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NUMERICAL SIMULATION OF THE SOLIDIFICATION OF A BEAM BLANK IN THE MOULD

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Abstract

The stability of the casting process and the wear of the mould as well as the quality of the product depend strongly on the mould design. This is particularly true when considering the production of beam blanks. If the mould is too tight, thermal contraction can generate considerable contact forces between the product and the mould on the inner surfaces of the flanges. If it is not tight enough, the thermal coupling between the mould and the beam blank is not sufficient to allow the solidification of a skin of the required thickness upon exit from the mould. A finite element model was developed to assist in the mould design. This model takes both the thermal and mechanical interaction between the mould and the product into account. The effect of solidification is implicitly included in the (thermal) finite element formulation while the ferro-static pressure is applied by the means of a specially developed element. The model was applied to a medium gage beam blank cast at ProfilARBED Differdange. The skin thickness as well as the localisation of the wear of the mould predicted by the model are in good agreement with observations made in industrial practice.

1 Introduction

Over the past two decades, continuous casting has almost completely replaced the ingot route in steel production. The main advantage of this technique is a streamlining of the production process, which is associated with large cost savings. After initial problems with both the product quality and the stability of the process, the technology is now considered fully mature.

While the design of moulds for the casting of convex sections such as billets and blooms is fairly straight forward, the moulds used in the casting of dog-bone beam blanks remain more of a challenge to the designer. Considering the cost associated with industrial trials, not to mention the safety risks associated with breakouts, it soon becomes clear that the classical rout of “trial-and-error “ is not viable in the context of continuous casting.

It is hence not surprising that numerous investigators have developed mathematical models for the different aspects of continuous casting. Given the complexity of the process, the validity and applicability of analytical models is very limited and numerical techniques stand a much better chance of success.

One of the major difficulties in establishing a mathematical model for the casting process is the coupling between the mechanical and the thermal processes. In addition to the non-linear thermal and mechanical constitutive equations, this coupling introduces a further non-linearity. Fully coupled thermo-mechanical models have been presented in the literature, see for example CHANDRA *etal.* [1], KELLY *etal.* [2], or, more recently, HUESPE *etal.* [3]. These models are fairly complete and accordingly complex, and seem to produce results, which are fairly accurate.

The guiding principle in the present approach however was pragmatism. Because the model is being used in mould design, it is important that the numerical procedure is both fast and stable. A relatively large number of analysis steps with modified parameters have to be performed, so clearly the trade-off between scientific rigour and reduction of computation time will ere on side of the computation time. It goes without saying that the accuracy of the model has to be sufficient to take design decisions based on model results with confidence.

For all these reasons, a 2-dimensional, semi-coupled model in which the mechanical and thermal model equations are solved sequentially was chosen. This model is described in full detail in PASCON [4]. The approach allows the determination of the temperature field, the stress- and strain fields as well as the contact pressures and the thermal flow between the strand and the mould between the meniscus and the exit of the mould.

2 The model

The model consists of a 2-D slice of the strand taken perpendicular to the casting direction. Generalised plane strain conditions are applied. The thickness of this slice is assumed to be constant in space but variable in time. This allows the slice thickness to be linked to the stress component in casting direction, thus providing a model, which is more complete than a conventional plane strain model without inflating the computational effort unduly.

2.1 Thermal model

The thermal model includes heat conduction in the strand, latent heat of solidification and a set of boundary conditions. Because the velocity field is essentially three dimensional in the liquid steel, energy flow through convection cannot be accounted for explicitly with the present model. However, it can be included implicitly though increasing the thermal conductivity in the liquid steel.

The energy balance equation is formulated in terms of the enthalpy H as

$$\frac{\partial H(T)}{\partial t} = div(\lambda \nabla T) \tag{1}$$

with

$$H(T) = \int_0^T \left(\rho c - \rho L_s \frac{\partial f_s}{\partial \Theta} \right) d\Theta \tag{2}$$

where λ is the thermal conductivity, c the specific heat, ρ the mass density, L_s the latent head of solidification and $f_s \in [0,1]$ the solid volume fraction. In general, the derivative of the solid fraction with the temperature is not known. Therefore, a linear variation was assumed.

