

WATER COOLING IN DIRECT CHILL CASTING : PART 1, BOILING THEORY AND CONTROL

J.F. Grandfield*, A. Hoadley*, S. Instone**

*Comalco Research Centre

15 Edgars Rd.

Thomastown, Victoria 3074, Australia

**Department of Mining & Metallurgical Engineering

The University of Queensland

Brisbane, Queensland 4072, Australia

Abstract

The intensity of the water cooling in direct chill casting affects the process heat flow and therefore performance of the casting process. The intensity depends on the boiling behaviour on the ingot surface. The basic mechanisms of boiling, the different boiling modes (nucleate, unstable film and stable film boiling) and application to DC casting are discussed. The published effects of water compositional and physical factors (flow rate, temperature etc) on cooling intensity and the methods used to measure these effects are reviewed. A newly developed technique for quantifying heat transfer coefficients and heat fluxes for DC water sprays and new results on the effect of composition, temperature and flow rate are reported. Practical implications for controlling the effect of water cooling on DC casting are suggested.

1. Introduction

Features of interest in DC casting such as the occurrence of cracks, ingot shape and dimensions, microstructural refinement, macrosegregation and surface condition all depend on the temperature distribution during casting. The temperature distribution depends on the heat flow controlled by casting speed, ingot size, alloy thermal conductivity and the intensity of the water cooling as measured by the heat transfer coefficient (h). Variation in water cooling intensity can adversely affect the process, and therefore to control DC casting, the factors affecting water cooling must be understood.

The water spray heat transfer is controlled by the boiling behaviour of the water on the hot ingot surface. In this paper the basics of boiling heat transfer are briefly covered and the specifics relating to DC casting are reviewed. The effects of changes in the water cooling on DC casting will be examined in Part 2 (1).

2. Fundamentals of Boiling Heat Transfer

Boiling heat transfer has been studied for something like three centuries, consequently the volume of literature on boiling heat transfer is enormous and somewhat daunting. However, the basics of water cooling with boiling under forced convection are well described in standard heat transfer texts (see references (2, 3, 4) for example). The description of boiling modes below is based on that from Rohsenow (2).

2.1 Various types of Boiling

Boiling is usually categorised into various types, and analysis usually begins with so called pool boiling with saturated liquid. In this case the liquid is at the saturation temperature and there is no convection of the liquid. Then the case of subcooled liquid boiling where the liquid is

below the saturation temperature is examined and finally the case with convection of the liquid. DC casting water cooling belongs to a special case, which is falling film convection subcooled boiling.

2.2 The boiling curve and boiling modes

The heat transfer is strongly dependent on the temperature of the cooled surface. In the DC case, this is the ingot surface temperature. This dependence is usually described using a boiling curve (Figure 1). By electrically heating some wire or plate in saturated liquid the steady state heat flux q'' can be measured against the surface temperature of the heater. This is usually plotted as a log log plot with the x axis as surface temperature minus saturation temperature. As the heat input is raised the surface temperature goes up and at some superheat, bubbles begin to nucleate on the heated surface (B in Fig.1). This is the nucleate boiling regime. As the heat input increases, more and more bubbles nucleate until at some critical surface temperature a maximum heat flux is reached called the critical heat flux (q_{CHF}) (C in Fig. 1). At this point there are too many bubbles nucleating to allow the liquid to wet the surface. If the heat input is maintained, the next stable surface temperature is at E'. Film boiling occurs where a stable vapour layer separates the liquid from the heater. In many applications this results in the heater melting. Hence, there is much interest in predicting q_{CHF} . On cooling down there is a hysteresis in the curve where the surface temperature can jump from point D to point F. In the intermittent region of partial film boiling the mode may alternate between nucleate and film boiling.

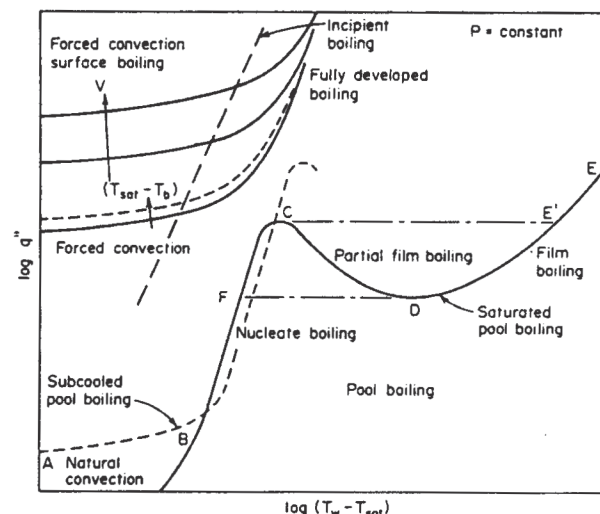


Figure 1: Boiling curve describing regions in boiling heat transfer (after Rohsenow (2)). T_b is the water temperature, T_w the surface temperature and T_{sat} the boiling temperature.

List of Symbols	
δ	liquid film thickness
C_p	specific heat (J/kgK)
c	a constant
d	jet thickness (m)
D	diameter of the ingot (m)
g	acceleration of gravity (m/s ²)
g_0	conversion factor 4.17×10^3 (lb _m ft/hr ² lb _f) in English units, 1 in SI units
h_c	convective heat transfer coefficient W/m ² K)
h_b	effective heat transfer coefficient with nucleate boiling (W/m ² K)
H_{fg}	latent heat of evaporation (J/kg)
k	thermal conductivity (W/mK)
L	latent heat of solidification
(q/A)	heat flux (W/m ²)
q_{CHF}	critical heat flux (W/m ²)
Q	total cooling water flow rate (m ³ /s)
Q'	cooling water flow rate per unit width of film (m ² /s)
r	radial coordinate (m)
$Re = \frac{4\Gamma}{\mu}$	Reynolds number
\bar{T}	average bulk fluid temperature and wall temperature (K)
T_f	final water temperature
T_{sat}	Saturation temperature of water (K)
T_{wall}	ingot surface temperature (K)
T_m	melt temperature (K)
T_l	liquidus temperature (K)
V	casting speed (m/s)
z	vertical coordinate (m)
$\Gamma = \frac{\rho Q}{\pi D}$	mass flow rate of cooling water per unit width of film (kg/sm)
ΔT	temperature difference between the bulk fluid and
T_{wall} (K)	
ΔT_x	temperature difference between T_{wall} and T_{sat} (K)
μ	dynamic viscosity of liquid water (kg/ms)
ν	kinetic viscosity
ρ	density (kg/m ³)
ρ_v	density of water vapour (kg/m ³)
σ	surface tension of water (N/m)

For subcooled boiling there is some natural convection cooling when the surface is below the saturation temperature. In the nucleate boiling area the curve may be left or right of the saturated liquid curve depending on geometry of the heater (dashed line in Figure 1).

