Residual stresses in a stress lattice—Experiments and finite element simulations

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**Abstract**

In this work, residual stresses in a stress lattice are studied. The residual stresses are both measured and simulated. The stress lattice is casted of low alloyed grey cast iron. In fact, nine similar lattices are casted and measured. The geometry of the lattice consists of three sections in parallel. The diameter of the two outer sections are thinner than the section in the middle. When the stress lattice cools down, this difference in geometry yields that the outer sections start to solidify and contract before the section in the middle. Finally, an equilibrium state, with tensile stresses in the middle and compressive stresses in the outer sections, is reached. The thermo-mechanical simulation of the experiments is performed by using Abaqus. The thermo-mechanical solidification is assumed to be uncoupled. First a thermal analysis, where the lattice is cooled down to room temperature, is performed. Latent heat is included in the analysis by letting the fraction of solid be a linear function of the temperature in the mushy zone. After the thermal analysis a quasi-static mechanical analysis is performed where the temperature history is considered to be the external force. A rate-independent $J_2$-plasticity model with isotropic hardening is considered, where the material data depend on the temperature. Tensile tests are performed at room temperature, 200 °C, 400 °C, 600 °C and 800 °C in order to evaluate the Young's modulus, the yield strength and the hardening accurate. In addition, the thermal expansion coefficient is evaluated for temperatures between room temperature and 1000 °C. The state of residual stresses is measured by cutting the midsection or the outer section. The corresponding elastic spring-back reveals the state of residual stresses. The measured stresses are compared to the numerical simulations. The simulations show good agreement with the results from the experiments.

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**1. Introduction**

Residual stresses are important when designing components. The fatigue life of a component is influenced by the residual stress state. It can be increased or decreased by these stresses. For instance, compressive stresses at the surface region created by shot peening is a well-known method for increasing the life time of a component. When a component is casted it is obvious that residual stresses are developed during the solidification. It would be most interesting to include these stresses in the design of casted components. This can be done by first applying a thermo-mechanical stress analysis of the solidification and then export this result into an established design procedure. It is crucial that the thermal stresses are predicted accurately when adopting such a design method. The accuracy of such predictions is therefore investigated in the following paper. Residual stresses in a stress lattice of low alloyed grey cast iron are measured and simulated. The simulations are performed in Abaqus.

The stress lattice, which is an established method in order to obtain residual stresses, consists of three sections in parallel where the section in the middle is thicker than the outer sections. The difference in dimensions of the sections will give different cooling rates and residual stresses will develop. Because of the lower cooling rate in the section in the middle tensile stresses will occur there, while compressive stresses will occur in the thinner sections. In the literature surprisingly few papers are using the stress lattice as an example for validating numerical simulations. The work by Eigner-Walter (2006) is though one example of that. Comparisons on other simple geometries have also been performed by Liu et al. (2001). It is much more common to present a real application, see e.g. Jacot et al. (2000) where the residual stresses in a cast iron calender rolls are evaluated. Of course the goal is always to be able to predict the residual stresses as good as possible in real components, but the stress lattice is a well-defined experiment for evaluating the finite element model.

There are two main ways of doing residual stress calculations, either as a coupled thermo-mechanical analysis or as a separate...
thermal analysis followed by a mechanical analysis where the temperature history from the solidification calculation is used. Coupled analyses have been the subject of discussion in recent years and more details regarding this can be found in, e.g. Cervera et al. (1999), where a coupled approach by using a thermo-mechanical contact model is presented, Lewis and Ravindren (2000), where an overview of the coupled approach is presented, and Celentano (2001), where a large strain thermo-visco-plastic formulation is presented. Regarding uncoupled thermo-mechanical analyses more has been done. For instance, Metzger et al. (2001) and Chang and Dantzig (2004) are recent works using such an approach, where a sand surface element and an improvement of that sand surface element are presented, respectively. A possible drawback of an uncoupled approach is that the effect of the gap that are formed in the interface between the mould and casting during solidification and cooling is not included in the analysis. On the other hand significant computational efficiency is lost by using a coupled approach.

In this work, we use an uncoupled thermo-mechanical approach for the analysis, where the thermal problem and the mechanical problem are simulated separately. The temperature history from the thermal problem are what connects the two specific simulations, since the temperature history is used as the external force in the mechanical simulation. In the mechanical simulation a rate-independent $J_2$-plasticity model is used together with isotropic hardening. Since residual stress analyses of casted components includes liquid as well as solid material this assumption might be improved by including viscosity in the material model.

