1. Introduction

The cored wire injection method has been developed for the addition of low-density alloys like Ca-alloys into the steel bath. The Ca-recovery in this process is relatively higher than in the conventional and some other improved addition methods like shooting bullets, ‘pouring over’ etc. Factors such as minimum interaction with the slag, reduced liquid steel movement, possibility of suppressing the premature evaporation and, thereby, improving utilisation, possibility of preheating the filling material before release and simple and inexpensive operations have given an edge to this method over the others.

To realise these benefits, however, it is imperative that the filling material is released at such a depth in the ladle that the resultant residence time is the highest. This paper presents an understanding of the dissolution mechanism developed through a numerical approach and thereby suggests modification in the operating and the design parameters to increase the depth of penetration before the release of the powder. Mathematical models for tracking the melting of cored wire during its injection into the steel bath have been developed in the past though important aspects of the formulations have not been discussed in sufficient detail. As a result, it is difficult to use the results of these models to derive benefits for a specific steel melting shop.

A general purpose mathematical model has been developed at R&D, Tata Steel, using the finite difference approach with a fully implicit scheme to simulate the process of cored wire injection taking into account the different operating practices encountered in the steel shop. Numerical simulation of this kind of problem, involving moving boundary, typically suffers from the limitation that the progressive solidification of frozen layers that takes place is not made part of the thermal balance till it attains the size of a full node and thus the heat gained or lost by this “partial node” is not accounted for till such time. An alternative numerical formulation has been developed to rectify this.

Owing to the difficulty in making a direct validation, this model has been verified through a novel approach. This work suggests that the use of different wire dimensions (13–18 mm diameter and 0.4–0.6 mm casing), depending on the steel grades to be processed, is necessary in order to extract the maximum benefit.

KEY WORDS: mathematical model; steelmaking; calcium treatment; cored wire; injection metallurgy; deoxidation; alloy addition.
2. Development of Mathematical Model

2.1. Formulation of Model

The temperature distribution inside the cylinder shaped cored wire for a fixed observer can be described by the steady state heat conduction equation expressed in cylindrical coordinates (Fig. 2):

\[
\frac{1}{r} \frac{\partial}{\partial r} \left( kr \frac{\partial T}{\partial r} \right) + \frac{1}{r^2} \frac{\partial}{\partial \theta} \left( k \frac{\partial T}{\partial \theta} \right) + \frac{\partial}{\partial z} \left( \frac{\partial T}{\partial z} \right) + \dot{q} - \rho c_v \frac{\partial T}{\partial z} = 0 \quad \text{........................(1)}
\]

The temperature around the circumference of the cored wire is uniform and thus \( \partial T / \partial \theta = 0 \) and Eq. (1) can be rewritten as

\[
\frac{1}{r} \frac{\partial}{\partial r} \left( kr \frac{\partial T}{\partial r} \right) + \dot{q} - \rho c_v \frac{\partial T}{\partial z} = 0 \quad \text{........................(2)}
\]

Since the wire moves at a high speed, the heat transfer in the \( z \) direction by bulk motion is much higher than the heat brought in by conduction thereby permitting \( (k \partial T / \partial z) \) term to be dropped. The Peclet number (Pe) has been calculated for this condition and found to be 2000, which justifies the above hypothesis. Thus the Eq. (2), takes the following form.

\[
\frac{1}{r} \frac{\partial}{\partial r} \left( kr \frac{\partial T}{\partial r} \right) + \dot{q} = \rho c_v \frac{\partial T}{\partial z} \quad \text{........................(3)}
\]

The above formulation has been derived by the heat balance method for an observer fixed in space as shown in Fig. 2 with the material passing with a velocity \( \dot{v} \). If the observer were to move with the same speed of material in the \( z \) direction, he would notice a change in the temperature of the wire with time. Thus, if \( z \) is the distance travelled in time \( t \)

\[
z = \dot{v} \cdot t \quad \text{and} \quad \frac{\partial z}{\partial t} = \dot{v} \quad \text{........................(4)}
\]

and the right hand side of Eq. (3) becomes

\[
\rho c_v \frac{\partial T}{\partial z} = \rho c_v \frac{\partial (z \dot{v})}{\partial t} = \rho c_v \frac{\partial z}{\partial t} \frac{\partial \dot{v}}{\partial t} \quad \text{........................(5)}
\]

Thus, for a moving observer, the Eq. (3) takes the form of an unsteady state heat transfer.

\[
\frac{1}{r} \frac{\partial}{\partial r} \left( kr \frac{\partial T}{\partial r} \right) + \dot{q} = \rho c_v \frac{\partial T}{\partial z} \quad \text{........................(6)}
\]

\( \dot{q} \) takes the values 0, +ve or −ve depending on whether heat is generated or absorbed during internal freezing or melting.