The above formulation is known as the enthalpy method. It is well suited for the problem at hand; there is no need for tracking of the solidification front and the method is naturally compatible with materials that solidify over a temperature interval such as steel. Only a description of the enthalpy as a function of the temperature, as given in equation 2, is required. Rather than including this relation in the analysis code, it can be evaluated at a finite number of temperatures and tabulated. Linear interpolation between these values can than be used in the program. It should be noted that all material parameters used in equation 1 and 2 vary with the temperature.

The boundary conditions applied model the heat transfer between the strand and the mould. As long as there is contact between the strand and the mould, the heat exchange is given by

$$q = h_c (T_{strand} - T_{mould}) \tag{3}$$

When contact is lost, a radiation term has to be added to the equation

$$q = h_g (T_{strand} - T_{mould}) + \sigma_{Bolzman} \varepsilon_r (T^4_{strand} - T^4_{mould}) \tag{4}$$

Note that the heat transfer coefficient h changes once contact is lost. In fact, while the h_c is fairly easy to measure, the factor h_g and the relative emissivity ε_r can only be estimated. Note that in the present model, the mould temperature is assumed to be given.

The thermal expansion/shrinkage is assumed to be isotropic and is described by the equation

$$\frac{\partial \varepsilon}{\partial t} = \alpha(T) \frac{\partial T}{\partial t} E_3 \tag{5}$$

where α is the (temperature dependent) coefficient of thermal expansion and E_3 the 3 by 3 unit matrix.

2.2 Mechanical model

The solid phase is modelled as following an elasto-viscoplastic material law. The usual additive decomposition of the strain tensor is applied, e.g.

$$\varepsilon^{total} = \varepsilon^{el} + \varepsilon^{vp} \tag{6}$$

The elastic part of the strain tensor $\boldsymbol{\varepsilon_{el}}$ is given by

$$\varepsilon_{ij}^{el} = C_{ijkl}\,\sigma_{kl}^{el} \tag{7}$$

where $\boldsymbol{C_{ijkl}}$ is the compliance tensor. A Norton-Hoff type constitutive law is used to describe the inelastic behaviour of the steel. The viscoplastic part of the strain tensor is gained through integration of the strain rates given as

$$\hat{\dot{\varepsilon}}_{ij}^{vp} = \frac{\left(J_2\right)^{\frac{1-p_3}{p_3}} . (\bar{\varepsilon})^{-\frac{p_4}{p_3}} . e^{\frac{p_1}{p_3}\bar{\varepsilon}}}{2(K_0 p_2)^{\frac{1}{p_3}}}\,\hat{\sigma}_{ij} \tag{8}$$

where $\boldsymbol{J_2}$ is the second invariant of the deviatoric part of the stress tensor, the superposed ^ denotes the deviatoric part of a tensor component and the superposed ‾ denoted von Mises equivalent values.

The parameters $\boldsymbol{p_{1\text{-}4}}$ and $\boldsymbol{K_0}$ are material dependent and have to be determined from experimental data. They were fitted by means of a least square procedure to data gained from experiments performed on a Gleeble device at temperatures between 800 and 1475°C and at strain rates between 10^{-4} to 5 10^{-2} s^{-1}.

The loading of the model includes contact pressure and friction as well as the ferro-static pressure. The contact pressure is applied by the means of a penalty method and a Coulomb type law is used for the friction. As opposed to conventional plane strain models, the generalised plane strain mode employed here permits inclusion of an out-of-plan friction stress. This can be interpreted as an implicit inclusion of the forces which the casting machine generates in casting direction.

The ferro-static pressure is applied by the means of special loading element. Because the solidification front is not explicitly tracked, this element is applied to the whole analysed domain. The pressure in the element is defined as

$$p_f = \rho g h_m (1 - f_s) \tag{9}$$

with $\boldsymbol{\rho}$ the mass density, $\boldsymbol{g}$ the gravity acceleration, $\boldsymbol{h_m}$ the height of the liquid steel column and $\boldsymbol{f_s}$ the solidified volume fraction. Clearly, expression (9) yields zero in the solidified shell. In the mushy layer, it changes linearly to value normally generated by a fluid column.