For forced convection, if the surface temperature is below the boiling point the water cools by convection and as the velocity of the fluid increases so to does the heat flux. However, when the surface goes to some superheat above the saturation temperature boiling commences and at high heat fluxes the curves for various coolant temperatures and velocities begin to merge into what is known as the fully developed boiling curve ie the forced convection subcooled behaviour approaches the pool boiling behaviour. Note that flow velocity has no effect on the magnitude of q'' in the nucleate boiling regime, although it can effect the point of transition to film boiling.

In the nucleate boiling mode bubbles nucleate at crevice sites on the surface where there is some trapped gas due to incomplete wetting. They grow and are released into the moving water stream. Heat transfer during this period of nucleate boiling is high because the bubble growth and release causes considerable stirring of the liquid and transport of heat

into the bulk fluid. In DC casting most of the bubbles in fact collapse after release as they are cooled in the liquid film. Very little energy escapes as steam. This can be verified by measuring the water film temperature down the ingot surface and doing a heat balance (Appendix A).

Bubble nucleation behaviour is clearly going to influence the heat fluxes achieved during boiling. Factors such as surface roughness, surface tension and wetting angles affect the number of active nucleation sites, the rate of bubble nucleation, growth and coalescence. Changes in composition and temperature of the water will affect these properties. Dissolved gas makes it easier for bubbles to form by adding to the vapour pressure. There is extensive theory dealing with bubble nucleation, but the reader is referred to Cole (5) and Rohsenow (2) as starting points. For the purpose of this discussion it is enough to be aware that these factors play a role because of their affect on bubble behaviour. There is certainly scope for boiling researchers to examine this area in detail with reference to the particular conditions in DC casting.

To predict the heat transfer coefficients and heat fluxes as a function of the surface temperature in each region of the boiling curve, various equations using dimensionless groups of quantities (Reynolds, Nusselt, Prandtl numbers) and have been derived in the literature. It should be noted that the correlation factors are derived for particular fluid surface combinations and care must be taken in extrapolating to different conditions. The intrinsic properties of the surfaces are not taken into account in these equations.

Critical Heat Flux

In many industrial applications the transition from nucleate to film boiling is of great interest because of the sudden change in either surface temperature or heat flux. DC casting is no exception. As casting speed increases, if steady state is maintained, then total heat input increases linearly. Similarly heat input increases with the square of diameter. The response of the water cooled surface to meet the increased heat flow requirement is for the surface temperatures to rise (increasing q in the nucleate boiling region) and for a longer cooled region below the mould. However, there is a critical casting speed at which the surface temperature may exceed the critical transition temperature and the transition to film boiling occurs. At this point the maximum heat flux q_{max} required, exceeds the critical flux q_{CHF} . Variation in water composition can change the critical temperature and q_{CHF} therefore reducing the maximum casting speed before the onset of film boiling.

Generally, the transition from nucleate to film boiling is proposed to occur when the bubbles start to cover a large fraction of the surface, preventing liquid rewetting the surface. The critical heat flux q_{CHF} is found to increase with increasing flow rate (see for example (6, 7)). The relationship is found to follow the form

$$q_{CHF} = c \rho_g H_{fg} U_f \left(\frac{\rho_f}{\rho_g} \right)^{0.867} \left(\frac{\sigma}{\rho_f U_f^2 D} \right)^{1/3} \quad (1)$$

which with constant water properties, simplifies to the critical flux increasing with the velocity to the third power.

Note that the factors which increase bubble nucleation while increasing heat transfer in the nucleate boiling regime at a given surface temperature, also promote the transition to film boiling at a lower critical temperature. So depending on whether the surface temperature is above or below the transition temperature, a factor may increase or decrease heat transfer. This makes the prediction of their effect during casting all the more complicated.

3. Boiling Heat Transfer in Direct Chill Casting

3.1 Water Jet and Falling Film Physical Description

The water jet emerges from holes or a slot in the mould, hits the ingot surface and then forms a falling film down the ingot surface (Figure 2). The impact point has a different cooling behaviour to the falling film and although the jet impact region is small (3-4 mm) it has a large impact on heat flow because this is where the initial boiling regime is determined.

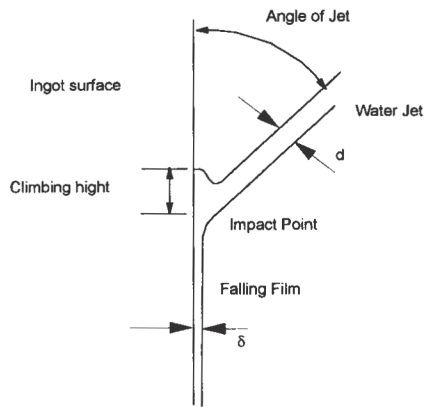


Figure 2: Geometry of water jet.

Where the water jet hits the ingot water flows both up and down the surface. At the impact point velocities are high and steam bubbles are rapidly swept away making it hard for a stable film to form. Above the impact point there is a low velocity pool. The important features of the jet and film are

- the angle of the jet (typically 15-30°)
- the velocity (typically 2 m/s)
- the thickness of the jet D (typically 1-2 mm)
- the height of the stagnant region above the impact point
- film thickness
- average velocity in the film

If the angle is too perpendicular to the wall the water will bounce off the ingot surface without forming the falling film. The velocity of the jet increases with increasing flow rate and decreasing outlet area. The climbing height is hard to measure but Bergstrom (8) gives an equation from Milne-Thomson which is evaluated and shows for a typical 30° angle and 2 mm jet the climbing height would be 3 mm. This is consistent with our observations.

The falling film is ~ 1-3 mm thick film running down the ingot. The average velocity down the ingot surface is set by the water flow rate and the thickness of the film. The thickness of falling films has been studied (9, 10) and found to follow the following type of relationship when the friction forces equal gravity

$$\delta = c \left(\frac{v^2}{g} \right)^{1/3} Re^n \quad (2)$$

This equation is shown in figure 3 for typical conditions. The film thickness increases with flow rate and decreases with diameter. Equation (2) enables U_f in the film to be calculated by

$$U_f = \frac{Q}{\delta \pi D} \quad (3)$$

giving a velocity within the film of around 1 m/s. The flow rate per length of perimeter is the key flow rate measure ie. m³/ms. Typical values are between 0.002 to 0.004 m³/ms (20-40 cm³/cms). An important point to note is that the mould design has little effect on this part of the cooling. The film basically does what it likes after exiting the mould and while the impact point is important most of the heat is extracted in the falling film region.

