On the In-Die Conditions and Process Parameter Settings in Indirect Squeeze Casting

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Abstract: The current study investigated the relationship between the process settings and in-die conditions to understand the transitions between the different filling stages and the final pressure settings in indirect squeeze casting. A pressure sensor was placed in the die cavity to indirectly measure the evolution of pressure over time and monitor the filling process to study the in-die conditions. The pressure–time profile was analysed, and the maximum pressure and acceleration of the pressure were investigated empirically. The main conclusion of this paper is that the use of increasing intensification pressures is positive for the casting soundness. However, it must be stressed that there is a strong effect from the intensification pressure on the acceleration that has a far more reaching influence than the actual speed setting. A direct practical outcome is that a high intensification pressure has a more substantial effect than the second stage fill speed. This translates directly to a possibility of reducing the second stage fill speed to stabilise the fill front. Furthermore, this also pinpoints the need for improvements in hydraulics system designs to decouple the intensification pressure from the filling piston motion control.

Keywords: squeeze casting; process parameters; pressure; sensing; aluminium; component casting; hydraulics

1. Background

Squeeze casting (SC) can, in comparison to high-pressure die-casting, produce high-quality castings (HPDC). The trade-off using SC instead of HPDC is a longer cycle time. There are two variants of SC, direct squeeze casting (D-SC) and indirect squeeze casting (I-SC). The main difference between D-SC and I-SC is that pressure acts directly on the part for D-SC, and for I-SC, pressure acts through an oversized runner with a minor pressure loss compared to HPDC. I-SC also easily allows for multi-cavity die solutions [1].

In a recent work by Fiorese et al. [2], the root mean square acceleration significantly impacted the part quality in HPDC and could be directly related to tensile strength. This entity was defined as
The equation is given by:

\[
\alpha_{RMS} = \frac{1}{T} \int_0^T \sqrt{\left(\frac{\alpha}{\pi}\right)^2} \, dt
\]

where \(v\) is piston speed (m/s), \(t\) is time (s), and \(T\) is period (s). The \(\alpha_{RMS}\) value for the switch between the first and the second phase in HPDC was concluded to determine the quality and not just the actual speed itself. The greater the value of \(\alpha_{RMS}\), the higher the tensile strength. The analysis was made assuming that the maximum force was limiting the acceleration, which is reasonable from the idealised shot curve settings based on motion primitives or assumptions on plunger motion control. There was no direct coupling to the design of the hydraulic system and its consequences.

In the study by Fiorese et al. [2], the modelling was of the piston motion itself, but in reality, the motion of the melt front would be most critical, and a possibility to assess this indirectly would be through die cavity pressure measurements. In this manner, pressures in the runners and die cavity and their relationships to the in-die service conditions can be assessed. This will also aid in understanding the filling and solidification dynamics.

In an HPDC system, the pressure varies with location and time. The high plunger speeds induce oscillations originating from hydraulic pressure waves and the filling dynamics of the melt. Firstly, during filling due to the melt flow dynamics from viscosity and the venting dynamics acting to the counter pressure in a high-velocity process as HPDC. Secondly, in feeding to compensate for solidification shrinkage [3].

The mechanisms and their relationships in I-SC are expected to have some similarities but are not identical to HPDC, including the relationships with the plunger acceleration identified for HPDC by Fiorese et al. [2]. This was investigated by Jarfors et al. [4] and demonstrated a strong interaction between the first phase speed and the intensification pressure not identifiable in the study by Fiorese et al. [2]. Jarfors et al. [4] showed significant relationships between pressures and pressure acceleration with the rejection rate in the die. They were acceptable combinations at different locations in the die and combinations that resulted in high rejections.

The melt is supplied below as an under-up casting process in I-SC to provide the best possible filling conditions. I-SC has the capability for the multi-cavity solution, with increased productivity, adding a runner to the die, making it similar to HPDC. Speeds are, on the other hand, significantly slower [5,6].