2.3 Thermo-mechanical coupling

Both the thermal and the mechanical model equations are solved by the means of a finite element method. The mechanical and thermal analyses are separated. Coupling is achieved though exchanging data at a finite number of points in time, that is, the geometry computed in the mechanical analysis (which defines the contact conditions between the strand and the mould) are fed into the thermal analysis. In the thermal analysis, a new temperature field is computed, which in turn is fed back into the mechanical analysis, where the associated thermal strains are computed. In addition, the mould shape is adapted as the model slice travels down the mould to account for the mould taper. Clearly, the more frequently data are exchanged, the closer the coupling and the more accurate the results.

2.4 Boundary conditions

The geometry of the mould evolves with time as prescribed by casting-speed and mould taper. The mould is assumed to be rigid, i.e. the pressure of the strand does not cause the mould to deform. Furthermore, the surface temperature of the mould is prescribed. The interaction between the mould and the strand is modelled through a contact condition.

3 Example

The model was applied to a medium gauge beam blank, termed the BB0, cast at ProfilARBED Differdange. The outer measurements of the BB0 are 500 by 400 mm and it weights approximately 900kg/m. For this section, accurate data about the solidification front are available from a breakout that occurred in the secondary cooling chamber several meters below the mould. This breakout was caused by a hard object that got caught between the strand and a support roller, and it can therefore be assumed that the process was running in its normal state when the breakout occurred. The empty skin was cut into several slices and the thickness of the skin was measured at different points along the periphery.

3.1Parameters

The mould taper on the outside and on the tips of the flanges is approximately 1%. On the inside of the flanges, the mould has a double taper of approximately 1.5% in the top part and 0.5 in the bottom part of the mould. The normal casting speed is 1 m/min for this product and the casting temperature is 20K above liquidus. For the modelled steel, the solidus temperature is 1743K and the liquidus temperature 1793K. It was assumed that the surface temperature of the mould is uniform and constant at 433K. Numerical experiments have confirmed that variations of mould temperature by 40K do not lead to a major change in the results.

3.2 Results

Solidification: As mentioned earlier, the solidification front is not explicitly tracked in the present model. Rather, it can be determined from the calculated temperature field through tracing the iso-lines of the solidus and the liquidus temperature. Figure 1 shows a plot of such iso-lines, located at well-defined positions below meniscus level