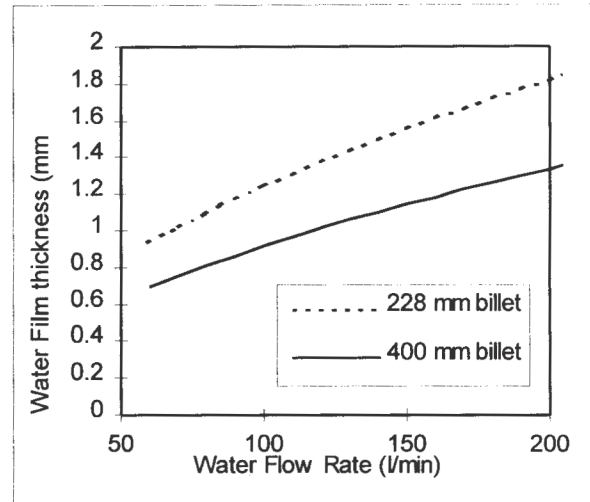


Figure 3: Predicted water film thickness.

3.2 Typical Steady State Conditions

Many measurements have been published of near surface temperatures, fluxes and heat transfer coefficients which give a good idea of the conditions occurring during casting(10-16). A typical set of cooling curves is shown in Figure 4.

Weckman and Niessen's (11) modelling of heat flow during DC casting was the first to include an analysis of the water spray heat transfer coefficients. Taking thermocouple implant data from 2 casts they did a type of inverse calculation where they adjusted the surface temperature boundary condition input to the model, until the predicted temperature histories in the ingot matched the measured values. They were then able to get a heat flux versus temperature curve from the model. This was compared to the following equation from McAdams (2) for h in a free falling turbulent film and a good fit was obtained.

$$\frac{h_c}{\sqrt[3]{k^3 \rho^2 g / \mu^2}} = C_0 \left[\frac{C_p \mu}{k} \right]^{1/3} \left[\frac{4\Gamma}{\mu} \right]^{1/3} \quad (5)$$

This equation depends on the properties of water which vary with temperature. The properties were evaluated and a linear regression analysis done giving h as a function of the flow rate and the surface temperature ie.

$$h_c = \left[-1.67 \times 10^5 + 704 \bar{T} \right] Q^{1/3} \quad (6)$$

shown in Figure 5. Interestingly, as the water temperature rises the h for the convection cooling also rises due to the effect of water temperature on water properties, but the flux decreases because it is the product of the h and the temperature difference.

Using the usual assumption that the forced convection nucleate boiling would give the same values as pool boiling Weckman and Niessen took the following equation from Rohsenow (2)

$$\frac{C_p \Delta T_s}{H_{fg}} = C_f \left[\frac{\left(\frac{q}{A}\right)_b}{\mu H_{fg}} \sqrt{\frac{g \sigma}{g(\rho - \rho_v)}} \right]^n \left(\frac{C_p \mu}{k}\right)^s \quad (7)$$

A good fit was obtained with a value of C_f of 0.016 $n=1/3$ and $s=1.7$ which when the properties of water are substituted simplifies to

$$\left(\frac{q}{A}\right)_b = 20.8(\Delta T_s)^3 \quad (8)$$

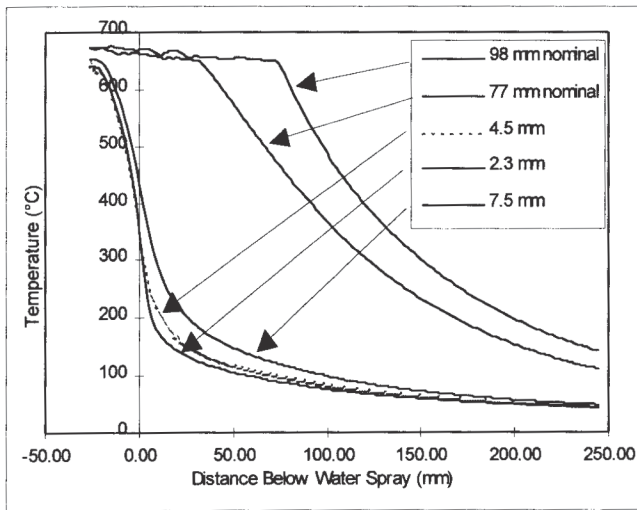


Figure 4: Typical temperature measurements at various radial positions for casting of a 228 mm 6061 alloy billet at 110 mm/min.

Following Rohsenow, Weckman and Niessen added the contributions from forced convection and boiling together (eqn 6 & 7) in the nucleate boiling regime giving

$$h = Q^{0.33} (352(T_s + T_w) - 167000) + 20.8(T_s - 273)^3 \quad (9)$$

Rohsenow's latter works show film cooling approximates pool boiling and this remains a valid assumption.

The exponent of one third for the sensitivity to flow rate shows that the values are not very sensitive to flow rate in the nucleate boiling regime. Increasing flow rate has another small effect by reducing the water temperature of the film. The film temperature increases as it falls down the ingot. This causes a small increase (1- 2%) in heat flow by increasing the difference in temperature between the water and the ingot surface.

These h equations used by Weckman and Niessen produced a good fit between predicted and experimentally measured ingot temperature, indicating film boiling was not occurring. However, in cases where film boiling occurs these equations are appropriate only for the surface temperature below the critical temperature. Weckman and Niessen noted the possibility of exceeding the critical temperature at some critical casting speed; the surface temperature at the impact point rising as the speed increases.

Bäken et al., (12,13) measured heat transfer coefficients during casting, by freezing in thermocouples and calculating the surface temperature and

fluxes. More of this data is also reported in Bergstrom (6). The peak h values were $50 \text{ kW/m}^2\text{K}$ with a maximum flux of 5 MW/m^2 for a rolling ingot and $20 \text{ kW/m}^2\text{K}$ and 3.5 MW/m^2 for a 216 mm diameter billet. These numbers are of the same sort of order as predicted by Weckman and Niessen. The critical temperature was about $150 \text{ }^\circ\text{C}$. The maximum heat flux increased with increasing casting speed and increasing water flow rate. The increase in q_{CHF} with flow rate looks linear although there are not many data points.

