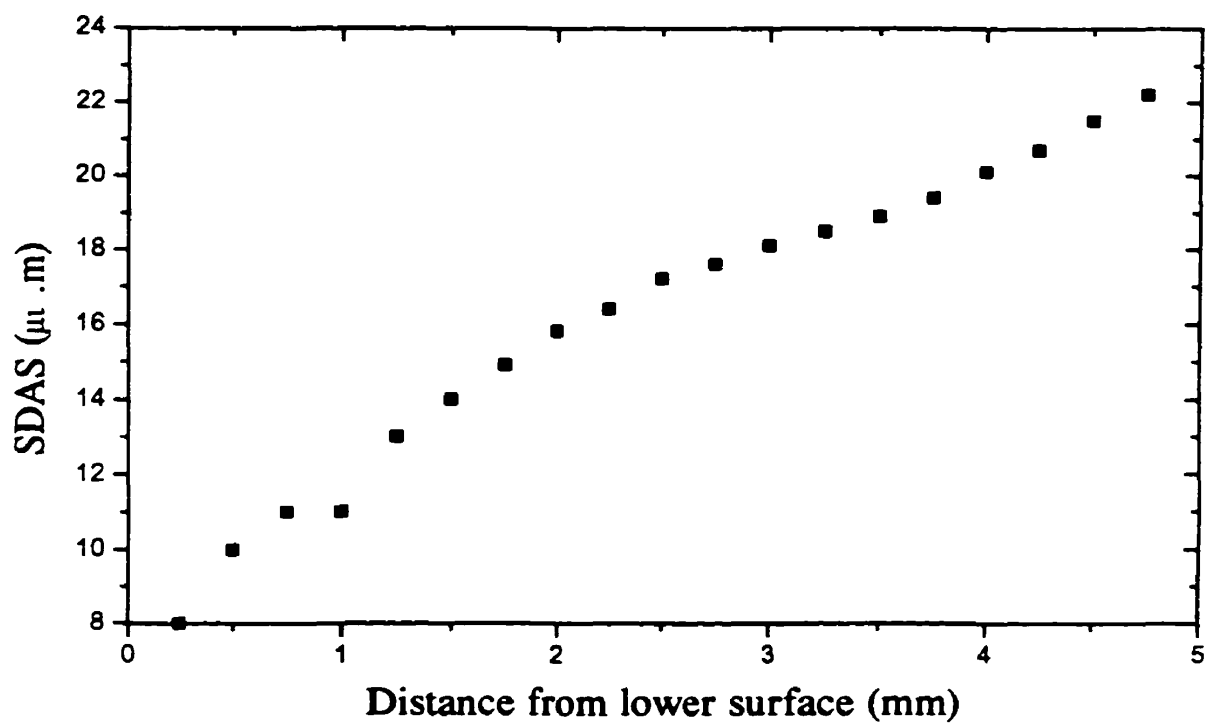
Table 6-2 Values of parameters B and n for SDAS calculations.

Material	B ($\mu\text{m.K}^n.\text{s}^{-n}$)	n	Reference
Carbon Steel	143	0.41	(6)
Carbon Steel	710	0.39	(4)
Carbon Steel	158	0.38	(7)
Carbon Steel	146	0.386	(8)
Stainless Steel	63.91	0.347	(9)
Stainless Steel	68	0.45	(6)

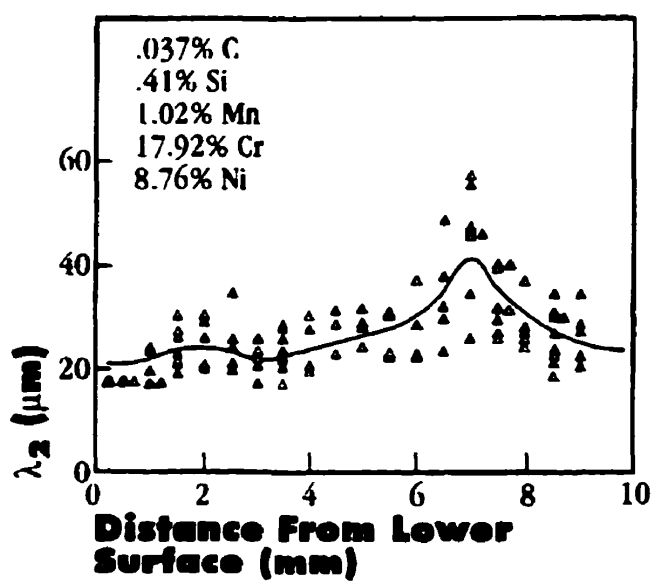
In Figure 6-6, it is shown the predicted SDAS from the cooling rates calculated by the model, using the correlation suggested by Loser et al⁽⁶⁾, and the values of SDAS measured on stainless steel strips cast on the single-belt laboratory caster of the University of Clausthal⁽¹⁰⁾. To simulate the conditions for stainless steel strip casting, the following thermophysical properties were considered:

Table 6-3 Thermophysical properties of stainless steel

Thermophysical property	Value
Specific heat	700 J/kg.K
Thermal conductivity	31 W/m.K
Latent heat of fusion	264 KJ/Kg
Liquidus temperature	1454° C
Solidus temperature	1400° C



(a)



(b)

Figure 6-6. Predicted (a) and measured (b) SDAS for stainless steel strips cast in the University of Clausthal.

The operational conditions and thermal boundary conditions are given in Table 6-4.

Table 6-4 Parameters for the model simulations.

Parameter/Operational Condition	Value adopted in the simulation
Strand width	170 mm
Strand thickness	10 mm
Typical casting velocity	0.15 m/s
Inlet superheat (estimated)	20°C
Heat transfer coefficient (W/m ² .K)	3000

It is seen in Figure 6-6 that the SDAS are predicted to increase from approximately 10 to approximately 20 μ .m for the bottom surface and the middle of the strip (distance = 5 mm), respectively. On the other hand, the SDAS measured at Clausthal point to a relatively stable value of 20 μ .m up to 4 mm from the bottom surface. Then it increases, reaching a high of roughly 40 μ .m at distance = 7 mm. Several points should be raised before a comparison between the measured and predicted results is made:

- The heat transfer coefficient was assumed constant and equal to 3000 W/m².K. The reason for this assumption was that no values are given in the literature for the specific conditions and the material of

- the belt was low-carbon steel, which has thermal conductivity lower than copper. The superheat was estimated, as well;
- There are various correlations between SDAS and cooling rates, for the diverse steel grades, including stainless steels. Therefore, the predicted SDAS can vary significantly, according to the correlation chosen;
 - It is not clear in Reference 10 if the data were extracted from a single strip, or if it is an average of a number of runs.

The values presented in Figure 6-6 show that the predicted SDAS, despite the limitations outlined above, are not far from the measured values. The divergence resides in the fact that the measured SDAS did not seem to vary significantly next to the bottom surface, contrary to the predictions of the model. Considering that the thermal resistance increases as the solid shell develops, it seems unlikely that the cooling rates are maintained the same up to almost half the strip thickness. Indeed, recent work has showed⁽¹¹⁾ that the SDAS of a 0.22%C steel strip cast in the same machine presented a tendency to increase along the thickness (Figure 6-7). The experimental conditions for the casting of the 0.22%C steel strip were not disclosed. Therefore, a more consistent set of experimental data would be necessary to allow a better comparison of the results of the model with experimental values.

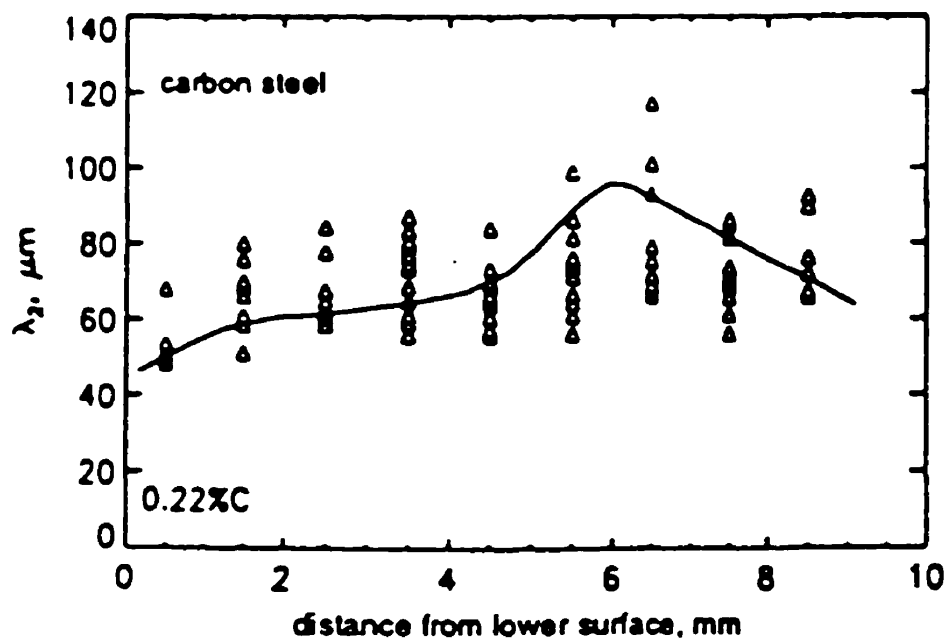


Figure 6-7. Predicted SDAS for carbon steel strips cast in the University of Clausthal⁽¹⁰⁾.

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STRESS AND DISPLACEMENT MODELLING

7.1 INTRODUCTION

The main objectives in the development of the stress model were to identify those regions of the strip more prone to cracking and also to determine the likely displacements of the strand with respect to the conveyor belt.

In principle, thin slabs are more susceptible to crack formation than continuously slab casts⁽¹⁾. Reasons for this can be attributed to the higher frictional forces associated with casting thin slabs at a higher speed. This speed also accentuates temperature gradients at the solid-belt interface and within the solid shell, enhancing thermal stresses. However, depending on the process, the level of mechanical constraint can be lower than that in the conventional continuous casting process. This is the case of the single-belt process, where the existence of a free upper surface and a little liquid column largely avoid stresses other than thermal stresses. In addition, no reheating of the shell takes place at the mould exit, as in conventional casting. This is known to contribute to the development of high tensile stresses within the shell increasing the likelihood of cracks.

As mentioned in the literature review, an element of material subjected to

thermal load may exhibit an elastoplastic behavior, i.e., if the stresses applied surpass the yield strength of the material, a permanent plastic strain will remain in the material. In order to adequately model the stresses within a solidifying strip, one must incorporate the plastic properties of the material at the corresponding high temperatures, in addition to providing the mechanical properties that will define the elastic region. A brief review of the elasticity and plasticity fundamentals is given below.

7.2 ELASTICITY

Elastic deformations in a body are linearly related to the internal stresses by means of the modulus of elasticity, E , or Young's modulus, defined in Hooke's law:

$$\sigma_x = E \cdot \epsilon_x \quad (7-1)$$

where E is the modulus of elasticity in tension or compression (Figure 7-1).

Whenever a tensile force acts in the x direction, it produces an extension along that axis and contraction in the other transverse (y and z) directions. The transverse strains can be related to the longitudinal strain by the proportionality constant called Poisson's ratio ν :

$$\epsilon_y = \epsilon_z = -\nu \epsilon_x = -\nu \frac{\sigma_x}{E} \quad (7-2)$$

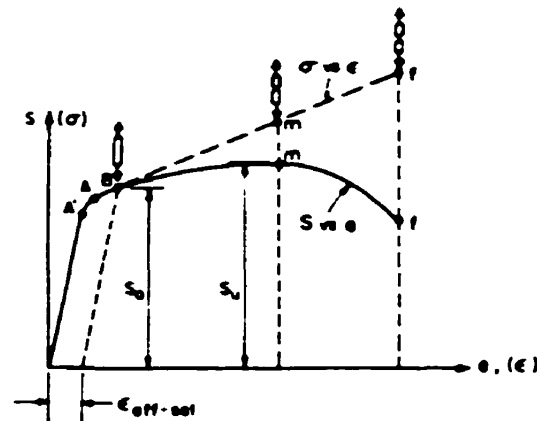


Figure 7-1. Typical stress-strain curve.

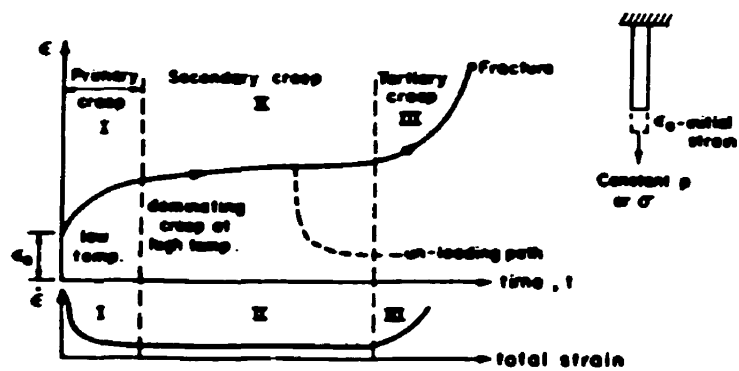


Figure 7-2. Creep deformation behavior.

Thus, for a single cubic element, the x, y and z, elastic strains are given by:

$$\epsilon_i = \frac{1}{E} [\sigma_i - \nu (\sigma_j + \sigma_k)] \quad (7-3)$$

where ϵ_i and σ_i are the deformation and tension in direction i, respectively and σ_j and σ_k are the tensions in the two other directions.

In an equilibrium state, there is a unique relationship between stress and strain components in an isotropic solid. This relationship is given below in matrix form, that renders it more suitable for finite-element formulations:

$$\sigma = \frac{E}{(1+\nu) \cdot (1-2\nu)} \begin{bmatrix} (1-\nu) & \nu & \nu & 0 & 0 & 0 \\ & (1-\nu) & \nu & 0 & 0 & 0 \\ & & (1-\nu) & 0 & 0 & 0 \\ & & & \frac{(1-2\nu)}{2} & 0 & 0 \\ & & & & \frac{(1-2\nu)}{2} & 0 \\ & & & & & \frac{(1-2\nu)}{2} \end{bmatrix} \cdot [\epsilon] \quad (7-4)$$

SYM

This matrix is called the stiffness matrix.

A solid body is said to be deformed when the relative positions of its internal points are changed. Thus, the displacement of a point is defined as the vector distance from the initial to the final position of such a point. Usually, u, v and w are used to denote the displacement components along the x, y and z axes, respectively.

The relationship between strains and displacements may be expressed in indicial form:

$$\epsilon_{ij} = \frac{1}{2} (U_{i,j} + U_{j,i}) \quad (7-5)$$

or in matrix form:

$$(\epsilon) = \begin{bmatrix} \epsilon_{xx} \\ \epsilon_{yy} \\ \epsilon_{zz} \\ \epsilon_{xy} \\ \epsilon_{yz} \\ \epsilon_{xz} \end{bmatrix} = \begin{bmatrix} \frac{\partial u}{\partial x} \\ \frac{\partial v}{\partial y} \\ \frac{\partial w}{\partial z} \\ \frac{\partial u}{\partial y} + \frac{\partial v}{\partial x} \\ \frac{\partial v}{\partial z} + \frac{\partial w}{\partial y} \\ \frac{\partial w}{\partial x} + \frac{\partial u}{\partial z} \end{bmatrix} = \begin{bmatrix} \frac{\partial}{\partial x} & 0 & 0 \\ 0 & \frac{\partial}{\partial y} & 0 \\ 0 & 0 & \frac{\partial}{\partial z} \\ \frac{\partial}{\partial y} & \frac{\partial}{\partial x} & 0 \\ 0 & \frac{\partial}{\partial z} & \frac{\partial}{\partial y} \\ \frac{\partial}{\partial z} & 0 & \frac{\partial}{\partial x} \end{bmatrix} \cdot \begin{bmatrix} u \\ v \\ w \end{bmatrix} \quad (7-6)$$

In the case of a thermal-stress problem, the relationships between stresses and strains are the only ones which differ from those in the general elasticity problem. The expansion or contraction caused by the thermal load should be taken into account through the inclusion of a new term in the calculation of the elastic strains. When an element of material is subjected to thermal load, the strain in the element can be divided in two parts:

- a) elastic strain: the part ϵ_{xx} that is related through a modulus to the

stresses imposed on the surfaces of the element by the surrounding material and

- b) thermal strain: due to the variations in the element's temperature. When this variation is positive, this part of the strain is also positive (expansion). The thermal strains are function of the linear expansion coefficient α and the thermal gradient ΔT :

$$\epsilon_T = \alpha \cdot \Delta T \quad (7-7)$$

The coefficient of expansion α varies with the temperature T and, to a lesser extent, with stress⁽³⁾.

7.3 PLASTICITY

In the stress-strain curve shown in Figure 7-1, the yielding point (point B) is clearly visible for some materials, such as carbon steels, but not for aluminum alloys. As can be seen, in the plastic regime, the strain is no longer proportional to the stress; each strain component contains a term linearly related to the stresses, plus a plastic term that is not linearly related.

One of the most important parameters that must be included in stress analysis is the material's "stiffness." In the elastic range, the stiffness is a constant represented by the modulus of elasticity E . However, as mentioned above, this property varies when the material reaches the plastic limit. The function that describes the variations of stiffness in the plastic regime is called the constitutive law of the material and is a necessary condition for any type of elastic-plastic

stress analysis. This function can be calculated from equations in the literature or may be given by experimental investigation. The curves that represent the relation between σ and ϵ beyond the yield point are known as flow curves.

For most practical cases, the only flow curve available to the engineer is derived from a uniaxial tensile test on the material. In reality, however, most analyses include the appearance of multiple stress components. Therefore, some modifications should be made to the original uniaxial flow curve to make it adequate to the multidimensional stress state. Furthermore, an additional obstacle to the precise determination of flow curves is the difficulty to carry out measurements at high temperatures, near the yielding point of the material. The flow rate plays also an important role, as shown by Wray⁽⁴⁾.

7.3.1 STRAIN-HARDENING

If a material deforms continuously once reached its yield strength, with no further increase in stress and at a given temperature, this material is known as an elastic-perfectly plastic material⁽³⁾. However, in most practical cases, further plastic deformation beyond the elastic limit requires additional loads. This behavior is known as strain-hardening and needs to be included in the constitutive law of the material. Figure 7-1 illustrates both cases.

The strain-hardening behavior of most engineering materials during plastic deformation brings up the fact that the yield strength of a material is not a constant, but rather a function of stresses and strains.

7.3.2 CREEP

When a material presents progressive deformation at a constant load or stress, such behavior is called creep deformation⁽³⁾. This phenomenon, as strain-hardening, is believed to be caused by microstructural relaxation and is very complex, depending on a number of factors, such as temperature and stress histories, composition of material and microstructural patterns. Figure 7-2 represents creep deformation behavior. As already discussed in the literature review, creep is almost indistinguishable from plastic strain, especially at high temperatures, and its effects are usually considered together.

7.3.3 YIELD CRITERIA FOR THE MULTIDIMENSIONAL STATE

During a simple uniaxial test in a material, identifying the yield point in an idealized stress-strain curve is fairly easy. However, for most situations, the material is subjected to biaxial or triaxial loadings. Therefore, a yield criterion is necessary for assessing the likelihood of cracks in a working material. For this purpose, several criteria have been presented, but only two of them have earned general acceptance: Tresca and von Mises criteria.

a) Tresca criterion: The Tresca criterion states, in essence, that flow occurs when the maximum shear in any plane in the body reaches a specified critical value. The general expression for the Tresca criterion is⁽²⁾:

$$[(\sigma_1 - \sigma_2)^2 - \sigma_y^2] \cdot [(\sigma_1 - \sigma_3)^2 - \sigma_y^2] \cdot [(\sigma_2 - \sigma_3)^2 - \sigma_y^2] = 0 \quad (7-8)$$

where σ_y is the yield strength of the material from a uniaxial tension test.

From the above equation, the following expressions are derived:

$$\sigma_1 - \sigma_2 = \sigma_y ; \sigma_1 - \sigma_3 = \sigma_y ; \sigma_2 - \sigma_3 = \sigma_y \quad (7-9)$$

b) **Von Mises criterion:** The von Mises criterion was derived from the distortion energy theory. Its mathematical expression is⁽²⁾:

$$\sigma = \sigma_y = \sqrt{3}\sigma_s, \text{ where:} \quad (7-10)$$

$$\sigma = \frac{1}{\sqrt{2}} [(\sigma_{xx} - \sigma_{yy})^2 + (\sigma_{yy} - \sigma_{zz})^2 + (\sigma_{xx} - \sigma_{zz})^2 + 6(\sigma_{xy}^2 + \sigma_{yz}^2 + \sigma_{zx}^2)]^{1/2} \quad (7-11)$$

and σ_s is the yield strength of the material under pure shear.

c) **Comparison between Tresca and von Mises criteria:** The Tresca criterion, due to its simplicity, is more suitable for classical elastic-plastic problems. However, it is mathematically represented by discontinuous functions⁽²⁾, which can induce errors in the solution. On the other hand, the von Mises criterion is more complex mathematically and its application is not straightforward. Nevertheless, since it behaves as a continuous function⁽²⁾, it becomes more precise than the Tresca criterion and it is then suitable to more complex elastic-plastic problems. The von Mises criterion suggests that yielding takes place in a pure

tension at a stress level about 15% lower than that predicted by the Tresca criterion⁽²⁾.

For steel casts, as mentioned in the literature, the von Mises criterion has been widely accepted, along with less important criteria. Therefore, von Mises will be taken as the standard criterion to determine the likelihood of cracks in the solid shell.

7.4 FINITE-ELEMENT STRESS MODEL

The stress model was written and run in the commercial Abaqus⁽⁹⁾ software. Temperature fields generated by the coupled fluid flow-heat transfer model were imposed on the mechanical model, in an uncoupled manner. The reason for that was the huge numerical complexity that would arise from coupling the models. Furthermore, the main objective in this stress modelling was to obtain preliminary information on the mechanical behavior of a solidifying strip, which has cooling conditions and mechanical constraints quite different from the conventional continuous casting process.

The hypothesis of plane stress and plane strain were at first investigated, in order to simplify the stress problem. However, since the thermal gradient is very high in the thickness direction and fairly high in the longitudinal one, none of these cases would apply to the specific problem. Therefore, a three-dimensional steady-state model was considered for the mechanical problem, and symmetry was assumed in the width direction, as in the heat transfer/fluid flow finite-difference model.

Rectangular stress/displacement elements with 8-node linear brick shape were used in the three dimensions. The computational domain comprised the carbon steel belt and the solid shell, i.e., the points where the temperature was below the solidus temperature of the low carbon steel. The region of the delivery system was not modelled, since the solid shell in that region was very thin and constrained by a considerable metallostatic head. Therefore, the computational domain for the stress model started on the nozzle exit. Figure 7-3 shows the standard computational domain for the stress problem.

Since the dimensions x and y are much bigger than the thickness z of the solid shell, the aspect ratio between the elements in these two directions and the ones in the thickness was controlled by employing finer grids in such directions. This restriction also avoided further refining of the grid in the z direction. Typically, at the end of the domain the solidified shell thickness reached 1-2 mm.

Interface elements were initially used to monitor the relative displacements between the belt and the solid shell, which lead to the formation of an air gap. However, the belt was found to undergo a small deformation, for the small temperature gradient it experiences within the domain, as will be mentioned later.

A thermal-elastoplastic approach was adopted for the stress model. Creep was not considered relevant, given the high cooling rates involved in the process. Furthermore, creep is a rate-dependent phenomenon and stresses are considered in steady-state.

