On the CFD modeling of slamming of the metal melt in high-pressure die casting involving lost cores

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Abstract

This paper investigates the forces on the salt core in high-pressure die casting when being exposed to the impact of the inflowing melt in the die filling stage, with particular respect to the moment of first impact. The melt-air system is modeled via an Eulerian volume-of-fluid approach treating the air as a compressible perfect gas. The turbulence is treated via a RANS approach using the Menter SST $k$-$\omega$ model. The results for the mesh independence study indicate that uncommonly fine meshes and time steps are required to capture the effect of slamming on the core properly in order to be in line with existing analytical models and empirical measurements. As a second step, it is then being discussed what response should be expected when this force with its spike-like morphology and small force-time integral impacts the core. It is found that the displacement of the core due to the spike in the force is so small that, even though the force is high in value, the bending stress inside the core remains below the critical limit for fracture. It can therefore be concluded that when assuming homogeneous crack-free material conditions the spike in the force is not failure-critical.

\textit{Keywords:} compressible two-phase flow; slamming; OpenFOAM; high-pressure die casting; lost salt cores; solid continuum mechanics

1. Introduction

High-pressure die casting (HPDC) is an important process for manufacturing high volume and low cost automotive components, such as automatic transmission housings, crank cases and gear box components [1–3]. Liquid metal, generally aluminium or magnesium, is injected through complex gate and runner systems and into the die at high speed, typically between 50 and 100 ms$^{-1}$ at ingate, and under pressures as high as 100 MPa [4].

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From an economic point of view, the process is typically constrained by a huge base investment in machinery and tooling, but, on the other hand, low incremental costs for each additional unit produced. In other words, it scales very well with increasing output. However, this complicates the task for the design engineer, who has to be sure about the viability of a process and part beforehand, i.e. before the budget is invested in tooling and machinery. One technological constraint to date is that, so far, no serial production is in place where foundries produce high-pressure die casting parts with inlying hollow shapes or undercuts formed by lost cores. Several ideas for such products exist [5, 6], but a serial application is as yet unknown. One material that has been put forward to form these shapes is salt [5, 7, 8].

For several application and process issues, it has to date proven difficult to employ lost salt cores within the process [9]. The basic idea of using salt cores is to block parts of the die volume by inserting cores as placeholders; in so doing, the melt will not penetrate into this space. The cores may then be removed after solidification and one creates undercuts or hollow sections with them, which may then later act as cooling or oil-flow channels [5, 7, 8]. This is still a disadvantage for HPDC, as other casting techniques have employed lost cores for decades [1, 4].

Given this process constraint in design freedom for the CAD-engineer, the idea of using salt as the material for lost cores has been put forward by machine manufacturers, as well as automotive companies [6, 10]. One way to determine whether this is indeed a viable option for a given geometry is to employ numerical simulation - in particular, computational fluid dynamics (CFD) [9, 11–13].

This paper’s particular focus shall be on determining the load on the core during slamming. Slamming is a concept and phenomenon investigated in particular within the context of naval architecture problems [14–16] and appears when a two-phase interface with one phase of high density and another of low hit an obstacle. This can also be the case when investigating lost cores in high-pressure die casting, when the air surrounding the core is replaced by the much heavier melt. Also, in all previous simulations of high-pressure die casting and lost cores this peak appeared. It was therefore of scientific interest by the authors to investigate this peak more in detail and also evolve on the core’s response to it.

2. Model equations, parameters and boundary conditions

2.1. The compressible solver compressibleInterFoam

We model the two-phase flow of molten metal and air in high-pressure die casting by using the volume-of-fluid (VOF) method [17], wherein a transport equation for the VOF function, \( \gamma \), of each phase is solved simultaneously with a single set of continuity and Navier-Stokes equations for the whole flow field; note also that \( \gamma \), which is advected by the fluids, can thus be interpreted as the liquid fraction. Considering the molten melt and the air as Newtonian [18],
Compressible and immiscible fluids, the governing equations can be written as \[19, 20\]

\[
\frac{\partial p}{\partial t} + \nabla \cdot (\rho U) = 0,
\]