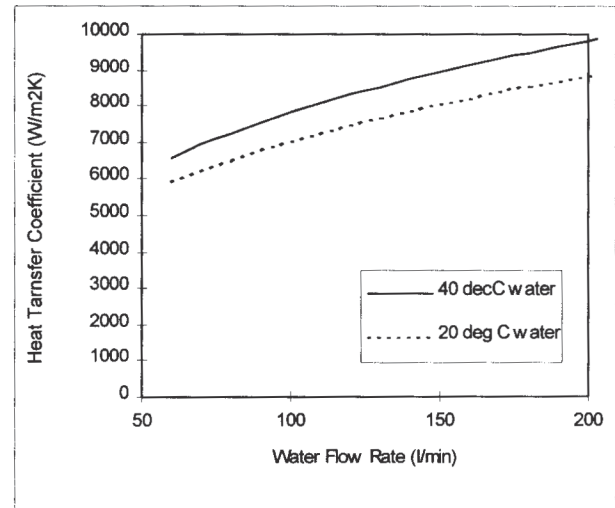


Figure 5: Predicted h in the convection cooled region using equation (6) on a 90°C surface as a function of flow rate; for a 400 mm billet.

Rappaz et al (1995) (14) by freezing in thermocouples and using inverse calculation, found the peak heat flux in the water spray was 4 MW/m^2 , similar to other published values. Tarapore (15) modelled casting of 2024 alloy 396 mm diameter billets, found that to get the best fit with measured frozen in thermocouples, the peak h was $20 \text{ kW/m}^2\text{K}$ with values of $1.25\text{-}1.68 \text{ kW/m}^2\text{K}$ in the convection cooled region. Watanabe and Hayashi (16) found a good fit between predicted and actual pool profiles using a critical temperature of $150 \text{ }^\circ\text{C}$ and a peak value of h of $\sim 10 \text{ kW/m}^2\text{K}$ for the falling film with a low flow of $0.0014 \text{ m}^3/\text{ms}$. For the impact point the peak h value was $30 \text{ kW/m}^2\text{K}$.

The dependence of q_{CHF} on flow rate can be evaluated from equations (1) (2) and (3). This comes out to

$$q_{CHF} \propto Q^{0.17} \quad (10)$$

In other words, the flow rate per perimeter must be increased a great deal to increase the maximum heat flux. Since larger diameters supply more heat ie q_{max} required for steady state is greater, then Q needs to increase with diameter. This is found in practice. The values of q_{CHF} predicted from the equations, for the 178 mm example in Bergstrom (6), are ~ 2 times higher than measured ($\sim 10 \text{ MW/m}^2$ versus $\sim 5 \text{ MW/m}^2$). One possible explanation is that these equations are for fully developed falling films and the DC case has the impacting jet and stagnant zone above the jet. Also, the surface roughness qualities of the as cast material may be different to the test material used to obtain the data these equations are fitted to. Another interpretation is that the maximum fluxes measured in the DC cases are not at q_{CHF} . For the maximum to be at q_{CHF} then the surface temperature must be above the critical temperature and film boiling must be occurring to start with. This could disrupt the film. The flux vs surface temperature curves obtained in casting are not necessarily equivalent to true boiling curves.

3.3 Effect of the Impacting Jet Velocity

Ma and Bergles (17) conclude that jet impingement nucleate boiling heat transfer is similar to forced convection inside tubes i.e. the same as the falling film of water below the impingement point. However, this remains an open question.

By freezing in thermocouples on a 155 mm gas pressurised mould during casting, Grandfield and Baker found in 1987 (18) that under normal water flows for this mould (0.002 m³/ms) the surface temperature at the impact point was around 250-300 °C and nucleate boiling occurred with a peak h of 40kW/m²K and a critical temperature of around 150 °C (Figure 6). These values gave an excellent fit with predicted and measured temperatures. Some unstable film boiling may have been occurring for a few mm near the impact point.

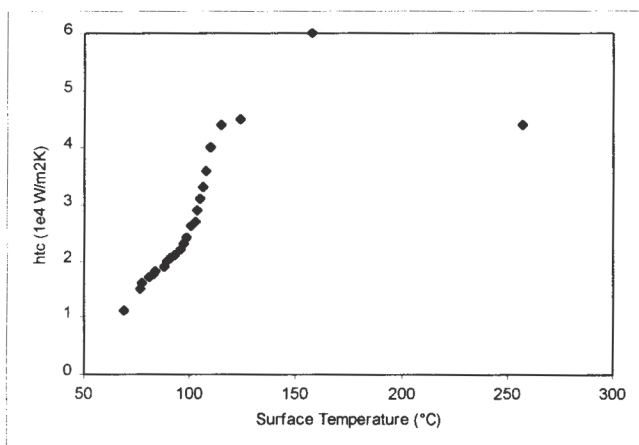


Figure 6: Heat transfer coefficient as a function of surface temperature from thermocouple implants during casting.

Normal conditions were with an impact velocity of 2 m/s. By doubling the slot width to 2 mm wide and using the same flow rate (60 l/min) the impact velocity was reduced to 1 m/s. Under the low velocity condition, film boiling occurred giving high surface temperatures (Figure 7). The ingot was found to have a much coarser microstructure. This result is interesting because it shows the independent effect of the impact velocity. The flow rate was still the same and the falling film characteristics were the same, but because the h in the impact point was reduced the surface temperature remained above the critical temperature.

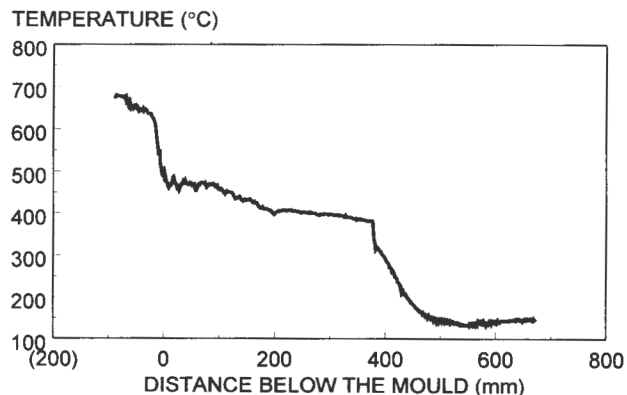


Figure 7: High near surface temperature on 155 mm 6063 billet caused by film boiling.

A 1996 paper by Matsuda et al (19) also reported the affect of various slot widths from 1 mm to 5 mm by measuring heat fluxes with

thermocouples in a heated billet lowered into the water spray. Again, the boiling curves clearly showed that the heat flux decreased with decreasing impact velocity. Also, the maximum flux decreased with decreasing impact velocity. This was used to reduce cracking tendency.