7.4.1 CONSTITUTIVE LAW

Two different constitutive laws were tested in the model and their results

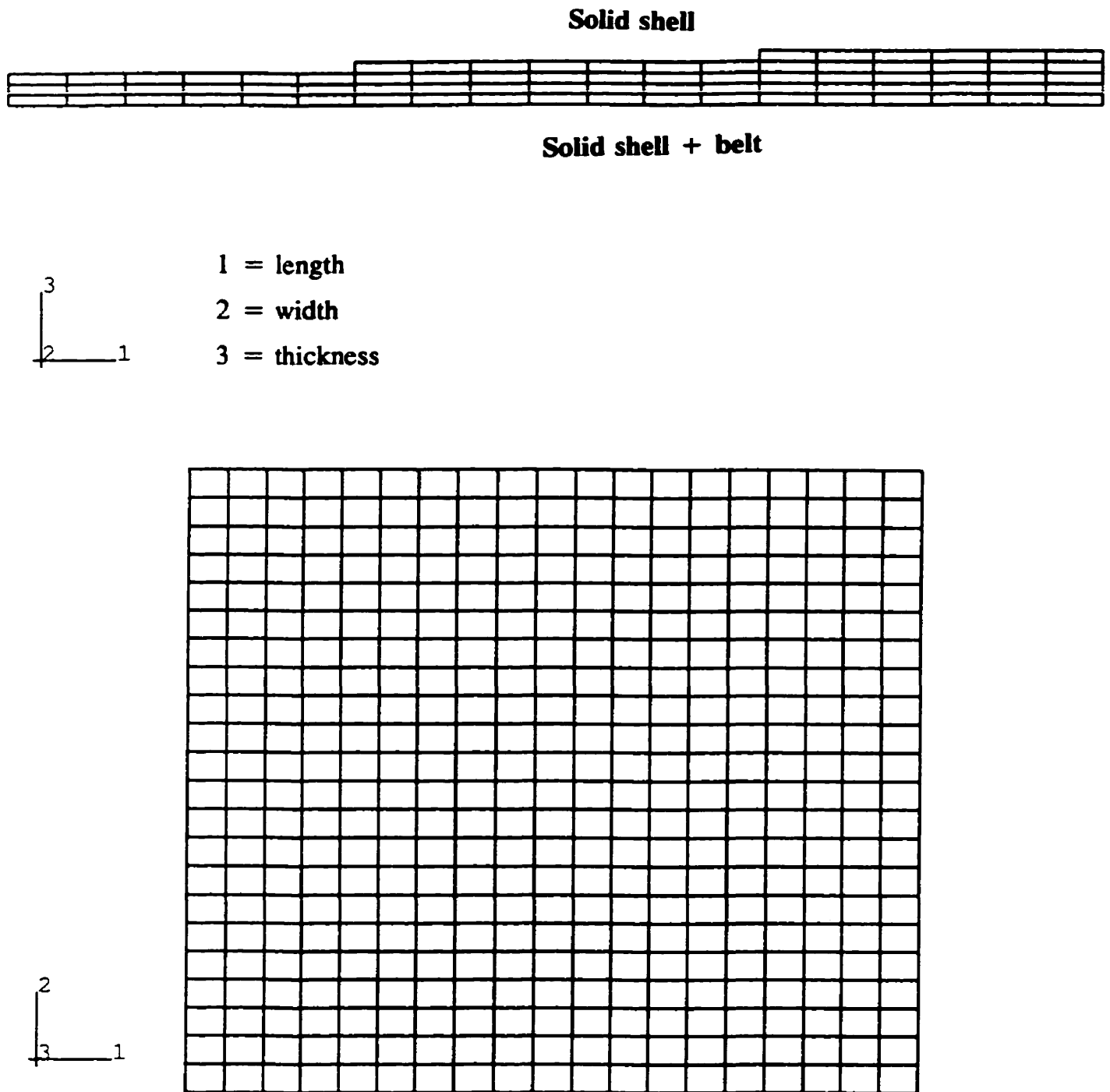


Figure 7-3. Standard computational domain for the stress problem

were compared. Both laws included strain-hardening. The first was given by Brimacombe et al⁽⁶⁾ and its main features are:

- Young's modulus:

$$E = 1.08 \times 10^6 - 0.103 (T - T_{tr}) \times 10^4 \quad (7-12)$$

where T_{tr} is the temperature of the austenite-ferrite transformation.

- Plastic modulus:

$$H_p = 0.1 E \quad (7-13)$$

where H_p is the hardening coefficient.

- Strain limit of elasticity: 0.1 % at 800°C and

$$\epsilon_{e,l} = 1.61 \times 10^{-3} - 7.56 T \times 10^{-7}, \text{ above } 800^\circ\text{C} \quad (7-14)$$

- Poisson's ratio $\nu = 0.33$

The second constitutive law was taken from Wray⁽⁴⁾, which supplied tables with the values of elastic modulus, yield stress and plastic modulus, between 900 and 1600°C, for carbon steel at a strain rate of $4 \times 10^{-4} \text{ s}^{-1}$.

Despite some divergence in the input data, the results were qualitatively identical and not much quantitative difference was observed when using either of the two constitutive law in this particular problem. Therefore, Brimacombe's constitutive model was employed in the simulations, for low-carbon steel ($< 0.1 \text{ \% C}$).

The coefficient of thermal expansion was considered constant for the range of temperatures involved (900 - 1492°C), and equal to $2 \times 10^{-5} \text{ }^{\circ}\text{C}^{-1}$, both for the strip and for the belt.

7.4.2 GRID DEPENDENCY

As in the fluid flow-heat transfer model, the grid dependency of the stress model results was analyzed. Initially, a 10-element grid was assigned to the 24-cm length of the domain, 22 elements to the width and an element for every 0.25 mm of solidified shell, matching the grid of the original model. However, due to the high aspect ratio mentioned earlier, increasing the number of elements in the length up to 15 elements was necessary, to have physically meaningful results. An increase in the number of elements in the length up to 15 produced some variation, especially in the displacement results. After this limit, little difference was observed for further grid refining.

Some numerical difficulty was present, however, due to the very high aspect ratios existent in the three directions. This abnormal aspect ratio (approximately 1: 40) caused numerical singularities that might have partially compromised the absolute value of stresses calculated, especially in certain regions of the domain. The usual simplifications to this problem could not be applied here, since the problem could not be reduced to two-dimensional without losing important information, nor could the grid be further refined in the other directions, due to the additional computational demands. Nevertheless, the results seemed qualitatively sound and the absolute values are reasonable, as well, within the expected range.

7.4.3 BOUNDARY CONDITIONS

In general, the cast was considered unconstrained. Only the lateral sides of the strip were taken as constrained by the side dams. So, a simply-supported beam was considered, which means that the strip is free from bending at the ends:

$$U(y = 0) = 0 \quad ; \quad \frac{\partial U}{\partial y}(y = W/2) = 0 \quad (7-15)$$

where W is the width of the strip and U is the transversal displacement.

The metallostatic head and the mushy region were not included in the computational domain, but rather replaced with the corresponding pressure, which was applied as a boundary condition uniformly over the solid shell, as in some works presented in the literature⁽⁷⁾.

It was also considered that, apart from the metallostatic pressure, no external forces are acting on the solid along the axial direction, and that the shear stress is minimal (solid moving with the same speed of the belt) in comparison to the bending stress.

Due to additional numerical difficulties involved in calculating the temperature within the very thin C-steel belt (0.75 mm), and considering that the belt temperature does not vary much in the computational domain, its value was linearly interpolated, between 100 and 200°C, for the 24 cm of the domain.

The temperatures within the solid shell were generated by the original heat transfer model and transferred to Abaqus with the use of a couple of small

programs.

7.5 RESULTS FOR THE SINGLE-BELT CASTER

7.5.1 DISPLACEMENT PROFILES ALONG THE WIDTH (U3)

The displacement profile along the width of the strip (44 cm) at the end of the domain (24 cm from the exit gap) is seen in Figure 7-4. It is observed that the displacement increases up to the center of the strip and reaches a maximum value of around 0.2 mm. Displacements at $x=0.12$ m are shown in Figure 7-5. It is noticed that the shape is the same as in Figure 7-4, but the values are smaller. This is explained by the fact that the displacements in such a problem are mainly caused by the thermal contraction of the casting. Considering the additional cooling of the shell between $x=0.12$ m and the end of the domain, the displacements were gradually increased. The thermal contraction, however, is not the only determinant factor of the displacements of the solid shell and the growth of the shell can reduce the displacements at the interface. For instance, the maximum value of U3 was found at $x=0.23$ m, as seen in Figure 7-6. At the beginning of the domain, the displacement U3 was negative, and at approximately $x=0.08$ m, a reversion of the displacement sign occurred. Figure 7-7 shows the evolution of the displacement along the width of the strip from the end of the nozzle to the end of the domain, i.e., 24 cm from the exit nozzle.

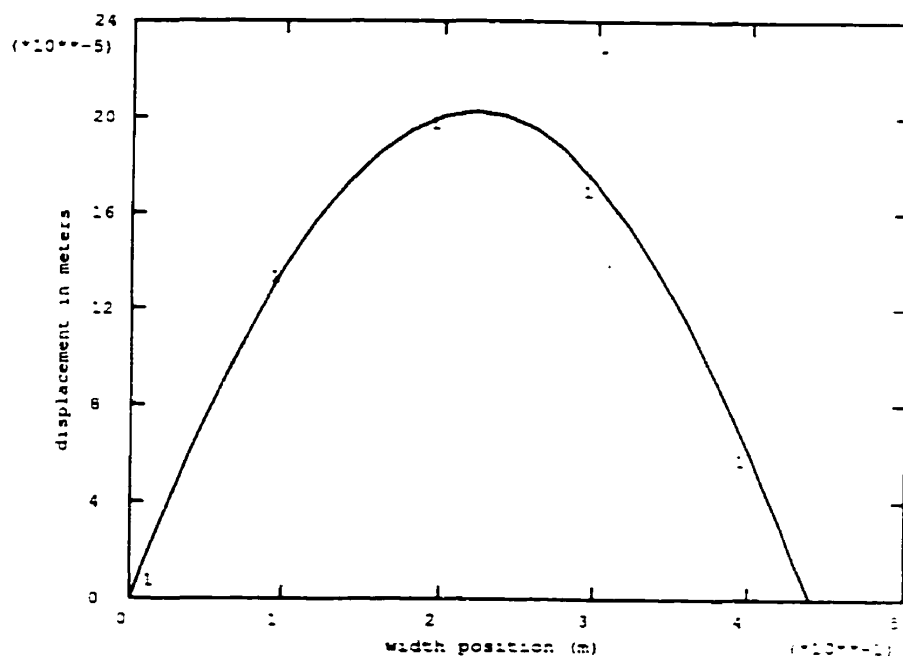


Figure 7-4. Displacement profile along the width direction at the end of the domain, i.e., 24 cm from exit nozzle.

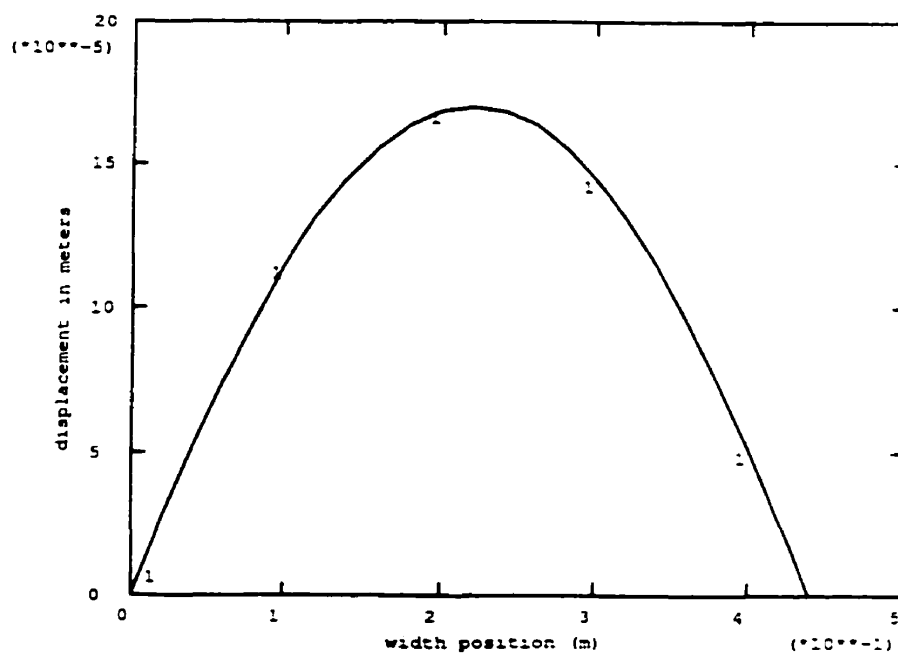


Figure 7-5. Displacement profile along the width direction at 12 cm from the exit gap.

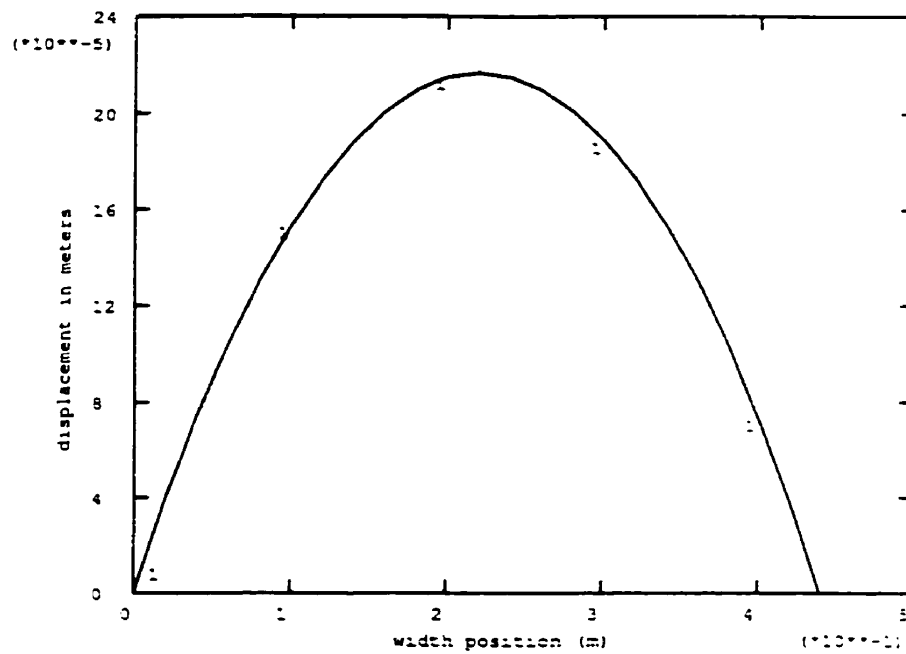


Figure 7-6. Displacement profile along the width direction at 23 cm from the exit gap.

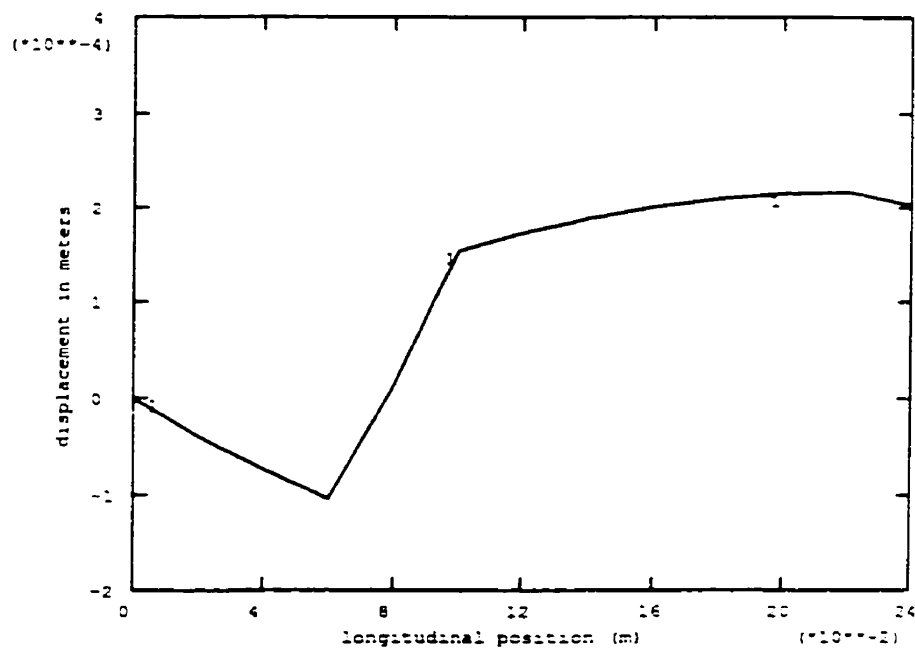


Figure 7-7. Evolution of the displacement in the center of the strip.

7.5.2 STRESS PROFILES

The longitudinal stresses at the shell-belt interface are shown in Figure 7-8. The stresses are initially compressive, and at approximately $x=0.10$ m, there is a reversion of the stress sign. Maximum stresses in the order of 2 MPa were calculated, with a slight increase on the edges, where the solid shell is cooler. In this Figure, and in the following, the stresses are given in the plane x - y , for the longitudinal position (x) and the width (y), from the wall ($y=0$) to the center of the strip ($y=W/2$)

In the middle of the strip, it was found that the stresses were always compressive, as given in Figure 7-9, and a maximum stress of -7 MPa was found.

On the top surface of the shell, near the solid-liquid interface, compressive stresses were calculated, but perhaps with a tendency to become tensile (positive). Figure 7-10 shows that, after reaching a peak of -7 MPa, the compressive stresses fell down to less than -2 MPa.

Considering that it is well known that the tensile stresses are responsible for cracks and other defects in steel casting, a more detailed study was carried out on the cooling surface.

The stress evolution shown in Figure 7-8 suggests a continuous build-up of stress. This is aligned with the fact that the thermal contraction (strain) in the solid shell is the main source of stress in the specific problem. As the contraction of the shell is opposed by lateral constraints and the non-uniform temperatures create a

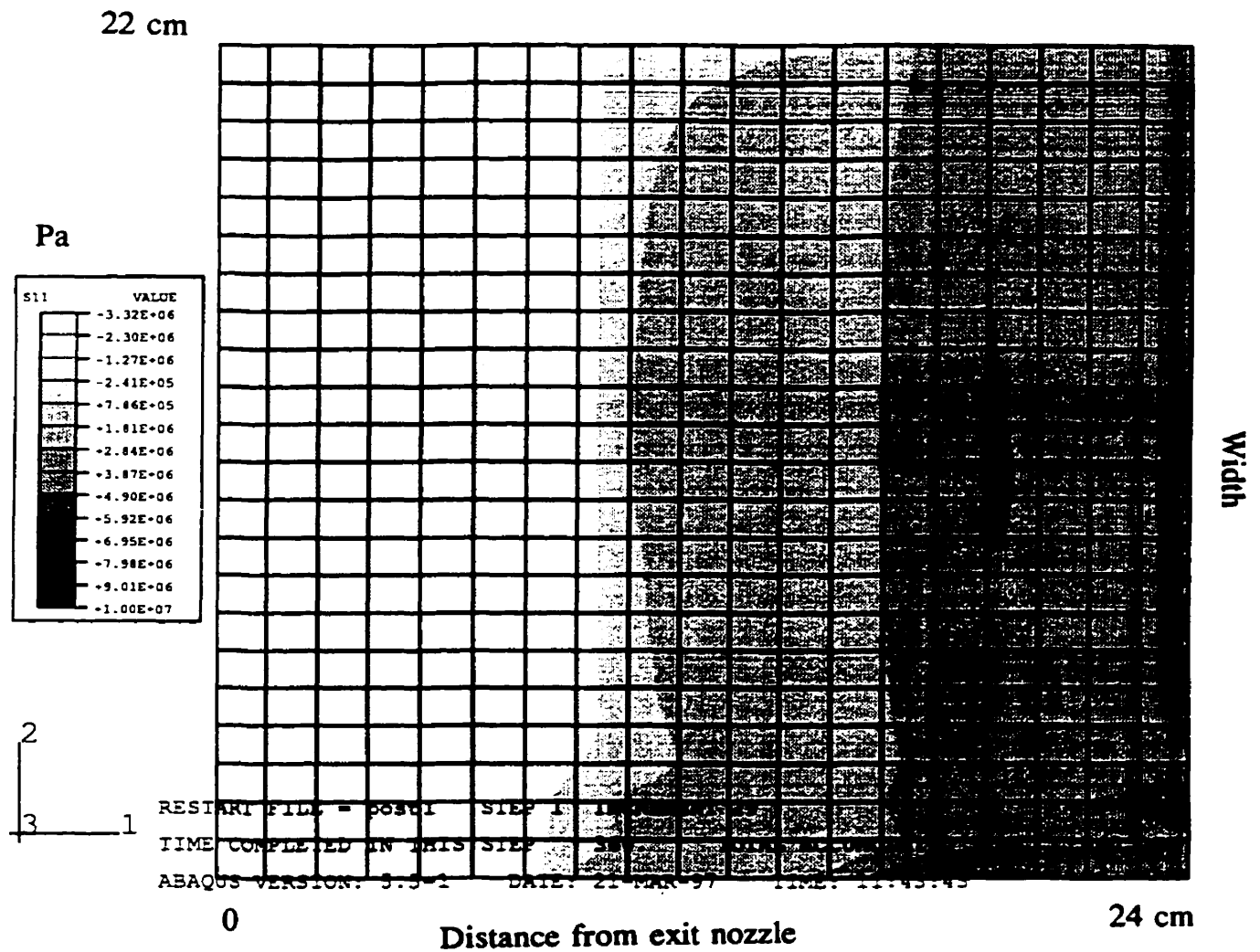


Figure 7-8. Longitudinal stresses at the interface shell-belt.

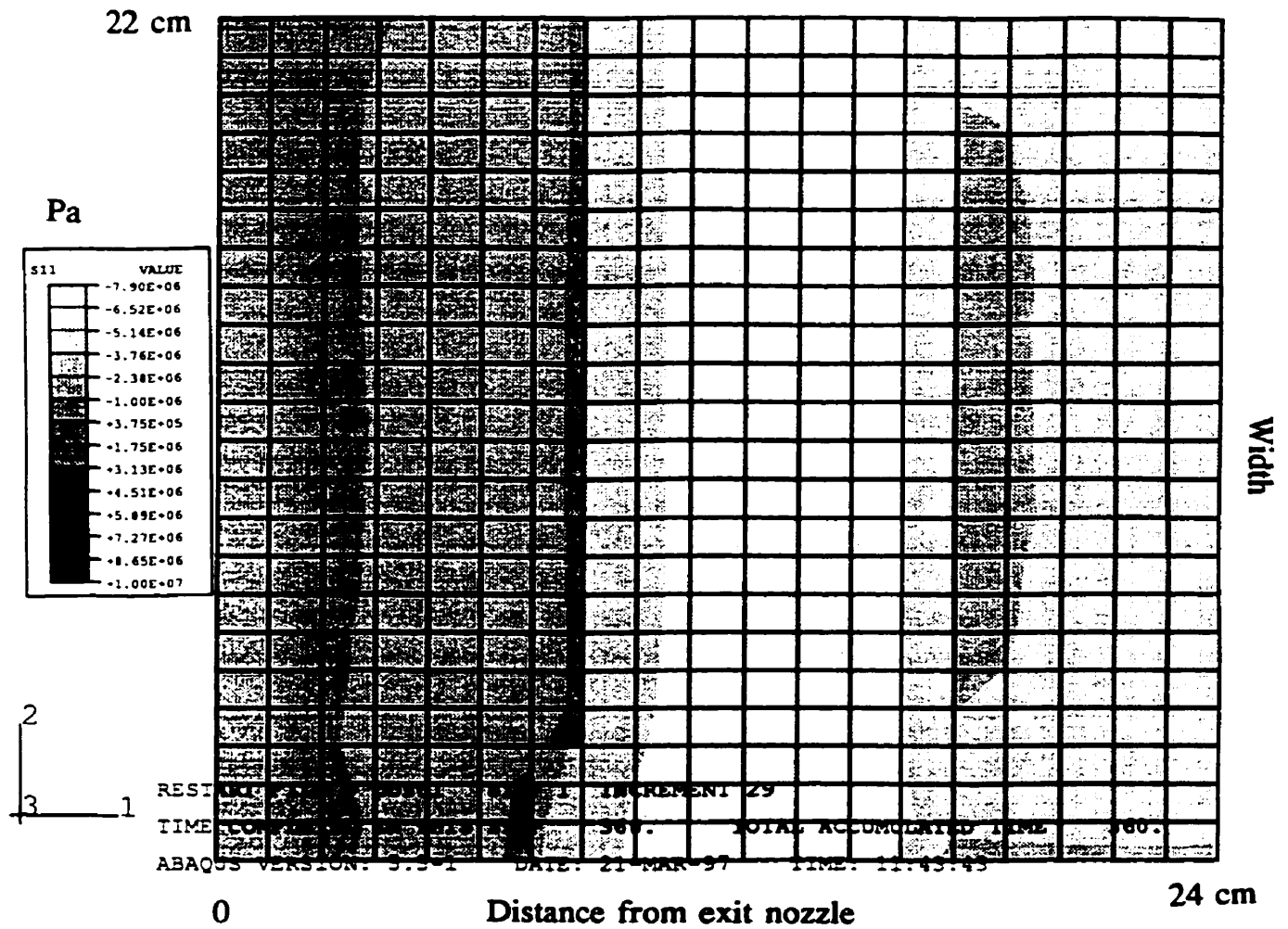


Figure 7-9. Stress profile in the middle of the strip.

