HEAT TRANSFER MEASUREMENTS DURING DC CASTING OF ALUMINIUM

PART I: MEASUREMENT TECHNIQUE

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A method for determination of surface heat trans-
fer to the cooling water and mould based on in-situ
temperature measurements in the DC cast ingot has been
developed. Three or more steel mantled coaxial thermo-
couples (0.5 mm diam.) are mounted on a wire frame
called a "harp". Allowing the "harp" to freeze into
the solid ingots during the casting time-temperature
plots $T_1(t)$, $T_2(t)$, $T_3(t)$ are obtained for three mov-
ing points positioned typically 3, 7 and 11 mm from
the ingot surface. From these measurements surface
temperature, heat flux and heat transfer coefficients
are computed as functions of vertical distance. The
computer program is based on steady-state two-dimen-
sional heat balances with convective terms for two
fixed volume elements: one around thermocouple T_1
and one surface element. A special numerical smooth-
ing procedure is incorporated. The heat of solidifi-
cation is taken into account.

INTRODUCTION

Reliable heat transfer data are essential as
boundary conditions in the more sophisticated mathe-
matical models developed during the last two decades
for simulation of the semocontinuous DC casting pro-
cess. The more advanced numerical models solve the
steady state or transient heat flow differential equ-
ation with temperature dependent thermal properties
for two (1-3) or three (4) dimensional geometries.

Generally speaking, computer simulation of a
casting process is aimed at i) increasing the under-
standing of how the solidification phenomena are
affected by the temperature-time histories of differ-
ent parts of the ingot, ii) optimizing the relevant
operating parameters included the mould depth for a
particular ingot size and alloy composition, and even-
tually, iii) process control by an on-line process
simulator, particularly in the critical transient
period after start-up. Thus, optimalization of DC
casting of aluminium requires precise knowledge of
the surface heat flux distribution in the secondary
cooling zone below the mould, as well as in the water-
cooled mould itself.

Relatively little work seems to have been done
in the past on experimental determination of heat
transfer coefficients pertaining to the secondary
cooling zone (5). Here the hot ingot surface is cool-
ed by direct contact with a vertical water film flow-
ing down the ingot. The water film is usually formed
by separate jets emerging from an array of nozzles or
holes through the bottom part of the mould, and

inclined typically 15-30° to the vertical surface.

On the other hand, water spray cooling as used
in the continuous casting process for steel has been
studied quite extensively. Here, the hot surface is
hit by discrete, fast moving water droplets.

To get the necessary heat transfer input data
required to simulate DC casting of aluminium one has
taken recourse to semi-empiric formulaes referring to
pool-boiling conditions, water spray cooling or heat
transfer in tubes.

The secondary cooling zone can be divided in at
least three sub-regions: the two narrow regions just
above and below the line where the water jets hit the
ingot surface, and where the flow field is probably
rather complicated, and the hydrodynamically fully
developed region further downstream.

Extensive experimental studies by one of the
authors indicate that the heat transfer rates vary
considerably from one sub-region to another (6). In
these cooling experiments thin (∿ 10 mm) aluminium
plates with the rear face well insulated were heated
to above 600°C. The test plates were then suddenly
exposed to an array of inclined water jets forming a
water film on the exposed front face. From the mea-
sured temperature-time history of the insulated sur-
face the temperature and heat flux on the cooled sur-
face were calculated as functions of time assuming
one-dimensional heat conduction.

Although the geometry of the cooling system and
the water flow rates were the same as in industrial
DC casting, the direct application of laboratory mea-
surements of this type to DC casting is, however,
rather dubious. The reason is that the cooling rates
(°C/s) are very much higher than under industrial con-
ditions, and one can probably not assume that the
various heat transfer mechanisms (stable and unstable
film boiling, nucleate boiling) are active in exactly
the same temperature intervals irrespective of the
cooling rate. In other words: it is probably not justi-
fied to assume that the "boiling curve", i.e. the
relationship between heat flux (or heat transfer coeffi-
cient) and surface temperature is uniquely defined by
the external cooling condition: water flow rate per
unit length ingot circumference, water jet geometry
and angle of inclination, etc.

It was therefore decided to try a more direct
approach, where the surface heat transfer rates are
determined from in-situ temperature measurements during

the actual industrial casting process.