Chang et al. [5] furthermore concluded that for I-SC defects such as entrapped air or gas could be counteracted by squeeze pressure above 80MPa for the A356 alloy.

The current study focuses on the in-die conditions and their relationships to process parameters. The main target is how the settings influence these conditions to provide an in-depth understanding of the relationships between speed settings, intensification pressures, and the in-die pressure acceleration since this profoundly impacts part quality [4,7].

### 2. Experimental Work

#### 2.1. Material

The material used in the current study was A356 with its typical specification, as shown in Table 1.

| Si  | Mg  | Fe  | Cu  | Zn  | Mn  | Ti  | Al  |
|-----|-----|-----|-----|-----|-----|-----|-----|
| 6.5–7.5 | 0.25–0.45 | 0.2 | 0.2 | 0.2 | 0.1 | 0.1 | Bal |

#### 2.2. Casting Process and Component

A 650-ton vertical squeeze casting machine (China Academy of Machinery Sciences and Technology (Jiangle) Institute of Semi-solid Metal Technology Co., Ltd., Sanming, China) was used for the study. In all experiments, the casting temperature was kept...
constant at 653 °C during the investigation to limit the study to the effects of the filling process.

Pressure sensors were added to the die to assess the pressures in the runners, main die cavity, and one of the overflows. The sensor placed in the actual part cavity was used for the current analysis. The pressures were also measured at the piston side of the hydraulic system and the return side of the hydraulic pump corresponding to the hydraulics control pressures. Three switch points for plunger travel were set to 100 mm, 220 mm, and 270 mm to control filling. At a travelled distance of 300 mm, the switch to the intensification phase of the squeeze casting cycle started, Figure 1, to control feeding. In the current study, the sensor measuring the pressure in the die cavity, P2, near the location of the switch point at 270 mm is being analysed.

Figure 1. The component illustrating the metal levels for the switch points during filling [4].

2.3. Experimental Design

The current work is an extended analysis of the data presented by Jarfors et al. [4]. This means that the experimental plan was based on an offset around the ideal conditions in production for the part in, Figure 1. The first switch point at 100 mm stroke represents the location of a choke in the gating system and represents, in that sense, a similar flow restriction as to the gates in HPDC. The first switch point was chosen as one of the parameters to investigate and corresponds to the switch to the second stage in HPDC studied by Fiorese et al. [2]. The first phase speed was also chosen to be studied. Based on the study by Xu and Ying [6], under the assumption that the filling process was at an optimum, all other settings were kept constant apart from the intensification pressure that was varied as well. In the work by Jarfors et al. [4], both pressure acceleration and final pressure were found critical for the part rejection rate. Pressure intensification was initiated after a stroke of 300 mm and was typically at an end at 307 mm.

The design employed by Jarfors et al. [4] was a D-optimal Response Surface Design and included five lack-of-fit testing points, and five replicates, using the DesignExpert™ software (StateEase Inc., Minneapolis, MN, USA). At each setting, 10 samples were taken. In the execution of the experiments, 10 dummy shots were made before sample taking to reach a new steady state for the new conditions. A total of 400 castings were made, of which 200 castings were evaluated. The data presented are the averages of the 10 evaluated runs at each set-point combination, Table 2.
Table 2 Experimental plan and results [4].