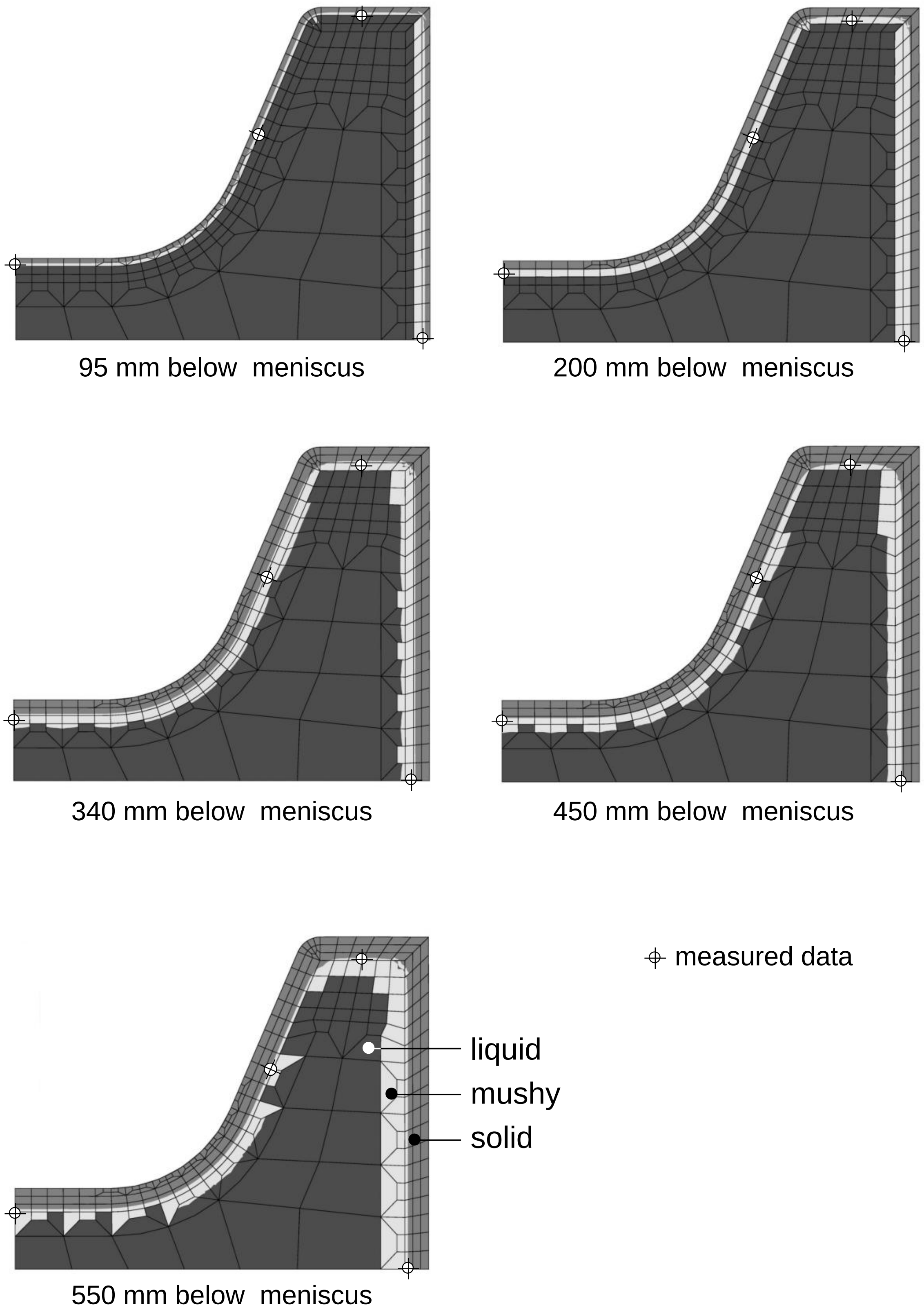

Figure 1: Solidification front at different positions in the mould

to co-inside with the slices of the solidified shell taken from the breakout. It may be assumed that all of the liquid and a part of the mushy material is lost from the solidified shell at the breakout. The thickness of the shell is hence somewhat larger that the solid layer, but does not reach the liquid domain. The figure clearly shows that the measured points from the sliced shell are all located within the (computed) mushy layer. It can hence be concluded that the model results are fairly accurate.

Surface temperature: The surface temperature of the beam blank in the mould is shown in Figure 2. It can be seen that the temperature drops to the solidus temperature almost immediately upon contact with the mould. The temperature decreases along casting direction to about 1300K at the exit of the mould. Only the edges of the flanges are markedly colder at about 1000K. These observations tie in well with field observations, although no accurate measured data are available due to the in-accessibility of the strand.