In previous works temperature measurements using cast-in thermocouples have apparently only been done in order to check the results of computer simulations of the casting process - in particular to verify or adjust the heat transfer data or surface temperature distributions used as boundary conditions (2, 7).

The method described in this paper is based on a moving temperature measuring device (called a "harp") embedded in the ingot, and a computer program ("Harpcomp") which solves for the local surface temperature T_o, the local heat flux $\dot{q}_o$ and the heat transfer coefficient $h = \dot{q}_o/(T_o - T_w)$, where T_w is the local water film temperature.

The experimental approach is Lagrangian: the three thermocouples of the harp (i = 1,2,3) register the temperature $T_i(t)$ in a coordinate framework that moves with the metal being cast, i.e. with the casting speed v_z . Thus, the thermocouples, which are situated typically 3, 7 and 11 mm from the ingot surface, first pass through the solidification region between the liquidus and solidus isotherms before entering the solid region.

Steady-state conditions with fully developed liquidus and solidus profiles are assumed. Thus, the moving thermocouples measure the axial temperature profiles $T_i(z)$ in three different radial distances x_i from the surface.

PRINCIPLES

Figure 1 shows the physical system considered in the calculations. The local surface temperature $T_o(z)$ of the ingot is calculated from a steady-state heat balance for a stationary circular volume element with height Δz corresponding to the length of casting per sampling interval Δt:

$$\Delta z = v_z \Delta t \Rightarrow \left(\frac{\partial T}{\partial z}\right)_{R_i} = \frac{1}{v_z} \frac{dT_i}{dt} \qquad (1)$$

The outer boundary R_{o1} of this element is positioned midway between the surface R_o and the point R_1 where the temperature T_1 is measured, while the inner boundary R_{12} lies midway between R_1 and R_2.
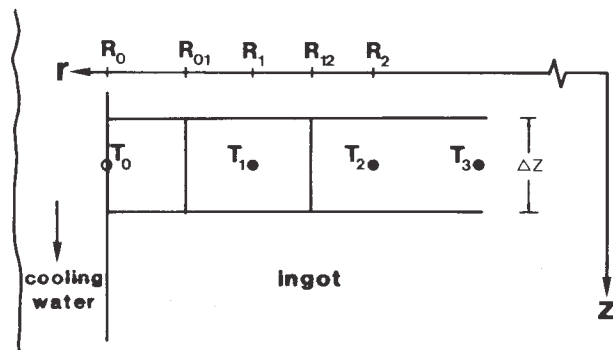

Figure 1. Geometry and nodal system employed to predict temperature, heat flux and heat transfer coeffi-

The steady-state assumption implies that the measurements should not be done before the sump profile is fully developed. The governing heat conduction equation can be expressed as

$$\rho\, c_p v_z \frac{\partial T}{\partial z} = \frac{\partial}{\partial z}\left(k\frac{\partial T}{\partial z}\right) + \frac{1}{r}\frac{\partial}{\partial r}\left(r\, k\frac{\partial T}{\partial r}\right) \qquad (2)$$

where ρ is the density, c_p is the specific heat capacity and k is the thermal conductivity. The left hand side accounts for convective heat transfer caused by the motion of the ingot metal relative to the fixed volume element. The right hand side accounts for axial and radial heat conduction.

The partial differential equation (2) is now integrated over the finite control volume shown in figure 1. We neglect the spatial variations of the thermal conductivity caused by temperature gradients in the control volume. The small dimensions of this volume (typically 4 mm in the radial direction and 1 mm in the axial direction) ensure that it is well justified to assume that the thermal properties k, ρ and c_p are constant in each individual volume element. On the other hand, because of the large temperature span encountered during the casting process, i.e. from the liquid metal temperature to the cooling water temperature, one has to take into account the temperature dependence of the thermal properties when proceeding from element to element in the axial direction, and to the outer surface volume element (see below). It is assumed that k, ρ and c_p are linear functions of the temperature, e.g. $k = k_o^p + k_1 T$, where k_1 is the temperature coefficient. Thus, k, ρ and c_p in eq. (2) are assumed equal to their values at the nodal temperature T_1. The result of the integration becomes:

$$\left(r\frac{\partial T}{\partial r}\right)_{R_{o1}} - \left(r\frac{\partial T}{\partial r}\right)_{R_{12}} = \frac{\Delta A_1}{2\pi}\left[\frac{v_z}{\alpha}\left(\frac{\partial T}{\partial z}\right)_{R_1} - \left(\frac{\partial^2 T}{\partial z^2}\right)_{R_1}\right] \qquad (3)$$

where $\alpha = k/\rho c_p$ is the thermal diffusivity evaluated at temperature T_1 and $\Delta A_1 = \pi(R_{o1}^2 - R_{12}^2)$.