| Run | Switch Point (mm) | First Phase Speed Valve Setting (%) | Intensification Pressure (%) | (dP/dt)²Max | (P)Max |
|-----|------------------|------------------------------------|----------------------------|-------------|--------|
| 1   | 85               | 6                                  | 50                         | 3.685 × 10²⁰| 40.41  |
| 2   | 60               | 6                                  | 24                         | 2.6095 × 10²⁰| 43.04  |
| 3   | 100              | 18                                 | 50                         | 4.885 × 10²⁰| 43.58  |
| 4   | 60               | 12                                 | 22                         | 2.974 × 10²⁰| 49.99  |
| 5   | 100              | 6                                  | 10                         | 9.476 × 10⁹ | 40.61  |
| 6   | 100              | 10                                 | 33                         | 5.6366 × 10²⁰| 49.65  |
| 7   | 76               | 18                                 | 33                         | 5.746 × 10¹⁰| 51.06  |
| 8   | 76               | 10                                 | 10                         | 9.745 × 10⁹ | 46.82  |
| 9   | 60               | 6                                  | 50                         | 5.24 × 10¹⁰ | 45.16  |
| 10  | 60               | 18                                 | 10                         | 9.786 × 10⁹ | 47.76  |
| 11  | 100              | 18                                 | 10                         | 7.937 × 10⁹ | 49.27  |
| 12  | 100              | 10                                 | 50                         | 6.0679 × 10²⁰| 46.75  |
| 13  | 60               | 13                                 | 50                         | 6.303 × 10²⁰| 46.25  |
| 14  | 100              | 10                                 | 33                         | 5.2326 × 10²⁰| 46.49  |
| 15  | 82               | 14                                 | 50                         | 9.077 × 10¹⁰| 50.57  |
| 16  | 60               | 18                                 | 10                         | 2.3336 × 10¹⁰| 39.16  |
| 17  | 82               | 6                                  | 23                         | 7.613 × 10⁹ | 22.30  |
| 18  | 100              | 18                                 | 10                         | 2.299 × 10²¹| 24.51  |
| 19  | 100              | 9                                  | 37                         | 1.2035 × 10²⁰| 37.90  |
| 20  | 72               | 9                                  | 37                         |           |        |

3. Results and Discussion

3.1. Process Parameter and Pressure Acceleration

The in-die pressure acceleration (dP/dt)²Max was evaluated as a function of the process parameters, Table 3. It should be noted that the pressure acceleration was transformed after a Box–Cox analysis suggesting the use of the natural logarithm.

Table 3. ANOVA table for the in-die pressure acceleration (dP/dt)²Max.

| Source      | Sum of Squares | df | Mean Square | F-Value | p-Value |
|-------------|----------------|----|-------------|---------|---------|
| Model       | 11.25          | 3  | 3.75        | 51.76   | <0.0001 | significant |
| B-First phase| 0.4020         | 1  | 0.4020      | 5.55    | 0.0316  | significant |
| C-Intensification | 9.69     | 1  | 9.69        | 133.72  | <0.0001 | significant |
| C²          | 1.37           | 1  | 1.37        | 18.90   | 0.0005  | significant |
| Residual    | 1.16           | 16 | 0.0725      |         |         |            |
| Lack-of-fit | 0.6448         | 11 | 0.0586      | 0.5697  | 0.7972  | not significant |
| Pure Error  | 0.5145         | 5  | 0.1029      |         |         |            |
| Cor Total   | 12.41          | 19 |             |         |         |            |

The Model F-value of 51.76 implies that the model is significant. There is only a 0.01% chance that this large F-value is due to noise. In the current analysis, P-values less than 0.0500 indicate model terms were significant. In this case, B, C, C² are significant model terms. Values greater than 0.1000 were used to disregard model terms that were not significant. The lack-of-fit F-value of 0.57 implies the lack-of-fit is not significant relative to the pure error. There is a 79.72% chance that a lack-of-fit F-value this large could occur due to noise.

The resulting regression model, with the suggested response transformation, was

\[
\ln \left( \frac{(dP/dt)_{\text{Max}}^2}{(dP/dt)_{\text{Max}}} \right) = 21.49319 + 0.029575 \times B + 0.134922 \times C - 0.001541 \times C^2
\]  

(2)

Interestingly, in Equation (2), there is a significant effect, both statistically and physically, from the choice of intensification pressure, on the in-die pressure rate of increase,
Figure 2. An interaction between the intensification pressure setting and the plunger acceleration influences the pressure build-up in the die. The relationship between \( \frac{dP_2}{dt} \)\(_{\text{Max}} \) and in the first stages speed is linear and similar to what Fiorese et al. [2] showed in their sensitivity analysis with nearly linear dependence. The acceleration of the pressure in the die \( \frac{dP_2}{dt} \)\(_{\text{Max}} \) bears similar importance as the piston acceleration in Equation (1), and it is the large impact of the intensification pressure on both the rate of pressure acceleration and piston acceleration that is the focus of the current paper.