The expressions for the transient heat conduction in the powder, casing and the shells can be written using Eq. (6) as follows:

1. Powder:

\[
0 \leq r \leq r_p, \quad 0 \leq t \leq t_{TOT} \quad \frac{1}{r} \frac{\partial}{\partial r} \left( kr \frac{\partial T}{\partial r} \right) = \rho_p c_v \frac{\partial T}{\partial z} \quad \text{........................(7)}
\]

2. Casing:

\[
0 \leq r \leq r_s, \quad 0 \leq t \leq t_{TOT} \quad \frac{1}{r} \frac{\partial}{\partial r} \left( kr \frac{\partial T}{\partial r} \right) = \rho c_v \frac{\partial T}{\partial z} \quad \text{........................(8)}
\]

3. Slag Shell:

\[
s \leq r \leq r_s, \quad 0 \leq t \leq t_{TOT} \quad \frac{1}{r} \frac{\partial}{\partial r} \left( kr \frac{\partial T}{\partial r} \right) = \rho c_v \frac{\partial T}{\partial z} \quad \text{........................(9)}
\]

The positions of \( r_p, r_s, r_{c}, \) and \( r_{s} \) have been indicated in Fig. 1. The values of \( k, \rho \) and \( c_v \) of the relevant liquid phase have been considered in the Eqs. (8) and (10) when the powder or the slag shell is molten. The \( t_{TOT} \) is the total time up to the point of release of the powder and consists of different time intervals as

\[
t_{TOT} = t_{slag shell freezing} + t_{steel shell freezing} + t_{steel shell melting} + t_{slag shell melting} + t_{casting heating} + t_{casting melting} \quad \text{........................(11)}
\]

In case of the absence of slag shell the Eq. (11) simplifies to

\[
t_{TOT} = t_{steel shell freezing} + t_{steel shell melting} + t_{casting heating} + t_{casting melting} \quad \text{........................(12)}
\]

where,

\[
t_{slag shell freezing} + t_{steel shell freezing} + t_{steel shell melting} + t_{slag shell melting} = t_{SSP} \quad \text{........................(13a)}
\]

and

\[
t_{steel shell freezing} + t_{steel shell melting} = t_{SP} \quad \text{........................(13b)}
\]
Equations (7)-(10) have been used for the present study and solved with the relevant initial and boundary conditions to determine

1. the temperature distribution inside the cored wire and the solidified shell,
2. the total time taken for the melting of the casing and the shell and for the release of the powder,
3. the temperature of the powder at the time of release.

In indicating the initial conditions, boundary conditions (B.C.) and boundary equations, the listing has been done in a systematic way starting at the cored wire centre and starting of computation and proceeding radially outwards towards the liquid steel bath.

2.1.2.3. B.C. at Casing/Shell Interface

(a) Boundary conditions at the casing and the frozen layer simultaneously. Thus, B.C.3a., where the interface temperatures at the slag and casing are $T^*_s$ and $T^*_c$ respectively (Fig.1).

$$B.C.3a \quad 0 \leq t < t_{m,sl} \quad r = r_c \quad k_r \frac{\partial T}{\partial r} = k_d \frac{\partial q}{\partial r} = q'' \quad \text{and} \quad q'' = \frac{T^*_s - T^*_c}{R_f} \quad \text{(18)}$$

Secondly, the contact resistance also ceases to exist after complete melting of both the shells ($t_{m,sl}$) as depicted in B.C.3b.

$$B.C.3b \quad 0 \leq t < t_{m,sl} \quad r = r_c \quad k_r \frac{\partial T}{\partial r} = k_d \frac{\partial q}{\partial r} = q'' \quad \text{and} \quad q'' = \frac{T^*_s - T^*_c}{R_f} \quad \text{(19)}$$

Thus, as soon as the slag shell becomes molten the thermal contact resistance becomes zero (B.C.3c).

$$B.C.3c \quad t_{m,sl} \leq t < t_{m,sl} \quad r = r_c \quad R_i = 0 \quad \text{and} \quad T^*_s = T^*_c \quad \text{(20)}$$

2.1.2.4. B.C. at Slag Shell/Steel Shell Interface

The wire enters the steel bath after passing through the slag layer and the steel shell forms over the slag shell. The amount of heat leaving the steel shell should enter the slag shell as shown in B.C.4 below. The freezing of the last layer of the slag shell and the first layer of the steel shell occur almost simultaneously from the respective liquid phases and so the interface between these two layers has been assumed to be in perfect thermal contact and no contact resistance has been considered.