\[
\frac{\partial}{\partial t} (\rho U) + \nabla \cdot (\rho \nabla U) = -\nabla p + \nabla \cdot \left\{ (\mu + \mu_{\text{tur}}) \left( \nabla U + (\nabla U)^T \right) \right\} + \rho g + F_s,
\]

\[
\frac{\partial \gamma}{\partial t} + \nabla \cdot (\gamma U) + \nabla \cdot (\gamma (1 - \gamma) U_r) = -\frac{\gamma}{\rho_g} \left( \frac{\partial \rho_g}{\partial t} + U \cdot \nabla \rho_g \right),
\]

where \( t \) is the time, \( U \) the mean fluid velocity, \( p \) the pressure, \( g \) the gravity vector, \( F_s \) the volumetric representation of the surface tension force and \( T \) denotes the transpose. In particular, \( F_s \) is modelled as a volumetric force by the Continuum Surface Force (CSF) method \[21\]. It is only active in the interfacial region and formulated as \( F_s = \sigma \kappa \nabla \gamma \), where \( \sigma \) is the interfacial tension and \( \kappa = \nabla \cdot (\nabla \gamma / |\nabla \gamma|) \) is the curvature of the interface. The term \( U_r \) is a supplementary velocity field for compressing the phase interface introduced by the solving scheme for the \( \gamma \)-field. This solving scheme is named Multi-dimensional Universal Limiter with Explicit Solution (MULES) \[19, 22\]. In introducing this supplementary velocity field, the local flow steepens the gradient and the interface becomes sharper and more pristine. A typical form for \( U_r \) is \( U_r = \min(U, \max(U)) \), as given by \[19\]. The material properties \( \rho \) and \( \mu \) are the density and the dynamic viscosity, respectively, and are given by

\[
\rho = \gamma \rho_l + (1 - \gamma) \rho_g,
\]

\[
\mu = \gamma \mu_l + (1 - \gamma) \mu_g,
\]

where the subscripts \( g \) and \( l \) denote the gas and liquid phases, respectively. We take \( \rho_l, \mu_g \) and \( \mu_l \) to be constant, but assume the air to be an ideal gas, i.e. its density changes with pressure and temperature; hence, the equation of state for our model reads

\[
\rho_g = \frac{M_p}{R_s T} = \frac{p}{R_s T},
\]

where \( R_s \) is the specific gas constant and \( T \) is the temperature. The temperature is a new unknown in the system and it needs to be solved for via the heat equation \[19, 20\]

\[
\frac{\partial}{\partial t} (\rho T) + \nabla \cdot (\rho TU) = \nabla \cdot \left( \alpha_{ef} \nabla T \right) - \left( \frac{\gamma}{c_{v_l}} + \frac{1 - \gamma}{c_{v_g}} \right) \left( \nabla \cdot (\rho U) + \frac{\partial (\rho K)}{\partial t} + \nabla \cdot (\rho K U) \right),
\]

(7)
where \( K = \frac{1}{2} \mathbf{U} \cdot \mathbf{U} \) is the kinetic energy, \( c_{v_g} \) and \( c_{v_l} \) denote the specific heat capacities at constant volume for the gas and liquid phases, respectively, \( \alpha_{eff} \) is given by

\[
\alpha_{eff} = \frac{\gamma k_l}{c_{v_l}} + \frac{(1 - \gamma) k_g}{c_{v_g}} + \frac{\mu_{tur}}{\sigma_{tur}},
\]

(8)

where \( k_g \) and \( k_l \) denote the thermal conductivities for the gas and liquid phases, respectively, and \( \sigma_{tur} \) is the turbulent Prandtl number, whose value is set to 0.9 [23]. Note that \( \alpha_{eff} \) resembles a phase-averaged thermal diffusivity that includes the contribution of turbulence, although it lacks a density term in the denominator.

Furthermore, \( \mu_{tur} \) in equation (2) denotes the turbulent eddy viscosity, which will be calculated via the Menter \( k-\omega\)-SST model [24]. The implementation of this model inside the OpenFOAM framework has previously been shown to be robust and also to give results that are in excellent agreement with experimental data [25, 26].