Under usual conditions the flow velocity for the impacting jet is ~2m/s as opposed to ~1m/s in the falling film. The transition to film boiling is therefore more likely to occur in the falling film as q_{CHF} for the film is less than the impacting jet. We have observed this during casting, either going from nucleate to film or film to nucleate boiling at a point below the impacting jet. A clearly visible transition front moves up or down the ingot in the falling film.

3.4 Cast Start

At cast start depending on flow conditions, surface temperatures can exceed the critical temperature and film boiling can occur. Thermocouple implant measurements we have made, show clear evidence of this (Figure 8). Temperature measurements by others (16,20) also show plateaus in the cooling curve caused by film boiling.

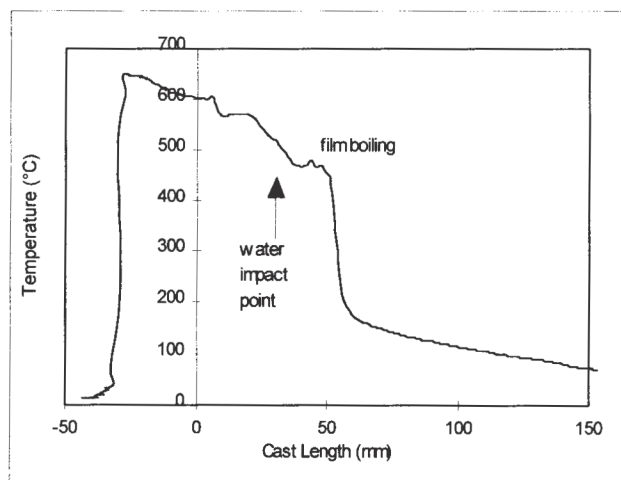


Figure 8: Near surface and centre temperatures for a 304 mm 6061.4 alloy billet. The thermocouple is positioned 20 mm above the base of the ingot near the surface.

During trials to optimise cast start conditions for rolling block we have observed film boiling occurring on the ingot surface. Film boiling produces a distinct hissing sound and an opaque water film effect. When film boiling occurs in the falling film, the stream can fly off the surface. We have observed this at the start of sheet ingot casting and on the DC water spray rig.

In summary, under normal conditions during steady state, mostly nucleate boiling occurs with perhaps some surface experiencing unstable film boiling near the impact point, while at cast start, film boiling can sometimes occur.

4. Quench Tests

It is difficult to conduct experiments on water composition with the actual process. To investigate the effect of composition specifically for DC casting, quench tests have been used (8,21-27). These methods involve quenching either a block or plate and examining the cooling rate.

In 1985 Ho Yu (21), used a hot missile dropped into water to show that various additives affect the boiling heat transfer. He discovered that, dissolved air, surfactant, cationic polyelectrolyte and dissolved castor oil were all found to reduce the boiling heat transfer. Bergstrom (22) measured the heat transfer of a water jet with an electrically heated hot

plate with thermocouples in it which was then quenched. Bergstrom's results showed;

- a) reduced q_{CHF} as mass flow rate went down.
- b) maximum heat flux occurred at a jet angle of 30°
- c) increasing q_{CHF} with decreasing water temperature (~ 10 - 50°C water temperatures)

Bramberger and Prinz (23) developed an equation for the h based on immersion cooling measurements from quench tests for non-ferrous metals. Their equation predicts values on the low side of those measured in DC casting of aluminium. They found increased water temperature decreased the heat transfer coefficient. Mueller and Jenzer (23) measured cooling rates in a bar quenched at one end with a water jet, and developed an equation for maximum h as a function of spray flux density and the properties of the metal bar. Kraushaar et al's (24) 1995 paper builds on the work of Mueller and Jenschar (25). A hot metal piece is quenched on one face only using similar flow conditions to DC casting. The rest of the part is inside a furnace to try and minimise the cooling rate. They measured the effect of water spray density ie the velocity of the jet. However, the values they use ($190\text{l/m}^2\cdot\text{min}$) are very low compared to DC casting $\sim 2000\text{l/m}^2\cdot\text{min}$. Their results show a maximum h of $50\text{ kW/m}^2\cdot\text{K}$ at a surface temperature of about 160 degrees C, and a very steep curve in the region from 100 to 160 degrees C.

In 1995 Langlais et al (26) using the same technique as Ho Yu and a modified version, where the hot slug is placed in front of the water spray in an open top block mould to test the actual plant water at the point of use, found the following;

- a) Water temperature has little effect on cooling rate up to 30°C .
- b) Oil concentration reduces cooling rate markedly above 10 ppm .
- c) Flocculant has little effect up to 100 ppm .
- d) Increasing cycles of concentration reduced cooling rate.

Hamilton and Chen (27) have recently conducted quench studies using a 200 mm long 75 mm square centrally preheated 460°C block immersed into water. Results consistent with previous work were obtained

- a) increasing water temperature promoted film boiling at lower surface temperatures
- b) dissolved CO_2 extended the time over which film boiling occurred
- c) forced convection promoted nucleate boiling
- d) NaCl and CaCO_3 additions promoted nucleate boiling
- e) particulate increased nucleate boiling heat transfer
- f) surface tension lowering surfactant prolonged film boiling but increased the intensity of nucleate boiling heat transfer

While these types of test give some useful results, they have a number of shortcomings. The missile test does not have the convective water flow conditions which occur during casting; pool boiling occurs rather than an impacting jet and a falling film. This means that the heat transfer coefficients are much lower than those occurring during casting. Transition temperatures are also quite different. While the use of actual water flow geometry overcomes some of these problems, the tests are still have a dynamic heat flow at much reduced fluxes than occur during casting. Hence, one cannot directly use the measurements from these tests in casting models to predict the effect of changes in h on the actual process.

5 Comalco DC Spray Rig

Considering the difficulties with dynamic quench tests which do not exactly duplicate the heat fluxes and flow conditions in casting, plus the problems of in situ measurement, such as,

- a) accurate positioning of thermocouples near the surface
- b) difficulty of accurate inverse calculation
- c) requirement for accurate property data
- d) expense.

we designed a test which has the same heat fluxes, and the same water flow conditions, operating at steady state.

The method consists of a heated body with a water cooled surface, the water jet arrangement being exactly the same as in DC casting (Figure 9). The heat source is arranged to give the same level of flux as occurs in steady state DC casting ie up to 8 - 9 MW/m^2 . The cooled body has thermocouples throughout. The heat flow in the body is modelled using the 3-MOS package and the full inverse problem solved for the heat transfer coefficients at the water cooled surface. The rig can be held at a stable heat flux and the heat transfer coefficient and surface temperature calculated. In this way we duplicate as closely as possible the behaviour in DC casting.