Tensile tests are performed at room temperature, 200 °C, 400 °C, 600 °C and 800 °C in order to get as good mechanical properties as possible as input to the structural analysis. Also measurements of the thermal expansion coefficient are performed for temperatures up to 1000 °C. For temperatures above solidus temperature ideal plasticity with a yield stress close to zero is assumed. This assumption might be improved by including viscous effects according to the discussion above. This is a subject for further research.

The outline of the paper is as follows: in Section 2, the experimental setup and the experimental results are presented, in Section 3, the finite element model is defined and the material properties are given and, finally, in Section 4, some concluding remarks are given.

2. Experiments

2.1. Stress lattice

The experiment was performed at Volvo Powertrain in Skövde, Sweden, in 16–17 January 2007, and contained a batch of nine similar stress lattices, shown in Fig. 1. During the casting the stress lattices were fed with melt through a gating system at one end of the stress lattices. The stress lattices were casted by hand with gravity casting. The mould was made of silica sand, containing 25% feldspat with a furan binder. No cooling channels or similar were used. The stress lattices were casted using low alloyed grey cast iron. Eight stress lattices were used to evaluate residual stresses and the purpose of the ninth stress lattice was to log cooling curves (temperature as a function of time) for two different positions, TC1 and TC2, in the stress lattice (see Fig. 1(b)). This was performed by using thermocouples. In order to avoid cracks due to shrinkage of the stress lattices in the mould, the moulds were broken after a given time. The time for breaking the mould was determined from the temperature logged on the ninth stress lattice. Thus, the ninth stress lattice with thermocouples was casted before the other eight ones. The time for breaking the moulds was determined by the time at which the thermocouple, in the thick center bar, showed a temperature of 600 °C. During the remaining time the stress lattices were cooled in air.

Geometrically the stress lattices consist of three sections, or bars, in parallel. The dimensions of the three sections of the stress lattices are described in Fig. 1(a). The weight of a stress lattice is 4.7 kg. In order to evaluate the residual stresses, the section in the middle of the stress lattices were cut with a hack-saw on seven of the stress lattices. The cut was positioned at the center of the section in the middle and was directed from one of the outer sections to the other, see Fig. 2(a). During the cut, one end of the stress lattice was fixed to a vice. Five of the stress lattices were cut 1 day after casting and another two were cut 2 weeks after casting in

![Fig. 1. CAD-models of stress lattice. The gating system are positioned in the lower part of the stress lattice, on the opposite side to the thermocouples. (a) Dimensions (mm). (b) Position of strain gauges (SG) and thermocouples (TC) (mm).](image-url)
order to evaluate relaxation effects of the residual stresses. It turned
out that after having cut through almost half the middle section of
the stress lattices, the section got broken. In order to evaluate the
residual stresses the load-carrying area and the depth of the cut at
the moment of break was measured. Knowing the geometry of the
cut, one similar cut was made on one half of the remaining mid-
section. Then a tensile test was performed (see Fig. 2(b)), where
the force needed to pull the midsection apart was recorded. That
force should be equal to the force present in the stress lattice at the
moment of break. On the eighth stress lattice strain gauges were
mounted. In that case the outer sections were cut instead of the
section in the middle. The purpose of the strain gauges was to mea-
sure the relief in elastic strain after having cut the stress lattice. In
Fig. 1(b) the positions of the strain gauges, SG1–SG3, are viewed.
It seems to be two strain gauges, SG1 and SG2, mounted on the
same position, but they are mounted on opposite sides of the stress
lattice.

2.2. Measurement equipments

For the temperature logging during casting, thermocouples of
type N model number 8102000 and manufactured by Pentronic
were used. Those were connected to a computer via an amplifier. A
software named ATAS Research version 5.1.5 logged the tempera-
ture in the computer. After the break the load-carrying area of the
midsection and the depth of the cut was measured. For the ten-
sile tests of the section in the middle a tensile test machine named
Zwick Z250-SN5A was used, which can provide a force of 250 kN
in tension. The accuracy of the tensile test machine is ±0.28%. The
strain gauges for the elastic strain measurements were also con-
ected to a computer via an amplifier.