The major purpose of this paper is to calculate the forces on the core during slamming. Those are governed by the following formula. For \( i = x, y, z \), the total force acting on the core in direction \( i \), \( F_{i,\text{tot}} \), is given by

\[
F_{i,\text{tot}} = F_{i,p} + F_{i,s},
\]

(9)

where \( F_{i,p} \) and \( F_{i,s} \) are pressure and viscous shear forces, respectively, and are given by

\[
F_{i,p} = -\int \int_{A_c} p n_i dA,
\]

(10)

\[
F_{i,s} = \int \int_{A_c} (\mu + \mu_{tur}) \left( \frac{\partial U_i}{\partial x_j} + \frac{\partial U_j}{\partial x_i} \right) n_j dA,
\]

(11)

where \( \mathbf{U} = (U_x, U_y, U_z) \) as the velocity and its components and \( A_c \) is the surface area of the core.

This concludes and completes the two-phase compressible flow model that was used throughout the simulations in the fluid region. The values of the model parameters are given in table 2 in section 3 later on.

3. Geometry, boundary and initial conditions and material parameters

All simulations were conducted on the 2D-geometry shown in figure 1. One sees that the geometry features an inlet and an outlet, as well as wall patches and the patch for the salt core, which has also wall properties with the addition that the forces on the patch were evaluated via a function object called \textit{forces}.

The corresponding mesh for the 2D-geometry shown in figure 1 is illustrated in figure 2. The reader can see that this is a structured mesh consisting of structured hexahedral cells. It is also parametrized and can therefore easily be adjusted with respect to its fineness for later mesh independency studies.
For this purpose the OpenFOAM utility `refineMesh` was used that, by default, halves the mesh spacing upon execution, i.e. quadrupling the cell count for a 2D mesh as in figure 2.

Figure 2: An example of a computational grid created with the utilities `blockMesh` and `mirrorMesh` for a mesh spacing of 2 mm

A summary of the mathematical expressions of the applied boundary conditions is presented in table 1.

<table>
<thead>
<tr>
<th>patch name</th>
<th>$\gamma$</th>
<th>$U$ / ms$^{-1}$</th>
<th>$p$ / Pa</th>
<th>$T$ / K</th>
</tr>
</thead>
<tbody>
<tr>
<td>inlet</td>
<td>$\gamma = 1$</td>
<td>$U = U_{in} \cdot e_x$</td>
<td>$n \cdot \nabla p = 0$</td>
<td>$T = T_{melt}$</td>
</tr>
<tr>
<td>outlet</td>
<td>$n \cdot \nabla \gamma = 0$</td>
<td>$(n \cdot \nabla) U = 0$</td>
<td>$p = p_{amb}$</td>
<td>$n \cdot \nabla T = 0$</td>
</tr>
<tr>
<td>salt core</td>
<td>$n \cdot \nabla \gamma = 0$</td>
<td>$U = 0$</td>
<td>$n \cdot \nabla p = \rho (g \cdot n)$</td>
<td>$n \cdot \nabla T = 0$</td>
</tr>
<tr>
<td>wall</td>
<td>$n \cdot \nabla \gamma = 0$</td>
<td>$U = 0$</td>
<td>$n \cdot \nabla p = \rho (g \cdot n)$</td>
<td>$n \cdot \nabla T = 0$</td>
</tr>
</tbody>
</table>

Table 1: Boundary conditions for the presented model

As briefly hinted when introducing the turbulence model, all simulations were conducted within the OpenFOAM software package [27–30] due to the niche nature of the field of application, as well as to make use of its extendability. OpenFOAM has most of its applications designed for solving fluid mechanics problems [28, 31] and applies the finite volume method [23, 32, 33] to solve them. With the later aim of scaling the model to complex industrial 3D-geometries in mind, OpenFOAM is also a very powerful tool since the distribution to a huge number of cores is not limited due to license restrictions, as is the case with commercial CFD codes.