$$B.C.4 \quad t_{m,sl} \leq t < t_{m,sl} \quad r = r_c \quad k_r \frac{\partial T}{\partial r} = k_d \frac{\partial q}{\partial r} = q'' \quad \text{(21)}$$

2.1.2.5. B.C. at Wire Surface

The temperature at the surface of the wire is at the melting temperature ($T_{MP}$) of the respective liquid. B.C.5a represents the temperature at the surface of the slag shell during the travel through the slag layer.

$$B.C.5a \quad 0 \leq t < t_{MP} \quad r = r_c \quad T = T_{MP,sl} \quad \text{(22)}$$

B.C.5b represents the temperature at the surface of the steel shell during its freezing and melting.

$$B.C.5b \quad t_{MP} \leq t < t_{MP} \quad r = r_c \quad T = T_{MP,bath} \quad \text{(23)}$$

After melting of steel shell ($t_{MP}$), the slag shell starts melting and so the temperature at the surface of the slag shell is again at the melting point of slag as shown in B.C.5c.

$$B.C.5c \quad t_{MP} \leq t \leq t_{MP} \quad r = r_c \quad T = T_{MP,sl} \quad \text{(24)}$$

When the shell melts completely (time $t_{MP}$) and the casing starts melting, the temperature at the surface of the casing then becomes the melting point of the casing:

$$B.C.5d \quad t_{MP} \leq t \leq t_{MP} \quad r = r_c \quad T = T_{MP,c} \quad \text{(25)}$$

2.1.2.6. B.C. at Solidification Front

Boundary conditions (6a–6d) represent the heat balance for the moving solidification (or melting) front at the cored wire dissolution. This contact resistance to the heat flow, of course, ceases to exist under two conditions. Firstly, if the slag shell interface starts melting internally (i.e. after the time $t_{m,sl}$, provided the $t_{m,sl}$ is less than the time for complete melting of the steel shell). This has been represented by B.C.3a., where the interface temperatures at the slag and casing are $T^*_s$ and $T^*_c$ respectively (Fig.1).
shell/liquid steel or casing/liquid steel interface where \( \dot{\lambda} \) is the latent heat of fusion of the respective melt. The B.C.6a is the heat balance for solidification of slag shell during the travel of the wire through the slag layer.

B.C.6a  
\[
0 < t < t_{SG} \quad r = r_d \quad \left( k_s \frac{\partial T}{\partial r} \right)_{Sgs} = \rho_s \dot{\lambda_s} \frac{\partial}{\partial r} + h(T_{SLAG} - T_{MP(sl)}) \quad (26)
\]

The B.C.6b represents the heat balance for the solidification and melting of steel shell when the wire travels through the steel bath.

B.C.6b  
\[
t_{SG} < t < t_{SSP} \quad r = r_d \quad \left( k_s \frac{\partial T}{\partial r} \right)_{Bath} = \rho_s \dot{\lambda_s} \frac{\partial}{\partial r} + h(T_{BATH} - T_{MP(bath)}) \quad (27)
\]

When the steel shell melts completely, the melting of slag shell is governed by the B.C.6c.

B.C.6c  
\[
t_{SSP} < t < t_{TSP} \quad r = r_d \quad \left( k_s \frac{\partial T}{\partial r} \right)_{Bath} = \rho_s \dot{\lambda_s} \frac{\partial}{\partial r} + h(T_{BATH} - T_{MP(sl)}) \quad (28)
\]

The melting of casing is represented by B.C.6d. The \( t_{MS(c)} \) is the time when the casing starts melting.

B.C.6d  
\[
t_{MS(c)} < t < t_{TSP(c)} \quad r = r_c \quad \left( k_c \frac{\partial T}{\partial r} \right)_{Bath,Casing} = \rho_c \dot{\lambda_c} \frac{\partial}{\partial r} + h(T_{BATH} - T_{MP(cas)}) \quad (29)
\]

2.1.2.7. B.C. at Bulk Liquid

The B.C.7a and 7b demonstrate that the temperature of the slag layer and steel bath respectively far from the cored wire can be regarded as being constant.