The temperature gradients in the axial direction are calculated in a special smoothing procedure (see below), which smooths the measured axial temperature profiles $T_1(z)$, $T_2(z)$ and $T_3(z)$. A third order collocation polynomial is fitted to the measured temperatures T_1, T_2 and T_3 and the unknown surface temperature T_o. This polynomial is used to express the radial temperature gradient in eq. (2) and one ends up with an algebraic equation, which determines $T_o(z)$.

The local heat flux $\dot{q}_o(z)$ from the ingot surface can now be determined from a heat balance for the outer volume element, where the nodal temperature profile $T_o(z)$ is known from the previous calculations:

$$\dot{q}_o = -\frac{R_{o1}}{R_o}k\left(\frac{\partial T}{\partial r}\right)_{R_{o1}} - \frac{\Delta A_o}{2\pi R_o}\frac{k}{1}\left[\frac{v_z}{\alpha}\left(\frac{\partial T}{\partial z}\right)_{R_o} - \left(\frac{\partial^2 T}{\partial z^2}\right)_{R_o}\right] \qquad (4)$$

where k and α are evaluated at the surface temperature T_o, and $\Delta A_o = \pi(R_o^2 - R_{o1}^2)$. It has been found desirable to apply the smoothing procedure to $T_o(z)$ before it is used for calculating $\dot{q}_o$.

We assume that the heat flux $\dot{q}_o$ leaving the ingot surface is completely absorbed by the cooling water film, and that turbulent mixing preserves uniform temperature normal to the flow direction. The local water temperature $T_w(z)$ is determined from

$$\frac{dT_w}{dz} = \frac{\dot{q}_o}{\Gamma_w \, c_{pw}} \qquad (5)$$

where Γ_w is the mass flow rate per unit ingot perimeter [kg/m·s] and c_{pw} the specific heat of water.

The local heat transfer coefficient $h(z)$ is defined with reference to the calculated cooling water temperature:

$$h = \dot{q}_o/(T_o-T_w) \qquad (6)$$

To calculate the correct surface temperature and heat flux in the solidification region one has to incorporate the latent heat of solidification H_m. This is accomplished by adjusting the temperature dependent specific heat between solidus and liquidus so that the specific heat c_p integrated from T_{sol} to T_{liq} equals H_m. The average or effective specific heat to be used in eq. (3) when the inner volume element is intersected by the solidus isotherm depends on the fraction of partly molten metal above T_{sol}. This fraction is estimated by interpolating between T_1 and T_2 if $T_1 < T_{sol} < T_2$, and between T_1 and the unknown surface temperature T_o if $T_o < T_{sol} < T_1$. In the last case, an iterative procedure must be applied to calculate successive approximations to c_p and T_o.

EXPERIMENTAL PROCEDURE

The harp consists of a U-shaped steel frame with dimensions as shown in figure 2. Three or more 0.5 mm o.d. steel mantled type K thermocouples are butt welded to steel wires and stretched out between the legs of the frame in roughly the same way as the strings of a harp. In order to ensure fast respons the thermocouple junctions are in direct metallic contact with the steel mantel and the surrounding aluminium metal. The thermocouple leads are thread through a 5 mm o.d. steel tube ($\sim$ 1 m long) fastened perpendicularly to the rear of the frame, and connected to a data logger via cold junctions in an isothermal block.

The calibrated harp is lowered into the molten metal pool in the mould, where it is held for about half a minute to obtain thermal equilibrium before the temperature measurements are started. The next step is to position the harp immediately below the hot-top ring. To facilitate correct orientation of the harp a steel bar is mounted perpendicularly to the upper end of the tube. An "anchor" fastened to a steel wire passing through the tube is pushed down and allowed to freeze into the solidified metal at the bottom of the sump. The harp assembly starts to move when the curved tip of the wire reaches the upper end of the tube. This event is recorded by transmitting an electric pulse to the data logger. Further down a second pulse is recorded when a mark on the harp tube passes a fixed reference point. Figure 3 shows the experimental set-up with the harp moving downwards with the speed v_z of the ingot.