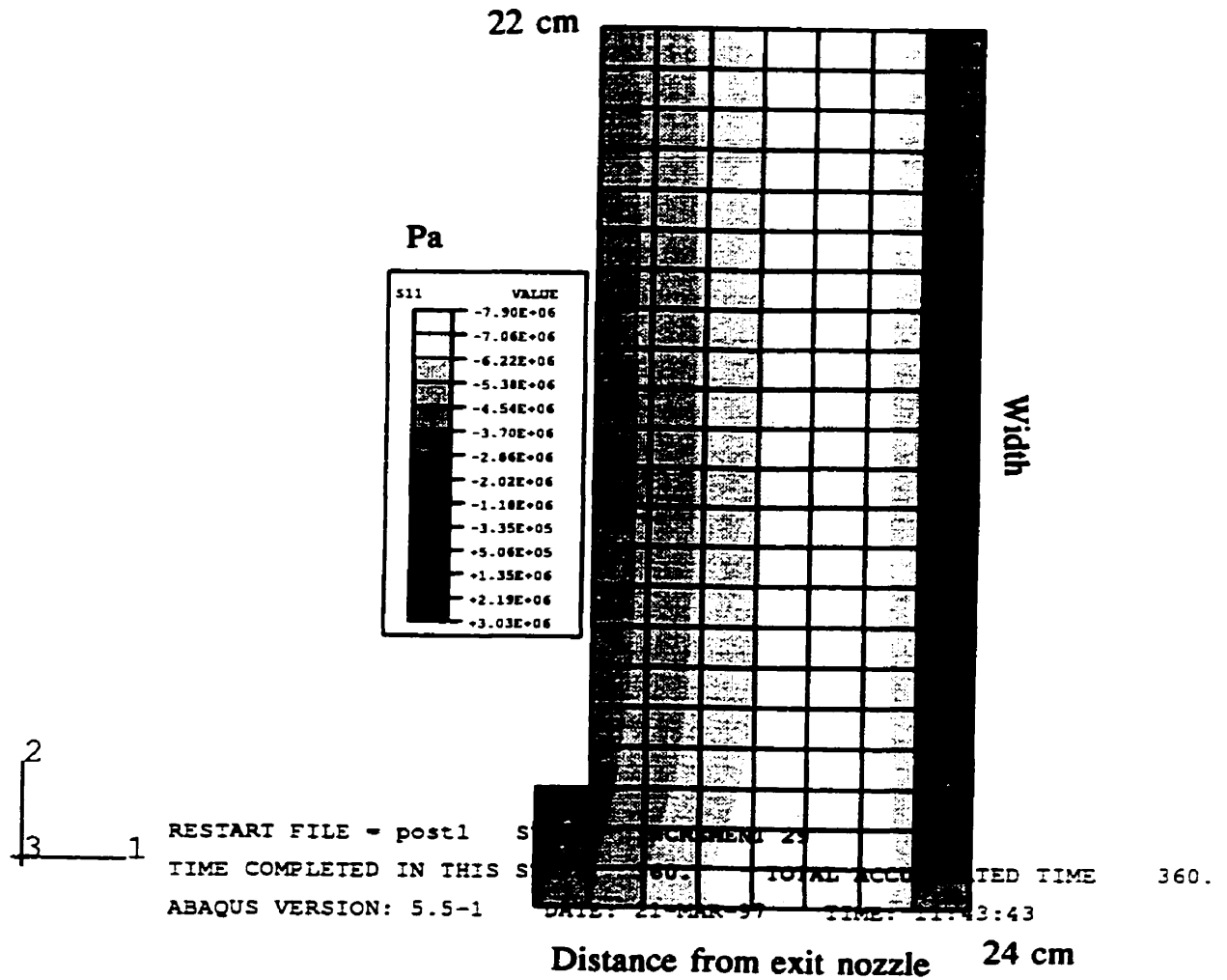


Figure 7-10. Stress profile at the interface solid-mushy zone.

mismatch, tensile stresses can develop at the shell. As the bottom of the strip initially cools, it contracts causing the strip to arch away from the belt. Then, the tensile stresses are created.

Also shown in Figure 7-8 are some elements of higher stresses in comparison to the surrounding elements, such as at $x=0.20$ m, for example and $0.10 \leq y \leq 0.14$ m. This position coincided with the growth of the solidified shell, with the addition of a new layer, which might have been responsible for the higher stress.

Nevertheless, as emphasized in the literature survey, in the study of stresses in castings what is really relevant are not the absolute values of the stresses, but rather the likelihood of cracks occurrence, based on the adopted crack index criterion. Some criteria presented in the literature were tested in the present work and the results are discussed in the following.

7.5.3 CONSIDERATIONS ON CRACK LIKELIHOOD IN THE STRIP

Cracks are more likely to occur in the temperature range of $T_s - (40/70^\circ \text{C}) \leq T \leq T_s^{(8)}$. Therefore, the possibility of cracks in the interface shell-belt should be small, since in the region of stress reversion, from compressive to tensile, at approximately 10 cm from the exit gap, the shell is already 80°C below the solidus temperature (Figure 7-11). In the region of maximum stress previously mentioned, the shell is more than 100°C below T_s . Nevertheless, the possible reversion of the stress sign on the top surface of the shell with time may originate regions of tensile stresses and temperatures barely below the solidus temperature.

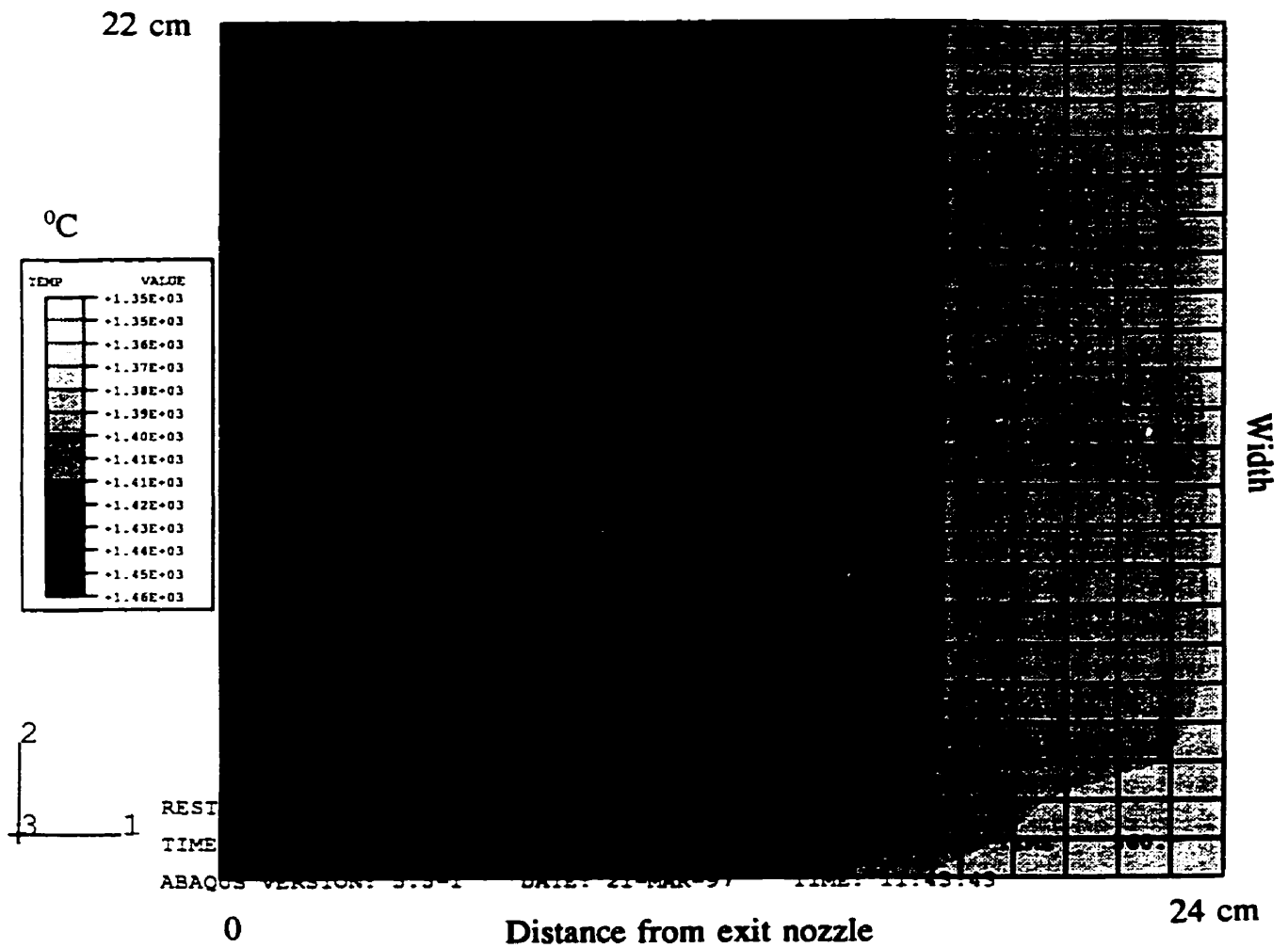


Figure 7-11. Temperature at the solid shell-belt interface.

These regions would be more susceptible to cracks.

To determine the likelihood of cracks in the solid shell, the Von Mises criterion was chosen, for its recognized efficiency as a crack criterion for materials that fail in a ductile manner. Other criteria were tested and the results evaluated.

Considering the Von Mises criterion, an interesting parameter is the ratio between the calculated principal tensile stresses and the Von Mises stresses. Where the ratio is above 1, the tensile stresses are significant in comparison to the compressive stresses. Figure 7-12 shows the calculated Mises stresses for the interface shell-belt. It is seen that the Mises stresses increase with shell cooling, up to 2.45 MPa.

Figure 7-13 gives the ratio S_{11}/Mises stresses at the interfacial shell, for the center of the strip and for the region next to the lateral wall. From Figure 7-13, it is seen that the ratios near the wall are higher than in the center. This is expected, due to the higher cooling rates near the wall. However, as seen in Figure 7-11, the temperatures at the critical regions ($\text{Ratio} \geq 1$) are below the critical temperature range for brittleness, which means that these regions are not likely to form cracks. It can also be observed that the ratio reaches a peak at approximately 14 cm from the exit gap and then falls continuously, up to the end of the domain. The peak can be explained by the addition of a new solidified layer at that position, which increases the local tensile stresses.

The Von Mises criterion was employed to determine the crack index: the ratio between the Mises stresses and the yield stresses. Figure 7-14 shows that the



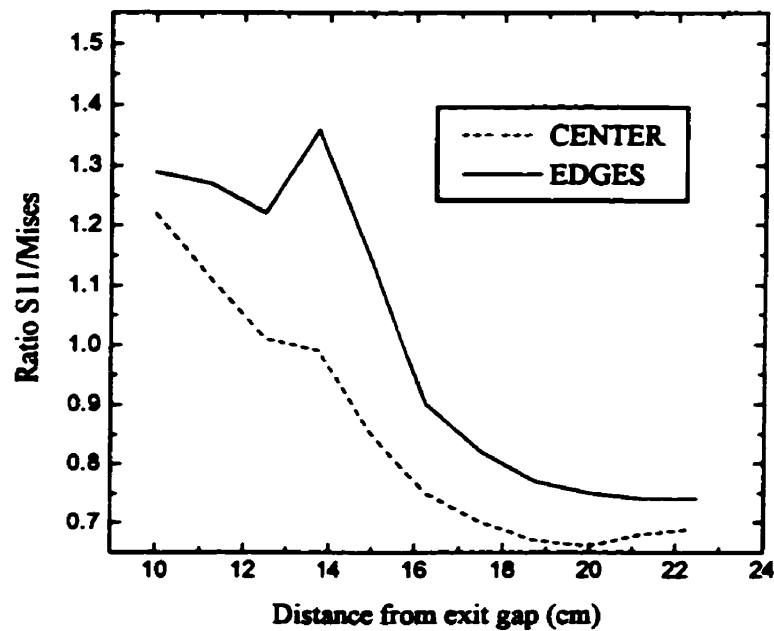


Figure 7-13. Ratio $S_{11}/Mises$ at the interfacial shell - Center and edge of the strip.

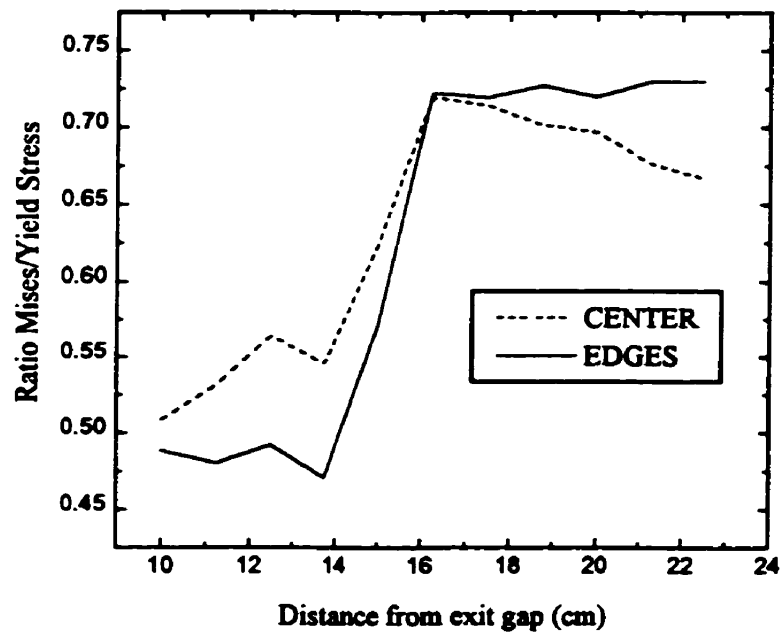


Figure 7-14. Crack index at the interfacial shell - Center and edge of the strip - Von Mises criterion.

crack indices actually increase with the distance from the exit gap, and the values are relatively low. The maximum index was 0.73 near the wall and 0.72 in the center. Therefore, according to this criterion, cracks would be very unlikely (as found in practice at the University of Clausthal and at MEFOS).

Using the critical strain criterion, according to which the zones susceptible to crack would be those where the strain reached 0.3% within the ductility zone, it is seen in Figure 7-15 that the maximum strain of 0.21% was obtained at the end of the domain. At this position, the temperature in the cooling surface was out of the low-ductility range. Therefore, no sites within the computational domain were identified as prone to cracks. Recalling that the strains here originated mainly by thermal contraction, this result is within the expectations. Nevertheless, with the evolution of solidification and growth of the solidified shell, it is expected that very high strains are created, since for a linear expansion coefficient of $2.0 \times 10^{-5} \text{ K}^{-1}$, for every 100 K of ΔT throughout the strip, a strain of as much as 0.20% will be caused. It was calculated that at 1.5 m from the meniscus, the ΔT between the bottom and the top of the shell will be around 185°C, yielding strains of as much as 0.37%.

7.5.4 FACTORS AFFECTING THE MAGNITUDE OF THE STRESSES

In the literature review, it was mentioned that the calculated stresses are a result of the stress-strain relationship at high temperatures, the material properties, the transient temperature history, and the boundary conditions chosen for the problem. The constitutive equation, which relates stress-strain is arguably the most important factor for a successful stress model. In this work, elastoplastic behavior was assumed for the solid shell, with the inclusion of hardening, but not creep.

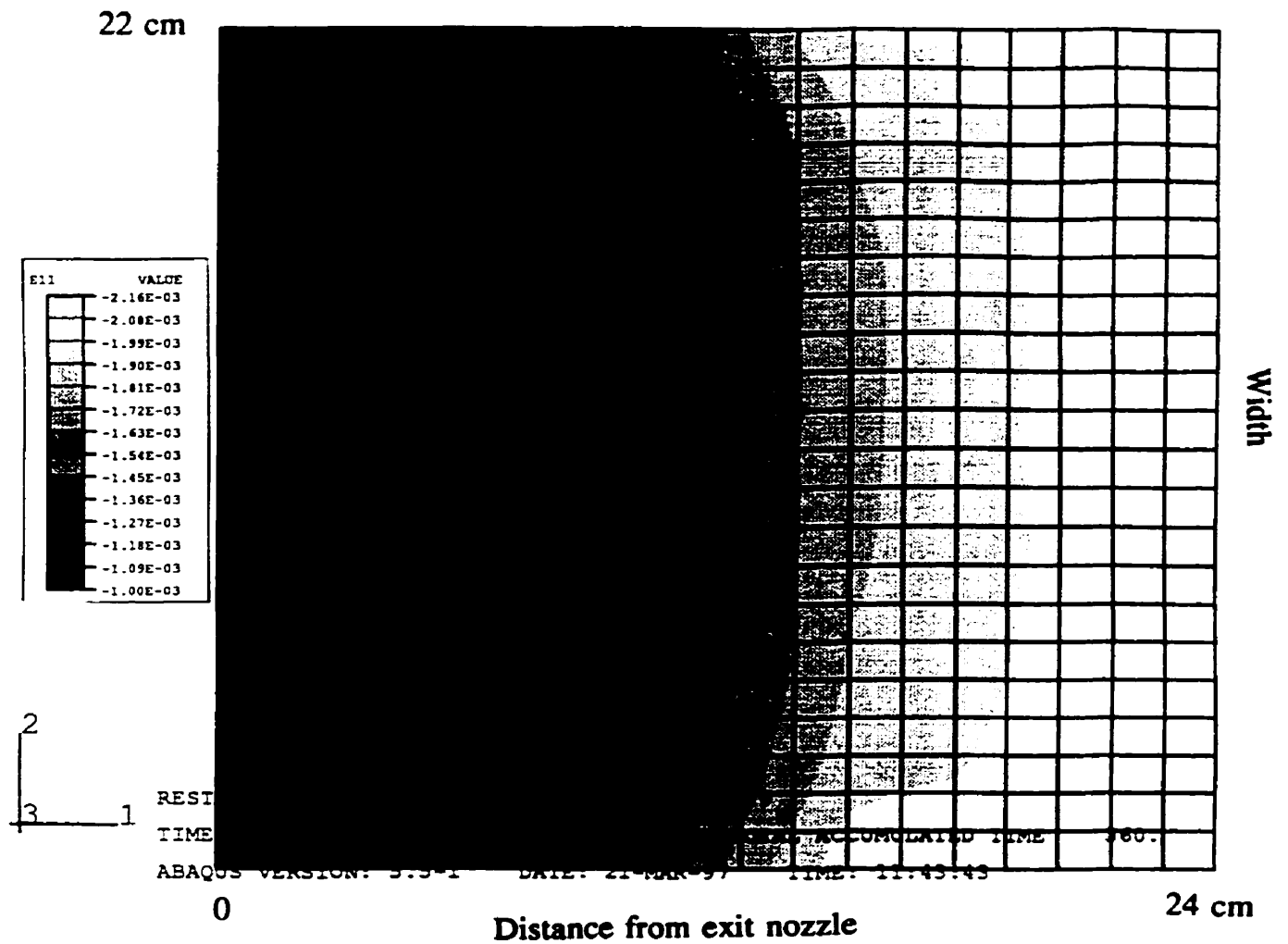


Figure 7-15. Strain distribution in the solid shell.

With respect to the no-creep assumption, Landau et al⁽⁹⁾ have shown that when the cooling is so rapid as to cause considerable plastic yielding, the strip would not remain at high temperatures long enough for the viscous effects to influence the residual stress values. These observations, although referred to plate cooling, may be valid for the current problem. The effect of creep would be of reducing the stress levels at the strip.

7.5.4.1 Hardening

The inclusion of hardening increases the yield stress and retards the plastic behavior of the material. For the inclusion of hardening in the plastic behavior of the material model, the yield stress should be given as a function of the corresponding temperatures and plastic strains. Therefore, the nominal stress-strain data have to be converted into "true" stress and log strain values:

$$\sigma_{true} = \sigma_{nom} (1 + \epsilon_{nom}) \quad (7-16)$$

$$\epsilon_{ln} = \ln (1 + \epsilon_{nom}) \quad (7-17)$$

Including hardening in the plastic model did not alter significantly the results, as seen in Figure 7-16. A reversion of the stress was observed at approximately 10 cm from the origin and the highest stress are located at 20 cm from the origin. It is observed, though, that this previously mentioned "island" of higher stress is larger for the hardening case. The maximum tensile stresses were observed to be around 2 MPa in the center of the strip and up to 3 MPa on the

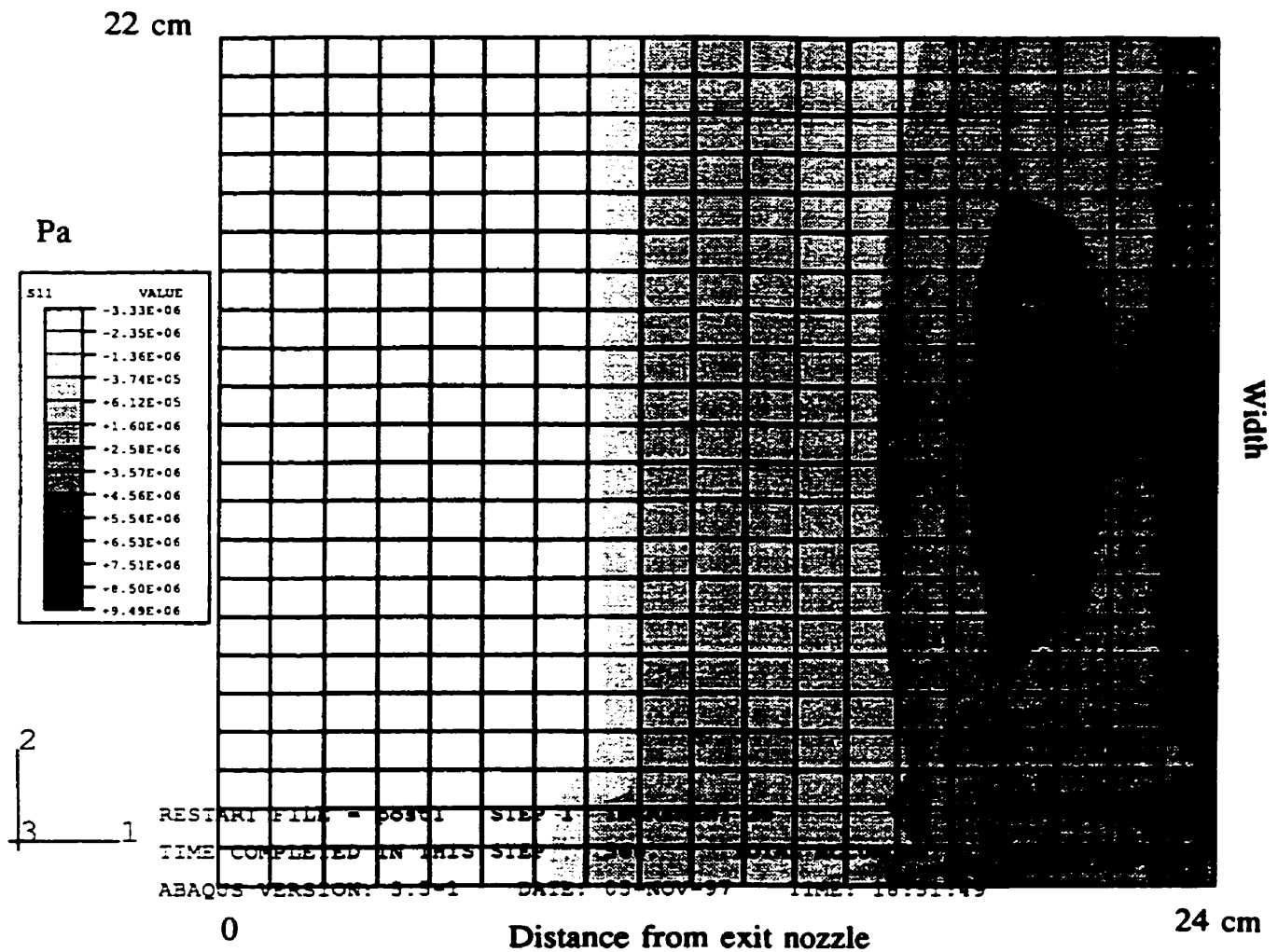


Figure 7-16. Longitudinal stresses at the solid shell-belt interface, including hardening.

edges.

The changes in the absolute values of the stresses with the inclusion of hardening ranged from -3 to +2%, in comparison to the ideal behavior. This is not actually surprising, given the relatively unconstrained state of the casting and the little metallostatic head, which has marginal influence on the stress development. For the crack index, Figure 7-17 shows a similar trend, although the values are up to 10% higher. Therefore, the inclusion of hardening mechanisms in the model did not alter significantly the results.

The thermal expansion coefficient and the elastoplastic properties of the material have relative importance, depending on the process studied. Becker et al⁽¹⁰⁾ found that they made little difference on curvature predictions of an aluminum quenched bar. In such cases, the thermal history at the bar was crucial for the predictions of their model to approximate the experimental results.