As for the initial conditions of the die, the cavity is filled with air ($\gamma = 0$) at rest ($U = 0$), as warm as the ambient air ($T = T_{amb}$) and has not been evacuated ($p = p_{amb}$).
Table 2: Model parameters. The parameters for gas are those for air; those for metal are for the alloy AlSi9Cu3 [13, 18, 34]

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>$c_{vg}$</td>
<td>720 J kg$^{-1}$ K$^{-1}$</td>
</tr>
<tr>
<td>$c_{vl}$</td>
<td>1000 J kg$^{-1}$ K$^{-1}$</td>
</tr>
<tr>
<td>$k_g$</td>
<td>0.026 W m$^{-1}$ K$^{-1}$</td>
</tr>
<tr>
<td>$k_l$</td>
<td>70 W m$^{-1}$ K$^{-1}$</td>
</tr>
<tr>
<td>$M$</td>
<td>0.028 kg mol$^{-1}$</td>
</tr>
<tr>
<td>$T_{amb}$</td>
<td>293 K</td>
</tr>
<tr>
<td>$p_{amb}$</td>
<td>$10^5$ Pa</td>
</tr>
<tr>
<td>$T_{melt}$</td>
<td>823 K</td>
</tr>
<tr>
<td>$U_{in}$</td>
<td>10, 20 m s$^{-1}$</td>
</tr>
<tr>
<td>$\mu_g$</td>
<td>$1.8 \times 10^{-5}$ Pa s</td>
</tr>
<tr>
<td>$\mu_l$</td>
<td>$1.62 \times 10^{-3}$ Pa s</td>
</tr>
<tr>
<td>$\rho_l$</td>
<td>2520 kg m$^{-3}$</td>
</tr>
<tr>
<td>$\sigma$</td>
<td>0.629 N m$^{-1}$</td>
</tr>
</tbody>
</table>

The initial conditions for the turbulence model are set via the length scale of the largest eddies and the turbulence intensity. Those quantities were set to 2 mm and 5 %, respectively.

What is now still missing are the material properties of the phases. Those are presented in table 2. Please note that $g$ may need to be transformed for the particular coordinate system.

One additional comment concerns the specific heat capacities $c_{pg}$ and $c_{pl}$: the index $p$ means that their values have been measured and documented for constant pressure, which are more commonly tabulated rather than $c_{vg}$ or $c_{vl}$. Equation (7), however, requires $c_v$. This conversion can be conducted via the value of the isentropic expansion factor or heat capacity factor, which can for air be assumed to be constant and equal to $c_{pg}/c_{vg} = 1.4$ in the given temperature range [13, 35, 36]. For the aluminum melt, this additional step is not necessary, as it was treated as being incompressible; thus, $c_{vl} = c_{pl}$.

As the physics of the flow domain are a rather complex flow problem in the field of CFD research, it will be worthwhile to additionally look at the flow problem and its solving algorithm a little more closely. In order to do that, figure 3 is presented. Here, the standard PIMPLE algorithm, a combination of PISO [37] and SIMPLE [38], was modified and for this purpose an additional step at the beginning was added that solved the transport equation of the phase variable $\gamma$ according to equation (3) at the beginning of the solving algorithm, i.e. before the momentum predictor.

Figure 3 shows the original PISO algorithm on the left and on the right the reader sees the implemented extension of it including the additional step for the phase field for handling the two-phase flow. The corrector loops can either be broken by a residual or a maximum number criterion. One additional comment for the sake of completeness is that the outermost loop for the increasing time
step is omitted as the chart only shows the processes that happen within each time step.

4. Results and discussion

4.1. The concept of the dimensionless slamming factor

Following the results of recently published articles on salt cores in high-pressure die casting [9, 11, 13, 39], in almost every simulation where the core was treated as rigid, the signature peak appeared in the force vs. time plot. The ambition was therefore to investigate its nature in more detail. This will be done by introducing the dimensionless slamming factor $C_s$, which is defined as follows:

$$C_s = \frac{F}{\rho RU^2 t}.$$  

The letters on the right-hand side in equation (12) are the computed force $F$ according to equations (9) to (11), density $\rho$, velocity $u$ as well as the radius $R$. 