3.2. Mathematical Analysis of the Correlation between In-Die Pressure and the Process Parameters

The hydraulics used for feeding and pressurising the melt in squeeze casting equipment has a schematic shown in Figure 3.
Figure 3. Schematic drawing of the plunger intensifier with, 1-pump, 2-conical on-off valve, 3-hydraulic cylinder, 4-conical proportional/servo valve and 5-oil tank.

The pump in a hydraulic system provides the flow and pressure and follows the hydraulic law for conical valves [8]:

\[ PQ^2 = C_1 \]  \hspace{1cm} (3)

where \( P \) is pressure (Pa), signifying the maximum pressure available at 3-Hydraulic cylinder. \( C_1 \) is the pump constant (Pa m\(^3\)/s\(^2\)), and \( Q \) is the volumetric flow (m\(^3\)/s). Variations in speed and pressure affect the pump and the volumetric flow, which can be evaluated by taking the time derivative of Equation (3) as:

\[
\frac{d(PQ^2)}{dt} = \frac{dP}{dt}Q^2 + 2PQ \frac{dQ}{dt} = 0
\]  \hspace{1cm} (4)

resulting in that

\[
\frac{dP}{dt} = -2 \frac{P}{Q} \frac{dQ}{dt}
\]  \hspace{1cm} (5)

Translating this into piston speed can be made using continuity and incompressibility as

\[ Q = A_3 v \]  \hspace{1cm} (6)

where \( A_3 \) is the hydraulic cylinder cross-section area (m\(^2\)) at 3 in Figure 3 and \( v \) is piston speed (m/s). Taking the derivative of Equation (6) and reintroducing it into Equation (5) allows for the elimination of the volumetric flow, expressing the relationship between pressure and piston speed:

\[
\frac{dP}{dt} = -2 \frac{P}{Q} A_3 \frac{dv}{dt} = -2 \frac{P^{3/2}}{\sqrt{C_1}} A_3 \frac{dv}{dt}
\]  \hspace{1cm} (7)

However, to control the motion of the piston, a net force, \( F \) (N), is driven by the pressure at 3-Hydraulic Cylinder, a counter pressure, \( P_4 \), is generated at 4-Proportional/Servo valve on the surface, \( A_4 \), resulting in that:

\[ F = P_3A_3 - P_4A_4 = m \left( \frac{dv}{dt} \right) \]  \hspace{1cm} (8)

Equation (8) is also related to the pressure and conical pressure reducing valves, allowing Equation (8) to be rewritten as

\[ F = P(\psi_3A_3 - \psi_4A_4) = m \left( \frac{dv}{dt} \right) \]  \hspace{1cm} (9)

Here \( \psi \) is the dimensionless pressure reduction factor, and \( m \) is the mass (kg) to be accelerated, including the piston and melt mass in the shot sleeve. Taking the derivative of Equation (9) with respect to time:
The solution of the complementary equation is expressed as:

\[ \frac{d^2v}{dt^2} = \frac{1}{(\varphi_3A_3 - \varphi_4A_4)} \left( m \frac{d^2v}{dt^2} - P \left( \left( \frac{d\varphi_3}{dt} A_3 - \frac{d\varphi_4}{dt} A_4 \right) \right) \right) = -2 \frac{\rho^{3/2}}{\sqrt{c_1}} A_3 \frac{dv}{dt} \]  

(12)

And after rearranging Equation (12) as

\[ m \frac{d^2v}{dt^2} = P \left( \left( \frac{d\varphi_3}{dt} A_3 - \frac{d\varphi_4}{dt} A_4 \right) \right) - 2 (\varphi_3A_3 - \varphi_4A_4) \frac{\rho^{3/2}}{\sqrt{c_1}} A_3 \frac{dv}{dt} \]  

(13)