Ideally, the thermocouple junctions should have been precisely positioned in predetermined points equally spaced along a radius, i.e. $x_i = i \, \Delta x$ and $z_{i \, rel} = 0$. In actual practice, unfortunately, this is virtu-

ally impossible due to the expansion of the harp body and the thermocouple leads when the harp is immersed in the hot melt. It is also very difficult to ensure that the leads are oriented exactly horisontally. We therefore have to go through the labourious task of determining the thermocouple positions after each experimental casting, i.e. the radial distances $x_i = R_o - R_i$ from the ingot surface and the relative axial displacements $z_{i \, rel}$ must be measured exactly. The harp is first located by means of ultra sound or x-ray photography, and a conveniently sized slice containing the harp is removed from the ingot. By carefully machining this slice the thermocouple junctions appear one after the other, and their coordinates are measured using standard tools.

The harp should also be placed as close to the surface as possible, but here one must compromize in order to reduce the risk of accidentally rupturing the ingot surface.
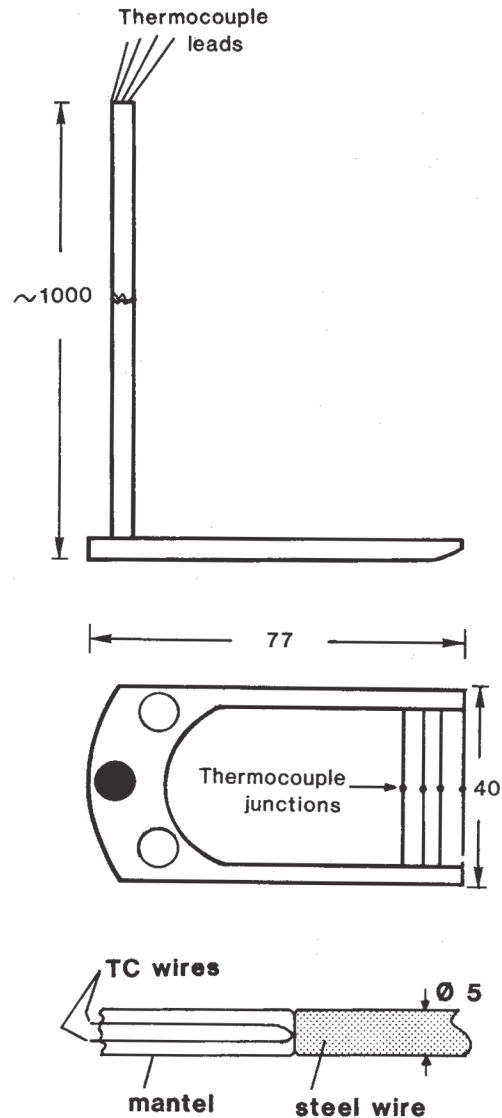

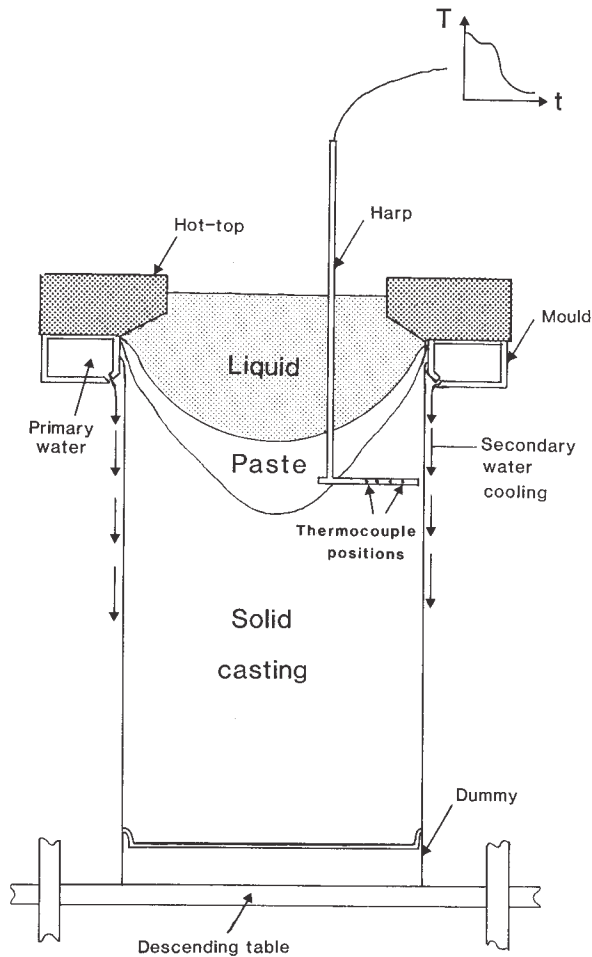Figure 2. The harp assembly with thermocouple junction.