7.5.4.2 Elastoplastic Properties of the Material

The elastoplastic properties of the material have been referred throughout this work as being one of the most important components of a stress model. To check its influence on the absolute value of the stresses and, ultimately, on the crack indices, another set of material properties, given by Wray et al⁽⁴⁾ was considered. The crack index results using this model are shown in Figure 7-18. The crack indices are lower than in the previous case, and the peak for the wall region is more pronounced than before. Actually, using these properties, the highest crack index on the edges and in the center would happen at approximately the same position as in Brimacombe's model, at 18 and 16 cm from the exit gap,

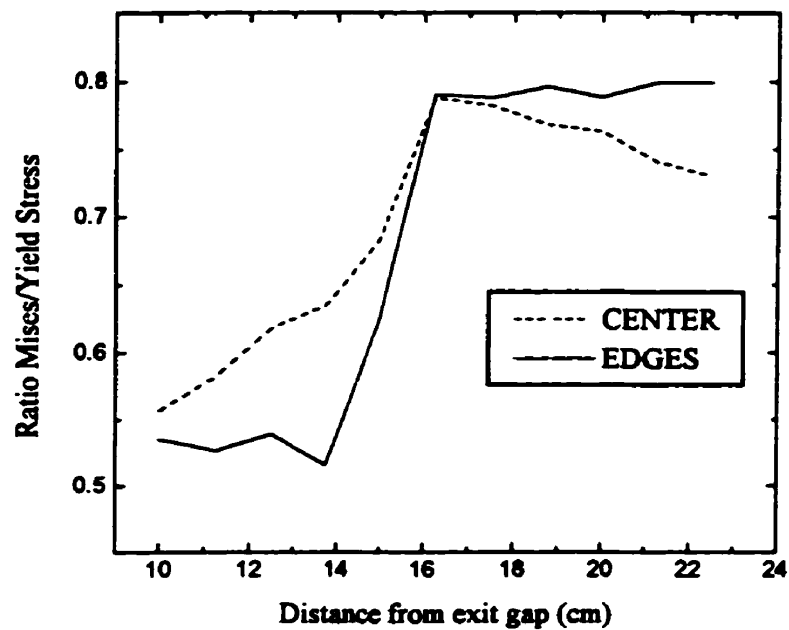


Figure 7-17. Crack index at the interfacial shell including hardening - Center and edge of the strip.

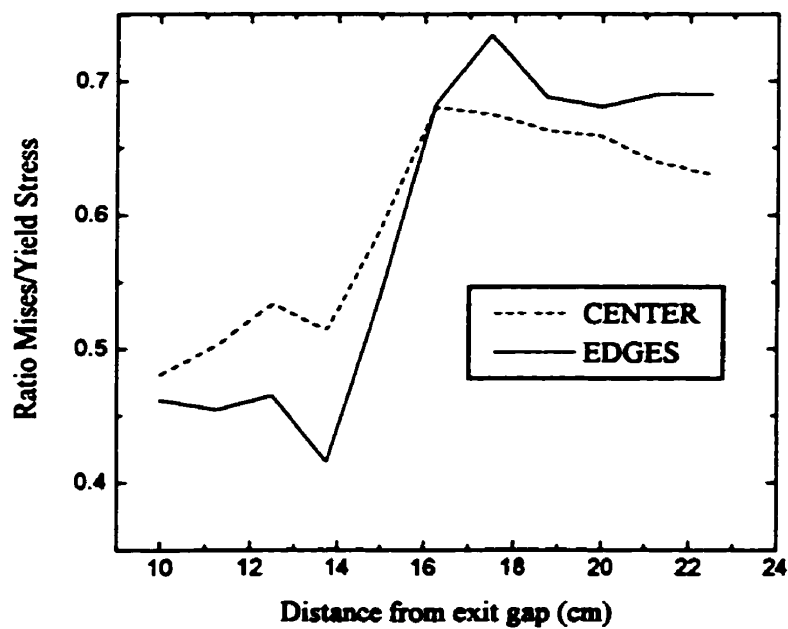


Figure 7-18. Crack index at the interfacial shell according to Wray et al's model. Center and edge of the strip.

respectively. Using hardening in the new model confirmed such findings.

7.5.4.3 Expansion Coefficient

One property understandably important to this problem is the coefficient for linear thermal expansion, α , since the main source of stresses and strains is the thermal contraction. A standard value of $2 \times 10^{-5} \text{ }^{\circ}\text{C}^{-1}$ was assumed for α . However, this value varies over the temperature range pertinent to this work. Therefore, an alternative value of $1.5 \times 10^{-5} \text{ }^{\circ}\text{C}^{-1}$ was employed and the importance of choosing an accurate value for α was assessed.

The crack index ratios for the lower α are given in Figure 7-19. It is seen that a lower α reduces the crack index in the center of the strip, for its lower stresses are accompanied by even lower Mises stresses. The reduction is slight at first, but more accentuated after the inflexion occurred at $x = 14 \text{ cm}$. Accordingly, the crack indices decreased slightly on the edges up to the occurrence of the peak, at $x = 14 \text{ cm}$. After that, the ratio fell more sharply, as in the center. The absolute variation was in the range of 15% to 20%.

7.5.5 FACTORS AFFECTING DISPLACEMENTS

The influence of the relevant factors was studied for the displacement profiles, as well. As for the inclusion of hardening, the increase in the maximum displacement was 2%, as shown in Figure 7-20 and the reduction in α from 2 to $1.5 \times 10^{-5} \text{ }^{\circ}\text{C}^{-1}$ reduced the maximum displacement by 13% (Figure 7-21). The choice of Wray's properties to define the elastoplastic behavior of the solid shell would yield the most relevant change in the maximum displacement: an increase

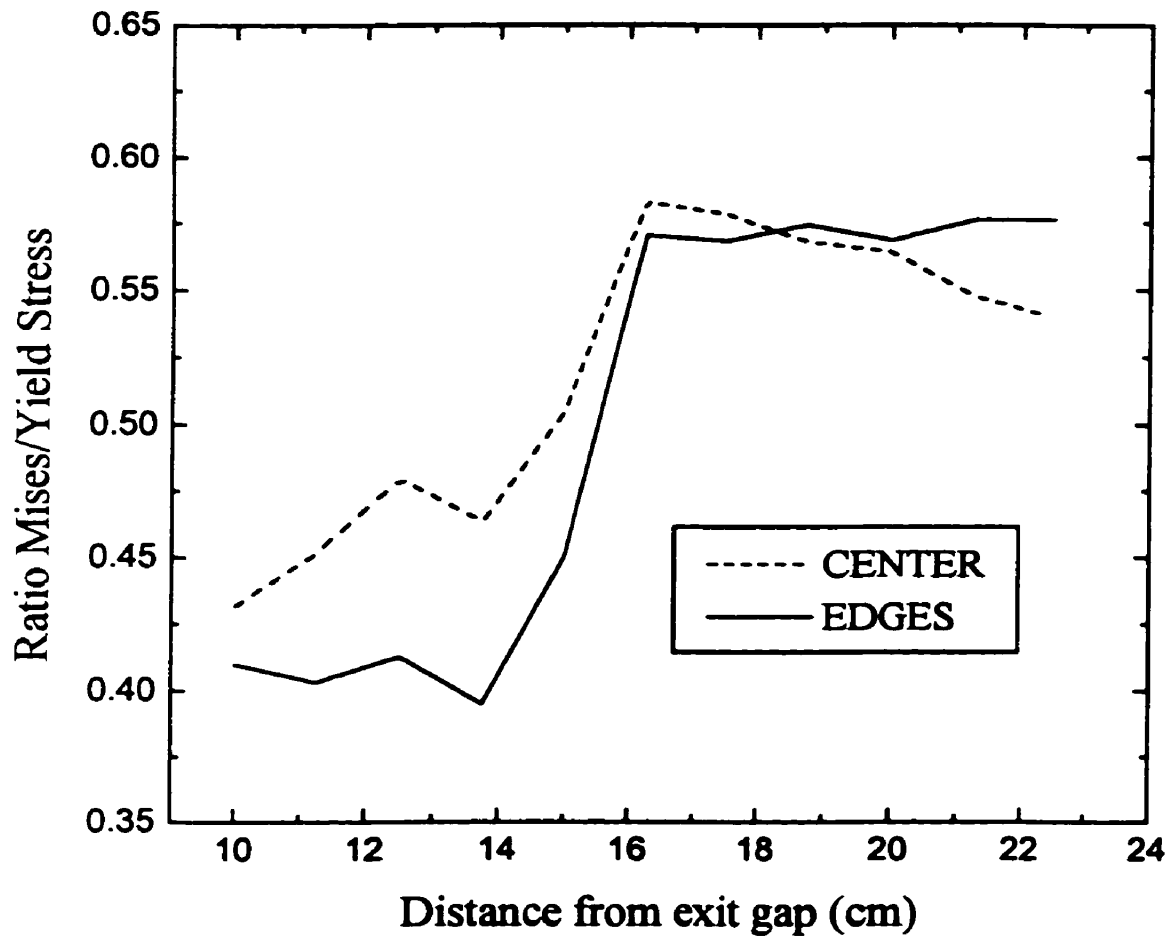


Figure 7-19. Crack index at the interfacial shell for $\alpha = 1.5 \times 10^{-5} \text{ }^{\circ}\text{C}^{-1}$ - Center and edge of the strip

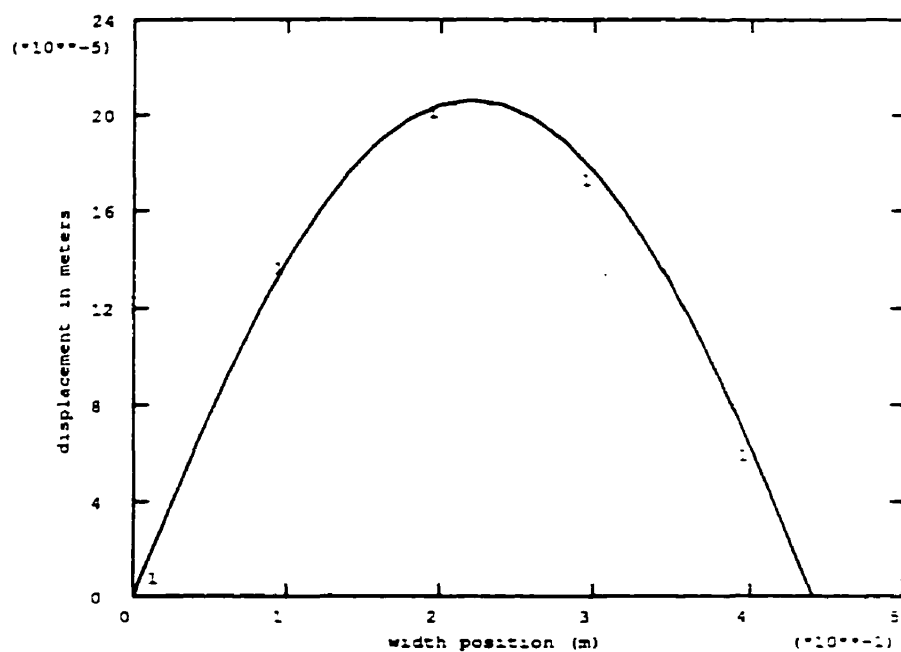


Figure 7-20. Displacement profile along the width direction, at the end of the domain - Hardening included.

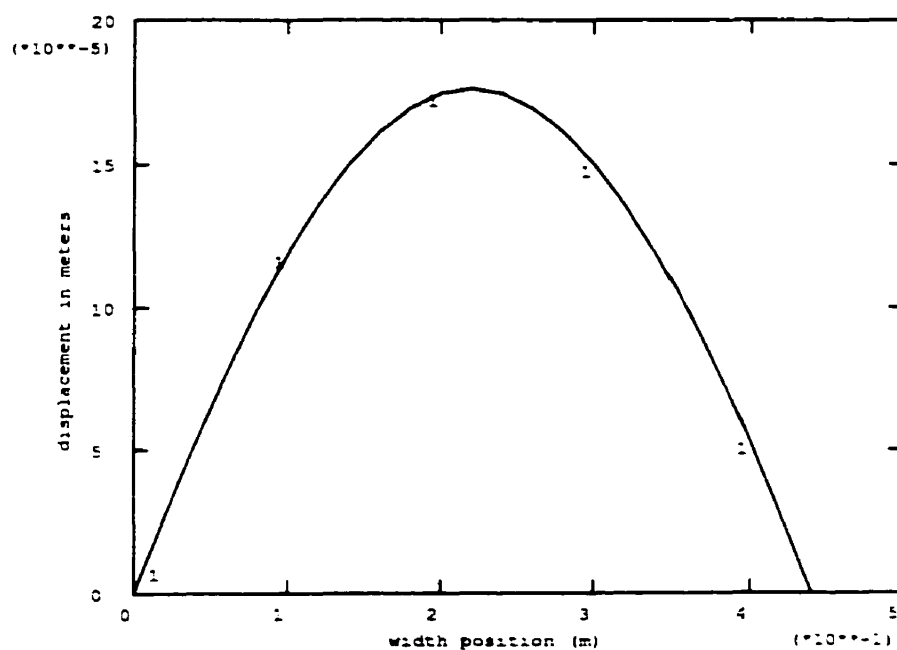


Figure 7-21. Displacement profile along the width direction, at the end of the domain for $\alpha = 1.5 \times 10^{-5} \text{ } ^\circ\text{C}^{-1}$

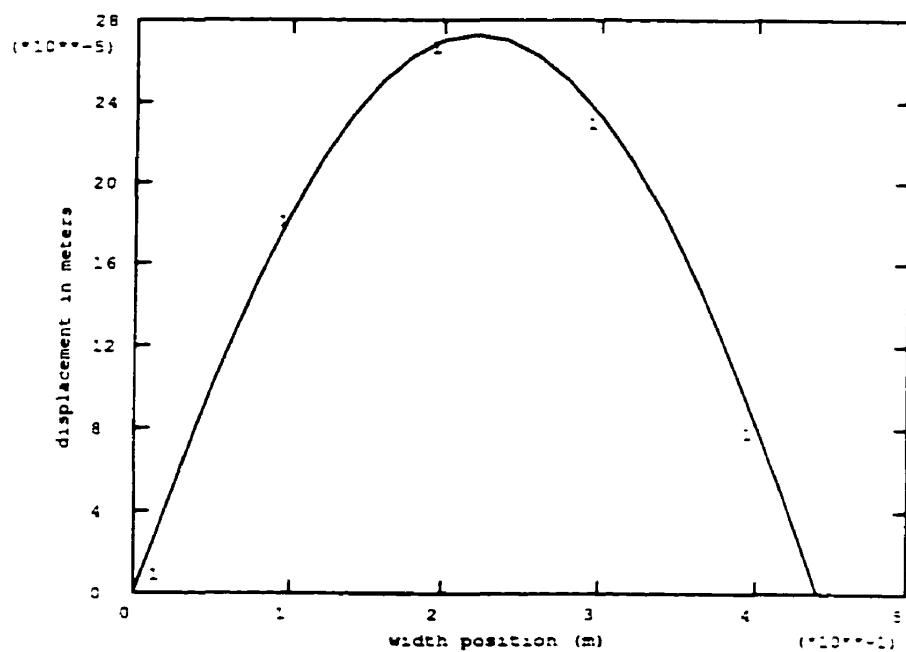


Figure 7-22. Displacement profile along the width direction, at the end of the domain, according to Wray et al's model.

of 35 % (Figure 7-22).

7.6 DISCUSSION

In conventional continuous casting of steel slabs, the stresses are compressive on the surface and tensile in the core. This stress profile is explained analytically by the classical solution of Weiner and Boley⁽¹¹⁾. Reviewing their boundary conditions, however, it is observed that while they do apply very well to constrained castings against molds, they do not fit the conditions existent in the present problem, where the upper surface of the strip is free for bending.

Their assumption of discontinuous temperature change at the surface is not realistic, though it has no influence on the nature of the stresses within the casting. However, the second basic assumption, that the casting is constrained against bending, it is not true in our case, since the material is not encased in a mold. As Tien and Koump⁽¹²⁾ pointed out in their work for slab solidification, this assumption would be valid provided the bending was outward (towards the cooling surface). Weiner and Boley⁽¹¹⁾ also reported that the stresses were found compressive at the cooling surface, because of the inclusion of plastic flow in their model. A purely elastic model would yield tensile stresses at the cooling surface. These results were again explained using the restriction of total force across the beam, for constrained castings: in the initial stages of solidification, the tensile stresses at the liquid side have to be compensated by compressive stresses at the cooling surface. So, as in Tien and Koump's solution, when the strip is allowed to bend along the transverse direction, the reversion of the stress due to inelastic behavior is not likely to occur.

Therefore, the results here obtained were qualitatively compared with Tien and Koump's⁽¹²⁾ analytical solution. Figures 7-23 and 7-24 are reproductions of the transient results obtained by Tien and Koump. It is seen in Figure 7-23 that the displacement in the transverse direction is initially outward and then reverses to inward. This behavior is similar to the evolution of the displacement presented in Figure 7-7. The difference in the sign of U is due to the different axis orientation. As in the analytical solution, the displacement at the cooling surface starts outward, changes the sign and reaches a peak, to fall in the sequence. Drezet and Rapaz⁽¹³⁾ also found non-uniform inward contraction (towards the liquid pool) for the solidified shell of Al ingots cast by the DC Process, under pseudo-steady state conditions.

As for the stress, the final stress profiles partially confirmed the results obtained by the proposed stress model: the stresses were compressive in the middle of the slab and tensile on the cooling surface and on the solid-liquid interface. At the interface liquid-solid, Figure 7-24 shows that the stresses were compressive at first and changed to tensile, and this may be compared to Figure 7-10. In such Figure, the stress is compressive at the solid-liquid interface, but as it was previously mentioned, the values of the stresses are decrescent, which might indicate that the stress sign would be ultimately reversed.

7.7 CONCLUDING REMARKS

The displacements of the strip were ultimately inward, with a maximum in the center of the strip, due to symmetry. The displacements increased with time (given by L/v), due to cooling, since the thermal strains are responsible for most

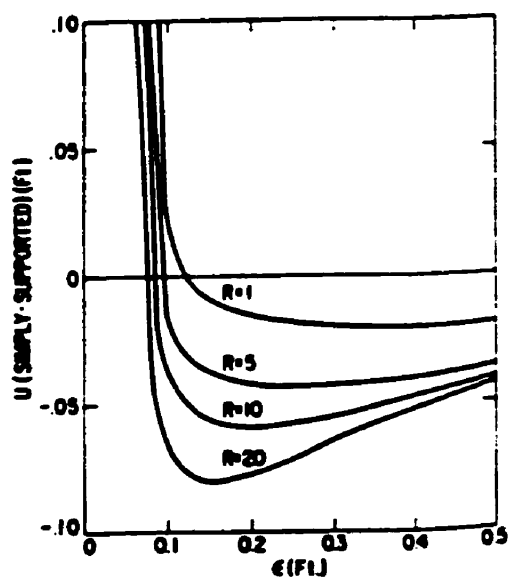


Figure 7-23. Displacement in the transverse direction of a simply-supported solidifying slab, according to Tien and Koump.

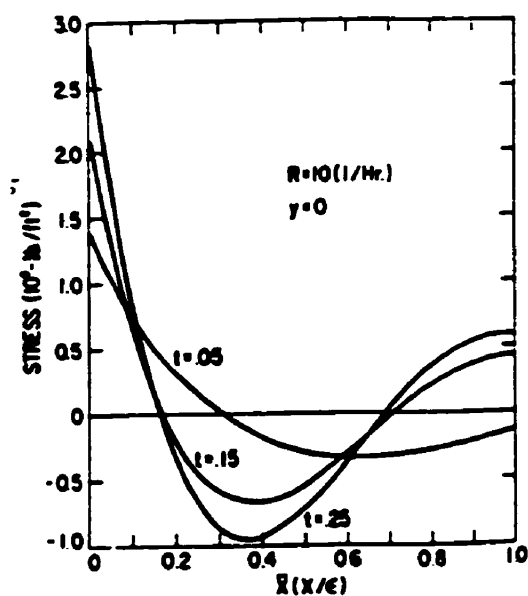


Figure 7-24. Residual stress profiles along the thickness of a simply-supported solidifying slab, according to Tien and Koump.

of the strip deformation. The peak on the displacement, however, can occur before the end of the domain.

The stresses at the cooling surface of the strip were found to be tensile and in the middle, compressive. Tensile stresses and strains are considered to cause cracks in the region of low ductility below the solidus temperature. Therefore, the work focused on the cooling surface.

The tensile stresses build-up on the strip with further cooling and the addition of new solid layers. The growth of the solidified layer creates regions of localized high stresses.

Cracks are more likely to occur in the cooling surface, immediately following solidification. Since the stresses in the middle of the strip are compressive, no cracks would be expected in that region. On the top surface, the possibility of stress reversion might cause cracks. Also, crack indices are higher on the lateral wall region than in the center of the strip.

With respect to the factors affecting the magnitude of stresses, the following conclusions were obtained:

- The inclusion of creep in the constitutive model reduces the stress levels in the casting, but since the cooling is so fast, perhaps there is not enough time for creep to happen;
- Hardening has the general effect of slightly increasing the stresses, but it also has a decreasing effect in some regions. Its inclusion did not make much

difference in the model, which can be explained by the preponderance of thermal strains;

- The material properties may change the crack indices and even the position of the peak. Nevertheless, good general qualitative agreement is found between the two models tested, which means that either could be employed in subsequent studies;

- A change in the coefficient for linear thermal expansion, α , may yield different implications for the center of the strip and on the lateral walls, with respect to the crack index. However, the stress profile is basically the same.

Displacements are more quantitatively affected by changes in the constitutive model and material properties than the associated stresses. The elastoplastic properties and the thermal expansion coefficient, α , are particularly important in the determination of displacements. This would be explained by the relatively unconstrained state of the casting, which makes the thermal strains the main source of strain in the solid shell.

The results obtained for stresses and displacements were qualitatively validated by Tien and Koump's analytical solution. Their problem matches more closely the boundary conditions of the proposed problem. The difficulty in validating quantitatively this model is inherent to the evident lack of experimental data and to the numerical difficulties and simplifications assumed in the model. Nevertheless, the primary objective of the model was fulfilled, which was to give an insight into the level of stresses in the early stages of the solid shell and, more important, to give a clue about the amount and shape of the displacements along

the shell width.

Suggestions for further work would include the consideration of microscopic phenomena in the formation of the solid shell. It has been proposed that the tensile stresses and strains responsible for cracks have their origin in the formation of dendrites⁽¹⁴⁾. For a very fast-cooling process, such as strip casting in a single-belt caster, the interdendritic flow should be crucial in determining the level of stress in the low ductility zone, immediately below the solidus temperature. The task of adequately coupling the macroscopic model with the microscopic one is of paramount difficulty, given the discrepancies involved for the grid sizes. Moreover, for such models to be fully effective, a coupling of the fluid flow/heat transfer part with the stress part would be mandatory. This would create enormous computational demands, which are not currently available to the researchers.

It is important to stress that the following results apply to practical situations where the casting conditions fit the assumed boundary conditions, i.e., a simply-supported strip, constrained on the sides. For different casting conditions, these results may no longer apply. For the best of the author's knowledge, no previous work has been presented on the study of stresses/displacements in strips cast on single-belt casters, which turns this investigation into a daring endeavour.

In practice, it has been observed by researchers at Lulea, that the strip can bend concavely, with respect to the belt, as now predicted, or convexly. Furthermore, these displacements can be much greater than those computed in the present calculations. To account for these observations, it is possible that the 7% shrinkage that take place during solidification of the strip may play an even more important role.

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HEAT TRANSFER MEASUREMENTS

8.1 INTRODUCTION

Strip casting processes produce thin strips directly from liquid metal, thereby eliminating rolling steps and associated annealing/reheating operations. Strip versus slab casting operations have the potential for reduced operating and investment costs. Current major research programs on this subject, worldwide, demonstrate steelmakers' commercial interests in the anticipated benefits of such technologies.

While single-belt processes or horizontal casting processes are not as popular as twin-roll processes, research-wise, they enjoy several important advantages⁽¹⁻⁴⁾. The single-belt processes have strong potential related to productivity, and quality in a deformation free environment, rendering them potentially viable for a wide range of "difficult-to-cast" copper, iron and light metal alloys. An appropriate choice of composition for a given set of parameters can produce a very fine and uniform solidification microstructure across the section of the product. The high cooling rates possible with single-belt processes versus conventional slab casting technologies can reduce the structural anisotropy

of the casting, by drastically reducing the columnar zone. High cooling rates also promote decreased micro and macro-segregation of elements across the section of the strip.

Recent attempts to develop near-net-shape casting have shown that one of the most important aspects to be considered is the interfacial heat transfer between the liquid melt and the moving substrate. For the wide range of technologies under development, different types of contact can occur between the solidifying metal and the solid substrate. The values of heat fluxes and heat transfer coefficients reported in the literature reflect this reality, differing in some cases by several orders of magnitude, as can be concluded from Wang and Matthys's recent literature review⁽⁵⁾.

Therefore, if one is interested in having a realistic estimate of the heat transfer involved in a particular process, one must perform experimental measurements either on-site or, more commonly, on an experimental apparatus that should match, as closely as possible, the real process.

Among the various casting configurations tested for near-net-shape casting, the roll processes and the belt processes are the most common. Processes have also been developed for very thin strips or ribbons, for free-jet spinning, for planar flow casting and for spray deposition. As mentioned before, thermal contact between the melt and the substrate can differ in these processes, depending on geometrical configuration, casting speed, strip thickness, hydrostatic pressure, etc.

Recently, Wang and Matthys⁽⁵⁾ presented a comprehensive assessment of the

data available for the various near-net-shape casting processes. They concluded that a significant amount of data for twin-roll casters and melt spinning had already been generated; however, for other processes, such as belt casters, little data are available and even these have been obtained under diverse experimental conditions.