2.3. Results

The temperatures logged by the thermo-couples are shown in
Fig. 3. We decided to use the time when the thermo-couple in the
middle section showed a temperature of 600 °C as the time for
breaking the moulds. As seen in Fig. 3, this time is approximately
25 min. It is important to mention that the stress lattice used for
logging the temperature was aligned in the mould all the measur-
ing time, while the other moulds were broken after 25 min. Thus,
only the first 25 min of Fig. 3 are comparable to simulations. In this
figure, it is also noticeable that the cooling in the outer sections
are much faster than the cooling in the middle section. The differ-
ent cooling rates in the outer sections compared to the midsection
resulted in different compositions of the material. Due to the high
speed cooling in the outer sections the so-called white iron was
developed.
Load carrying area of the cross-section.

<table>
<thead>
<tr>
<th>Stress lattice nr</th>
<th>Time to cut (days)</th>
<th>Area (mm²)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>1</td>
<td>509</td>
</tr>
<tr>
<td>2</td>
<td>1</td>
<td>514</td>
</tr>
<tr>
<td>3</td>
<td>1</td>
<td>517</td>
</tr>
<tr>
<td>4</td>
<td></td>
<td>525</td>
</tr>
<tr>
<td>5</td>
<td>1</td>
<td></td>
</tr>
<tr>
<td>6</td>
<td>14</td>
<td>517</td>
</tr>
<tr>
<td>7</td>
<td>14</td>
<td>511</td>
</tr>
</tbody>
</table>

Force needed to break the middle section of the stress lattice.

<table>
<thead>
<tr>
<th>Stress lattice nr</th>
<th>Part</th>
<th>Force (N)</th>
<th>Area (mm²)</th>
</tr>
</thead>
<tbody>
<tr>
<td>3</td>
<td>a</td>
<td>70,144</td>
<td>517</td>
</tr>
<tr>
<td>3</td>
<td>b</td>
<td>65,204</td>
<td>525</td>
</tr>
<tr>
<td>4</td>
<td>a</td>
<td>63,540</td>
<td>520</td>
</tr>
<tr>
<td>4</td>
<td>b</td>
<td>69,477</td>
<td>523</td>
</tr>
</tbody>
</table>

The thermal problem is governed by the well-known heat equation
\[
\rho \frac{\partial \mathcal{H}}{\partial t} = (kT_i)_i, \tag{1}
\]
where \( \rho = \rho(T) \) is the density, \( \mathcal{H} = \mathcal{H}(T) \) is the enthalpy, \( k = k(T) \) is the thermal conductivity, \( T \) is the temperature, \( t \) is time and \( (,)_i = \partial / \partial x_i \). The enthalpy contains the effects of the specific heat, \( c_p = c_p(T) \), as well as the latent heat, \( L \), as
\[
\mathcal{H}(T) = \int_0^T c_p \, dT + L(1 - f_s(T)), \tag{2}
\]
Fig. 6. The boundary conditions. (a) Mould and casting model, one quarter of real casting and mould. Valid for the first 25 min of the thermal simulation. \( \Gamma_I \) shows the surface with heat convection boundary condition. (b) The casting. Model valid for \( t > 25 \) min of the heat simulation and the elasto-plastic simulation. \( \Gamma_{II} \) shows the surface of the heat convection boundary condition. (c) Mould and casting model. Valid for the first 25 min of the heat simulation. Showing surfaces \( \Gamma_{\text{sym,1}} \) and \( \Gamma_{\text{sym,2}} \) which are given symmetry boundary conditions. (d) The casting. Valid for \( t > 25 \) min in the thermal simulation and the elasto-plastic simulation. Showing surface \( \Gamma_{\text{sym,1}} \) which is given symmetry boundary conditions. (e) Symmetry boundaries. \( \Gamma_{\text{sym,2}} \) is released when the cut is simulated.