Figure 3. Schematic drawing of continuous DC casting showing the immersed harp.

COMPUTER PROGRAM

HARPCOMP is an interactive numeric program for calculation of surface heat transfer data from in-situ harp measurements of two or three axial temperature profiles during steady-state DC casting. The input parameters which must be specified are: 1) the diameter of the ingot (if cylindrical), 2) the positions of the thermocouples - i.e. radial and axial coordinates $(x_i, z_{i\ rel})$ 3), the casting velocity, 4) the liquidus and solidus temperatures and the heat of solidification of the alloy being cast, 5) the thermal properties k, ρ and c_p of the solid as well as the liquid metal and their linear temperature coefficients, 6) the water mass flow rate and the initial water temperature, and 7) the sampling interval used in the harp measurements.

The program output includes: 1) the smoothed versions of the measured temperature profiles T_1, T_2 and T_3 (corrected for axial displacements of the thermocouples),2) the calculated surface temperature T_o, 3) the surface heat flux $\dot{q}_o$, 4) the heat transfer coefficient h, and 4) the cooling water temperature T_w for each axial position z.

The calculation of second derivatives of a signal subject to irregular white noise is a difficult task,

and it has been found necessary to apply a smoothing of the experimental T_1-, T_2- and T_3-profiles. This is done with a cubic spline algorithm adopted from H. Späth (8). The cubic spline coefficients are expressed in terms of the axial second derivatives d^2T/dz^2, which are determined by solving a pentadiagonal symmetric algebraic set of equations with positive definite coefficient matrix. A smoothing parameter p has to be prescribed. When p is large, we get pure interpolation between the data points, and there is no smoothing effect. When p is small, the present smoothing algorithm approaches fitting of a single linear relation to the complete set of data points by the method of least squares.

An alternative smoothing procedure is the standard method of gliding polynomial fit of e.g. cubic splines to a prescribed number of data points (> 4). This method, however, seems to be inferior to the algorithm described above.

RESULTS

To demonstrate the usefulness of the Harp Method a few examples only will be given of results obtained with 178-229 mm diameter extrusion billets and 1600 x 600 mm rolling ingots of Al-Mg-Si-Fe alloys. The harp measurements were done at three different aluminium plants with moulds of various designs.

Figures 4 and 5 show the experimentally determined surface temperature T_o, heat flux $\dot{q}_o$ and heat transfer coefficient h as functions of vertical distance z for an 178 mm extrusion billet cast at 2 mm/s. In the upper zone (z → 0), where the solidifying alloy is in direct contact with the water cooled mould, the surface temperature first decreases. As the solidification proceeds and the ingot contracts an air gap is formed, and the surface heat flow becomes very small. The temperature profile flattens out and usually also increases as shown in Figure 4. If the mould is too deep, remelting in the air gap zone can occur. When the ingot emerges from the mould the influence of the secondary cooling water is seen to be strong even several millimeters above the water impingement line. The surface temperature decreases rapidly through the secondary cooling zone before it flattens out below about 100°C. Correspondingly, the surface heat flux is very high, reaching a sharp maximum near the water impingement line. Unstable film boiling could occur in the upper part of this zone where the surface temperature is as high as 350°C.

Figure 6 shows corresponding values of the surface temperature and the surface heat flux. From the T_o-$\dot{q}_o$-curve, which should not be interpreted as a "boiling curve" for a water-cooled aluminium surface, it is seen that the maximum heat flux $\dot{q}_{o\ max}$ appears at almost the same temperature $T_{o\ max}$ as in forced convection nucleate boiling. The locus of the maximum $(T_{o\ max}, \dot{q}_{o\ max})$ depends on the cooling water flow rate, the spray geometry and the subcooling (i.e. $100°C-T_w$). Some results are given in Figure 7, which shows that $\dot{q}_{o\ max}$ increases with decreasing $T_{o\ max}$ for extrusion as well as rolling ingots.