Farouk et al⁽⁶⁾ performed measurements of heat fluxes between a belt and either liquid aluminum or steel. Their apparatus was motionless, nor was any relationship between heat fluxes and relevant variables proposed.

Chen and co-workers⁽⁷⁾ investigated some of the parameters involved in the interfacial heat transfer behavior in free-jet casting of Wood's alloy onto a moving substrate. The thicknesses of ribbons varied between 0.1 and 0.25 mm.

A simulation of low-carbon steel strip casting was carried out by Couture et al⁽⁸⁾. They used a copper mold moving at very low speed for the substrate. Their work focused on microstructural characterization.

The objective of the present study is to investigate interfacial heat transfer between solidifying aluminum/aluminum alloy strips and a moving substrate, focusing on the most important parameters that affect the heat fluxes and local cooling rates. To achieve this objective, an experimental apparatus, consisting of a stationary refractory mold and a moving substrate to simulate DSC caster operation, was built. The substrate was capable of moving at speeds within the range intended for industrial single-belt casters, and the thicknesses of the strips were chosen accordingly. The parameters studied in the present work were: substrate coatings, substrate roughness, superheat and substrate speed. The cooling

rates predicted were compared with correlations based on the SDAS (Secondary Dendrite Arm Spacing) measured in actual castings. Empirical relationships between the SDAS and such parameters are available in the literature for diverse aluminum alloys.

8.2 EXPERIMENTAL APPARATUS AND METHODOLOGY

8.2.1 DESCRIPTION OF THE EXPERIMENTAL APPARATUS

A schematic view of the strip casting simulator is given in Figure 8-1. The apparatus is composed of a containment mold, a gas furnace, a flat steel bar onto which metal is to be deposited, together with a pneumatic piston to push the bar, at the desired speed of casting, and a data acquisition system. In the middle of the bar, there is a 3-mm thick, 50-mm wide and 1-m long groove for metal deposition. The bar slides over lubricated cylindrical bars.

The 10-mm thick bars were coated by flame-spraying at Hazelett Strip Casting Corporation. Four different types of coatings were applied on respective bars, each with two levels of roughness, as shown in Table 8-1. Holes were drilled along the longitudinal axis of the bar, at chosen depths, to embed thermocouples so as to be able to determine local heat fluxes to the substrate.

The bond coat of the coatings was Metco 480NS. The values of R_a (surface arithmetic average roughness) were derived from five readings of which the highest and lowest were eliminated and the remaining three averaged. Further information concerning coatings compositions and degrees of roughness are given in Reference [9].

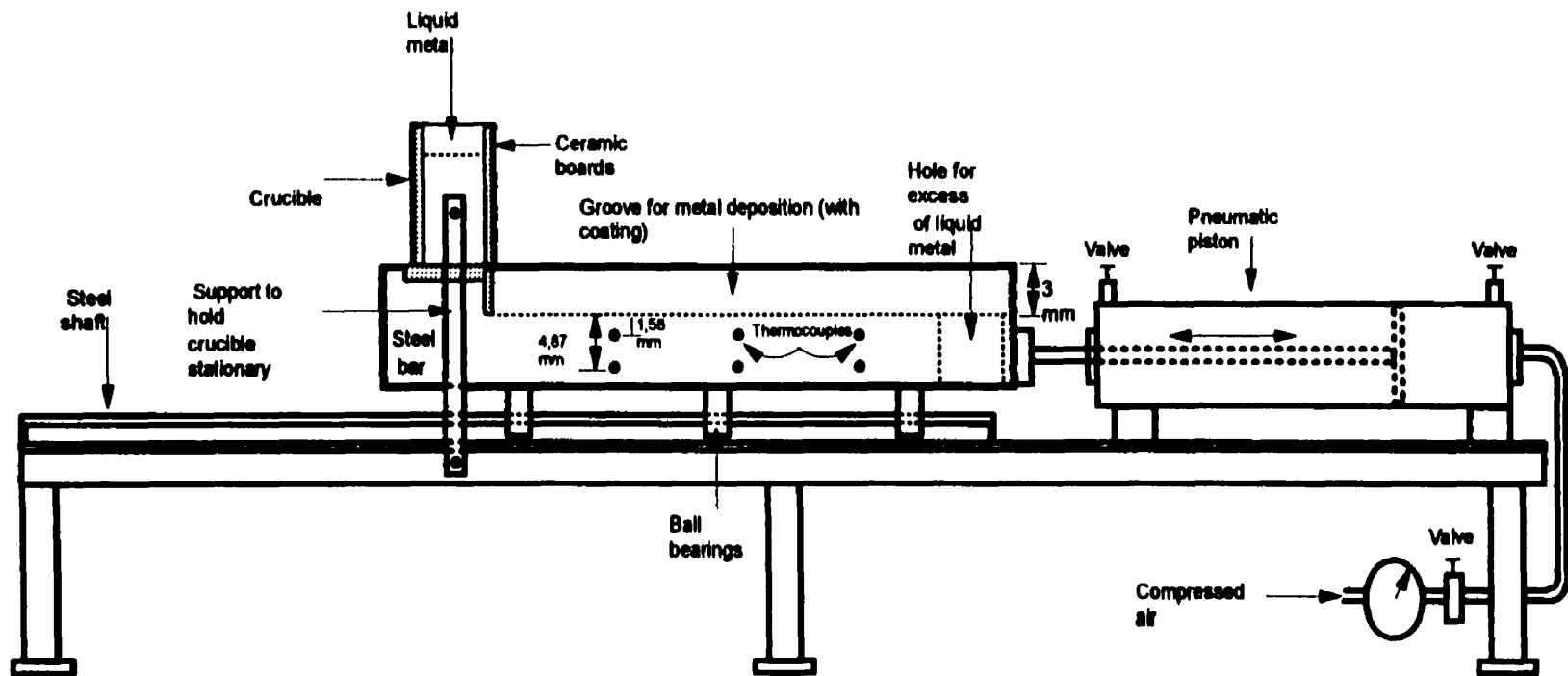


Figure 8-1. Schematic of the strip casting simulator.

Table 8-1 Types of coatings and roughness

Type of Coating	Coating N°	Thickness (μm)	Roughness (μm)
MATRIX Z - ALUMINA	01	75	11
MATRIX HC - NICKEL based - Metco 461 NS	02	75	13
MATRIX M - YTTRIA stabilized zirconia	03	75	11
MATRIX Y- magnesium zirconate MgO-ZrO ₂	04	75	11
MATRIX Z - ALUMINA	05	75	18
MATRIX M - YTTRIA	06	75	18
MATRIX HC - NICKEL	07	75	17
MATRIX Y - MgO-ZrO ₂	08	75	17

The speed of the substrate was calibrated using a voltmeter, a He-Ne laser source, a steel sheet with holes equally spaced along the sheet and a photo-transistor connected to a voltmeter imposing a constant voltage. Further details of this methodology can be found elsewhere⁽¹⁰⁾.

Since the bar of the strip casting simulator is subject to inertia during its initial period of acceleration, it takes some time (a fraction of a second) for the flat bar to assume constant lateral speed. Evidently, determining the position where the bar reaches its final velocity of translation is important in positioning the embedded thermocouples, as detailed below. Three different speeds were used in

this work: 0.4, 0.8 and 1.2 m/s.

Three pairs of thermocouples were positioned along the center of the substrate, at depths of 1.58 mm and 4.87 mm from the surface, at longitudinal distances of 0.39 m, 0.51 m and 0.63 m from the leading edge of the bar. The thermocouples were type K and were bonded to the substrate by OMEGATHERM[®] (High Thermal Conductivity Paste). Temperature data at these points were acquired at a rate of 10-30 Hz, by an Omega data acquisition system, in the Low Noise option. Care was taken to position the thermocouples in those regions of the bar where it was moving at the constant speed desired.

A gas furnace was used to melt the aluminum and aluminum alloys and a ladle spoon was used to pour the molten metal into the mold. Before pouring, argon was injected into the melt, for degassification.

The static mold comprised a carbon steel box lined with refractory boards, whose main function was holding the liquid metal without significant heat losses before the machine was started.

The experiments were carried out ten seconds after the mold had been filled, so as to remove any effect of pouring turbulence in the melt on subsequent strip solidification.

The machine was started by electric power and, as soon as the piston started moving the bar, the metal flowed from the mold to the groove through an opening

on the bottom of the mold, which was blocked at time zero. At that point, the data acquisition system had already been activated. All excess metal remaining within the mold flowed through a matching opening at the end of the bar, and was collected in a sump.

The initial height of the metal in the mold was kept constant, in the order of 6 cm. Considering that there was an excess of metal in the mold, it was calculated by mass balance that the final height of metal was 2 cm. Therefore, using a discharge coefficient of 0.8⁽¹¹⁾ and applying the well-known formula:

$$v = C_D \sqrt{2gh_m} \quad (8-1)$$

the initial and final horizontal velocities of the liquid metal contacting the substrate bar were found to be 0.87 and 0.5 m/s, respectively. These values fall within the range of substrate speeds (0.4-1.2 m/s), thereby reducing the likelihood of any hydraulic jumps.

A thermocouple was placed in the mold to determine the superheat of the metal. Different levels of superheat were investigated, ranging from 0°C to 50°C.

8.2.2 CALCULATION OF HEAT FLUXES

Inverse Heat Conduction Problem:

The inverse heat conduction problem consists of estimating the surface

conditions from interior measurements. This problem has risen in importance, for interfacial heat transfer calculations, since in most industrial processes or prototypes, it is very difficult to carry out direct and accurate heat fluxes and temperature measurements in the surface of the metal or at the metal/substrate interface. On the other hand, the inverse heat conduction problem has an analytical solution that is much more difficult to solve than the direct heat conduction problem. Therefore, the researcher has a choice to be made: In cases where dynamic phenomena and/or geometrical difficulties lead to impossibilities or inaccuracies of measurements, the inverse heat transfer problem has to be solved. This was the case for the present study.

In general, the mathematically "ill-posed" problems are extremely sensitive to measurement errors. The inverse techniques also tend to produce temporal oscillations in the surface condition estimates that are larger than the temporal oscillations in the interior measurements⁽¹²⁾. Some techniques have been developed to reduce the sensitivity of the IHCP (Inverse Heat Conduction Problem) to measurement errors⁽¹²⁾, and to improve the stability of the solutions. Two of the most common techniques are: to increase the size of time step between discretization points and to reduce the order of approximating functions (deconvolution method).

There are a number of ways to classify methods used for solving the inverse heat conduction problem. In general, the two basic approaches are the function specification and the regularization methods. Both can handle nonlinear problems, provided the solution of the nonlinear heat conduction equation is available.

Other ways to classify the methods are according to the method of solution of the heat conduction equation, the time domain and the dimensionality of the IHCP.

Beck et al⁽¹²⁾ have presented some of the criteria for evaluation of the IHCP methods. The most relevant for the present work are reproduced below:

- 1) The predicted temperatures and heat fluxes should be accurate if the measured data are of high accuracy;
- 2) The method should be insensitive to measurement errors;
- 3) The method should be stable for small time steps or intervals;
- 4) Temperature measurements from one or more sensors should be permitted;
- 5) Temperature-variable properties should be permitted;
- 6) The method should be easy to program.

The regularization method can fulfill these requirements, provided the nonlinear heat conduction equation is solved by a finite-difference method. Therefore, this method was chosen to estimate the heat fluxes in this work. Its main feature is that it increases the effect of the stabilizing terms in the formulation. The challenge is to keep resolving power while improving stability⁽¹³⁾, since the bigger time steps reduce somehow the ability of the method to capture sudden fluctuations in the heat fluxes. Alternatively, if the measurements are closely spaced in time and noise is present, very large oscillations can occur in the predicted surface conditions. To control the growth of errors, the alternatives are the use of future time information and the use of larger time steps. The

particularities of this method are not in the scope of this work and the algebraic form together with more details are given in Reference 12.

A one-dimensional finite-difference model was developed to solve the inverse transient heat conduction equation in the vertical sense of the substrate:

$$\frac{\partial}{\partial x} \left(k \frac{\partial T}{\partial x} \right) = \rho c_p \frac{\partial T}{\partial t} \quad (8-2)$$

This approach was valid, given the high lateral speed of the bar, that rendered axial conduction effects less than 3% of vertical components, at velocities of 1 m/s.

The heat fluxes were determined using a second order regularization method^[12] in conjunction with temperature readings within the bar for the required boundary conditions.

The use of the "Low-Noise" option on the Omega Quicklog^(®) software proved crucial in ensuring the accuracy of the temperature measurements.

For every condition, three experiments were carried out, each of them involving the three pairs of thermocouples. The results showed some scatter, but were reasonably reproducible. The following methodology was employed: for each coating, the same combinations of substrate speed and superheat were adopted. For some experiments, the metal column in the mold was also varied, aiming at the

production of thinner or thicker strips.

The thermophysical properties for the liquid metal, the substrate and coatings used, are presented in Table 8-2.

Table 8-2 Thermophysical properties of materials used

MATERIAL	k (W/m.K)	C_p (J/kg.K)	ρ (Kg/m³)
Coating Z	3.5	1070	3970
Coating HC	17.0	444	8900
Coating M	0.7	475	5680
Coating Y	0.7	600	5000
Carbon Steel	64.1 - 0.0427T	2368 - 1492E-2 T + 4107E-5 T ² - 4696E-8 T ³ + 1953E-11 T ⁴	7200
Aluminum	80 (l), 220 - 0.77 (T-525)(s)	1084 (l) 743 - 0.4985T (s)	2700
Al-8.4% Si	64(l) 176 - 0.77 (T-455)(s)	976(l), 900(s) 950 (mushy)	2680

Other properties: Al-8.4%Si: $T_{\text{liquidus}} = 610^{\circ}\text{C}$; $T_{\text{solidus}} = 570^{\circ}\text{C}$;

Latent heat (Al, Al alloys): 390 kJ/kg.

It is common to measure dendrite arm spacings (DAS) as a means to characterize dendritic structures of solidified alloys, and as a measure of the fineness of a cast structure. This information allows one to establish a link between processing parameters and the microstructure, and hence to empirically predict the mechanical properties of a given alloy configuration and casting route.

Predicted values of secondary dendrite arm spacing (SDAS) as calculated by theory using the cooling rates (determined by the solution of the IHCP - Inverse Heat Conduction Problem) were compared to the secondary dendrite arm spacings measured in aluminum-silicon strips. The SDAS were measured under the microscope using a LECO Image Analyser System. Standard techniques were used for the preparation of metallographic analyses. Transverse sections of the specimens were prepared using TECHNOVIT 4004 SYSTEMS, ground with SiC papers, polished to 0.3 μm alumina powder, and etched in 10% HF in H_2O , for about 10 seconds to reveal the dendritic structure.

8.3 RESULTS AND DISCUSSION

The effects of the most important variables on solidification and cooling rates were studied by casting pure aluminum onto cold substrates of various roughnesses and types of coatings, moving at various speeds.

8.3.1 EFFECT OF COATINGS

The overall appearances of the strips produced were very good. The smoother coatings generally yielded smoother bottom surfaces. In most cases, the bottom surface of the strips had a smooth texture, while the free surface exhibited some roughness. In initial tests, holes and defects formed on the strips' bottom surfaces, indicating the presence of dissolved gas. Argon injection of melt prior to casting reduced the problem considerably.

Figure 8-2 shows the calculated heat fluxes for the eight coatings used. The standard conditions for these experiments were: substrate speed = 0.8 m/s and superheat = 10°C. Since the thickness of the strips was found to be an important parameter for heat transfer, the results shown in Figure 8-2 refer to strips of approximately the same thickness (2 mm).

As one would expect, the only metallic coating used, matrix HC, gave superior results in terms of heat fluxes. Peaks of up to 3 MW/m² were found for that type of coating, depending on conditions. The most insulative was the magnesium zirconate coating, with a peak around 0.7 MW/m².

The peak in the heat flux marks the point at which solidification on the interfacial layer is nearly completed. The drop in the heat flux thereafter is caused by contraction of the solidified layer, and consequent formation of an air gap between the coating and the decoupled shell. In addition, the reduction in the temperature difference between the metal and the coating yields a further reduction in the heat fluxes. The peak in most cases occurred within 1 s from contact of the metal with the substrate. A time period ranging between 12 s and 18 s was needed before the heat flux dropped to nearly zero.

In some cases, the heat flux increased again almost immediately after the initial drop following the peak. This happened more often for thicker strips, the reason being that thicker strips solidify more slowly, making it possible for a re-melting of the solidified layer, thereby improving the contact with the coating once more.

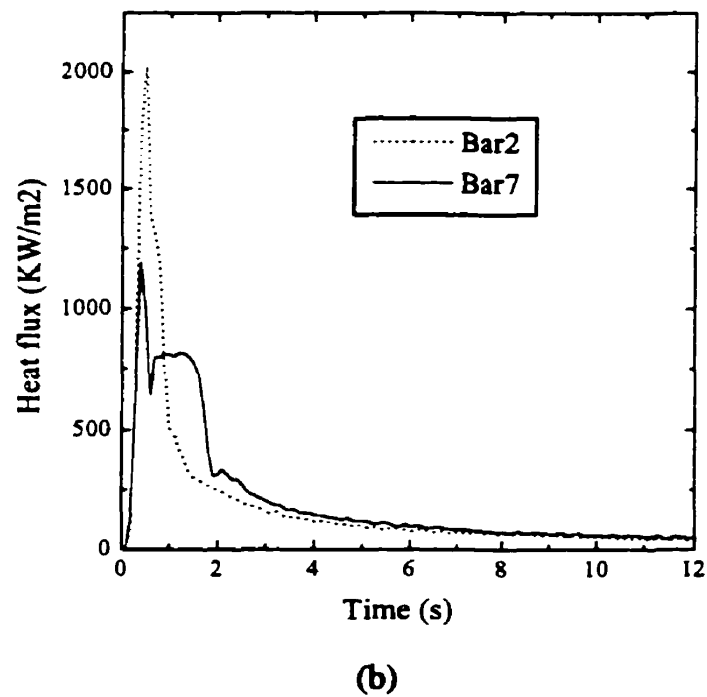
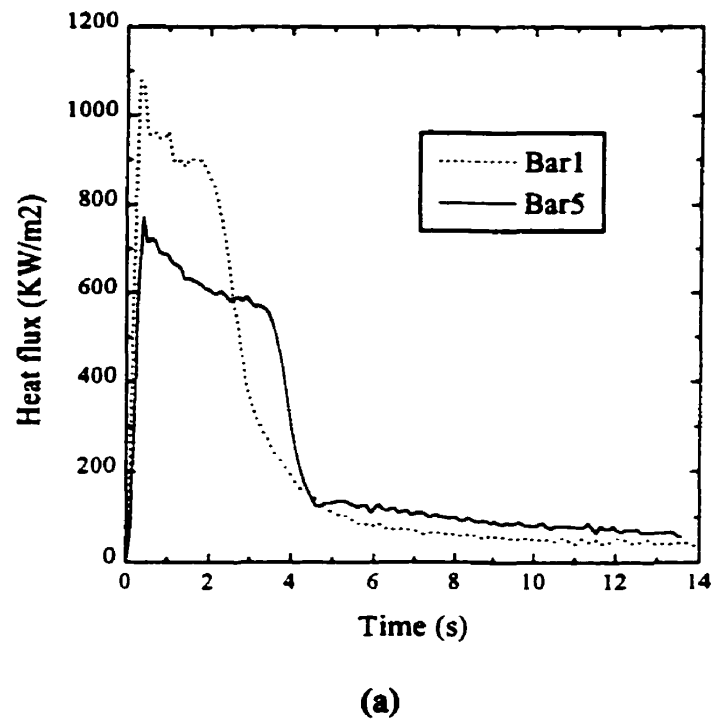
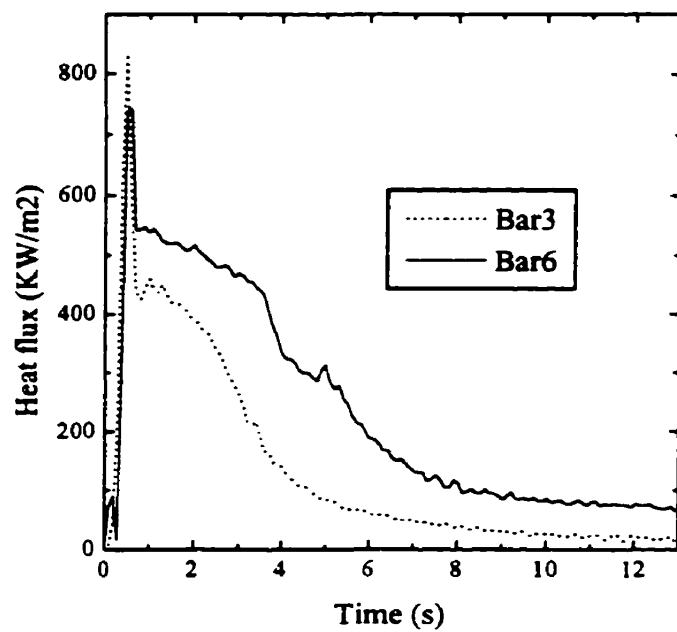
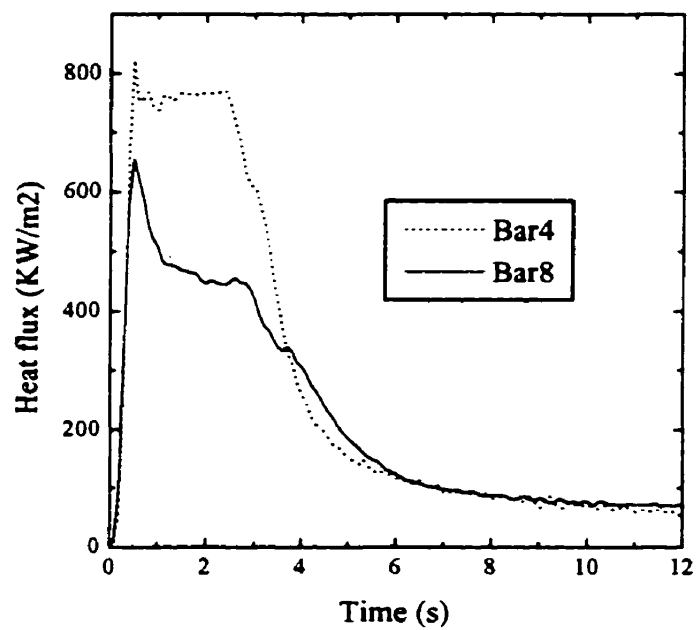


Figure 8-2. Calculated interfacial heat fluxes for coatings: a) 1 and 5; (b) 2 and 7; (c) 3 and 6; and (d) 4 and 8.



(c)



(d)

In order to check the accuracy of the assumption of one-dimensional heat transfer, a global heat balance was carried out through the insertion of a thermocouple on the solidification site of the metal, at the same longitudinal position as one pair of thermocouples inserted in the substrate. The change in melt temperature was registered and the global heat balance indicated errors ranging from -5 to +5%. Therefore, the assumption was considered correct. A typical plot of the temperature evolution in the bar is given in Figure 8-3.

The effects of other variables, such as thickness of strip and superheat were evaluated for the whole range of coatings, but for the sake of brevity, only results for some coatings are now shown. Very good qualitative reproducibility was found for the diverse bars, in terms of the variables mentioned.

8.3.2 EFFECT OF STRIP THICKNESS

Figure 8-4 gives the effect of the strip thickness on the heat flux peaks for coatings 1 and 5. Despite some scatter, it is clear that a thicker strip yields a greater peak. The results seem to be in qualitative disagreement with those compiled by Wang and Matthys^[5], who advocated the following correlation between the average interfacial heat transfer coefficient and the thickness of the strip (Figure 8-5):

$$h = 18.9 T_b^{-0.82} \quad (8-3)$$

where T_b is the thickness of the strip. As such, the heat transfer coefficient would increase for thinner strips.