B.C.7a  
\[
0 \leq t \leq t_{SG} \quad r \rightarrow \infty \quad T = T_{SLAG} \quad (30a)
\]

B.C.7b  
\[
0 \leq t \leq t_{SSP} \quad r \rightarrow \infty \quad T = T_{BATH} \quad (30b)
\]

2.1.2.8. Additional B.Cs. for Unique Situations

There may be a situation when the shell is completely molten, but the surface of the casing is yet to reach its melting point. The casing would then start receiving heat directly from the bath before commencement of its melting. The B.C.8 represents the heat balance for such condition. The \( t_{MS(c)} \) is the time when the casing starts melting.

B.C.8  
\[
t_{TSP(c)} < t < t_{MS(c)} \quad r = r_c \quad \left( k_c \frac{\partial T}{\partial r} \right)_{Bath,Casing} = h(T_{BATH} - T_c) \quad (31)
\]

If the slag shell reaches its melting point before the complete melting of steel shell, the temperature of the node representing the outer layer of the slag shell is held constant at its melting temperature until it gains heat equal to the latent heat of fusion to initiate melting. B.C.9 represents the heat balance for such a situation which is relevant for the period when the slag shell is melting \( t_{MS(sl)} \).

B.C.9  
\[
t < t_{MS(sl)} < t_{SSP} \quad r \leq r_d \quad \text{and} \quad T_d = T_{MP(sl)} \quad \left( k_s \frac{\partial T}{\partial r} \right)_{in} = \left( k_s \frac{\partial T}{\partial r} \right)_{out} = \rho_s \dot{\lambda_s} \frac{\partial}{\partial r} \quad (32)
\]

The internal melting of the powder is demonstrated by the B.C.10 which is relevant for the period when the powder is melting \( t_{MP(p)} \).

B.C.10  
\[
t < t_{MP(p)} < t_{TOT} \quad r \leq r_p \quad \text{and} \quad T_p = T_{MP(p)} \quad \left( k_p \frac{\partial T}{\partial r} \right)_{in} = \left( k_p \frac{\partial T}{\partial r} \right)_{out} = \rho_p \dot{\lambda_p} \frac{\partial}{\partial r} \quad (33)
\]

2.1.2.9. Evaluation of the Heat Transfer Coefficient

The heat transfer coefficient, \( \nu \), from the bath to the shell/casing surface has been deduced from the following dimensionless correlations.\(^5\)

\[
\nu = 0.4 \operatorname{Re}^{0.6} \operatorname{Pr}^{0.45} \quad \text{for laminar flow } i.e. \quad \operatorname{Re} < 10^4 \quad (34)
\]

\[
\nu = 0.0296 \operatorname{Re}^{0.8} \operatorname{Pr}^{0.53} \quad \text{for laminar flow } i.e. \quad \operatorname{Re} > 10^5 \quad (35)
\]

\( \nu \) for a transition flow has been obtained by proportionating the values at Re of 10^4 and 10^5. The \( x \) and \( v \) in the Nu and Re numbers, defined as follows,

\[
\nu = \frac{h x}{k} \quad (36)
\]

\[
\operatorname{Re} = \frac{x v}{\mu} \quad (37)
\]

refer to the bath height and the wire injection speed respectively. The relevant physico-chemical properties of liquid slag\(^2\) and liquid steel\(^3\) are given in Table 1.

2.2. Solution Methodology

As the set of partial differential Eqs. (7)–(10) is too complex for analytical solution a numerical approach has been taken to predict the melting time and the distance travelled as a function of time. These equations have been converted into finite difference equations using the fully implicit scheme. The calculation domain in radial direction (powder+casing+shell) has been discretised into nodes \((0, 1, ..., M-1, M)\) as shown in Fig. 3, each node being the centre of a control volume. The change in the outer shell thickness, \( \delta r \), of the moving solidification front, has been calculated as per the Eqs. (26)–(29). A positive value of \( \delta r \) indicates melting of the shell (or casing after complete melting of shell as per Eq. (29)) and a node is reduced when an appropriate amount of the shell melts. The reduction of a surface node makes the node underneath, the surface node where the
temperature is known. Thus, no separate effort is needed to compute the new surface temperature. During the calculation of the surface node temperatures, the actual shell volume at that point of time has been considered instead of considering only the half-node volume as discussed in Appendix-A. This has enhanced the accuracy of the model and in the opinion of the authors represents an improvement over the existing numerical modelling techniques for cored wire modelling.

A computer program for the solution of the above equations has been developed in FORTRAN. To check the accuracy and consistency of the basic numerical solutions, the model has been adjusted to run under the limiting condition of an infinitely long cylinder subjected to a sudden change of surface temperature for which analytical solutions are available. The results were within the acceptable limits of deviations (0.5%).