As the casting speed is increased the cooling water has to extract more heat from the ingot and the maximum surface heat flux $\dot{q}_{o\ max}$ becomes higher as indicated in Figure 8.
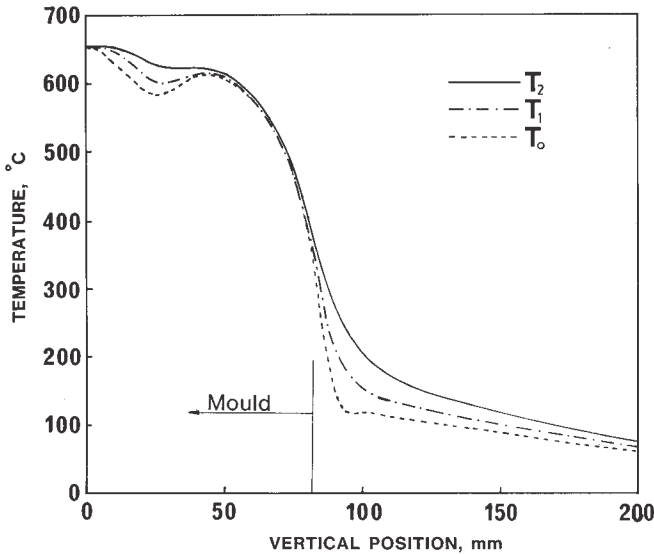
Figure 4. Temperature profiles in an extrusion ingot 178 mm ϕ cast at 2.0 mm/s. T_1 and T_2 are measured temperatures at $x_1 = 2.7$ mm and $x_2 = 7.1$ mm, respectively. T_o is the calculated surface temperature.
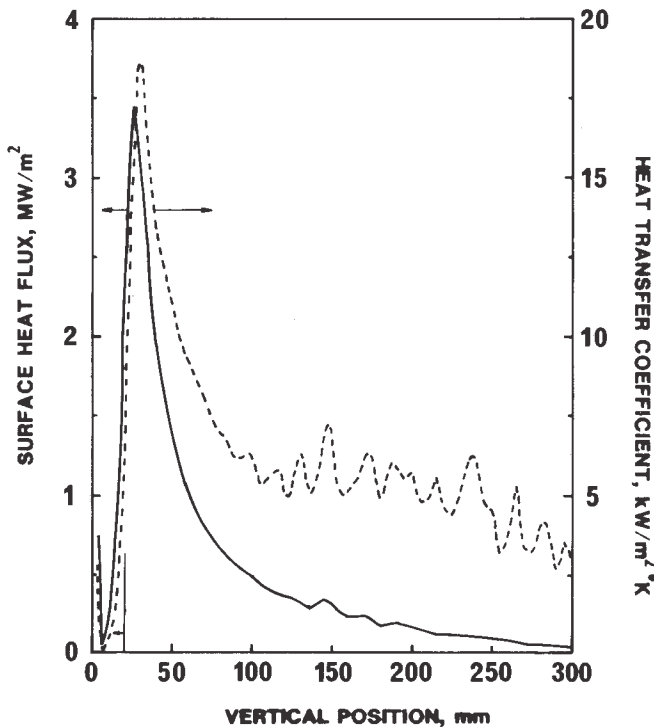

Figure 5. Calculated surface heat flux and heat transfer coefficient profiles for an extrusion ingot 216 mm ϕ cast at 1.67 mm/s.

In part II of this paper further results from DC-casting of extrusion ingots are reported. Heat transfer coefficients determined from harp measurements are used as input data in the computer simulation model Dystal (3). Axial temperature profiles $T_1(z)$ and $T_2(z)$ predicted by Dystal for the same mould geometry, casting speed v_z, water flow rate Γ_w, etc., are compared with measured T_1- and T_2-profiles, and Dystal predicted surface temperatures are compared with $T_o(z)$ determined from harp measurements.