However, as they pointed out, relatively few data are available on heat

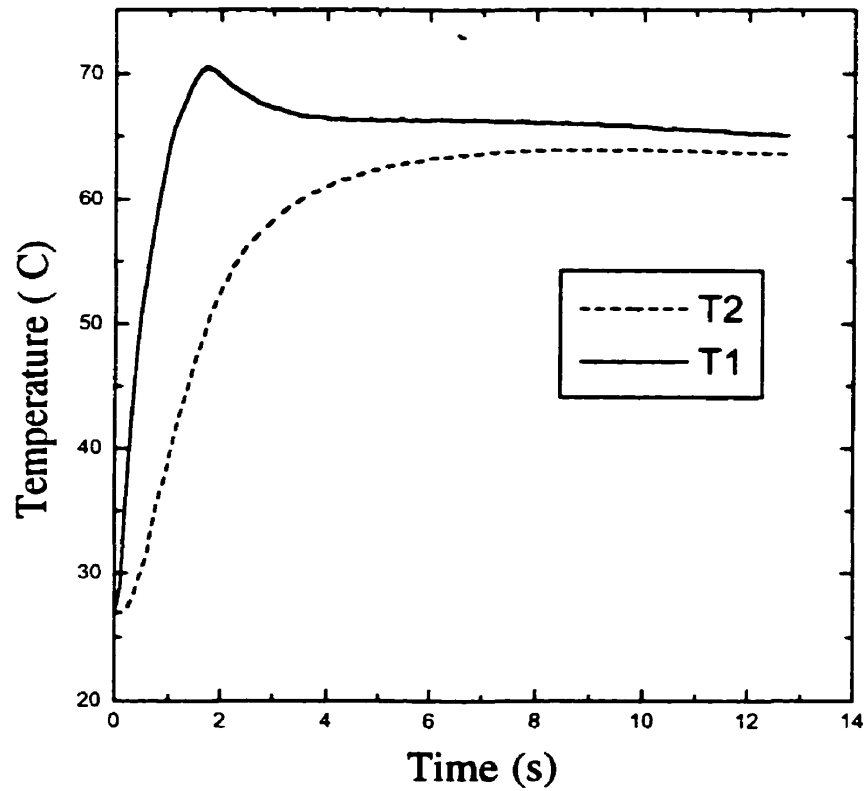
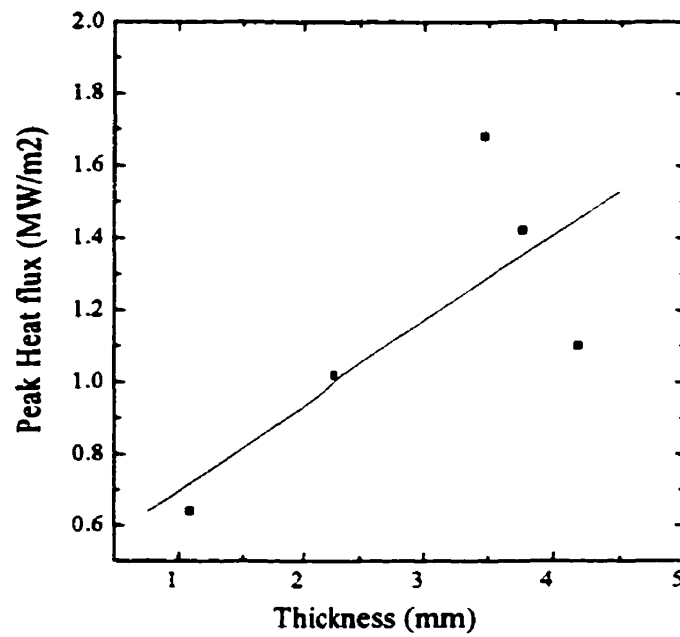
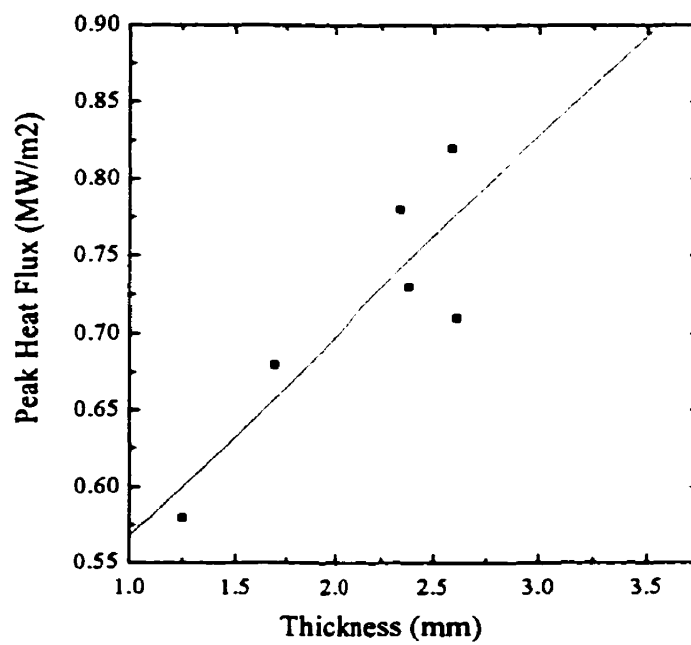


Figure 8-3. Typical plot of the temperature evolution in the substrate (T1 at 1.58 mm from surface; T2 at 4.87 mm from surface).



(a)



(b)

Figure 8-4. Effect of strip thickness on the peak heat flux: a) Coating 1 and b) Coating 5

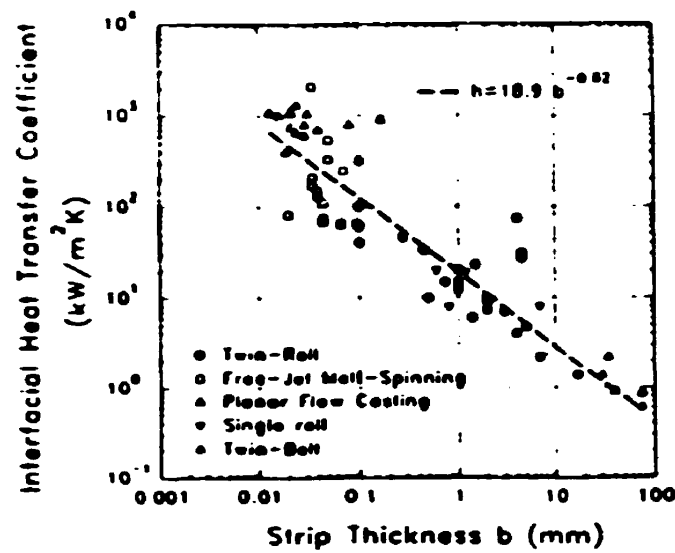


Figure 8-5. Correlation between the interfacial heat transfer coefficient h and the strip thickness for twin-roll caster, free-jet melt spinning, and planar flow casting⁽⁵⁾.

transfer coefficients for twin-belt casters. Indeed, looking at the figure where they showed the data, it is clearly seen that most of the points for which the correlation was derived are data from roll casters, while the few points for the twin-belt casters are all situated in the low-speed range.

Furthermore, and more importantly, the type of interfacial contact is different in single and twin-belt processes, since in the latter there is no free surface and pressure on the metal is exerted from both sides.

A possible explanation for these results is that thicker strips exert slightly more downward (pressure) force on the bottom surface, improving its contact with the substrate, i.e., there is a slightly increased metallostatic head. In order to confirm this tendency, some static tests were carried out with samples varying from 5 to 10 mm of thickness. Again, the peak heat fluxes increased with increased thickness.

These results were valid for the range of velocities chosen for this study, between 0.4 and 1.2 m/s, and for strip thicknesses between 1 and 4 mm. Very good reproducibility was found for the different coatings, for both smooth and rough surfaces. For some coatings, the influence is more pronounced than others, as shown in Figure 8-4.

8.3.3 EFFECT OF SUBSTRATE SPEED

In this study, it was not possible to separate the effect of substrate speed from other variables, since there was no strict control of the strip thickness with

the present equipment. Nevertheless, considering that in an industrial process high speeds will generally yield thinner strips in a free form delivery system, one can expect, on single-belt casters, lower peaks in the heat fluxes for thinner strips, for the range of thickness studied in this work.

8.3.4 EFFECT OF SUPERHEATING

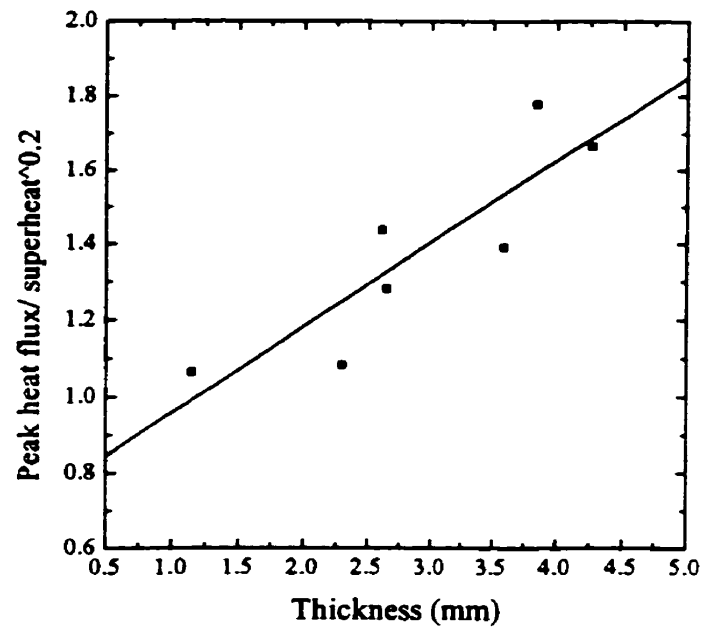
A higher initial superheat increased the peak heat flux. For coating 1, for example, the peak heat flux varied from 1.092 MW/m² to 1.357 MW/m², for a superheat variation of 20 °C, and for the same thickness of strip (2.3 mm).

In order to assess the effect of the initial superheat on the peak heat flux, for different thicknesses, a correlation of the type:

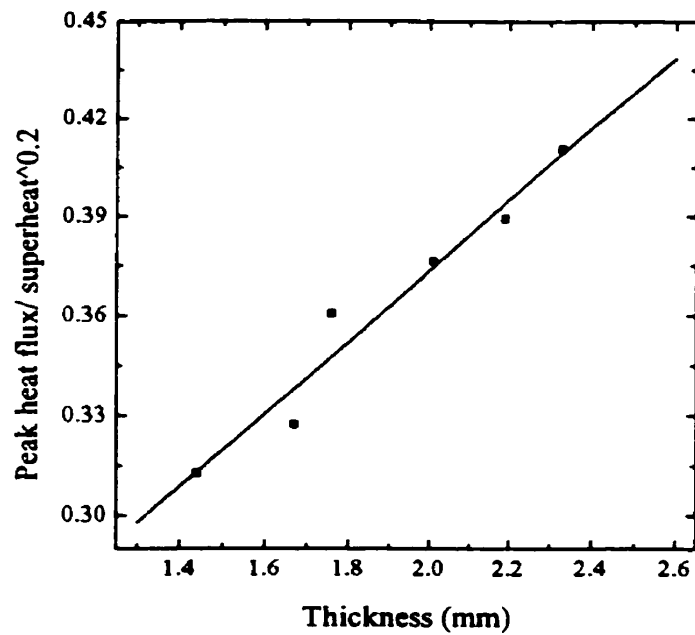
$$\frac{q_p}{\Delta T^m} = aT_h + b \quad (8-4)$$

was assumed, where ΔT is the superheat and q_p is the peak heat flux. The value of m that gave the best fit was 0.2, for all bars. Very good agreement was obtained between this correlation and the experimental data, as can be seen in Figure 8-6, for coatings 1 and 5.

Higher superheats yielded smoother bottom surfaces of the strips. As already discussed by previous researchers^(7,14,15), an improved fluidity and a lower surface tension at higher superheats contribute to a better wettability of the



(a)



(b)

Figure 8-6. Combined effect of thickness and initial superheat on peak heat fluxes: a) Coating 1 and b) Coating 5.

substrate, resulting in a smoother bottom surface. On the free surface, no significant difference was observed.

Elevated superheats produced thinner strips. Similar trends have been reported by Chen et al⁽⁷⁾. In the present work, it might have been caused by the reduction in the film thickness at a higher superheat. Another possibility mentioned by other authors⁽⁷⁾, the spreading of the metal out to the sides caused by a delay in the onset of solidification, did not apply to the system used in this work. A quick visual inspection of the strip allowed us to conclude that any variations in the width of the strip, at different superheats, were minimal.

8.3.5 VARIATION OF HEAT FLUX WITH TIME

In the study of interfacial heat transfer, it is important to determine not only the peak heat fluxes, but also the heat flux characteristics following solidification of the interfacial layer.

Kumar and Prabhu⁽¹⁶⁾ have proposed a correlation between heat flux and the peak heat flux, for the solidification of bars of aluminum base alloys. The correlation was based on the thermal diffusivity of the substrate and was able to predict the heat fluxes following the occurrence of a peak heat flux.

Muojekwu et al⁽¹⁴⁾ have suggested two different stages after the peak. In the first, the heat flux would fall linearly with time while in the second, the relation between q and q_p would depend on the coefficients of linear expansion of the

casting and the mold and on the thermal diffusivity of the mold.

In this work, since only one type of substrate was used, a simpler correlation was proposed:

$$\frac{q}{q_p} = A_1 t^{-d_1} \quad (8-5)$$

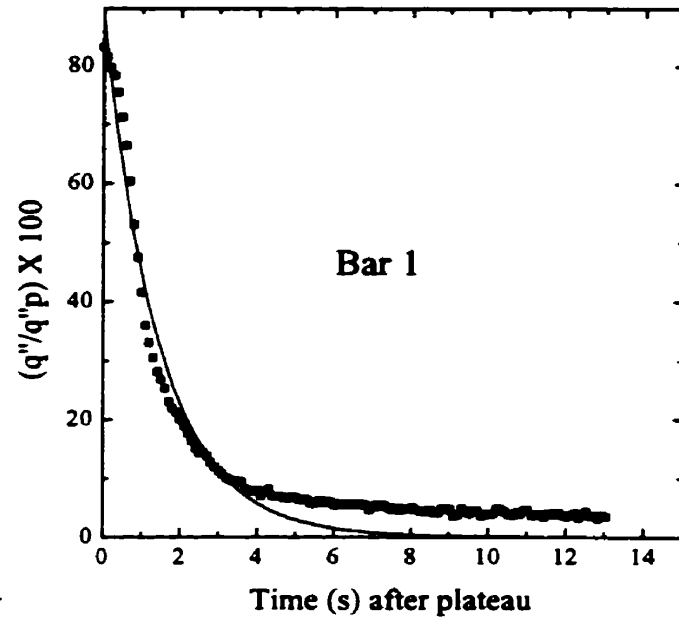
In some cases, mentioned in Section 8.3.1, a plateau follows the occurrence of the peak. In these cases, the applicability of the correlation is limited to the points after the plateau, i.e., where there is a sharp drop in the heat flux. For the points after the plateau, the best correlation proved to be:

$$\frac{q}{q_p} = A_2 e^{-d_2 t} \quad (8-6)$$

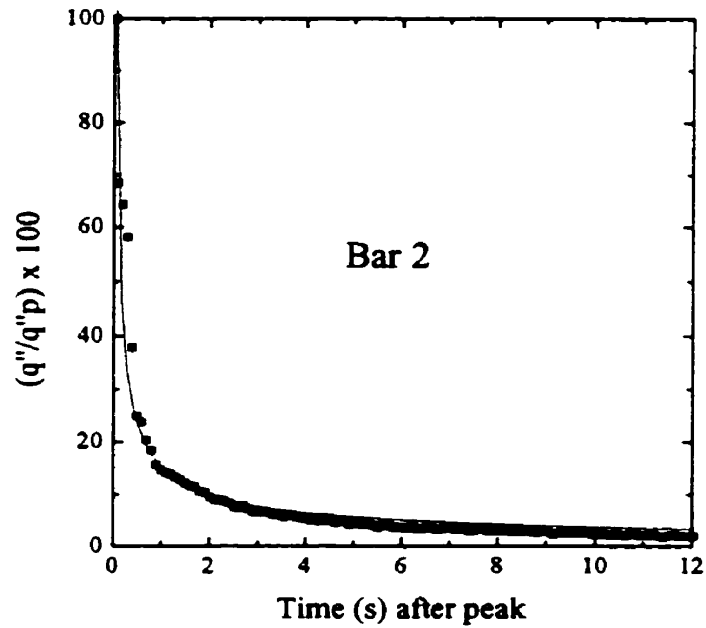
Figures 8-7 and 8-8 show the fitted curves for the heat fluxes in the region after the peak or after the plateau, if it happens, for the coatings, in the standard conditions (substrate speed = 0.8 m/s; initial superheat = 10⁰ C). There was very good agreement between the curves and the experimental values. The fitting parameters A_1 , A_2 , d_1 and d_2 are given in Table 8-3.

8.3.6 EFFECT OF ROUGHNESS

The heat flux values for coating HC were found to be much higher than those for the others, in the case of a smooth surface. For rough surfaces, the

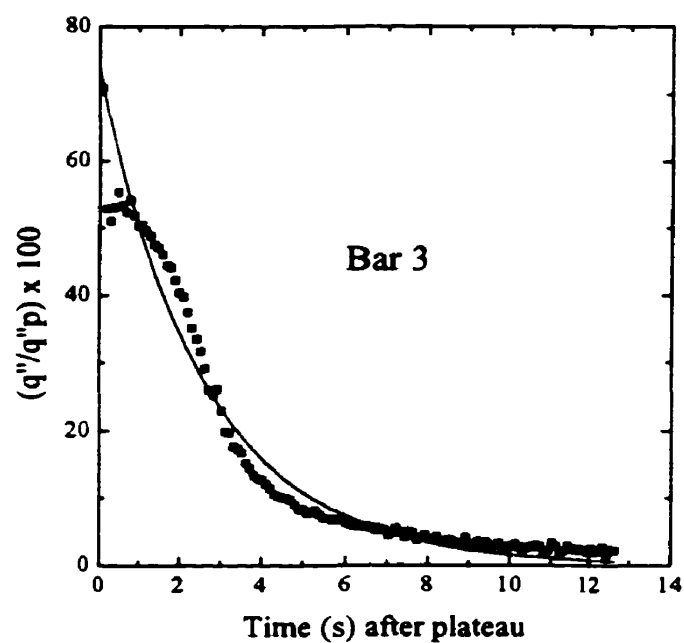


(a)

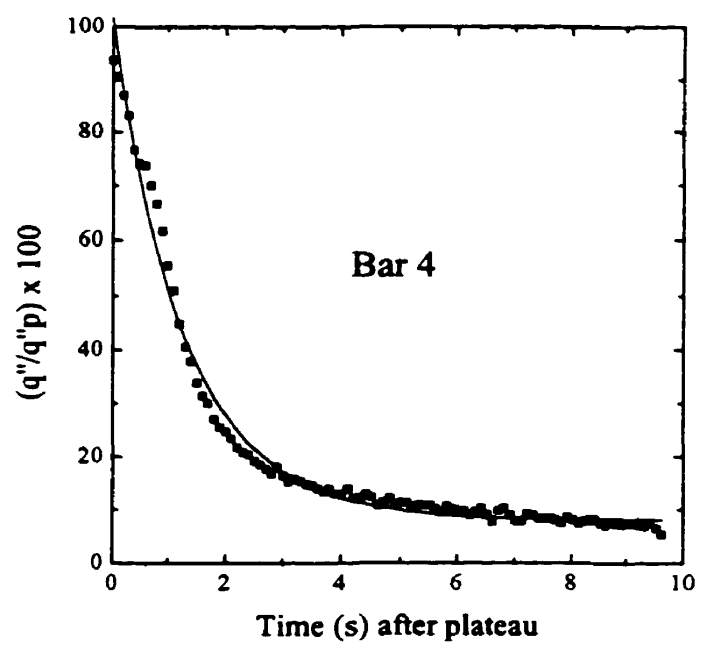


(b)

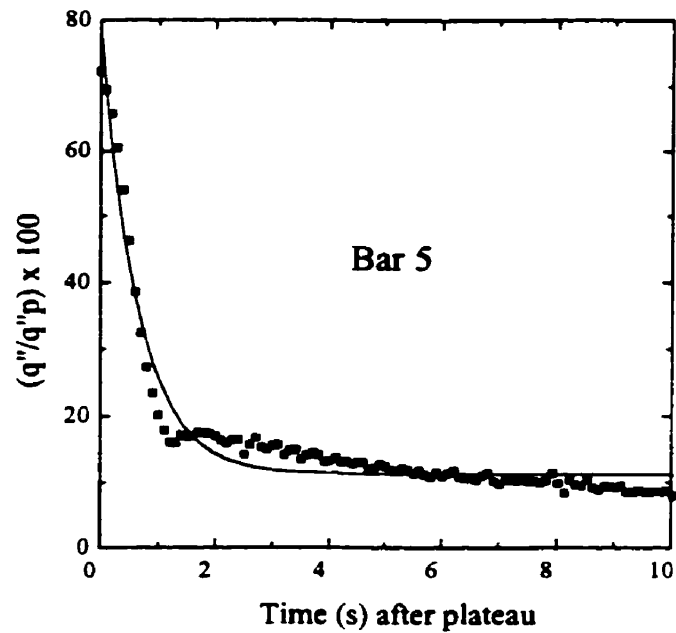
Figure 8-7. Variation in normalized interfacial heat fluxes versus contact times following peaks or plateaux in interfacial heat fluxes, for coatings: a) 1; b) 2; c) 3 and d) 4.



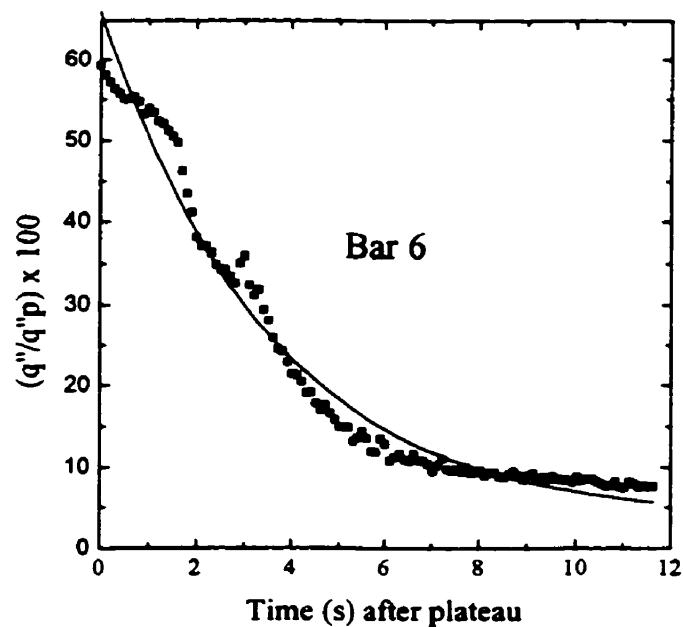
(c)



(d)

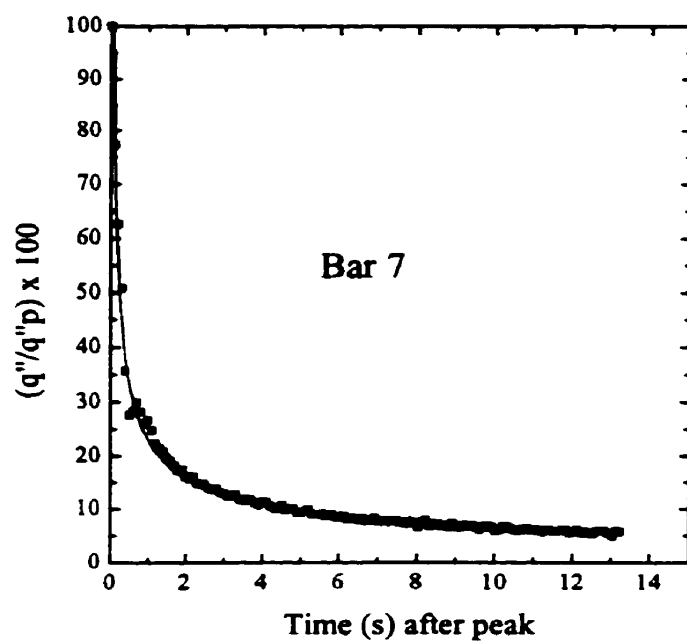


(a)

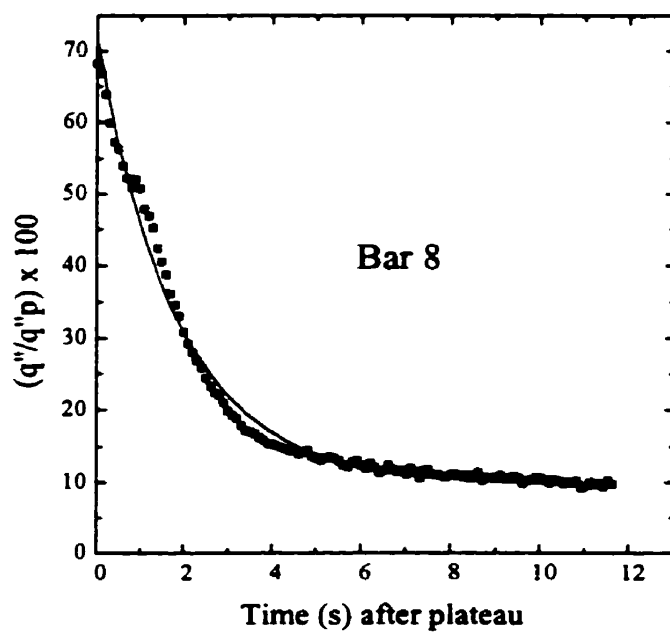


(b)

Figure 8-8. Variation in normalized interfacial heat fluxes versus contact times following peaks or plateaux in interfacial heat fluxes, for coatings: a) 5; b) 6; c) 7 and d) 8



(c)



(d)

coatings presented lower peaks, as expected, and the difference between them was also smaller. It is readily observed that the effect of substrate roughness is different for every coating. Similar findings were reported by Wang et al⁽¹⁵⁾, who found little variation in h for the solidification of copper onto smooth and rough stainless steel substrates. For coating HC, there was a decrease of 45% in the heat transfer using a rough substrate, whereas for coating M the decrease was about 17%. The wettability properties of the coatings have not been within the scope of this study, but will hopefully be included in future work.