3. Model Validation

3.1. Validation with Published Work

Though few published work on cored wire injection are available, their simplified assumptions and limited validation have forced the authors to compare the model results with the published work on the dissolution of static metallic cylinders in quiescent/inductively stirred steel melt. The validation results with the work on dissolution of pure Titanium, Niobium and Tantalum cylinders in a quiescent steel melt have been presented in Figs. 4 to 6 along with the published data.

Titanium—Figure 4 shows the variation of the centre temperature during dissolution of a 2.54 cm diameter Ti cylinder in molten steel at 1590°C. Ti shows a change of phase from \( \alpha \)-Ti to \( \beta \)-Ti at 882°C. The critical temperature for initiation of the exothermic dissolution reaction at the original cylinder surface/shell interface is 1090°C. The shell/original cylinder interface achieves this temperature at the eighth second after immersion in the bath.

Niobium—In contrast to Ti, Nb does not undergo phase change. However, it exhibits an exothermic dissolution in liquid steel, which starts at 1370°C. Figure 5 shows the growth and decay of radius of a 3.81 cm diameter Nb cylinder when immersed in liquid steel at 1600°C.

Tantalum—The predictions for the variation of radius and the centre temperature of a 2.54 cm diameter Ta cylinder during its dissolution in molten steel at 1600°C is presented in Fig. 6 along with the published data.

3.2. Validation with Plant Results

An innovative approach for indirect validation with the plant results has been attempted and discussed here. The model predicts the zone or level of the steel bath where the powder is released. The direct verification of this parameter in a steel ladle is nearly impossible. No simple method exists for collecting sample from different depth in a ladle of molten steel.

To assess the variation in Ca and Si content samples were collected from the tundish at definite intervals during casting. The steel in the tundish was assumed to represent a particular zone of the ladle depending on the flow rate and the time of sample collection. The samples were subsequently analysed for Ca and Si and the results have matched well with the predictions. In the opinion of the authors, these evidences are supportive but cannot be taken as conclusive.

4. Routes for Cored Wire Melting

With reasonable validation obtained, the model was run for several conditions to develop an understanding of the melting behaviour of cored wire. The results were analysed and the different routes of melting have been grouped into four broad categories as discussed here. Mainly the presence or absence of slag at the ladle top and the bath Temperature Above the Bath Liquidus (TABL), commonly known as ‘superheat’, dictate these melting behaviours.

Whenever the first liquid (slag/steel) freezes on the casing, as discussed before, an interfacial resistance is devel-
formed between the casing and the frozen layer (shell) due to improper thermal contact between these two. The formation of this resistance at the surface of an immersed metal and its magnitude has been studied by many workers. The exact value of the resistance depends on factors like the ratio of thermal expansion coefficient and the ratio of thermal diffusivity of the immersed metal and the shell metal, the thickness of air and/or the oxide film at the interface. The resistances for different metal combinations have been derived experimentally which vary from 1.9 to 9.1 cm² s °C/J and particularly for steel/other metal combination the range is 1.9 to 2.1 cm² s °C/J. As no work is available for determining the resistance in case of steel immersed in liquid steel, the authors has assumed the value as 1.9 cm² s °C/J for the present study which seems to be a reasonable from the above discussion. However, experiments have been planned at R & D, Tata Steel to verify this assumption. The resistance subsequently vanishes in either of the two conditions: complete melting of the shell or internal melting of either casing or the first shell.

The shell growth occurs due to the large gradient of temperature between the surface and the interior. The first shell grows to a certain maximum thickness until the heat supplied to the surface through convection becomes equal to the heat conducted inside.

Route 1—No Slag Cover at the Ladle Top along with High TABL (>60°C)

In this case, the shell thickness (stage C1 of Fig. 7(a)) is relatively low owing to the larger amount of heat supplied through convection from the melt of high TABL. On complete melting of the shell the melting of the casing does not start immediately. This is because the interfacial resistance prevents the casing from being heated within such a short period of shell melt-back. The casing then starts receiving heat directly by convection from the melt (stage E1). Subsequently, it melts and releases a solid but hot powder to the bath (stage G1).

Route 2—No Slag Cover at the Ladle Top along with Low TABL (<60°C)

The convective heat input to the wire is less in this case due to the low TABL. So, this route sees a thicker shell and longer melt-back time (stage C2 and D2 of Fig. 7(a)). This long duration enables the powder to start melting internally much before the casing melts. On the complete melting of the casing partially or completely molten powder is released in the bath.

Route 3—Presence of Slag at the Ladle Top along with High TABL (>60°C)

The presence of the slag, as the first shell, changes the freezing and melting behaviour. This route is similar to ‘Route 1’ with the exception of a larger shell thickness and a longer melting period (Fig. 7(b)).