A modified version of the harp method has been applied to uni-directional solidification of stagnant aluminium in a rectangular mould (9). A stationary harp measured the temperature variations in two points near the spray-cooled bottom face of the ingot. The surface temperature and heat flux were calculated as functions of time assuming one-dimensional heat conduction. Thus, the harp method could be useful for determining heat transfer data in batch casting processes.

DISCUSSION

Principal error sources inherently associated with the harp method are: a) The presence of the thermocouple leads, and – probably less serious – the harp frame, creates disturbances in the temperature field around the points of measurements. b) The finite radial dimensions, i.e. $R_o - R_{o1}$ and $R_{o1} - R_{12}$, of the volume elements for which heat balances are solved. The axial dimension Δz can be made arbitrarily small by choosing a small sampling interval Δt.

Errors of type a and b are difficult to assess quantitatively. They can only be diminished by using mantled thermocouple leads of the smallest available diameter, and by positioning them as close to the ingot surface as practically possible. With the "large caliber" 0.5 mm thermocouple leads used in this work it is probably not worthwhile to reduce the radial distances much below $\Delta x \cong 2$ mm due to the problem of measuring the exact positions.
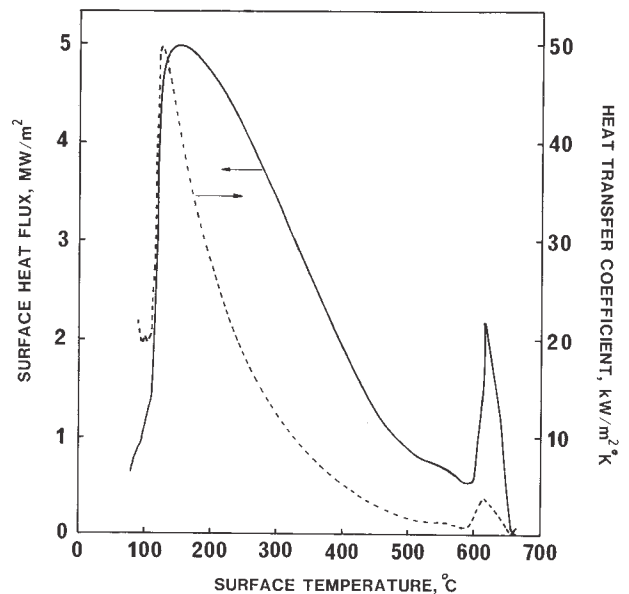

Figure 6. Surface heat flux and heat transfer coefficient versus surface temperature for a rolling ingot 600 mm x 1600 mm cast at 1.33 mm/s.
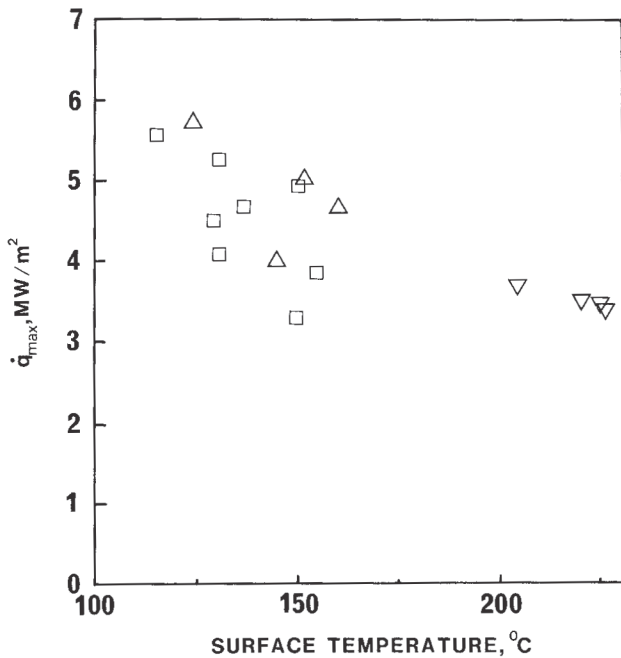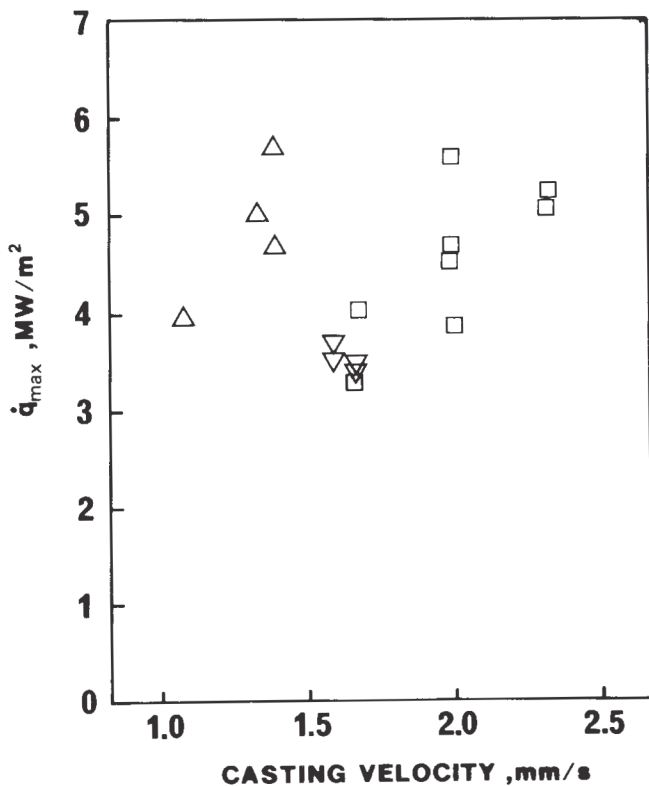
c) Axial contraction of the solidifying ingot implies that it is not quite correct to assume that the velocity v_z and $\Delta z = v_z \Delta t$ are constants corresponding to the nominal casting speed, i.e. the speed of the descending "table". Thus, the axial velocity in the mould zone could be 2 % higher than the casting speed. The axial conduction term $\partial^2 T/\partial z^2$ in eq. (3) is therefore somewhat overestimated and the calculated heat flux $\dot{q}_o$ becomes too <u>low</u> in the zones where the axial temperature profile has a negative curvature – in particular in the lower air gap zone where some reheating of the ingot surface takes place.