The driving pressure is usually kept constant or increased, meaning that \( \frac{d\varphi_3}{dt} \geq 0 \) whilst the counter pressure is reduced, meaning that \( \frac{d\varphi_4}{dt} < 0 \). Assuming for the sake of simplicity that \( \varphi_3A_3 \gg \varphi_4A_4 \) allows the simplification of Equation (13) to

\[ m \frac{d^2v}{dt^2} = P \left( \left( -\frac{d\varphi_4}{dt} A_4 \right) \right) - 2 (\varphi_3A_3) \frac{\rho^{3/2}}{\sqrt{c_1}} A_3 \frac{dv}{dt} \]  

(14)

Assuming a linear rate of change for the valve \( \varphi_4 = -ct \), with being a response rate constant for the valve (Pa/\( \text{Pa} \text{s} \)) results in that

\[ \frac{d^2v}{dt^2} + 2 \frac{(\varphi_3A_3) \rho^{3/2}}{m \sqrt{c_1}} A_3 \frac{dv}{dt} = \frac{P(cA_4)}{m} \]  

(15)

Equation (15) is a second-order linear differential equation to be solved in a two-step process, starting with the complementary solution and then followed by the particular solution. The complementary solution is given by Equation (16)

\[ \frac{d^2v}{dt^2} + 2 \frac{(\varphi_3A_3) \rho^{3/2}}{m \sqrt{c_1}} A_3 \frac{dv}{dt} = 0 \]  

(16)

The solution of the complementary equation is Equation (17), with the roots shown in Equations (18) and (19)

\[ v_c = c_1 \exp(\lambda_1 t) + c_2 t \exp(\lambda_2 t) \]  

(17)

\[ \lambda_1 = -2 \frac{(\varphi_3A_3) \rho^{3/2}}{m \sqrt{c_1}} A_3 \]  

(18)

\[ \lambda_2 = 0 \]  

(19)

resulting in that the complementary equation solution becomes Equation (20)

\[ v_c = c_1 \exp \left( -2 \frac{(\varphi_3A_3) \rho^{3/2}}{m \sqrt{c_1}} A_3 t \right) + c_2 t \]  

(20)

The complete solution is then as shown in Equation (21)

\[ v = v_c + v_p \]  

(21)

The extra root is a constant, and therefore the form of the particular solution is assumed to be constant, meaning that the particular solution is

\[ v_p = \text{const.} \]  

(22)

The boundary conditions are based on that there is a switch from the first phase speed \( v_1 \), to the second phase speed, \( v_2 \).
\[ v(0) = c_1 + c_2 + v_p = v_1 \] (23)
\[ v(\infty) = c_2 + v_p = v_2 \] (24)

The derivatives of Equation (21) then become
\[ \frac{dv}{dt} = -2c_1 \left( \frac{\rho_s A_3}{m} \right) \frac{3}{\sqrt{c_1}} A_3 \exp\left(-2 \left( \frac{\rho_s A_3}{m} \right) \frac{3}{\sqrt{c_1}} A_3 t\right) + c_2 \] (25)
and
\[ \frac{d^2v}{dt^2} = \left( 2c_1 \left( \frac{\rho_s A_3}{m} \right) \frac{3}{\sqrt{c_1}} A_3 \right)^2 \exp\left(-2 \left( \frac{\rho_s A_3}{m} \right) \frac{3}{\sqrt{c_1}} A_3 t\right) + c_2 \] (26)
and reinserting Equations (25) and (26) into Equation (15) into the base equation gives
\[ c_1 \left( 2 \left( \frac{\rho_s A_3}{m} \right) \frac{3}{\sqrt{c_1}} A_3 \right)^2 \frac{dv}{dt} \exp\left(-2 \left( \frac{\rho_s A_3}{m} \right) \frac{3}{\sqrt{c_1}} A_3 t\right) + c_2 + 2 \left( \frac{\rho_s A_3}{m} \right) \frac{3}{\sqrt{c_1}} A_3 = \frac{p(c_A)}{m} \] (27)