Route 4—Presence of Slag at the Ladle Top along with Low TABL (<60°C)

In this case, the steel shell period (i.e., the duration of existence of a steel shell) is significantly higher due to the lower heat input from the bath. The slag shell starts melting internally during the initial steel shell period (stage F4 of Fig. 7(b)) and the interfacial resistance between the casing and the slag shell disappears. This leads to an increased heat conduction from the shell shell to the casing through the molten slag. The steel shell then resumes its growth and reaches a maximum thickness which is less than the maximum thickness achieved during the initial growth. The subsequent melting of steel shell exposes the casing to the melt and melting of the casing starts. This period (stage G4) is substantially lower than other routes (F1, F2, G3). The powder also starts melting by this time. Finally, a partially or completely molten powder is released into the bath.

**Figure 8** presents a typical set of graphs showing the variation of casing and shell thickness during the above mentioned freezing and melting periods for both the “no-slag” and the “with-slag” conditions. As already mentioned, the shell initially grows with time before melting back. The thickness remains constant during the direct heating of casing in case of the curve of “no-slag” condition. In the case of curve of the “with-slag” condition, the secondary growth of steel shell starts and reaches a second peak. It can be clearly seen that after the complete melting of the steel shell, there is no holding for direct heating of the casing in the later cases and the melting of casing is also relatively

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5. Impact of Wire Parameters on Melting Behaviour

The four parameters of the wire which effect the distance travelled are discussed below. The distance travelled is the distance travelled by the wire before the powder is set free into the melt and is an indicator of the point of release of the powder in the ladle.

5.1. Wire Speed

The effect of speed on the melting time and thus on the distance travelled has been shown in Figs. 9 and 10 for two different thermal conductivities of the filling materials. As expected, these two figures indicate the decrease of the melting time with the increase in speed. However, the decrease in the melting time on account of this factor is not necessarily accompanied by a decrease in the distance travelled. On the contrary, as evident from the Figs. 9 and 10, the distance initially increases with speed (segment a–b) and reaches a maximum at a certain speed (speed b) and then decreases (segment b–c). The position of this point ‘b’ changes with the TABL.

5.2. Wire Diameter

If the wire diameter is decreased, the total heat requirement for melting of the wire decreases as there is less wire mass to be melted and the release of the powder occurs earlier. This is clear in the curves of Fig. 11 (for 10 mm dia. Wire) and Fig. 10 (for 13 mm dia. Wire).

5.3. Packing Density of Powders

The discussions thus far have been presented considering a very high thermal conductivity of the powder, which may be true for a densely packed powder (or solid) CaSi. The melting behaviour changes significantly, however, if the conductivity changes to a lower value as suggested by Ribiere. The distance travelled for a wire with lower thermal conductivity of powder (Fig. 10) is lesser than the distance travelled for the case of wire with higher thermal conductivity (Fig. 9). The lower conductivity of the powder reduces the amount of heat transferred from the casing to the centre of the wire. This reduction of heat conduction, in turn, reduces the thickness of the shell and causes faster melting of the shell as well as the casing. As is evident from Fig. 10, when the bath temperature is 1600°C or higher and the speed is higher than 2 m/s, the powder is released much earlier before it reaches the ladle bottom (which is usually 3 m for 140 MT capacity ladle).

5.4. Casing Thickness

The preceding discussion highlights the problem of early release of powder in case of high bath temperatures. This may result in higher evaporation loss as suggested by Pellicani et al. as well as loss of unreacted powder by reaction with the top slag.
To find out the suitable dimensions of the wire for such applications, the model was run with different wire diameters and casing thicknesses. Figure 12 suggests that the 13 mm wire with 0.8 mm casing is more suitable than 0.4 mm casing wire in case of high superheat melts as the former reaches the closer to the ladle bottom before releasing the powder.

6. Impact Of Steel Bath Parameters on Melting Behaviour

The parameters of the steel bath which effect the melting of CaSi cored wire can be divided in two broad categories:

6.1. Temperature above Bath Liquidus (TABL)

As the TABL is increased, the maximum thickness of shell decreases due to the increased amount of heat supplied from the bath to the wire. Additionally, the total time for melting decreases as well as shown in Figure 13. As the total shell period (TSP) decreases, the time available for the casing to be heated up by the shell is reduced. Thus, though the total time for melting is lower with increasing TABL, the horizontal portion of the graph is longer indicating the larger time interval for which the casing is in direct thermal influence of the bath.