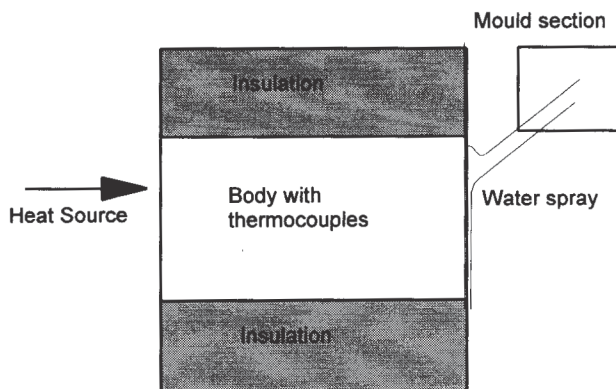


Figure 9 Schematic of DC spray rig.

Using the DC spray rig we are able to generate boiling curves for various conditions. Each point on the curve being at steady state rather than dynamic as usually occurs in quench tests. A typical boiling curve obtained from the DC spray rig is shown in figure 10. The four boiling regimes are evident, convection, nucleate boiling, transition boiling and film boiling. The results show similar values for h and fluxes to the published literature on measurements during casting ie critical temperatures ~ 150 - 200°C , $q_{CHF} \sim 5$ - 6 MW/m^2 and peak $h \sim 50$ - $60\text{ kW/m}^2\cdot\text{K}$. Values in the convection region follow the equation derived by Weckman very well. In the film boiling region h values are around 5 - $10\text{ kW/m}^2\cdot\text{K}$ depending on conditions.

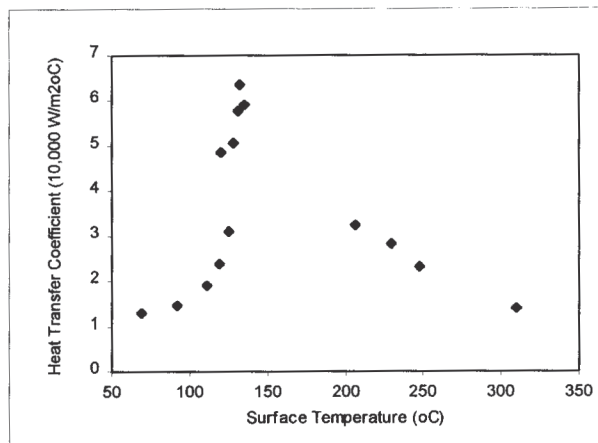


Figure 10: Typical boiling curve for deionised water obtained from the DC spray rig

5.1 Effect of water temperature

In our tests we observed no discernible differences in the boiling heat transfer for water temperatures in the range 15-35 °C. However, when we tested 43 °C water we found that the transition from nucleate boiling to film boiling occurred at a lower surface temperature but heat transfer was increased in the nucleate boiling regime (Figure 11).

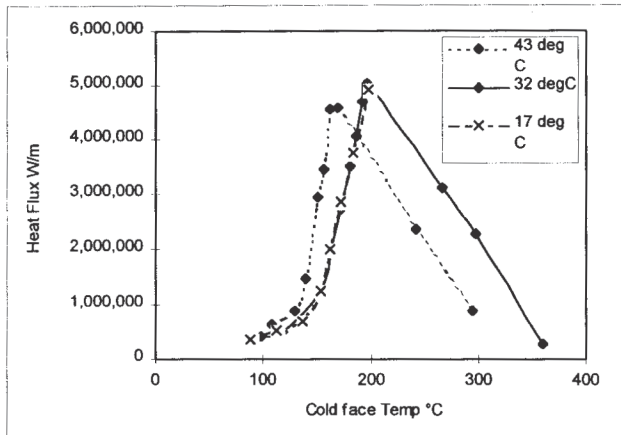


Figure 11: Effect of water temperature on boiling heat transfer.

5.2 Effect of Water Flow Rate

This small effect of flow rate has also been borne out in our DC spray rig results; where the measured heat transfer coefficients in the nucleate boiling regime do not vary much with flow rate (Figure 12). However, the flow rate has an important effect on where the transition to film boiling occurs. As rate increases the transition occurs at a higher surface temperature.

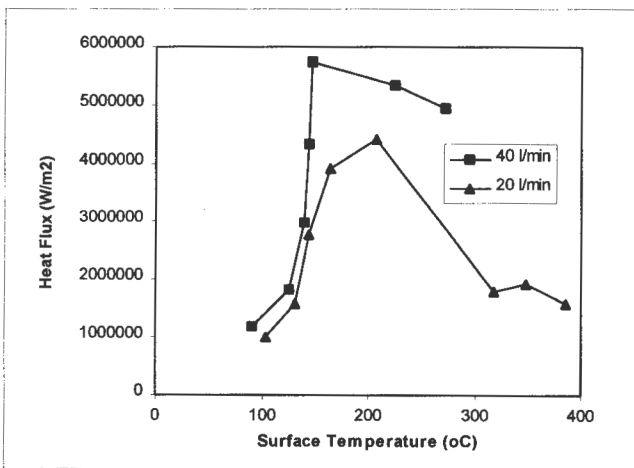


Figure 12: Effect of flow rate on h.

5.3 Effect of Total Dissolved Solids on Critical Temperature

When one compares the results of various tests for deionised water, Melbourne tap water and plant water there appears to be a trend of increasing critical temperature with increasing total dissolved solids (Table 1). This is consistent with the published literature (28) which shows that when quenching with deionised water, film boiling occurs for a longer time and the critical transition temperature is much lower than if hard water is used. Quench severity is known to be much greater when harder water is used ie cooling rates are higher. In fact this is why high

TDS water is known as “hard” water; the higher cooling rate producing harder metal!

TABLE 1: Effect of Total Dissolved Solids on Critical Temperature Above which Film Boiling Occurs.

Water Source	Total Dissolved Solids TDS (ppm)	Critical Surface Temperature (°C)
deionised	0	133
Melbourne Tap	53	149
Plant Water	380	>165

5.4 Effect of Water Treatment Chemicals

One of our plants was looking to change biocides. The effect of various combinations of additives on the boiling curves was tested on the DC spray rig. The results showed differences in q_{CHF} for the two additive combinations (Figure 13).