where \( f_s(T) \) is the fraction solid which varies linearly according to

\[
f_s(T) = \begin{cases} 
  1 & T < T_{\text{sol}} \\
  \frac{T_{\text{liq}} - T}{T_{\text{liq}} - T_{\text{sol}}} & T_{\text{sol}} \leq T \leq T_{\text{liq}} \\
  0 & T > T_{\text{liq}} 
\end{cases}
\]

where \( T_{\text{liq}} \) is the liquidus temperature and \( T_{\text{sol}} \) is the solidus temperature. The heat equation was solved for the following boundary conditions

\[
q = \begin{cases} 
  h_1(T_{\text{surf,sand}} - T_{\text{air}}) & \text{on } \Gamma_I \text{ when } t < 1500 \text{ s} \\
  h_2(T_{\text{surf,cast}} - T_{\text{air}}) & \text{on } \Gamma_{II} \text{ when } t > 1500 \text{ s} \\
  0 & \text{on } \Gamma_{\text{sym,1}} \text{ and } \Gamma_{\text{sym,2}} \text{ for all times } t
\end{cases}
\]

where \( h_1 \) is the heat transfer coefficient in the intersection between the sand mould and the surrounding air, \( h_2 \) is the heat transfer coefficient in the intersection between the casting and the surrounding air, \( T_{\text{surf,sand}} \) is the surface temperature of the sand mould, \( T_{\text{surf,cast}} \) is the surface temperature of the stress lattice, \( T_{\text{air}} \) is the temperature of the surrounding air (assumed to be constant at 20 °C), \( \Gamma_I \) is the surface of the mould that bounds to the surrounding air, \( \Gamma_{II} \) is the surface of the casting that bounds to the surrounding air while \( \Gamma_{\text{sym,1}} \) and \( \Gamma_{\text{sym,2}} \) are the symmetry surfaces belonging to the mould as well as the casting, see Fig. 6. The initial condition of the casting is set to \( T_{0,\text{cast}} = 1200 \) °C and of the mould is set to \( T_{0,\text{sand}} = 20 \) °C.

The residual stresses were calculated by performing a quasi-static rate-independent elasto-plastic analysis in Abaqus, where von Mises yield surface and isotropic hardening are utilized. The equilibrium equation of the static problem reads

\[
\sigma_{ij,j} = 0,
\]

where \( \sigma_{ij} \) is the stress tensor. The boundary conditions, which all are symmetry boundary conditions except for the last one, for the
Fig. 7. Meshes. (a) The mesh of the casting and mould. (b) The mesh of the casting only.

stress lattice are

\[
\begin{align*}
u_1 &= 0 \quad \text{on } \Gamma_{\text{sym,1}} \\
u_2 &= 0 \quad \text{on } \Gamma_{\text{sym,2-1/2}} \\
u_i &= 0 \quad \text{at point O}
\end{align*}
\]

(6)

where \( u_i \) is the displacement. The infinitesimal strain, \( \varepsilon_{ij} \), and Hooke's law of linear elasticity with thermal expansion are given by

\[
\varepsilon_{ij} = \frac{1}{2}(u_{i,j} + u_{j,i}) 
\]

(7)

and

\[
\sigma_{ij} = D_{ijkl}(\varepsilon_{kl} - \varepsilon_{kl}^p),
\]

(8)

where \( D_{ijkl} = D_{ijkl}(T) \) is the elastic tensor, \( \varepsilon_{kl}^p \) is the plastic strain tensor, \( \varepsilon_{kl}^T \) is the thermal strain tensor and \( \sigma_{ij} \) is the Cauchy stress tensor. The thermal strain is defined as

\[
\varepsilon_{kl}^T = \alpha(T)(T - T_{\text{ref}})\delta_{kl} - \alpha(T_0)(T_0 - T_{\text{ref}})\delta_{kl},
\]

(9)

where \( T_{\text{ref}} \) is a reference temperature, \( T_0 \) is the initial temperature, \( \alpha(T) \) is the secant thermal expansion coefficient for temperature \( T \), \( \alpha(T_0) \) is the secant thermal expansion coefficient for the initial temperature and \( \delta_{kl} \) is Kronecker's delta. Thus, \( \alpha \) is defined as the secant between the strain at a certain temperature and the strain at the reference temperature. Eq. (9) yields that if there is no expansion between the reference temperature and initial temperature the second term in that equation will disappear. In this work, we have chosen the reference temperature in accordance to the material data in such a way. That reduces Eq. (9) to

\[
\varepsilon_{kl}^T = \alpha(T)(T - T_{\text{ref}})\delta_{kl}.
\]

(10)

Regarding the plasticity model, the yield surface is defined by

\[
f(\sigma_{ij}, T) = \sqrt{3J_2} - \sigma_y - K,
\]

(11)

where

\[
J_2 = \frac{1}{2} s_{ij} s_{ij}
\]