Figure 3: PISO algorithm before and after the adjustments
and the length of the core $l$. Analytical considerations have been made by von Karman [40] and Wagner [41] to determine this slamming factor with the result that according to von Karman the slamming factor for a cylinder was $\pi$ while Wagner put it at $2\pi$. More recent studies [14, 42], however, indicate that the value according to von Karman represents a lower limit of the slamming factor, while the Wagner model acts as an upper cap. Both models also fail to provide an evolution of the value over time. Campbell and Weynberg [15, 43] present a model that offers results based on empirical data to determine the plot over time according to the formula

$$C_s = \frac{F(t)}{\rho R U^2 l} = \frac{5.15}{1 + 9.5 U t R^{-1}} + 0.275 U t R^{-1}. \quad (13)$$

The letters in equation (13) represent the same quantities as in equation (12).

The introduced dimensionless slamming factor makes calculated results indifferent from initial conditions such as the ingate velocity or geometric dimensions. This is further illustrated in the following plot. Figure 4 shows that the calculated slamming factor is the same no matter whether it is being evaluated for an ingate velocity of 10 or 20 ms$^{-1}$. Those values for $U_{in}$ were used for calculating the slamming factor according to the presented formulae.

![Figure 4: Normalized forces on the core for a mesh resolution of 0.3 mm](image)

### 4.2. Determining the slamming factor with respect to mesh resolution

The model setup was calibrated with existing data on similar cases such as the water entry of an axisymmetric projectile [44] with similar Reynolds numbers and also for the feature of the pile-up effect [42, 45, 46].

Figures 5 to 7 show the temporal development of the phase distribution, the pressure and the velocity at impact on the core. Please note that the current solver does not take the temperature difference between the core, the walls and the melt into account as zero heat-flux boundary conditions for the walls were applied (see table 1). The individual pattern in the near-wall area may therefore differ due to heat transfer induced forces.
Figure 5: Phase distribution of the melt at impact and bypassing

Figure 6: The pressure field at the times of impact and bypassing

Figure 7: The velocity magnitude field at the times of impact and bypassing
After calibrating the setup it was time to proceed to evaluating the forces on the core and benchmark them with previously published data. The first thing to determine was the necessary mesh resolution in order to compute plausible results for the slamming factor. The result of this mesh resolution study is presented in figure 8.

The meticulous investigation of this phenomenon of slamming showed that with generally used meshing standards in industry and mesh spacings in the range of 1 mm [11], the CFD simulation underpredicts the value of the slamming factor (see figure 8). It becomes evident that the simulation requires a mesh as fine as a spacing of 0.3 mm in order to be above the lower limit of the slamming factor according to the von Karman model. This is significantly lower than the typical mesh spacing in industry and requires a mesh for this rather small geometry to consist of total of 60,000 cells albeit being a 2D-mesh. Transferring these results onto a real world casting geometry including salt cores and designing a 3D-mesh according to these findings will in turn produce a mesh that is completely impractical to use with currently available computational capacity.

While figure 8 showed that coarser meshes underpredict the peak in the force, the stationary value of the force tends to be overpredicted the coarser the mesh is. From a design engineer’s point of view, this is a beneficial outcome as it includes a built-in safety factor into the design of a devised product.

Figure 9 shows this study’s result in comparison with the findings of pre-
Figure 9: Comparison of the computed result with reference studies in previously published articles; mesh cell spacing 0.025 mm

Previously published articles on the matter. The results are plotted for a mesh fineness of 0.025 mm spacing. Technically, only a refinement of the cells in the near core area would be necessary. However, given the fact that then the structured nature and the numerical benefits that come along with it, would have to be sacrificed, this opportunity was forgone here as the domain was sufficiently small and calculation efficiency was not a benchmark in this academic study. It should be noted that the analytical models start with $t = 0$ at the point of impact. The numerical simulation started earlier. The simulation time values were therefore adjusted to this time scale leading to sometimes negative values.