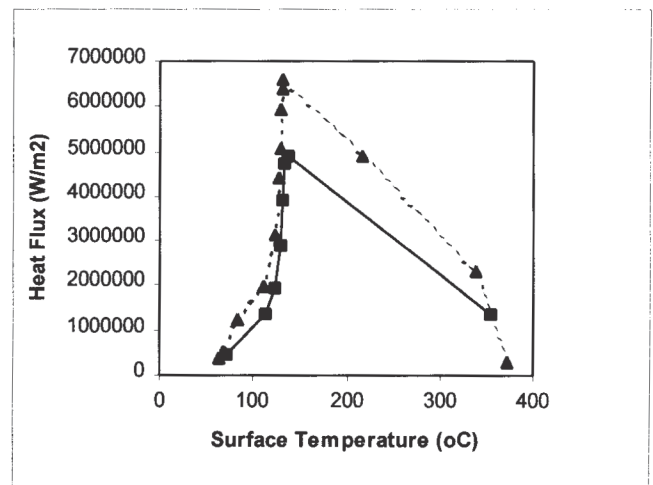


Figure 13: Effect of water treatment chemicals on boiling curve for two sets of treatment chemicals in deionised water.

5.5 Effects of Wagstaff Turbo™ System at Cast Start

A study using the DC spray rig of the effect of the Wagstaff Turbo™ system (used to reduce cooling rate and butt curl at cast start) on heat transfer and the interaction with water type and flow showed

- a) the Wagstaff Turbo™ system lowered the critical temperature and promoted the transition to film boiling at lower surface temperatures (Figure 14).
- b) Melbourne tap water (53 ppm TDS) had a lower critical temperature than plant water (380 ppm TDS), ie. film boiling occurred more readily with the purer water.
- c) the magnitude of the heat transfer coefficients in the nucleate boiling regime for plant water were less than those with Melbourne tap water.
- d) at high water flows, film boiling was suppressed but there was no change in the magnitude of the h at a given surface temperature.
- e) at high water flows, the Turbo system could not produce film boiling.

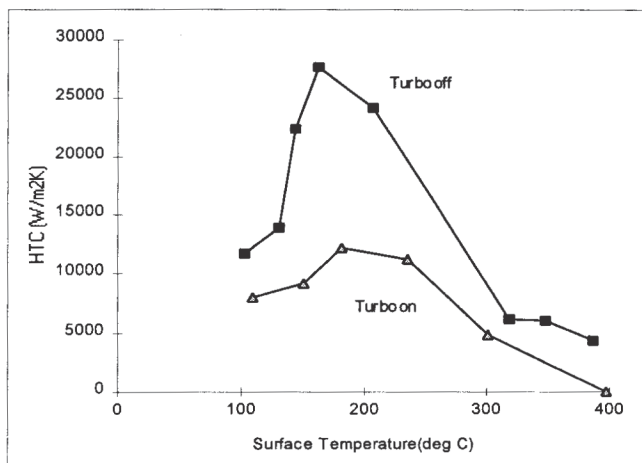


Figure 14: Effect of Turbo™ system on boiling heat transfer. Flow rate ~20 cc/cms.

6. Control of Factors Effecting Boiling Regime

While the above shows the effect of various factors on water cooling we need to know the corresponding effect on the temperature distribution and solidification behaviour to know if these effects are important. In Part II we show that the variation in water cooling intensity becomes significant when film boiling occurs. As long as nucleate boiling is achieved, then any variation in h has minimal effect on the process. Therefore, to minimise variation of the performance of DC casting due to water cooling, factors affecting transition to film boiling i.e. water temperature, flow rate, impact velocity and composition (TDS, oil and other chemical concentrations) must be controlled.

6.1 Physical Factors

Flow rate control systems are sufficiently accurate to ensure low flows do not cause film boiling. However, seasonal variation of water temperature may give rise to high enough ($>40^{\circ}\text{C}$) temperatures to cause film boiling. To counter the effect of high temperatures, higher flow rates can be used to suppress film boiling. However, the low sensitivity of q_{CHF} to flow means the flow rate must be greatly increased. Problems may arise if there is insufficient supply capacity. Indeed, to maximise productivity of a casting installation without expenditure on water system upgrades the flow rate per mould must be minimised possibly leading to further problems with film boiling.

Taking account of the above, moulds should be designed to achieve the required impact velocity to low flow rates. By reducing the area of the water outlet this can be achieved. However, the following engineering considerations dictate the mould water exit be designed so that

1. it does not block up, causing restriction in flow of the water.
2. it is not easily damaged
3. it is practical to make, with good dimensional tolerances.

If a small slot width or hole size is used they are more prone to blockage. Blockage can be caused by solids, and oil etc in the water. Ideally the filtration system should remove such objects but in practice many plants do not have good enough filtration. Holes tend to be better in this regard as the aperture size can be kept bigger but the exit area reduced by reducing the number of holes to keep the impact velocity high. Holes tend to be more robust and easier to make as well. With a slot the two major pieces can be off centre so that the slot width varies around the mould producing asymmetry in the billet cooling. Also, to achieve a

good angle with a slot requires the solid material at the mould face to be thin and damage prone.

The angle is also important because it changes the effective mould length. This is defined as the distance from the where the metal first touches the mould to the point where the water hits the ingot.

The mould must be designed to minimise pressure drop and flow variation around the perimeter of the mould. This is mostly a function of good baffling and ensuring that the point of exit has the highest flow resistance in the system. Again a smaller exit area will achieve this.

Another big difference between holes and slots is that with a slot the water curtain forms a continuous film around the mould. This effectively seals the base of the mould. Consequently, when using a gas pressurised mould, where the gas must escape down the mould, the water film periodically is disrupted by gas release when a slot is used. With water holes the jets only form a continuous film on the ingot allowing gas release.

By using a small orifice area, high impact velocities can be obtained while maximising the number of moulds and minimising individual flow without causing film boiling. Water system upgrades can be reduced while expanding capacity.

The Wagstaff dual jet (25) system serves to further enhance the effect of the impacting jet, by using two jets hitting at two points.

6.2 Compositional Factors

Water composition variation is largely dependant on the type of water system used; straight through or recycle. The latter is becoming more prevalent to minimise environmental impact. It makes sense to run high cycles of concentration to reduce makeup water and treatment chemical consumption and cost. This leads to high TDS.

The problem of excessive butt deformation during cast start causing cracking and an undesirable shape (butt curl) is well known. The technical solutions to this problem include a number of methods which change the water cooling to promote film boiling with a reduced heat transfer at cast start. These methods include Alcoa's CO_2 injection process, pulsed water, and Wagstaff's Turbo air injection system.

Extensive experimental studies of block cast start were made to optimise the conditions for reducing butt curl, including water analyses during CRC and plant tests. This work showed clearly that film boiling occurs at cast start and is essential to reducing cooling to minimise butt curl. Also, that higher TDS water tended to cause problems with the use of Turbo, consistent with the measurements on the DC spray rig which showed higher critical temperature with higher TDS and greater difficulty in promoting film boiling.