Thus, by using the fact that particular solution is a constant, Equation (22), and the boundary conditions in Equations (23) and (24) results in that
\[ c_2 = \frac{\sqrt{c_1} (c_A)}{2 (\rho_s A_3) A_3 \rho^{3/2}} \] (28)
and
\[ v_p = v_2 - c_2 \] (29)
and
\[ c_1 = v_1 - v_2 \] (30)

Taking the results in Equations (28)–(30) results in that the complete solution is
\[ v = v_2 + (v_1 - v_2) \exp\left(-2 \left( \frac{\rho_s A_3}{m} \right) \frac{3}{\sqrt{c_1}} A_3 t\right) + \frac{\sqrt{c_1} (c_A)}{2 (\rho_s A_3) A_3 \rho^{3/2}} (t - 1) \] (31)

The final expression plunger acceleration is obtained by taking the derivative with respect to time of Equation (31), resulting in Equation (32)
\[ \frac{dv}{dt} = -2(v_1 - v_2) \left( \frac{\rho_s A_3}{m} \frac{3}{\sqrt{c_1}} A_3 \exp\left(-2 \left( \frac{\rho_s A_3}{m} \right) \frac{3}{\sqrt{c_1}} A_3 t\right) + \frac{\sqrt{c_1} (c_A)}{2 (\rho_s A_3) A_3 \rho^{3/2}} \right) \] (32)

Fiorese et al. [2] expressed acceleration as a third-order polynomial based on time. In reality, this should be displayed as an exponential function. Fiorese et al. [2] used the maximum pressure as a boundary condition limiting the force acting on the piston. Equation (32) exhibits far more complex behaviour.

The actual pressure, \( P \) (Pa), recorded in the die is the reaction pressure from the plunger acceleration and speed of the melt in the die \( v_{sl} \) (m/s), neglecting venting pressure build-up. The measured change depends on location and geometry as well as the state of the melt, which generically can be expressed as
\[ \frac{d}{dt} \left( \frac{dP}{dx} \right) = \frac{d}{dt} \left( c_{DA} \frac{v_{sl}}{L} \right) \] (33)
where \( c_{DA} \) is a Darcy flow permeability coefficient, including the melt viscosity (Pa s/m). The cavity pressure, \( P_2 \) (Pa), is measured with the outside pressure, \( P_a \) (Pa), as a reference taking venting and solidification into account. This would then lead to a simplified expression of Equation (33) as
\[
\frac{d}{dt}\left(\frac{d\rho}{dx}\right) \approx \frac{d}{dt} \left(\frac{P_2 - P_1}{\Delta k_2}\right) = \frac{1}{\Delta k_2} \frac{dP_2}{dt} = \frac{1}{\Delta k_2} \left(\frac{\Delta \rho A}{v_{sl}} + C \frac{d v_{sl}}{dt}\right) \tag{34}
\]

Assuming that in the timeframe of the shot transition, there is no change in the ambient pressure, nor in the state of the melt and the cross-section geometry resulting in that any change in \(C_{DA}\) the Darcy flow permeability can be neglected, simplifying Equation (34) to

\[
\frac{dP_2}{dt} = C_{DA} \frac{d v_{sl}}{dt} \tag{35}
\]

The relationship with the plunger motion speed, \(v_s\) is possible to relate through continuity under the assumption of incompressibility can be expressed as Equation (36)

\[
v_{sl} A_{sl} = v A \Rightarrow \frac{d v_{sl}}{dt} A_{sl} = \frac{d v}{dt} A \Rightarrow \frac{d v_{sl}}{dt} = \frac{d v}{dt} \frac{A}{A_{sl}} \tag{36}
\]

It is worthwhile noting that the pressure increase in the die cavity is directly proportional to the filling speed and thus also directly proportional to the piston speed under the assumption that any change in \(C_{DA}\) the Darcy flow permeability can be neglected in Equation (34). This results in that

\[
\frac{dP_2}{dt} = C_{DA} A \frac{\sqrt{\frac{v_1}{C_A}} - 2(v_1 - v_2) \frac{\rho_2^3}{m} \frac{1}{\sqrt{v_1}} A_3 \exp \left(-2 \frac{\rho_2^3}{m} \frac{1}{\sqrt{v_1}} A_3 t\right)}{A_{sl}} \tag{37}
\]