Table 8-3 Constants for equations 8-5 and 8-6

COATING	A_1	A_2	d_1	d_2
01		0.8916		0.6812
02	0.15		0.631	
03		0.7907		0.5177
04		0.9362		0.7726
05		0.6792		1.5367
06		0.6268		0.2863
07	0.231		0.541	
08		0.6207		0.5358

It is relevant to note that the levels of roughness of the bars used for the present studies were relatively high. Values of R_a between 11 and 18 μm are well above data encountered in the literature. Therefore, even higher values for q and h can be expected, if a smoother substrate is to be used.

8.3.7 ESTIMATION OF TRANSIENT HEAT TRANSFER COEFFICIENTS

The interfacial heat transfer coefficients can be calculated by considering the transient heat flux from the cast to the point where the thermocouple is located, 4.57 mm below the surface.

The total thermal resistance (R_{th}) of the system in the present case will be due to three contributions:

- the interfacial thermal resistance ($R_i = 1/h$);
- the resistance due to conduction through the coating (R_c);
- the resistance due to conduction through the substrate (R_s) to the measuring thermocouple.

Mathematically:

$$R_{th} = R_i + R_c + R_s \quad (8-7)$$

The calculation of the thermal resistance to conduction through the coating

and the substrate involves two components:

$$R_c = \frac{\delta_c}{k_c} ; R_s = \frac{\delta_{sub}}{k_{sub}} \quad (8-8)$$

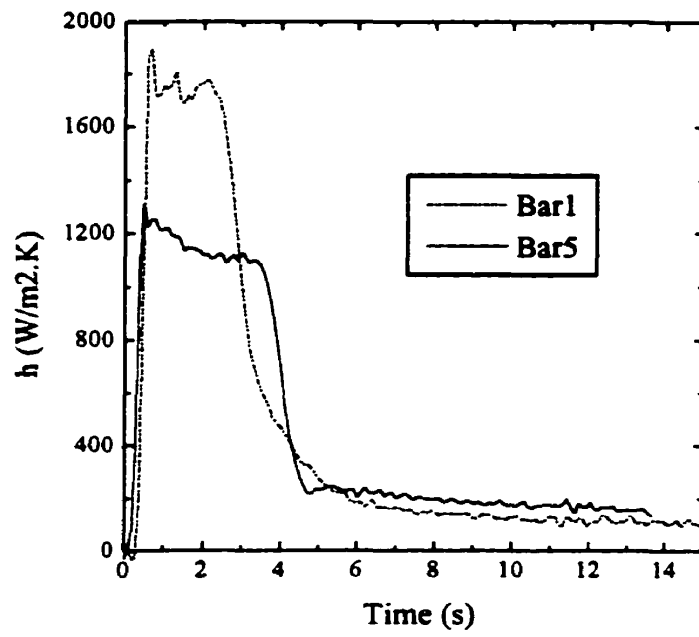
Given a coating thickness δ_c of 75 μm and the depth of the thermocouple, δ_{sub} , of 4.57 mm, then:

$$q(t) = \frac{T_{bs} - T_{sub}}{R_{th}}, \text{ where:} \quad (8-9)$$

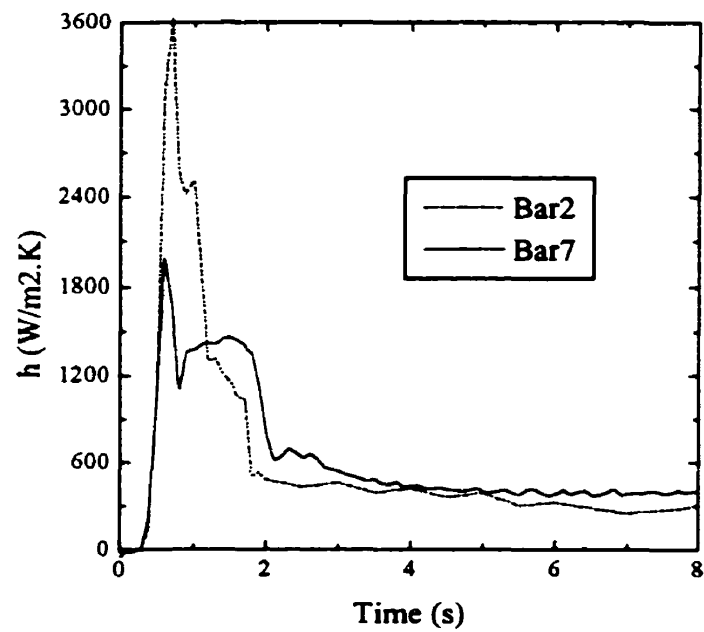
T_{bs} is the transient temperature on the bottom surface of the strip, and T_{sub} is the transient temperature 4.57 mm below the substrate surface, both of which are measured or evaluated.

The transient interfacial heat transfer coefficients were calculated from the heat flux data. Heat transfer by radiation and convection was considered for the interface. On the extremities (top surface of the strip and bottom of the substrate), heat was also lost by radiation and convection. Table 8-4 gives the thermal boundary conditions considered for the calculation of the heat transfer coefficients. Although included in the model, radiation could be considered negligible for the range of temperatures used in the work.

It is shown in Figure 8-9 that the evolution in the heat transfer coefficient

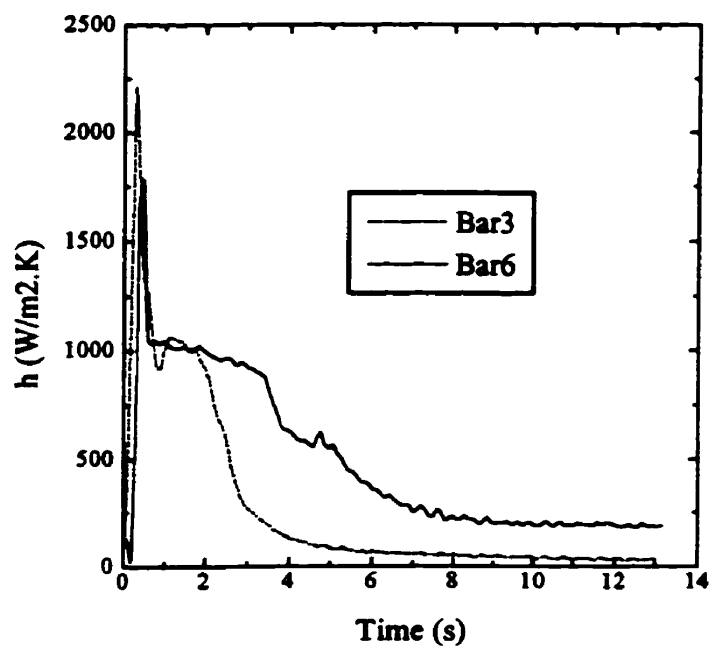


(a)

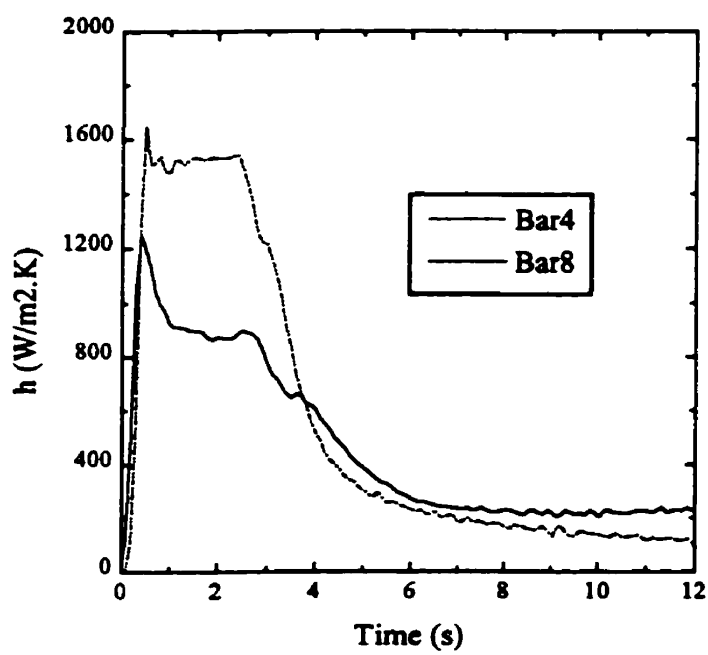


(b)

Figure 8-9. Calculated interfacial heat transfer coefficients for substrates with coatings: a) 1 and 5; b) 2 and 7; c) 3 and 6; d) 4 and 8.



(c)



(d)

h follows the same profile as the heat fluxes.

Table 8-4 - Thermal boundary conditions used for the calculation of the heat transfer coefficients.

Heat transfer coefficient for the bottom surface of the mold	=	5 W/m ² .K
Heat transfer for the surface in contact with the air	=	10 W/m ² .K
Emissivity of bottom surface of the mold	=	0.75
Emissivity of molten metal	=	0.20
Thermal conductivity of air	=	$5.8 \times 10^{-5} T + 2.5 \times 10^{-2}$ W/m.K
Ambient temperature	=	25°C

The minimum values of R_{th} , corresponding to the peaks in h , ranged from $3.08E-3$ m²K/W for coating 2 to $8.08E-3$ m²K/W for coating 5.

Another interesting aspect to be assessed is the relative importance of heat transfer through the various coatings. In the case of the smooth metallic coating HC, the average thermal resistance through the coating accounted for as little as 1.4% of the total thermal resistance. The interfacial resistance represented 89% of R_{th} . For the rough metallic coating, the resistance through the coating was even lower, at 0.85% of R_{th} . On the opposite side, in the case of the smooth coating M, the following results were found: 17.6% of R_{th} due to the coating and 77.6% due to the interfacial resistance.

8.3.8 EVOLUTION OF h AFTER THE PEAK

In the analysis of the heat fluxes and heat transfer coefficients, it is important to stress not only the peak values, but also the evolution of q and h after the peak, before solidification is complete. In this work, a relationship between the peak values and the occurrence, or not, of a plateau following the peak was generally observed. The higher the peak, the less likely the occurrence of the plateau.

In order to quantify this relationship, a simple criterion was chosen. The time delay from the peak occurrence to the moment when h reached a value of $0.25 h_p$ was calculated for the various coatings. The reason for choosing this value is due to observations that, once it reached $0.25 h_p$, the heat transfer coefficient in most cases remained stable or fell slightly, which is a fairly good indication that the solidification was over. Table 8-5 gives the results obtained for the different coatings deposited on smooth substrates.

Table 8-5 Time delay between h_p and $h = 0.25h_p$

COATING	$t_{h=0.25h_p} - t_p$ (s)
01	4.1
02	1.5
03	3.8
04	4.7

The data suggest that not only did the metallic coating result in higher peak

heat fluxes, but the solidification process was also finished earlier. On the other hand, coating Y yielded a flux curve with the most extended plateau, after its peak. This characteristic makes up for its much lower peak value. This information is useful for industrial processes, since it can be readily related to the productivity of a single-belt machine.

8.3.9 AIR GAP EVOLUTION

In general, the air gap starts after approximately 0.5 s, which is the time necessary for the superheat to be dissipated. It is also important to stress that the metal might not wet completely the substrate, so there is a possibility of an initial imperfect liquid metal-substrate contact, leading to a small air gap.

As seen in Figure 8-10, the air gap grows rapidly, up to the point where the solidification might be finished, following a contact time approximately equal to 6 seconds. After that, there is a slight and erratic increase in the calculated air gap. The reason for this could not be precisely determined. One possible explanation might be the growth of an oxide film on the metal's surface, as observed by Ho and Pehlke⁽¹⁷⁾. This oxide film, being of higher thermal resistance than the pure metal, would lead to a reduction in the calculated heat transfer coefficient in that region ($t > 6$ s) and, as a consequence, an increase in the calculated air gap. It is not clear, however, if there was enough time for the formation of such oxide.

A more realistic explanation is the existence of a normal fluctuation around a roughly stabilized value of the air gap, as observed in the heat transfer

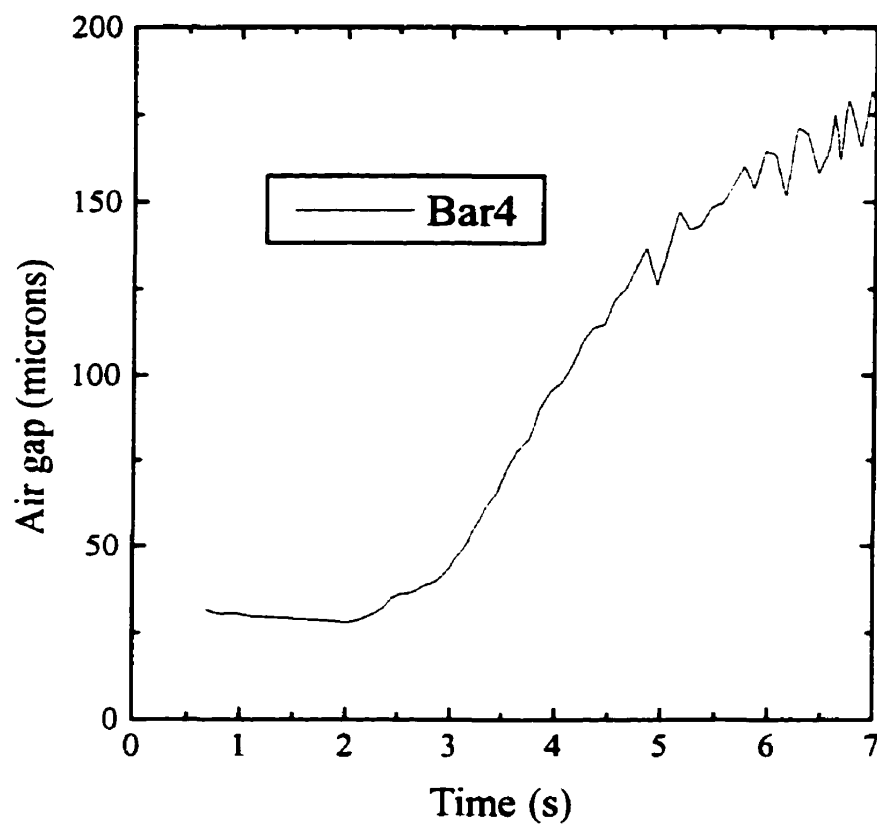


Figure 8-10. Typical curve of air gap evolution (Coating 4).

coefficient (Figure 8-9 d). In that region ($t > 6$ s), the numerical method is more sensitive to experimental error, given the stabilization in the heat transfer coefficient. The air gap was found to reach a final value around 200 μm .

8.3.10 COMPARISON BETWEEN PREDICTED AND MEASURED SDAS

Table 8-6 presents the conditions for the Al-8.4wt%Si strips produced using Type 02 bar coating (Matrix HC - NICKEL).

Table 8-6 Conditions and characteristics of Al-8.4wt% Si alloy strips produced using Type 02 bar coatings (Matrix HC - NICKEL).

Sample ID	Superheat (°C)	h_p (W/m ² .K)	Thickness (mm)	Cooling rate (°C/s) at bottom surface	
A1 AS	20	2384	1.10	21.7	19.4
A2 AS	20	2805	1.00	32.7	28.7
A3 AS	20	3112	0.72	49.9	45.1
B1 AS	60	3001	1.55	21.3	17.1
B2 AS	60	3263	1.69	22.9	17.5
B3 AS	60	3474	3.19	17.4	10.2
C3 AS	66	4385	1.80	42.0	25.6

The peak in the interfacial heat transfer coefficient, h_p , ranged between 1131 and 5871 W/m².K, for the various conditions studied. The cooling rates at the bottom and free surfaces were calculated to be in the range of 10 to 90 °C/s.

As in the casting of pure aluminum, the surface quality of the strips proved to be excellent. The bottom surfaces were generally characterized by a smooth texture, while the free surfaces exhibited some roughness.

Figure 8-11 presents variations in secondary dendrite arm spacing (SDAS) versus cooling rate, for the strip surface in contact with the substrate, and for the free surface.

The secondary dendrite arm spacing (SDAS) measured at the bottom surface of the strip varied from 7 to 13 μm , for variations in cooling rates from 49 $^{\circ}\text{C/s}$ to 17 $^{\circ}\text{C/s}$. In a similar way, the increasing of cooling rates determined the decreasing of SDAS in the free surface of the strip. The values of SDAS at the free surface are bigger (10 to 14 μm) than those at the bottom, due to smaller cooling rates. Increasing superheats yielded a decreasing SDAS in both surfaces of the strip.

In order to validate the predicted cooling rates, the SDAS were calculated based on the following formula⁽¹⁸⁾:

$$SDAS = C_s (t_f)^n \quad (8-10)$$

where C_s is a constant having values in the range of 11.5 to 15.3 for Al-Si alloys, t_f is the local solidification time determined on the basis of cooling rates, and $n=0.43$.

The SDAS values predicted based on cooling rates and therefore associated with local solidification times are generally in good agreement with measured values (see Figures 8-11 and 8-12). In our specific case, a value of 10.93 for the constant C_s gave the best agreement between the measured and calculated SDAS.

8.4. **UNCERTAINTY ANALYSIS**

Since there is no manual interpolation of data in the present calculations, the level of uncertainty is not very high. Also, some restrictions were employed to ensure stability of the IHCP (Inverse Heat Conduction Problem) solution, for the small time steps employed in the present case.

Some calculations were repeated using the method of Hills⁽¹³⁾, to check the uncertainty due to the IHCP solution. The differences, in most cases, amounted to a maximum of 10%.

No bias was considered, since the thermocouples were calibrated and also because in the specific case the random errors are considered much more relevant.

Using Kline's method⁽¹⁹⁾ to calculate the total uncertainty and considering the most important variables as the thickness of the strip and the initial superheat, the total uncertainty is calculated by:

$$\left(\frac{W_h}{h}\right)^2 = \left(\frac{\partial \ln h}{\partial \ln \delta} \cdot \frac{W_\delta}{\delta}\right)^2 + \left(\frac{\partial \ln h}{\partial \ln \Delta T} \cdot \frac{W_{\Delta T}}{\Delta T}\right)^2 + \left(\frac{W_{exp}}{h}\right)^2 + \left(\frac{W_{chena}}{h}\right)^2 \quad (8-11)$$

where:

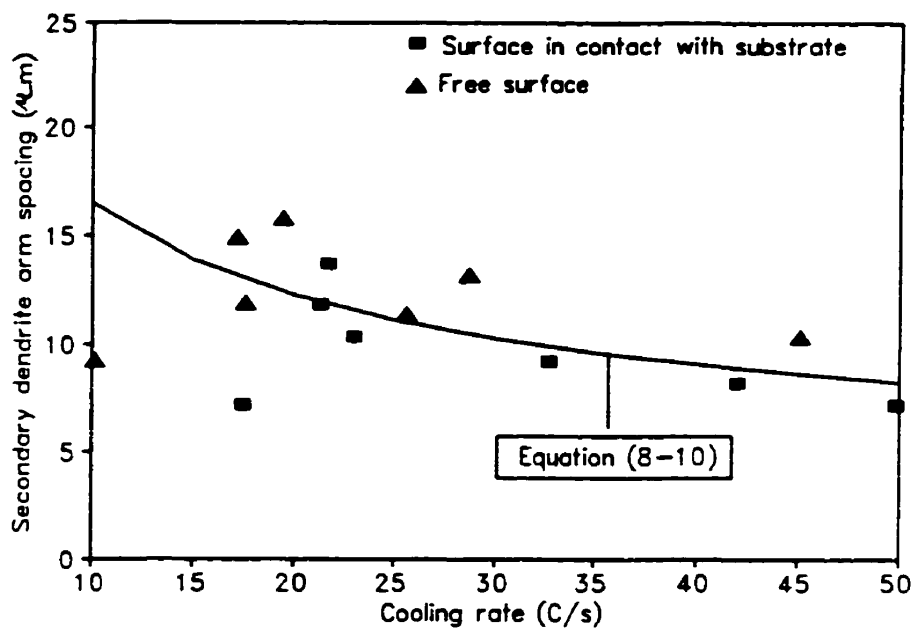


Figure 8-11. Variation of the Secondary Dendrite Arm Spacing (SDAS) versus cooling rate, for strips cast on coatings 1 and 2.

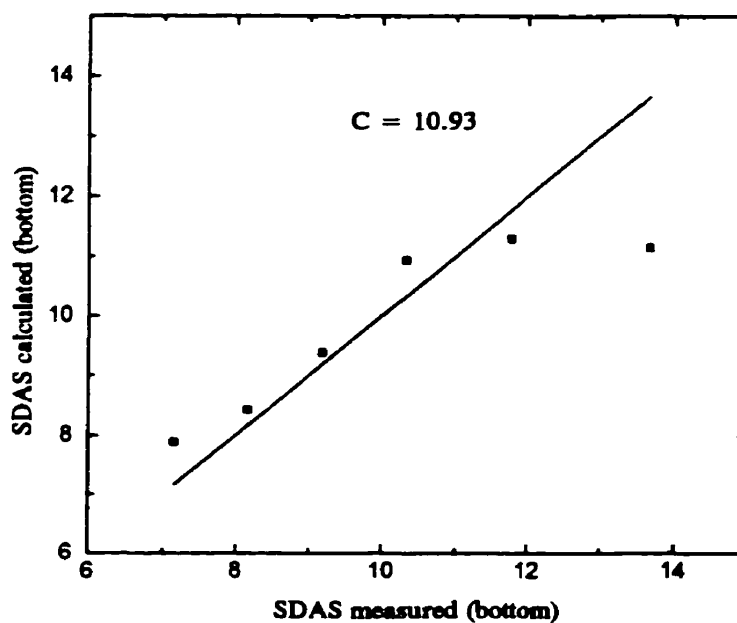


Figure 8-12. Calculated and measured SDAS on the bottom surface of the strips.

$$\frac{\partial \ln h}{\partial \ln \delta} = 1 \quad ; \quad \frac{\partial \ln h}{\partial \ln \Delta T} = 0.2 \quad (8-12)$$

These values were approximately representative of the various conditions used in this work. Also, the relative errors (W_δ/δ) and ($W_{\Delta T}/\Delta T$) were estimated to be 0.20 and 0.10, respectively. The time constants of the thermocouples were low ($\tau_1 = 0.194$ s and $\tau_2 = 0.035$ s); therefore, the error introduced by the thermocouples corrections was relatively low (between 3 and 5%). Since the experimental error (W_{exp}/h)² due to the temperature measurements was estimated to be about 0.10, the total uncertainty would be about 18-20% for the method used.

8.5. COMPARISON WITH DATA IN THE LITERATURE

As mentioned earlier in this Chapter, little data are available for conditions similar to those pertaining to this work. One of the main purposes of the author was to simulate a casting process with a moving substrate and a free surface, at casting speeds and strip thicknesses similar to single-belt processes currently under development. Therefore, any attempt to compare the results of this work to others is a tricky task.

Nevertheless, the values of heat flux and heat transfer coefficients obtained here are in the range of data recently published for the various near-net-shape casting configurations. For example, Wang and Matthys recent literature review^[5] reports values of h in the range of 850 and 2900 W/m².K for steel castings, at low speeds and at thicknesses between 20-80 mm. This work concentrated on a speed range of 0.4-1.2 m/s and thicknesses ranging from 1-5 mm.

8.6 SUMMARY AND CONCLUSIONS

An experimental apparatus has been developed and successfully used to simulate thin strip casting of aluminum and aluminum alloys. The most important variables affecting the casting process were successfully investigated, such as the type of coatings, substrate roughness, strip thickness and initial superheat. The interfacial heat fluxes were calculated by an IHCP method. From the calculated heat fluxes, it was possible to determine the heat transfer coefficients and the local cooling rates. The predicted cooling rates were compared with correlations based on the secondary dendrite arm spacing (SDAS).

Based on the results obtained, the following conclusions can be drawn:

- 1) For the standard conditions of 10°C superheat and substrate speed of 0.8 m/s, heat fluxes ranged between 0.66 MW/m² and 2.0 MW/m². Corresponding heat transfer coefficients for the various coatings ranged from 1200 and 3600 W/m².K. The wettability properties of the different coatings with different levels of roughness were determinant for the wide ranges above reported. These results demonstrate the critical importance of the metal-coating-substrate contact in the efficiency of heat transfer;
- 2) Interfacial heat fluxes were found to be dependent on the thickness of the strips, initial superheat and properties of the coatings (roughness, thermal conductivity). A thicker strip and a higher initial

superheat increase interfacial heat fluxes. The importance of strip thickness in this configuration can be explained by the existence of a free surface that exerts no pressure on the solidifying strip, except for the metallostatic head. Smoother surfaces were found to be significantly more efficient in promoting heat transfer between substrates and the molten metal;

- 3) Correlations were derived in order to predict peak heat fluxes and peak heat transfer coefficients for different superheats and thicknesses, and also to predict heat fluxes after the peak. Very good agreement was found between the experimental values and the values predicted by the correlations for all coatings used in this work;
- 4) A relationship was generally observed between the peak in the heat transfer coefficient and the occurrence of a plateau following the peak. The higher the peak, the less likely the occurrence of the plateau. A simple criterion was proposed to quantify this relationship. The information here obtained is considered useful for industrial processes;
- 5) The SDAS for Al-8.4wt% Si strips were found in the range of 7 to 14 μm for the bottom of the strips and from 10 to 14 μm for the free surface. These values are in good agreement with the predicted ones;
- 6) The SDAS were found to be dependent on the local cooling rates,

which result from the diverse experimental variables: type of coating and substrate roughness, strip thickness and initial superheat. Higher cooling rates yielded smaller SDAS;

- 7) Some scatter was present in the results. However, consistent qualitative agreement was found in the vast majority of simulated conditions.