As discussed previously, the lower TABL brings in the unique feature of secondary shell growth in case of with slag conditions (curves 1a and 2a of Figure 14). However, an increase in the TABL in this case reduces its possibilities. Another very prominent feature of melting in all with slag condition is the extremely fast melting of slag shell after complete melting of the steel shell; the difference between the melting temperature of the slag and the temperature of the bath is responsible for that. The model was run under three thicknesses of slag layers viz., 1 cm, 3 cm and 5 cm. No appreciable difference was noticed in the melting behaviour of melting in these three cases.

The total melting time of the shell and casing is of academic interest as the plant operator is more interested to know the point of release of the powder in the ladle and its variation with different operating parameters. Figure 9 shows the variation of melting time and the corresponding distance travelled for a typical wire specification. As it is evident, the wire melts much beyond the 3 m bath depth (assuming wire travels vertically inside the bath) and thus, there would be unmelted wire at the ladle bottom in case of lower TABLs. These results are different if a wire with different thermal properties is used as has been discussed in a preceding section.

6.2. Steel Grade

The melting temperature of the liquid steel bath has an impact on the melting time of the wire. The melting behaviour of the cored wire for three steel grades, viz. low carbon, medium carbon and high carbon, with three different melting temperatures have been furnished schematically in Table 2. The difference between the bath temperature and the melting temperature of the bath is constant at 100°C for these three grades of steel, the expectation that melting behaviour in these cases would be similar is however not met. The distance travelled by the wire is different for different grades and the reason for the same are discussed. The actual bath temperatures are 1625°C, 1600°C and 1560°C for low carbon, medium carbon and high carbon grades respectively. The melting time \( t_{\text{TOT}} \) in Eqs. (11) and (12) is influenced by \( t_{\text{casing melting}} \). The increase in \( t_{\text{casing melting}} \) (keeping all other terms of the RHS of Eqs. (11) and (12) constant), increases the time for complete melting of wire (i.e. \( t_{\text{TOT}} \)). This is the case for the steel baths with low melting temperatures (like high carbon grades). As the temperature difference between the steel bath and the melting temperature of
the casing is low, the time, \( t_{\text{ms(c)}} \), increases significantly which ultimately increases the \( T_{\text{TOT}} \) although the \( T_{\text{SPL}} \) is almost same. Thus, when processing a high carbon heat, the powder may be released close to the bottom, as shown in Fig. 15, though the TABL is of the same order as in other steel grades. The examination of plant data supports this as seen that the efficiency of wire injection in terms of calcium yield is higher in the high carbon heats. According to the authors, the release of the powder closer to the ladle bottom is the main cause. This is a significant observation for Tata Steel, which casts low to high carbon heats. Thus, the optimum wire dimension as well as the injection speed needs to be different for different grades of steel. A nomo-
gram (Table 3) has been developed to help the plant operator to choose the injection speed for a particular wire di-
mension according to the grade of the steel being processed.

7. Conclusion

a. A model has been developed to study the behaviour of cored wire additions in the molten steel bath. This model incorporates improvement over the existing models for dissolution studies as the concept of thermal resistance at the interfaces has been used. The presence of molten slag at the ladle top alters the melting behaviour of the wire to a great extent, though the exact amount of the slag, within the limits encountered in the plant, does not impact the melting behaviour significantly. The models validation with plant results has been done in a unique manner.

b. Four different schemes of wire melting have been pro-
posed based on the model predictions. It has been shown that the powder can be in a molten condition at the time of release.

c. Estimation of the effects of different bath parameters and wire parameters on the distance travelled has been done. The effects of TABL and the speed of injection are significant. The thermal conductivity of the powder also plays a major role.

d. The dependency of the efficiency of the injection process on the grade of steel to be processed has been assessed and a modification in wire dimension and operating parameters has been suggested. The increase in the wire diameter and the casing thickness shows favourable impact on the distance travelled for certain grades of steel, whereas, for some other grades the existing prac-
tice is in order.