Since \(v_1 < v_2\) both terms are positive with the result that the maximum pressure increase in the die is at \(t = 0\) and using this to simplify Equation (37) as

\[
\left(\frac{dP_2}{dt}\right)_{\max} = K_1 \left(\frac{v_2^2}{v_1^{1/2}} - 2(v_1 - v_2)K_2 \rho_2^3\right) \tag{38}
\]

The three constants were fitted to the data from the production experiments in Table 1, resulting in the appearance shown in Figure 4a and in the form of Equation (38) in Figure 4b. Based on this, it is clear that it is the intensification pressure that has a significant impact on the acceleration between the first and second phases. It should be noted that the relationship between the acceleration and the actual setting of the first phase speed is not following the same pattern. There is a weak trend for increasing acceleration in the experimental fitting based on the measurements, whilst Equation (38) suggests an opposite trend, Figure 4a. However, this was only a weak tendency, and in the current study, friction and difference in the relative response time and setting of the valves were not included.
Figure 4. The level of acceleration between the first and second phase filling as a function of B-First phase speed and C-Intensification pressure according to equation (38) with: (a) squared values \((dP/dt)^2_{Max}\) for comparison with the ANOVA model; and (b) actual predicted acceleration values \((dP/dt)_{Max}\).

The effect of the intensification set point dominating the acceleration is essential for casting quality. Based on the principles developed by Fiorese et al. [2], increasing the pressure may have an element of being counterproductive in terms of the rate of acceleration.

4. Concluding Remarks

The current study investigated the relationship between the process settings and the in-die conditions to understand the transitions between the different filling stages and the final pressure settings in I-SC. The previous work by Fiorese et al. [2] showed that the \(a_{RMS}\), the root mean square value between the first and the second stage-filling was critical to the casting quality in HPDC. Jarfors et al. [4] saw that the acceleration between the first and second stages strongly influenced the rejection rate. The acceleration is also the foundation for the \(a_{RMS}\) value. Jarfors et al. [4] also found that the intensification pressure influenced reaction rate positively, which was expected but also saw a significant interaction involving first phase speed, intensification pressures and pressure increase rates. The nature of these interactions was the focus of this study to better understand and control both intensification and acceleration effects, since increasing the intensification pressure is a standard action to counteract porosity.

A pressure sensor was placed in the die cavity to indirectly measure the evolution of pressure over time and monitor the filling process to study the in-die conditions. The pressure–time profile was analysed, and the maximum pressure and acceleration of the pressure were investigated empirically.

The results showed that the intensification pressure dominated the pressure increase rate between the first and second stages in the die, compared to the actual speed settings. This was analysed based on the laws of hydraulics and translated into the pressure increase in the die cavity. This resulted in the same behaviour with the intensification pressure on the hydraulics system to dominate the pressure increase and the piston acceleration as these are proportional to each other. The nature of this interaction is built into the hydraulics systems design, and the model shows a direct relationship between the piston acceleration and the pressure increase in the die and thus the filling rate.
A direct practical outcome of this is that since a high $a_{RMS}$ for the switch is beneficial to part quality, increasing the intensification pressure increases $a_{RMS}$. This coupling, between $a_{RMS}$ and intensification pressure, would directly allow the second stage fill speed to be reduced, allowing the fill front to stabilise.

Another conclusion of this paper is that the use of increasing intensification pressures is positive for the casting soundness since an increase of $(dP/dt)_{max}$ translates to an increase of $a_{RMS}$. However, it must be stressed that this interaction has a more far-reaching influence than the actual speed setting. Furthermore, this also pinpoints the need for improvements in the hydraulics system designs to decouple the intensification pressure from the filling piston motion control.

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