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CONCLUSIONS

9.1 SUMMARY

A three-dimensional, fully coupled, fluid flow and heat transfer model was developed and validated. The model predicted liquid metal flow characteristics in a horizontal-belt, metal-delivery system for thin strip casting of steel. Turbulence effects were accounted for by a Low-Reynolds κ - ϵ model. Flow through a porous medium was modelled using Darcy-Brinkman formulations. Macroscopic solidification was also included in the model and the temperature fields then generated were input in another model, to calculate the stresses and displacements along the width of the strip.

An experimental apparatus has been developed and successfully used to simulate thin strip casting of aluminum and aluminum alloys. The strip casting simulator could cast strips at thicknesses and speeds comparable to the intended industrial process: 1-3 mm and 0.4-1.2 m/s, respectively. A parametric investigation was conducted on some variables of the process, such as nature of coatings, substrate roughness, strip thickness and initial superheat.

9.2 MODEL VALIDATION

The validation of the fluid flow predictions was accomplished by comparison with measurements carried out in a static water model. Agreement within $\pm 20\%$ was found in all cases chosen for comparison, for the velocity in the direction of the movement.

As for the heat transfer/solidification study, the predicted thickness of the developing solid shell corresponded very closely to the semi-analytical solution, from the onset of solidification to the end of the domain. Considerable improvement was observed from the two-dimensional model presented by Jefferies⁽¹⁾ for the same delivery system. The very scarce data of SDAS for the system in study impeded a more detailed comparison of the model outputs.

The predicted stresses and displacements at the second part of the modelling matched qualitatively the results of an analytical solution given in the literature for a similar problem.

9.3 STANDARD PROPOSED CONFIGURATION

Simulations were carried out for the proposed delivery system, giving important insight into the major parameters of the process, such as geometry of the reservoir and initial superheat. Numerical techniques and boundary conditions were also a focus of discussion, in terms of their influence on the model's outputs. Some main conclusions were drawn:

- 1) In the absence of flow modification, the extended delivery system failed in providing a smooth flow to the conveyor belt. Recirculation zones were always present in the reservoir, although its size and shape could be altered. A concentration of the flow was observed in the exit gap region, which created a high-turbulence zone;
- 2) The turbulence levels in the region of the strip, inside the reservoir, were mainly affected by the belt speed. Therefore, the way the incoming liquid metal interacts with the high-speed belt would seem to be a determining factor for a smooth casting operation;
- 3) Divergences in the solid fraction between the center of the strip and the edges were found to increase with increasing turbulence in the neighbourhood of the exit gap. The adoption of larger pools contributed to a more uniform solidification along the width, but generated sizable "cold" zones in the vicinity of the front wall, in addition to recirculation whorls next to the back wall;
- 4) Among all numerical techniques and boundary conditions employed in the solution of this problem, the values of the morphology constant C and of the overall heat transfer coefficient h proved to be crucial to ensure accuracy of the results. Their precise determination, although difficult, should then be pursued;
- 5) The importance of modelling turbulence through a κ - ϵ model proved to be critical, for the particular configuration studied. The sudden contraction experienced by the liquid metal constituted a serious limitation to the applicability of an ad-hoc viscosity approach, leading to the overprediction of the solid shell thickness and underprediction of the mushy region.

9.4 FLUID FLOW MODIFICATION

The adoption of a flow modifier eliminated the recirculation zone in the reservoir. Moreover, the smoother flow reduced the flow concentration in the neighbourhood of the exit gap. Such improvements in the flow distribution were crucial to ensure more uniform solidification, such that the solid fractions in the center of the strip and on the edges were more closely matched.

Due to the reduced coefficient of discharge imposed by the insertion of the flow modifying filter, a higher pool was adopted for the delivery system. As a consequence, the lower inlet velocity made it difficult for the metal to reach the front wall and, thus, an extended cold zone was present in most simulations, especially when larger pools were employed. Using a higher back wall minimized this problem, while maintaining smooth the flow in the reservoir. Nevertheless, the adoption of more extended pools did not represent a major improvement in the flow pattern, as in the no-filter case.

Finally, a summary was presented, with the recommended operational conditions for strip casting in the proposed delivery system. These recommendations were determined for certain boundary conditions and are believed to be useful as an introductory guide for casting in an industrial apparatus.

9.5 STRESSES AND DISPLACEMENTS

The strip was found to displace in the inward direction. The displacements

were mainly due to the cooling and contraction of the solid shell, in contact with the water cooled belt. The stresses were tensile at the cooling surface, and compressive in the middle of the strip. Although the calculated crack indices were low, the addition of new solid layers created sites of localized high stresses that could increase crack likelihood.

The constitutive model and the material properties affected the predictions of displacements more significantly than the stress calculations. That could be explained by the relatively unconstrained state of the casting, which makes the thermal strains the main source of stress in the solid shell.

9.6 HEAT TRANSFER MEASUREMENTS

The interfacial heat fluxes and heat transfer coefficients were highly dependent on the contact metal-substrate-coating. Values of q and h varied threefold for the various coatings and levels of roughness investigated.

Other important variables of the high-speed casting were the strip thickness and initial superheat. A thicker strip increased the interfacial heat fluxes. This finding seemed in disagreement with previous reports in the literature, but it could be explained by the singular configuration of the single-belt caster. In this machine, only one surface of the strip is in contact with the cooling substrate; thus, the top surface exerts no pressure on the solidifying strip other than the metallostatic head.

Correlations were derived to predict peak heat fluxes and peak heat transfer

coefficients for different strip thicknesses and initial superheats. The evolution of the heat flux after the peak was predicted for the various coatings. Also, a relationship was observed between the peak in the heat transfer coefficient and the total time for solidification. The information obtained here is considered to be useful for industrial processes.

Aluminum alloys were cast and the SDAS values measured were compared with predictions from the inverse heat conduction model. Generally good agreement was obtained, for the high cooling rates reached (up to 100°C/s).

9.7 FUTURE WORK

Measurements of the thermal boundary conditions in an actual single-belt caster will be crucial to increase the usefulness of the model. The overall heat fluxes should be evaluated for diverse experimental conditions, as discussed in Chapter 8. The morphology of the mushy zone and the heat losses to the walls are also important inputs of the model and attempts should be made to determine them for this particular configuration.

The thin strip casting simulator can be very useful in the study of the wettability properties of the various substrate-coating pairs available on the market. Enlargement of the apparatus, allowing the casting of wider and thicker strips would help to clarify the solidification phenomena along the width of the strip. Measurements of the heat fluxes in the center and on the edges of the strip could reveal any divergences on the mechanism of air gap formation.

As for the mathematical modelling, the inclusion of microscopic solidification phenomena would certainly influence the morphology of the mushy zone. Interdendritic flow is believed to be important in the determination of stresses and this phenomenon may be crucial in fast-cooling processes.

The coupling of stress and displacement calculations to the fluid flow/heat transfer model would be justified in the absence of reliable experimental data for interfacial heat fluxes. Fully-coupling these models would, however, imply numerical complexity and huge computational demands.

9.8 STATEMENT OF ORIGINALITY

1. A thin strip casting simulator has been successfully developed and used to simulate the casting of aluminum and aluminum alloys. For the first time, the interfacial heat fluxes were evaluated under conditions similar to those intended for industry: strip thickness varying between 1 and 4 mm and substrate speeds in the range 0.4-1.2 m/s.
2. A thorough experimental investigation was conducted into the influence of the main variables of horizontal belt casting. Useful heat transfer correlations were derived for diverse substrate-coating pairs, for the strip thickness and for initial superheat.
3. A three-dimensional fully coupled fluid flow/heat transfer model has been developed for a proposed thin-strip-caster delivery system. The

model included macroscopic solidification and turbulent flow predicted by κ - ϵ equations. Besides, flow through porous media was modelled for the proposed system.

4. This is the first three-dimensional model for a planar-flow, single-belt caster. This feature allows flows in the neighbourhood of the containment walls to be predicted and enables the modeller to search for improvements in the flow pattern.
5. The adoption of a κ - ϵ model to account for the turbulence effects yielded a remarkable improvement in the solidification predictions, when compared with the earlier ad-hoc viscosity scheme.
6. Preliminary recommendations for casting operations were made possible, after an exploratory investigation conducted through model simulations.
7. Additional information on the performance of the flow modifier was obtained, due to the extension of the model to three dimensions and, mostly, due to the κ - ϵ model for turbulence.

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APPENDIX A - FRICTION WORK ON THE FRONT WALL

As mentioned in Chapter 4, the friction work in the exit gap and in the channel formed below the front wall could cause a considerable reduction in the velocity of the exiting flow. Although its calculation is not accurate, an initial estimate can be given by the following theoretical analysis.

In order to calculate the energy losses, the classical theorem of thermodynamics of open channels is used. In Figure A-1, the equation for the steady-state flow is given by:

$$dq + dw = g dz + d \left(\frac{v^2}{2} \right) \quad (\text{A-1})$$

The relationship between the flow at locations A and B can be given by:

$$\Delta q + \Delta w = -g (z_B - z_A) - \frac{1}{2} (v_A^2 - v_B^2) \quad (\text{A-2})$$

Considering zero variation for the terms Δq (internal energy) and Δw (work, e.g., friction), equation A-2 is reduced to:

$$\frac{1}{2} (v_A^2 - v_B^2) = g (z_B - z_A) \quad (\text{A-3})$$

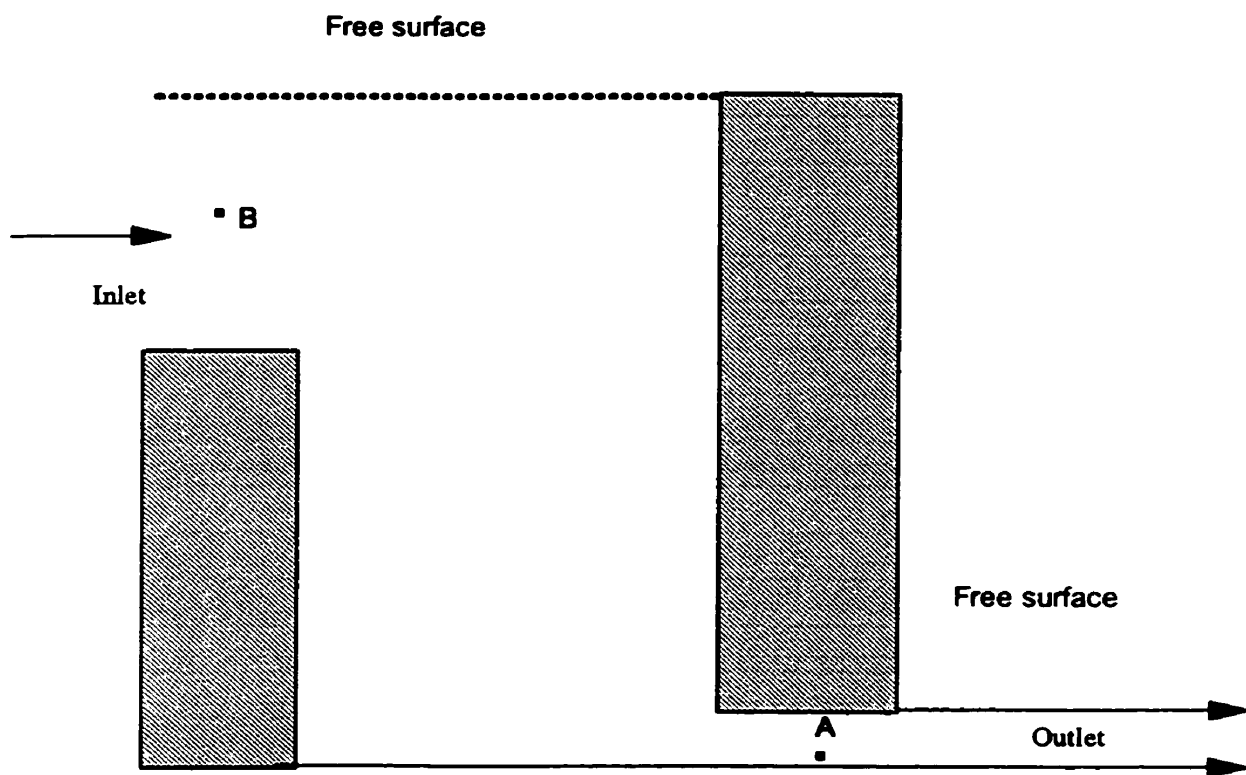


Figure A-1. Illustration of an open channel system.

Taking into account mass conservation in the system:

$$\rho_{1A} \cdot A_A \cdot v_A = \rho_{1B} \cdot A_B \cdot v_B \quad (\text{A-4})$$

where A_A is the transversal section area at the nozzle entrance and A_B is the transversal section of the inlet area. So, considering constant density of the liquid:

$$\frac{v_B}{v_A} = \frac{A_A}{A_B} \quad (\text{A-5})$$

Then, for a 10-mm strip and for the standard case without a filter, the following relationship is derived:

$$\frac{v_B}{v_A} = 0.33 \quad (\text{A-6})$$

Substituting (A-6) into (A-3):

$$v_A^2 - 0.111v_A^2 = 2gh_m \quad (\text{A-7})$$

where h_m is the metal head = 0.07 m. So, $v_A = 1.24$ m/s, for the frictionless case.

Now, to consider the energy losses at the entrance (sudden contraction) and due to the slit flow, calculating the friction factor e_f for both the entrance and the slit flow is necessary.

1) Entrance (turbulent flow)⁽¹⁾:

$$e_{f_1} = 0.5 [1 - (A_A/A_B)] \quad (\text{A-8})$$

So, $e_{f_1} = 0.429$.

2) Slit flow:

Using the same approach as Sung et al⁽²⁾ for slit flow, the following formulation was considered for the energy losses in the slit:

$$e_{f_2} = \left[1.14 - 2 \log \left(\frac{\epsilon_r}{D_h} + \frac{21.25}{Re^{0.9}} \right) \right]^{-2} \times \frac{L_s}{D_h} \quad (\text{A-9})$$

where:

ϵ_r is a roughness parameter and its value is tabulated for the various materials and was estimated as 1.4 mm for the refractory brick⁽³⁾;

D_h is the hydraulic diameter;

Re is the Reynolds number;

L_s is the length of the slit = 20 mm.

The hydraulic diameter is calculated by:

$$D_h = 2 \cdot \frac{ab}{(a+b)} \quad (\text{A-10})$$

where:

a is the height of the slit = 10 mm;

b is the width of the slit = 450 mm.

Since in this configuration **b** is much higher than **a**, equation (A-10) can be simplified to:

$$D_h = 2a \quad (\text{A-11})$$

An average Reynolds number of 10,000 was considered for this calculation. So, $e_n = 0.087$ and the total friction factor is $e_f = 0.429 + 0.087/2 = 0.473$. The reason for taking only half the e_n is the assumption that no friction occurs on the bottom surface of the strip (no-slip condition).

Back to Bernouilli equation, now considering losses by friction:

$$\frac{1}{2} (v_A^2 - v_B^2) = g(z_B - z_A) - E_f \quad (\text{A-12})$$

where E_f is the friction energy, given by:

$$E_f = \frac{1}{2} e_f \cdot v_A^2 \quad (\text{A-13})$$

Then:

$$v_A^2 - \left(\frac{A_A}{A_B} \right)^2 v_A^2 = 2g(z_B - z_A) - e_f \cdot v_A^2 \quad (\text{A-14})$$

So, according to equation (A-14), $v_A \approx 1$ m/s.

It is noticed that most of the losses happened in the entrance to the exit nozzle ($e_n = 0.429$). However, if the slit were extended to, for example 20 cm, the losses in the slit will be roughly the same as in the entrance and the velocity at the end of the slit would be approximately 12% lower than in the previous case.

These results were partially confirmed by the work of Scholz et al⁽⁴⁾, who predicted velocities up to 30% lower for flow of liquid steel in a refractory channel. However, a more accurate calculation would require a more precise value of ϵ_r , which should be determined empirically for the type of refractory employed on the front wall.

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APPENDIX B - NOMENCLATURE

Symbol	Meaning
A	Darcy coefficient (-)
A_A, A_B	areas of sections A and B (m^2)
A_1, A_2	constants for equations (8-5) and (8-6) (s^c), (-)
a, b	constants for equation (8-4) ($W/m^3 \cdot K^{-m}$), ($W/m^2 \cdot K^{-m}$)
a_i	coefficients for the discretization equation (-)
B	constant for SDAS calculation (Eq. 6-13) ($\mu m \cdot K^n \cdot s^{-n}$)
b_w	back wall height (m)
C	morphology constant (-)
C_D	coefficient of discharge (-)
C_p	specific heat ($J/kg \cdot K$)
C_s	constant for calculation of SDAS (Eq. 8-10) ($\mu m \cdot s^{-n}$)
C_1, C_2, C_μ	empirical constants for Low-Re turbulent models (-)
D	pore size of the porous medium (m)
D_h	hydraulic diameter (m)
D_κ	extra dissipation term in the κ -equation ($kg/m \cdot s^3$)
Da	Darcy number (-)
d	SDAS in equation (3-4) (mm)
d_g	exit gap (m)
d_p	thickness of the porous medium (m)
d_1, d_2	constants for equations (8-5) and (8-6) (-), (s^{-1})
E	Young's modulus (Pa)
E_f	friction energy (J)

E_ϵ	extra generation term in the ϵ equation (kg/m.s^4)
e_f	friction factor (-)
F	inertial constant of porous media (-)
f_L	liquid fraction (-)
f_S	solid fraction (-)
$f_1, f_2, f_\mu, f_{\mu 1}$	empirical constants for low-Re turbulent models (-)
G_κ	turbulent kinetic energy generation term in κ equation (kg/m.s^3)
g	acceleration of gravity (m/s^2)
H	enthalpy (J/kg)
H_n	hardening coefficient (Pa)
H_k	inlet enthalpy (J/kg)
H_L	enthalpy at the liquidus temperature (J/kg)
H_{ref}	enthalpy at reference temperature (J/kg)
H_S	enthalpy of the solid (J/kg)
h	heat transfer coefficient ($\text{W/m}^2.\text{K}$)
h_m	metal head (m)
h_p	peak heat transfer coefficient ($\text{W/m}^2.\text{K}$)
h_{ref}	sensible heat at reference temperature (J/kg)
h_s	sensible heat (J/kg)
K	empirical coefficient (equation 6-12) ($\text{mm/min}^{1/2}$)
k	thermal conductivity (W/m.K)
k_c	thermal conductivity of the coating (W/m.K)
$k_{\text{eff},f}$	effective thermal conductivity of the fluid (W/m.K)
$k_{\text{eff},p}$	effective thermal conductivity of the porous medium (W/m.K)
k_l	thermal conductivity of the liquid (W/m.K)
k_p	thermal conductivity of the porous medium (W/m.K)
k_s	thermal conductivity of the solid (W/m.K)
k_{subs}	thermal conductivity of the substrate (W/m.K)
L	characteristic length (m)

L_d	computational domain length (m)
L_f	latent heat of fusion (J/kg)
L_s	length of the slit (m)
l	reservoir length (m)
m	constant in equation (8-4) (-)
$\dot{m}$	inlet mass flowrate (kg/s)
n	constant for SDAS calculation in equation (6-13) (-)
Nu	Nusselt number (-)
nz	front wall thickness (m)
P	Pressure (Pa)
Pe	Peclet number (-)
Pr	Prandtl number (-)
Pr_κ, Pr_ϵ	Prandtl number for κ - ϵ equations (-)
Q	volumetric flowrate (m ³ /s)
q	heat flux (W/m ²)
q_p	peak heat flux (W/m ²)
R_c, R_s	thermal resistances to conduction (m ² .K/W)
R_h	reservoir height (m)
R_i	interfacial thermal resistance (m ² .K/W)
R_T	turbulent Reynolds number (-)
R_{th}	total thermal resistance (m ² .K/W)
Re	Reynolds number (-)
S_b	initial superheat (°C)
S_o	Carman's specific surface (mm ⁻¹)
S_ϕ	source term associated to variable ϕ (-)
SDAS	Secondary Dendrite Arm Spacing (μ m)
T	temperature (°C)
T_{amb}	ambient temperature (°C)
T_{bs}	temperature of the bottom surface of the strip (°C)
T_b	thickness of the strip (mm)
T_l	liquidus temperature (°C)

T_{melt}	temperature of the liquid metal ($^{\circ}\text{C}$)
T_{ref}	reference temperature ($^{\circ}\text{C}$)
T_s	solidus temperature ($^{\circ}\text{C}$)
T_{subs}	substrate temperature ($^{\circ}\text{C}$)
T_{tr}	temperature of transformation austenite-ferrite ($^{\circ}\text{C}$)
t	time (s)
t_f	local solidification time (s)
$t_{(h=0.25 h_p)}$	time for $h = 0.25 h_p$ (s)
t_p	time for $h = h_p$ (s)
U, V, W	displacements in directions x , y and z (m)
U_i	displacement in direction i (m)
U_s	superficial velocity (m/s)
u, v, w	velocities in directions x , y and z (m/s)
u_i	velocity in direction i (m/s)
u_{is}	velocity of the solid in direction i (m/s)
u_p, v_p, w_p	velocities in the porous medium region (m/s)
u_s, v_s, w_s	velocity of the solid in directions x , y and z (m/s)
v_A, v_B	velocity at positions A and B (m/s)
v_b	velocity of the belt (m/s)
$ \text{vel}_p $	resultant velocity in the porous media (m/s)
v_{it}	inlet velocity (m/s)
w	work (J)
W	width of the strip (m)
$(W_h/h)^2$	total uncertainty (-)
$(W_i / i)^2$	relative error (-)
$(W_{\text{exp}}/h)^2$	experimental error (-)
$(W_{\text{therm}}/h)^2$	error due to thermocouples correction (-)
x_i, x_j, x_k	coordinates in the Cartesian system (m)
X	thickness of the solid shell (m)
z_A, z_B	position (height) (m)

Greek Symbols

α	coefficient of expansion ($^{\circ}\text{C}^{-1}$)
α_F, β_F	Forchheimer coefficients (-)
α_S	thermal diffusivity of the solid (m^2/s)
β	temperature coefficient of volume expansion (K^{-1})
Γ_{eff}	effective diffusion coefficient (-)
Γ_h	diffusion coefficient for energy equation ($\text{W}/\text{m.K}$)
$\Gamma_{\kappa, \epsilon}$	diffusion coefficient for κ - ϵ equations ($\text{kg}/\text{m.s}$)
δ_c	thickness of coating (m)
δ_{subs}	thickness of substrate (m)
ϵ	rate of dissipation of turbulent kinetic energy (m^2/s^3)
ϵ_d	strain limit (-)
ϵ_i	deformation in direction i (-)
ϵ_{ij}	deformation in plane i-j* (-)
ϵ_{ik}	initial rate of dissipation of turbulent kinetic energy (m^2/s^3)
ϵ_l	emissivity of liquid steel (-)
ϵ_{nom}	nominal deformation (-)
ϵ_p	porosity of the porous medium (-)
ϵ_r	roughness parameter (mm)
ϵ_T	thermal deformation (-)
κ	turbulent kinetic energy (m^2/s^2)
κ_{ik}	initial turbulent kinetic energy (m^2/s^2)
κ_p	permeability of the porous medium (m^2)
λ	SDAS in equation (6-13) (μm)
μ	viscosity ($\text{kg}/\text{m.s}$)
μ_{eff}	effective viscosity ($\text{kg}/\text{m.s}$)
$\mu_{\text{eff},p}$	effective viscosity of the porous medium ($\text{kg}/\text{m.s}$)
μ_f	viscosity of the fluid ($\text{kg}/\text{m.s}$)
μ_t	turbulent viscosity ($\text{kg}/\text{m.s}$)

ϕ	general dependent variable (-)
ρ	density (kg/m ³)
σ_i	tension in direction i (Pa)
σ_{nom}	nominal tension (Pa)
σ_s	yield strength of a material under pure shear (Pa)
σ_{true}	true tension (Pa)
σ_y	yield tension from a material under a uniaxial tension test (Pa)
ν	Poisson's ratio (-)
τ_1, τ_2	time constants of the thermocouples (s)
Υ	Forchheimer's inertia coefficient (-)