(12)

is the second invariant of the deviatoric stress, \( \sigma_y = \sigma_y(T) \) is the initial yield strength and \( K(\varepsilon_{\text{eff}}, T) \) is the hardening parameter,

\[
\sigma_{ij} = p\delta_{ij} + \sigma_{ij}^p,
\]

(13)

is the deviatoric stress, where

\[
p = -\frac{1}{3}\sigma_{ii}
\]

(14)

is the hydrostatic pressure. The flow rule is expressed by

\[
\dot{\varepsilon}_{ij}^P = \dot{\lambda} \frac{\partial f}{\partial \sigma_{ij}} = \dot{\lambda} \frac{3\sigma_{ij}}{2\sqrt{3J_2}}
\]

(15)

and the effective plastic strain is defined by

\[
\varepsilon_{\text{eff}}^p = \int_0^t \sqrt{\frac{3}{2}} s_{ij} s_{ij}^p dt.
\]

(16)

3.2. Material properties

The material properties needed in the finite element simulations of the heat equation are presented in Tables 4–6. All material properties for the sand, Table 4, were found in Pehlke et al. (1982). For grey iron the density at room temperature is taken from Davies (1996) and from Lyman (1961) at \( T_{\text{sol}} \). Between room temperature and \( T_{\text{sol}} \) the density is linearly interpolated. For temperatures at and above \( T_{\text{sol}} \) the density is assumed to be constant. Specific heat and conductivity, are taken from Davies (1996). In Table (6) the latent heat was found in Veinik (1968), \( T_{\text{sol}} \) and \( T_{\text{liq}} \) in Davies (1996), the heat transfer coefficient, \( h_1 \), in Diószegi (2004), while the heat transfer coefficient, \( h_2 \), and \( T_{\text{air}} \) were found in Celentano (2001).

Table 4

<table>
<thead>
<tr>
<th>Temperature (°C)</th>
<th>Density (kg/m³)</th>
<th>Specific Heat (J kg⁻¹ K⁻¹)</th>
<th>Conductivity (W m⁻¹ K⁻¹)</th>
</tr>
</thead>
<tbody>
<tr>
<td>25</td>
<td>1520</td>
<td>676</td>
<td>0.733</td>
</tr>
<tr>
<td>27</td>
<td>-</td>
<td>820</td>
<td>-</td>
</tr>
<tr>
<td>127</td>
<td>-</td>
<td>1074</td>
<td>0.640</td>
</tr>
<tr>
<td>327</td>
<td>-</td>
<td>993</td>
<td>-</td>
</tr>
<tr>
<td>400</td>
<td>-</td>
<td>-</td>
<td>0.586</td>
</tr>
<tr>
<td>527</td>
<td>-</td>
<td>-</td>
<td>0.640</td>
</tr>
<tr>
<td>600</td>
<td>-</td>
<td>-</td>
<td>0.590</td>
</tr>
<tr>
<td>727</td>
<td>-</td>
<td>1123</td>
<td>-</td>
</tr>
<tr>
<td>800</td>
<td>-</td>
<td>1166</td>
<td>-</td>
</tr>
<tr>
<td>927</td>
<td>-</td>
<td>1201</td>
<td>0.703</td>
</tr>
<tr>
<td>1000</td>
<td>-</td>
<td>-</td>
<td>0.766</td>
</tr>
<tr>
<td>1127</td>
<td>-</td>
<td>-</td>
<td>0.766</td>
</tr>
<tr>
<td>1200</td>
<td>1520</td>
<td>1212</td>
<td>0.766</td>
</tr>
</tbody>
</table>
Table 5
Temperature-dependent heat transfer material properties of grey iron.

<table>
<thead>
<tr>
<th>T (°C)</th>
<th>( \rho ) (kg/m(^3))</th>
<th>( c_p ) (J kg(^{-1}) K(^{-1}))</th>
<th>( k ) (W m(^{-1}) K(^{-1}))</th>
</tr>
</thead>
<tbody>
<tr>
<td>20</td>
<td>7000</td>
<td>586</td>
<td>50.24</td>
</tr>
<tr>
<td>200</td>
<td>6876</td>
<td>628</td>
<td>48.99</td>
</tr>
<tr>
<td>400</td>
<td>6739</td>
<td>712</td>
<td>41.87</td>
</tr>
<tr>
<td>600</td>
<td>6601</td>
<td>879</td>
<td>35.17</td>
</tr>
<tr>
<td>1120</td>
<td>6230</td>
<td>879</td>
<td>35.17</td>
</tr>
<tr>
<td>1160</td>
<td>6230</td>
<td>879</td>
<td>35.17</td>
</tr>
<tr>
<td>2000</td>
<td>6230</td>
<td>879</td>
<td>35.17</td>
</tr>
</tbody>
</table>

Table 6
Temperature-independent heat transfer material properties.