During the motion the outer part of the ingot starts to contract before the central part. The $z_{i\ rel}$ values measured in the cold ingot are therefore not quite representative for the actual $z_{i\ rel}$'s in the mould zone.

Radial contraction – typically 1-2 % – has no significant effect on the calculated surface temperature and heat flux.

Practical experimental error sources are:

d) The uncertainty involved in the determination of the thermocouple positions $(x_i, z_{i\ rel})$ as mentioned above. Thus, an error of 0.5 mm in the axial displacement $z_{2\ rel}$ has a marked influence on the calculated heat flux $\dot{q}_o$ – particularly in the air gap zone and near $\dot{q}_{o\ max}$. A possible improvement would be to use the smallest possible mantled thermocouple leads now available, i.e. 0.25 mm, and establish the junction by squeezing the mantel midway between the harp legs, thus short-circuiting the internal wires.

e) Erroneous calibration of the thermocouples – in particular when the calibration errors are different. In this context deviations of $\pm 1^oC$ have no significant influence on the calculated heat flux.

f) Externally generated electric noise on the thermocouple signals and "digital noise" associated with the finite resolution of the AD convertor. High frequency noise is efficiently filtered out by the smoothing applied to T_1, T_2 and T_3.

g) The maximum error in determining the start position of the thermocouples is approximately ± 1 mm at a casting speed of 1 mm/s. The effect of this error is a corresponding displacement of the surface temperature and heat flux profiles.

CONCLUSION

As will be apparent from part II of this paper, the axial temperature profiles T_o (calculated) and T_1-T_2 (measured) are well reproduced by the DC casting simulation model Dystal using heat transfer coefficients determined by the Harp Method described above. It is therefore concluded that the Harp Method is a promising tool for evaluating relevant heat transfer data for simulation of the DC casting process.

Figure 7. Maximum surface heat flux and corresponding surface temperature in the secondary cooling zone.
□ : 178 mm φ, $1.9 < \Gamma_w < 3.7$ kg/m·s,
▽ : 216 mm φ, $\Gamma_w \sim 2.0$ kg/m·s, Δ: 600 x 1600 mm,
$1.9 < \Gamma_w < 4.7$ kg/m·s.

Figure 8. Maximum surface heat flux in the secondary cooling zone versus casting velocity.
□ : 178 mm φ, ▽: 216 mm φ, Δ: 600 x 1600 mm.

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