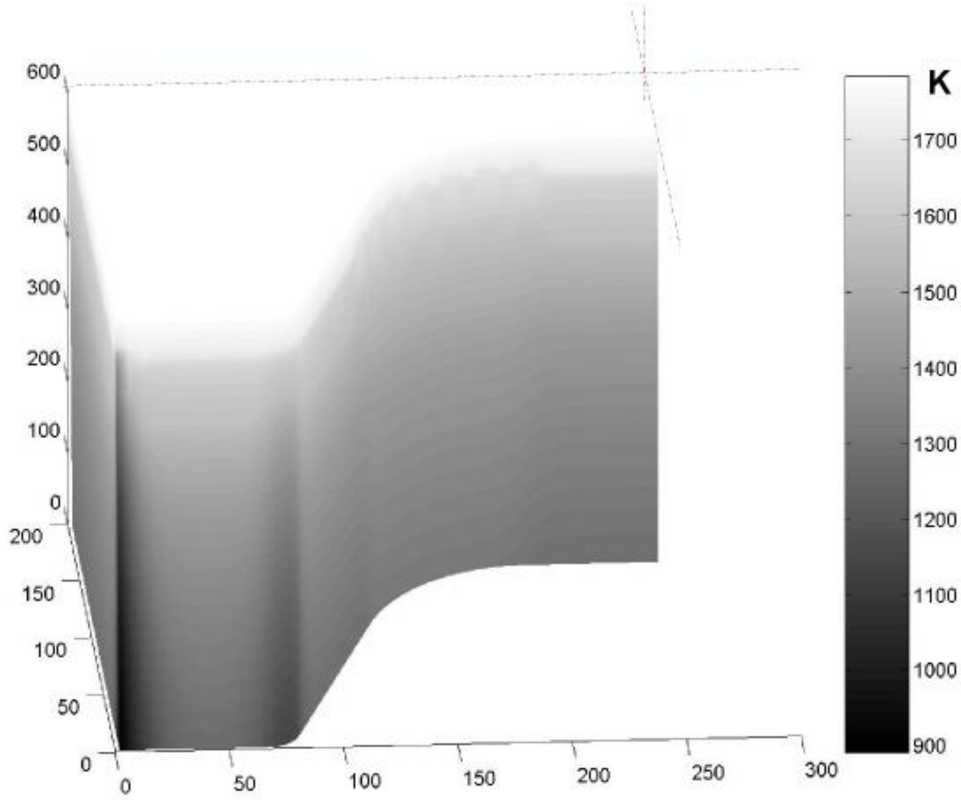

Figure 2: Surface temperature of the strand

Pressure: Figure 3 shows the surface pressure on the beam blank. A maximum of about 4MPa is found along the fillet linking the flange to the web at the level where the mould taper changes. Note that the oscillation of the pressure field (perpendicular to the casting direction) corresponds to the discretisation of the mould, i.e. it can be assumed that the field is smooth, but carries some noise generated by the approximation of the geometry.

One might assume that the maximum of the pressure would correspond to the maximum of the mould-wear. However, at the location of the pressure maximum, that is about 200mm below the meniscus, the surface temperature of the strand is still well in excess of 1500K. At this temperature, it is save to assume that the mould powder (flux) is liquid and the friction between the mould and the strand is therefore minimal. To assess where the mould is most likely to wear, only the lower halve of the mould is considered, because here the surface temperature of the strand is below 1500K. The pressure distribution on the lower half of the mould is shown in Figure 4. Pressure concentrations rising to between 2 and 2.5 MPa are found on the fillet between the web and the flange. This location corresponds exactly to where the mould wears most in industrial practice.