<table>
<thead>
<tr>
<th>( h_1 ) (W m(^{-2}) K(^{-1}))</th>
<th>( h_2 ) (W m(^{-2}) K(^{-1}))</th>
<th>( L ) (kJ/kg)</th>
<th>( T_{\infty} ) (°C)</th>
<th>( T_{\text{sol}} ) (°C)</th>
<th>( T_{\text{liq}} ) (°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>20</td>
<td>20</td>
<td>272</td>
<td>20</td>
<td>1120</td>
<td>1160</td>
</tr>
</tbody>
</table>

Regarding Table 5 there are some simplifications. Specific heat is assumed to be constant for temperatures above 600 °C, thus the eutectic phase transformation around 700 °C is neglected. Furthermore, the conductivity is also kept constant for temperatures above 600 °C in agreement with Overfelt et al. (2000).

Table 7
Temperature-dependent mechanical material properties.

<table>
<thead>
<tr>
<th>T (°C)</th>
<th>( E ) (MPa)</th>
<th>( \sigma_y ) (MPa)</th>
<th>( \nu )</th>
</tr>
</thead>
<tbody>
<tr>
<td>20</td>
<td>127,000</td>
<td>138</td>
<td>0.26</td>
</tr>
<tr>
<td>200</td>
<td>117,000</td>
<td>123</td>
<td>0.26</td>
</tr>
<tr>
<td>400</td>
<td>108,000</td>
<td>115</td>
<td>0.26</td>
</tr>
<tr>
<td>600</td>
<td>99,000</td>
<td>86</td>
<td>0.26</td>
</tr>
<tr>
<td>800</td>
<td>62,000</td>
<td>38</td>
<td>0.26</td>
</tr>
<tr>
<td>1120</td>
<td>5,000</td>
<td>1</td>
<td>0.49</td>
</tr>
<tr>
<td>1160</td>
<td>500</td>
<td>1</td>
<td>0.49</td>
</tr>
<tr>
<td>2000</td>
<td>500</td>
<td>1</td>
<td>0.49</td>
</tr>
</tbody>
</table>

The mechanical properties used in the residual stress calculations can be found in Tables 7 and 8 and in Fig. 8(a)-(f). Young’s modulus, yield strength and the hardening behavior are obtained from tensile tests performed at five different temperatures, room temperature, 200 °C, 400 °C, 600 °C and 800 °C. The test specimens were produced from the material of the stress lattices. The yield strength was calculated due to our hypothesis to use \( R_{0.02} \) instead of \( R_{0.2} \) for grey cast iron in order to capture the nonlinear behavior better. Also in literature there are proposals for such methods, see e.g. Ottosen and Ristinmaa (2005). For temperatures above 800 °C data are assumed or collected in the literature. For temperatures,
Table 8

Temperature-dependent thermal expansion coefficient.

<table>
<thead>
<tr>
<th>T (°C)</th>
<th>( \alpha (K^{-1}) )</th>
</tr>
</thead>
<tbody>
<tr>
<td>20</td>
<td>1.8 \times 10^{-5}</td>
</tr>
<tr>
<td>184</td>
<td>1.8 \times 10^{-5}</td>
</tr>
<tr>
<td>266</td>
<td>1.9 \times 10^{-5}</td>
</tr>
<tr>
<td>348</td>
<td>1.9 \times 10^{-5}</td>
</tr>
<tr>
<td>430</td>
<td>2.0 \times 10^{-5}</td>
</tr>
<tr>
<td>512</td>
<td>2.0 \times 10^{-5}</td>
</tr>
<tr>
<td>594</td>
<td>2.1 \times 10^{-5}</td>
</tr>
<tr>
<td>676</td>
<td>2.2 \times 10^{-5}</td>
</tr>
<tr>
<td>758</td>
<td>2.3 \times 10^{-5}</td>
</tr>
<tr>
<td>765</td>
<td>2.4 \times 10^{-5}</td>
</tr>
<tr>
<td>773</td>
<td>2.5 \times 10^{-5}</td>
</tr>
<tr>
<td>780</td>
<td>2.6 \times 10^{-5}</td>
</tr>
<tr>
<td>787</td>
<td>2.7 \times 10^{-5}</td>
</tr>
<tr>
<td>780</td>
<td>2.7 \times 10^{-5}</td>
</tr>
<tr>
<td>1119</td>
<td>0</td>
</tr>
<tr>
<td>1221</td>
<td>0</td>
</tr>
</tbody>
</table>