Figure 9 shows also the values for several other models, among them the static von Karman [40] and Wagner [41] models as well as more recent models that also represent the development of the force over time. It can be concluded that this study’s assessment of the slamming factor is very well in the middle of the earlier authors' findings. One interesting observation of the present volume-of-fluid model is that its increase is, compared to the mostly analytical models, more gradual. The other models feature a sharp step-like increase in value resembling the morphology of a Heaviside function [49].

The reason for this gradual increase is to be found most likely inside the volume-of-fluid approach itself. As equations (4) and (5) illustrated, the material properties are being averaged by their volume fraction, $\gamma$. Another reason for this feature is to be found in the numerical approach of this study, where the numerics are known to smear out the interface of volume-of-fluid simulations [22] and therefore artificial interface compression terms are to be introduced.
into the volume fraction’s PDE [13, 22]. This effect of a non-discrete interface causes additional problems in stability of the *interFoam* solver-family in the sense of spurious parasitic velocities at the interface and several strategies to mitigate the effects towards a sharper interface have since been proposed [50–53]. We can conclude that, taking into account figures 8 and 9, one has at the given boundary conditions at least maintain a mesh fineness corresponding with a spacing of 0.2 mm to reach a value above the limit defined by von Karman in [40], if one wants to achieve a proper result for the slamming factor. Transferring this finding to more applicable designs in industry, it underscores the necessity of boundary layers every time the load on salt cores is to be evaluated via a volume-of-fluid approach.

The aspect of turbulence treatment on the slamming factor in the simulations was also evaluated during the investigations. Figure 10 features this result. Two different turbulence models were benchmarked against each other for the given case and the different mesh spacings as illustrated in figure 10. The benchmarked turbulence models are Menter’s $k$-$\omega$ SST model [24, 54] and the RNG $k$-$\varepsilon$ model [55]. The reason why those two were picked are that the Menter model was proven to be of excellent stability and accuracy proven in studies published by other authors [25] and our own results published in previous papers [12, 13]. The RNG-model was selected as it is the standard of the commercial CFD casting software Flow-3D Cast. Both turbulence models belong to the RANS-model family (Reynolds-Averaged-Navier-Stokes equations).

As is evident from figure 10, the selection of the turbulence model is of minor importance. Even with different mesh spacings, the result of the slamming factor stayed more or less the same. This is plausible as the main contribution to the slamming results from the pressure forces on the core – an effect documented in more detail in [13]. For those forces, the momentum of the melt is more important compared to other factors that happen inside the fluid. This is also
4.3. Response of the core to the spike-like force impact

Although it is academically interesting to determine the value for the slamming factor as precisely as possible, the general underlying question is also whether such a small force-time interval is failure critical for salt cores in high-pressure die casting in general. We will therefore in the following present a couple of considerations to assess the necessity for the engineer to keep the slamming factor below a certain level.

If one for this purpose leaves the field of 2D considerations and imagines how a beam of length 70 mm that is situated within the die facing the inflowing melt stream orthogonally would react to the inflowing melt and the slamming, one would make the following observations. The consideration is of course based on the assumption that the inflowing melt stream stays as thin as in the test case, a fact that has emerged in more application oriented studies [13, 39]. In general any force impacting on the core at one point travels through the solid body according to the following formula. It assumes that no mechanical signal can proceed in solid media faster than the speed of sound

\[ v_s = \sqrt{\frac{K}{\rho_s}}, \]  

with \( v_s \) being the speed of sound in salt, \( K \) being the bulk modulus for salt cores and \( \rho_s \) being the density of salt cores. \( K \) relates to the documented quantities for salt cores [39] in the following way [57, 58].

\[ \]
Figure 11: Results of the time step size $\Delta t$ study

$$K = \frac{E}{3(1-2\nu)}, \quad (15)$$

with $E$ as the Young’s modulus and $\nu$ as the Poisson ratio. Assuming $\rho_s$ as 2056 kgm$^{-3}$, $\nu$ as 0.21 and $E$ as $1.5\times10^{10}$ Pa [39], equations (14) and (15) yield the speed of sound in salt as $2048$ ms$^{-1}$. Based on figure 4, one can now assume 0.02 as a scale for the dimensionless filling time yielding to $3.5\times10^{-6}$ s as an absolute time scale during which the peak force acts on the core. Together with the calculated speed of sound, this means the stress signal traveling a distance below 10 mm through the core, i.e. referring back to the beam with 70 mm in length, it will not even be able to reach the bearing.