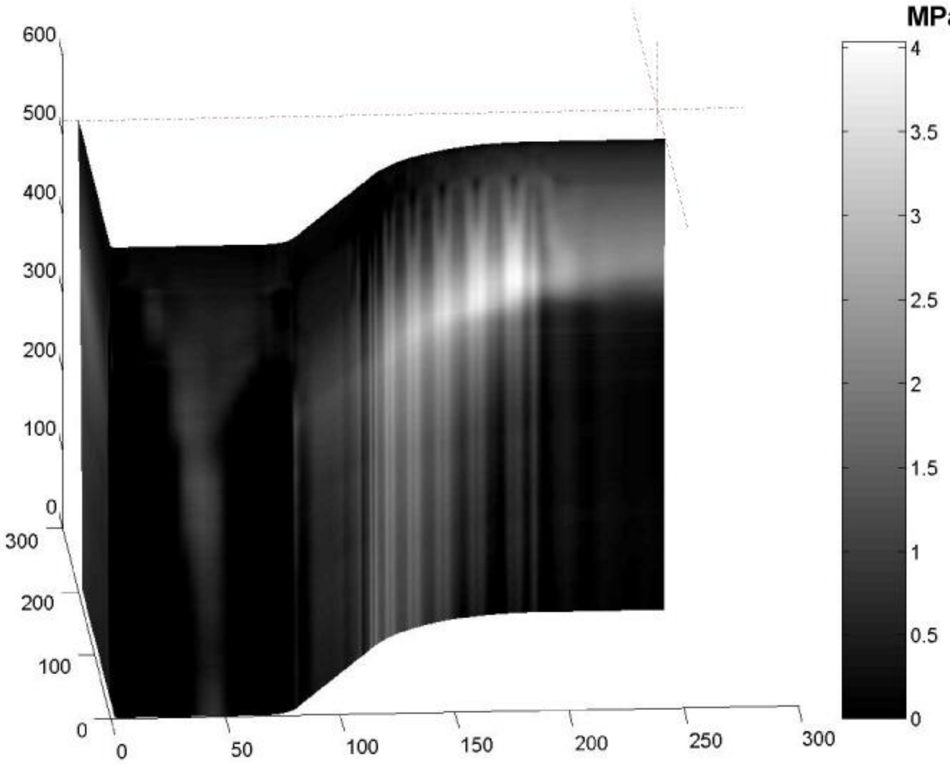

Figure 3 Pressure distribution in the mould

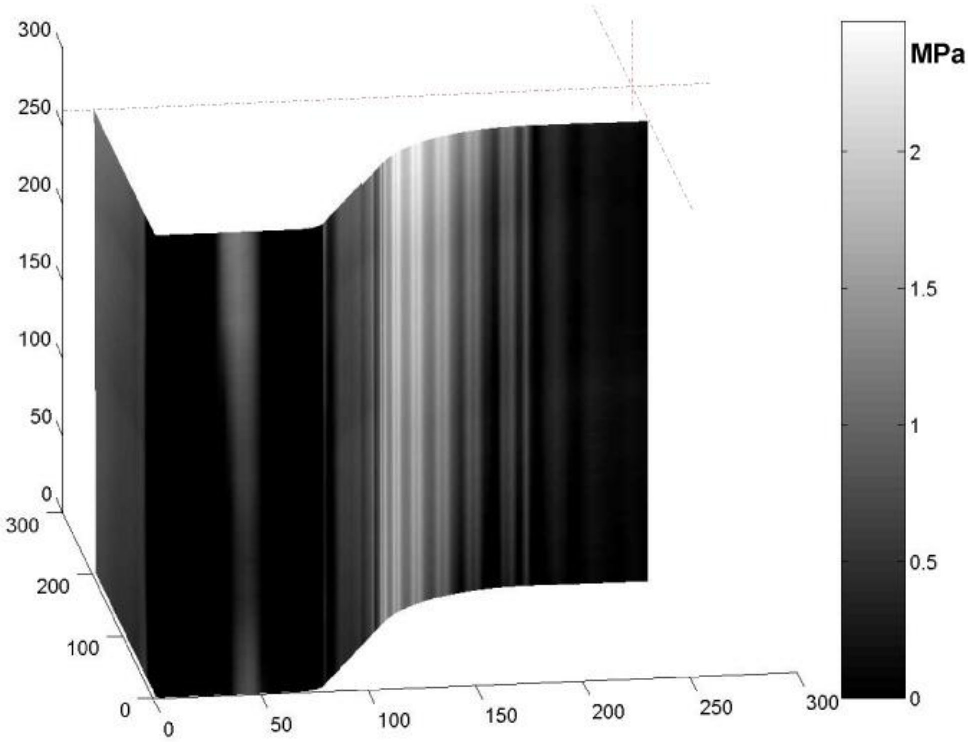

Figure 4: Pressure distribution in the bottom halve of the mould

Conclusions

A finite element based thermo-mechanical model for the solidification of the strand in the mould of a continuous caster was presented. The architecture of the model is very simple, leading to a robust procedure. The data exchange between the mechanical and the thermal model takes place at discrete, pre-defined points in time. This has lead to some concern about the accuracy of the results. However, the comparison between model results and field observations show very close agreement, and it can therefore be assumed that the produced results accurate.

References

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