\( T \geq T_{\text{sol}} \), the material is assumed to behave ideal-plastic with a low Young’s modulus as well as a low yield strength, in accordance with Metzger et al. (2001). The Young modulus at solidus temperature comes from Celentano (2001). Hardening data are shown in Fig. 8(a)–(f), where true stress as a function of true plastic strain for different temperatures are plotted. For each temperature there is a dotted line that represents an smoothed average of the present tensile tests. It was obtained by fitting a curve, by the least square method, to the average curve of the tensile tests. The differences between different samples at same temperature in Fig. 8 are certainly due to the fact that the test specimen was produced from different parts of the stress lattices. Different parts of the stress lattice did cool at different rates and might therefore have small differences in material properties. Poisons ratio, \( \nu \), was found in Walton (1971) for \( T < T_{\text{liq}} \) and for \( T \geq T_{\text{liq}} \) it was chosen to be close to 0.5 according to Lewis and Ravindren (2000). The thermal expansion coefficient, \( \alpha \), is calculated from experiment, see Fig. 9, for room temperature up to 1000 °C. Two specimens were tested even if it is hard to see two solid lines in the figure since they are well correlated. For temperatures between 1000 °C and \( T_{\text{sol}} \) the thermal expansion are assumed to have the same slope as for temperatures around 1000 °C, in Fig. 9. For temperatures above \( T_{\text{sol}} \) we assume that no thermal expansion occur.

3.3. Results

As seen in Fig. 10, the measured cooling curves shows a slight lower cooling rate than the simulated ones. The graphs show that simulations do not capture the eutectic phase transformation at about 700 °C, which is obvious considering the material data used. The correlation to the measured cooling curves possibly could have been better if that had been taken into account. Furthermore, it is obvious that the section in the middle of the stress lattice cools slower than the outer section.

The results from the simulation of the reaction force at the moment of break are shown in Table 9. The difference between measurements and simulations is below 12%.

In Table 10 the simulated released strains, which are comparable to the measured values from the strain gauges, are viewed. The simulation of the strain relief for position SG1 and SG2 (for alignment of position SG1-3 see Fig. 1(b)) show equal results, which is obvious since the symmetry boundary condition is utilized. The differences between position SG1/2 and SG3 are quite small, but even though it shows that the residual stresses changes along the section in the middle. The simulation captures this change nice and shows the same behavior as the measurements.

Table 9

Simulated and the average of measured reaction forces after cutting.

<table>
<thead>
<tr>
<th>Method</th>
<th>Reaction force (kN)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Simulation</td>
<td>59</td>
</tr>
<tr>
<td>Measured avg.</td>
<td>67</td>
</tr>
</tbody>
</table>

Table 10

Elastic strain relief due to cutting.

<table>
<thead>
<tr>
<th>Point</th>
<th>Simulated strain ( j_2 )-plasticity (( \mu ))</th>
<th>Measured strain (( \mu ))</th>
</tr>
</thead>
<tbody>
<tr>
<td>SG1</td>
<td>599</td>
<td>682</td>
</tr>
<tr>
<td>SG2</td>
<td>599</td>
<td>703</td>
</tr>
<tr>
<td>SG3</td>
<td>636</td>
<td>749</td>
</tr>
</tbody>
</table>

Fig. 10. Temperature vs. time for point TC1 and TC2, simulated as well as measured. The vertical line represents time of breaking the moulds. (a) Point TC1, (b) Point TC2.
and S2 one can recognize that there is a stress gradient present in
outer bars initially, even if it does to see, then the stress change
and becomes compressive stresses. That is the final state in
the cross-section of the center bar.

4. Concluding remarks

In this work experiments and finite element simulations of
residual stresses developed during solidification of stress lattices
are performed. The stress lattices are cast in low alloyed grey
iron castings in a satisfactory way such that these stresses can be included in the design
process.

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