Also, from the point of view that increasing TDS appears to increase the critical temperature; high cycles of TDS may be more desirable for billet casting as there is less likelihood of film boiling occurring. The reverse might be true for block casting when film boiling needs to be encouraged at cast start.

Although not quantified on the DC spray rig test, published data (22) indicate increasing oil concentration promotes film boiling. The solution to this problem is oil removal systems (also required for environmental reasons), but also to minimise oil usage. The Wagstaff LHC mould for sheet ingot is a mould technology using very low lubricant rates, but also limits of minimum oil flow rates needs to be explored with existing mould technology. With respect to other chemicals, there is no reason why systems for biocides and corrosion inhibitors etc should not be used which give a continuous steady concentration rather than slug dosing.

7. CONCLUSION

In this paper, we have reviewed and shown using the DC spray rig, the effect of compositional and physical factors on water cooling and suggested strategies for their control. In part II of these papers (1) we show using mathematical models, the quantified effect of changes in the quench heat transfer coefficient on the temperature distribution, solidification behaviour and such parameters as the pool depth. It is essential to realise that the effect of any variable on water cooling must be examined by its effect on the temperature distribution not solely the cooling intensity. This can be achieved using a combination of the DC spray rig technique and experimentally verified mathematic models.

Acknowledgments

The following people contributed to this work ; Brett Gebert, Reaghan Hill, and Rob Cook

6. References

1 J. Grandfield, K. Goodall, A. Hoadley, Water Cooling in Direct Chill Casting : Part 2, Control, in these proceedings

2 Handbook of Heat Transfer Fundamentals, 2nd ed W. M. Rohsenow, J.P. Hartnett, E.N. Ganic eds, McGraw-Hill, 1985 , p12-75

3 W. H. McAdams,Heat Transmission”,3rd ed,McGraw-Hill,1954

4 Handbook of Heat and Mass Transfer, V1, N.P. Chermisinoff ed., Gulf Publishing Company,1986

5 R.Cole, Bubble Nucleation, Growth and Departure in Boiling Heat Transfer, Chap 8 in Handbook of Heat and Mass Transfer, V1, N.P. Chermisinoff ed., Gulf Publishing Company,1986

6 I.A. Mudawwar,T.A. Incropera,&F.P. Incropera,Int.J. Heat Mass Transfer,V.30,no.10,pp2083-2095,1987

7 R.P. Baines, M. A. El Marsi & W.M. Rohsenow, Int.J. Heat Mass Transfer,V.27,no.9,pp1623-1629,1984

8 T H Bergstrom: "Heat transfer in the continuous DC casting process of Aluminium", Doktor Ingenioravhandling 1987, Metallurgisk Institutt, Trondheim 1987,(NTH Trykk 1988).p 100

9 Handbook of Heat Transfer Fundamentals, 2nd ed W. M. Rohsenow, J.P. Hartnett, E.N. Ganic eds, McGraw-Hill, 1985,p12-75

10 Brauer,H. Stromung und Warmeubergang bei reisefilmen VDI-Forschungsheft 457,VDI-Verlag, Dusseldorf,1956

11 D.C. Weckman and P. Niessen, Met. Trans. B, 13B, 1982, pp.593-602

12 J.A. Bäken, T. Bergstrom, in Light Metals 1986, TMS 1985, p.883-889.

13 E.K. Jensen, S. Johansen, T. Bergstrom, J.A. Bäken, in Light Metals 1986, TMS 1985, p.891-896.

14 M. Rappaz,J.-L. Desbiolles,J.-M. Drezet,Ch.-A.Gandin,A. Jacot, & Ph. Thevoz, in Modelling of Casting Welding and Advamced

Solidification Processes, M. Cross, J. Campbell eds, TMS 1995,p449-457

15 E. D. Tarapore, in Light Metals 1989, P.G. Campbell ed., TMS 1988,pp875-880

16 Y. Watanabe, N. Hayashi, in Light Metals 1996, W. Hale ed.,TMS 1996, p 979

17 C.-F. Ma & A.E. Bergles, Int.J.Heat Mass Transfer,V29,No.8,pp1095-1101,1986

18 J. Grandfield & P. Baker ,” Variation Of Heat Transfer Rates In Direct Chill Water Soray Of Aluminium Continous Casting”, in Solidification Processing 1987, 3rd conference, The Institute of Metals 1988, p260.

19 Kiyoshi Matsuda, et al “Prevention of Cast Cracks in Al-Zn-Mg-Cu Alloy Castings”, in 6th International Aluminium Extrusion Technology Seminar, V1 p525, Aluminium Association & Aluminum Extruders Council 1996,

20 Y. Caron & A. Larouche,in Light Metals 1996, W. Hale ed.,TMS 1996, p963

21 Ho Yu, Light Metals, TMS, 1331-1347.

22 T H Bergstrom: "Heat transfer in the continuous DC casting process of Aluminium", Doktor Ingenioravhandling 1987, Metallurgisk Institutt, Trondheim 1987,(NTH Trykk 1988).

23 M. Bramberger and B. Prinz, Materials Technology, April 1986, V.2, p.410-415.

24 H. Kraushaar, R. Jenschar,V. Heidt, E.K. Jensen, and W. Schneider,”Correlation of Surface Temperatures and Heat Transfer by DC Casting of Aluminium Ingots”, Light Metals 1905, TMS 1995.

25 H. R. Muller & R. Jenschar, Z. Metallkunde, vol74,num5,1983,p257.

26 J. Langlais, T. Bourgeois, Y. Caron, G. Beland & D. Bernard, “Measuring the Heat Extraction Capacity of DC Casting Cooling Water”, Light Metals 1995, TMS 1995, p 979.

27 S. Hamilton,Heat Transfer and Water Quality in DC Casting, 4th year Chemical Engineering Project ,October 1995, Chemical & Materials Engineering, The University of Auckland

28 Totten G.E., Barnes C.E., and Clinton N.A. “Handbook of Quenchants and Quenching”. ASM International, Materials Park, Ohio. 1993.p293

Appendix A Heat Absorbed by Water

Measured Case: 6063 alloy 155 mm billet cast at 150 mm/min with 60 l/min water. Heat Input from Casting

$$\text{Total Heat Flow} = V\rho\pi r^2(C_{pl}(Tm - Tl) + L + C_{ps}(Tl - Tf)) \sim 128 \text{ kW}$$

Heat Absorbed by Water
measured temperature rise of water ΔT= 30 °C

$$\text{Heat absorbed} = Q\rho C_{pl}\Delta T = 125.4 \text{ kW}$$