If we further assume a slamming factor as reported in figure 9 of $\frac{3}{2}\pi$ the
absolute force according to equation (12) is slightly below 1400 N. Now applying Newton's second law of mechanics

\[ F = ma \]  \hspace{1cm} (16)

and integrating it twice to obtain

\[ s = \frac{1}{2}at^2, \]  \hspace{1cm} (17)

one may get an idea of how far the center of the core travels. Based on the calculations above for the distance traveled in two directions of the beam and the reported dimensions of the core in figure 1, neglecting the round edges, one gets a displaced mass of 0.02 kg leading with the previously calculated value for the force to an acceleration of \(6.6 \times 10^{-6} \text{ms}^{-2}\) and in turn to a displacement of the core section of \(0.4 \times 10^{-6} \text{m}\) in the direction of the mean flow according to equation (17). Three-point bending tests by the authors conducted with salt cores show that the tested specimens manufactured via pressing and sintering [59] are even at room temperature capable of undergoing displacements in this scale without cracking, provided that they are free of internal cracks, i.e. qualified for applying the continuum mechanics approach.

This in turn leads to the conclusion that due to the short time scales of the slamming effects, they are not to be considered failure critical for the salt core. Based on the presented knowledge, only a small segment of the core will be displaced by the spike in the force causing the core in turn to vibrate. The time scale of the impact will be too small to reach the core bearings and produce a counter force. Due to the displacement, the core will start to vibrate before being permanently deformed by the steady state force. Since this steady-state force will be present for a longer period of time, this may in the end be failure critical. To finally conclude the discussion, the reader should bear two additional things in mind. The results presented, although relying heavily on dimensionless numbers, are hugely geometry-, parameter- and, most notably ingate/impact velocity-dependent. One may therefore be careful to transfer these findings to other geometries and setups. On the other hand, it is highly recommended to apply the presented CFD approach to the geometry at hand. Secondly, the assumption of the core being crack free may also be flawed with respect to reality. It may therefore be possible, that salt cores, as the material salt being of ceramics nature will most often contain small cracks. It thus provides space for future research work to develop a model for assessing how long potential cracks are allowed to be in order to withstand the slamming impact. Another interesting project will be to measure the forces during a die casting test and not only its displacement as in [39].

For a practical conclusion, this also suggests that pre-heated cores that are warmer than room temperature may yield a higher fraction of castings with intact cores and thus geometries within the tolerance limit inside in setups with high slamming factor conditions. The warmer temperatures naturally cause the material to fail more likely in a ductile way than in a brittle way.
5. Conclusions

In the course of this paper, the presented research work showed that the slamming phenomenon is something that is in general underestimated by state of the art CFD simulations in industry with typical mesh spacings of 1 mm. The mesh resolution had to be increased by a factor of 40 in order to be in the middle of the analytical data or near the empirical measurements. Interestingly, the reduction of the time step size $\Delta t$ did not show any impact on the proper estimation of the slamming factor’s value. The contribution of the turbulence in this study was to be neglected and the mathematical justification for this was shown with the presented formulae. This echoes the findings and conclusions of more application related studies [13]. It remains a topic for scientific debates, which response of the core the slamming peak in the force actually causes inside the material. This paper provides a reasoning based on simple continuum mechanics formulae that the core will, for the given parameters, only slightly be displaced and thus in turn only vibrate – thus rendering the slamming impact to be a non-failure-critical phenomenon. The reader is reminded that the provided results are only valid for the presented boundary conditions and transferring them to other setups has to be handled with care. It is never possible without the detailed geometric constraints and boundary conditions to decide whether a salt core solution for high-pressure die casting will be viable. Future space for research will be to undertake efforts to measure the force inside a die during filling and also to combine the concept of slamming with the laws of fracture mechanics.

References


[40] T. Von Karman, The impact on seaplane floats during landing, National Advisory Committee on Aeronautics.