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Nomenclature

| \( r \) | Variable radius (cm) |
| \( k \) | Thermal conductivity (J/s · cm · °C) |
| \( T \) | Temperature (°C) |
| \( \rho \) | Density (gm/cm³) |
| \( L \) | Latent heat of fusion (J/gm) |
| \( t \) | Time step (s) |
| \( t_{\text{sl}} \) | Residence time inside the slag layer of the ladle top (s) |
| \( t_{\text{p}} \) | Time for internal melting of slag shell (i.e. Total Shell Period) (s) |
| \( t_{\text{p}} \) | Time for internal melting of powder (s) |
| \( t_{\text{SP}} \) | Time for melting of steel shell (i.e. Steel Shell Period) (s) |
| \( t_{\text{TSP}} \) | Time for complete melting of shell and casing and release of powder (s) |
| \( t_{\text{SMP}} \) | Starting point for melting of casing (s) |
| \( R_{\text{c}} \) | Thermal resistance at the casing/shell interface, (cm²·s·°C/J) |
| \( T_{\text{S}} \) | Temperature of the slag shell and casing respectively at casing/slag shell |
| \( T_{\text{C}} \) | Interface in imperfect thermal contact (°C) |
| \( q \) | Heat flux (J/s·cm²) |
| \( T_{\text{M}} \) | Melting temperature (°C) |
| \( T_{\text{B}} \) | Bath temperature (°C) |
| \( Nu_{\text{c}} \) | Nusselt number |
| Re | Reynolds number |
| Pr | Prandtl number |
| \( h \) | Heat transfer co-efficient (J/s·cm²·°C) |
| \( x \) | Distance travelled (cm) |
| \( v \) | Velocity (cm/s) |
| \( \mu \) | Dynamic viscosity (g/cm·s) |

Subscripts

| p | Powder |
| c | Casing |
Appendix. Heat Balance around Surface Node

The heat balance for a surface node (aa’c’c”) can be written as follows (Fig. A-1):

\[
\begin{align*}
\text{Heat-in through surface } b'b' & \quad \text{Heat-in through surface } c'c'' & \quad \text{Heat gained by surface } bb'c'c'' \\
\quad \text{(Term-I)} & \quad \quad \text{(Term-II)} & \quad \text{(Term-III)} \\
\rightarrow & \quad \quad \rightarrow & \quad \rightarrow \\
\text{Surface temperature}\rightarrow & \quad \text{Surface temperature}\rightarrow & \quad \text{Surface temperature}\rightarrow \\
\text{M} & \quad \text{M} & \quad \text{M} \\
\text{P} & \quad \text{P} & \quad \text{P} \\
\text{bath} & \quad \text{bath} & \quad \text{bath} \\
\end{align*}
\]

According to the available technique of discretisation, the calculation of surface node temperature based on Eq. (A-1) is done by considering only the half node (bb’c’). Thus, the Eq. (A-1) becomes

\[
\text{Heat-in through surface bb'} + \text{Heat-in through surface cc'} = \text{Heat gained by surface bb'cc'}
\]

\[
\begin{align*}
\text{(Term-I)} & + \text{(Term-II)} = \text{(Term-III)} \\
\rightarrow & = \\
\text{Surface temperature}\rightarrow & \quad \text{Surface temperature}\rightarrow & \quad \text{Surface temperature}\rightarrow \\
\text{M} & \quad \text{M} & \quad \text{M} \\
\text{P} & \quad \text{P} & \quad \text{P} \\
\text{bath} & \quad \text{bath} & \quad \text{bath} \\
\end{align*}
\]

\[\text{.................................................(A-2)}\]

The volume of the ‘shell’ (aa’b’b’ as shown in Fig. A-1) is ignored as it is negligible as compared to the volume of a full node. The time when this ‘shell’ matures to a thickness of a full node, then only it is made part of the calculation by forming a new node (say M’+1). The new surface node again remains a half node till its maturity to a full node and the calculation goes on like this. Thus, the thermal effect of the ‘shell’ is getting ignored until the formation of the new node even if sometimes its size becomes very close to a full node. This leads to an error in estimation of the total thermal effect on the wire which leads to faulty prediction of melting time as well as the inner temperatures. Figure A-2 presents the predicted variation of the radius of a 3.81 pure Niobium cylinder during its dissolution in liquid steel using this method along with the published data.\(^\text{25}\) As the node size is reduced from 0.1 cm to 0.005 cm, the position of radius curve changes and the time for complete melting reduces. It is evident that the error increases as the node size is increased. Thus, it becomes essential to check the dependency of the model on the node size. This is the drawback of the above modelling technique.

A new method has been developed by the authors to take care of this problem. During the calculation of the surface node temperatures, the actual shell volume (i.e., total volume of \(\Delta r/2\) and shell in Fig. A-1) at that point of time has been considered instead of considering only the half-node volume (i.e., volume of \(\Delta r/2\)). Figure A-3 presents the predicted variation of the radius of a 3.81 pure Niobium cylinder during its dissolution in liquid steel, using the modified method, along with the published data.\(^\text{23}\) It is clear that the dependency of the model on the node size has significantly reduced and in the opinion of the authors this represents an improvement in the existing numerical modelling techniques in such conditions.