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Use of the Quasi-Static Johnson-Cook Model in the Failure Assessment of Tensile Specimens With Metallurgical Constraints

Riku Neuvonen, Tuomas Skriko, Timo Björk

Abstract

Direct quenching has recently been used to develop cost-effective novel high and ultra-high strength steels. In weldments made of these steels, a distinct soft zone, which can lower the ultimate strength and deformation capacity of the welded joint, can form in the heat affected zone of the joint. In order to accurately estimate the static strength and deformation capacity of a welded steel structure fabricated from these steels, the effect of this softening must be taken into account. In this study, the effect of the softened zone is explored based on round tensile test specimens with metallurgical constraints using experiments and numerical calculations. The applicability of the quasi-static part of the Johnson-Cook material model and failure model for fracture assessment under given conditions was hereby examined. Firstly, the material parameters were determined for direct-quenched armor steel and cold-formed structural steel using notched tensile test specimens and FE models combined with an optimization routine. A good agreement was achieved between the tests and the results from the FE models for the notched specimens. Specimens with metallurgical constraints were manufactured from the materials using rotational friction welding, and the corresponding FE models were created using the material parameters obtained from optimization. Two different fracture mechanisms were observed in the tensile tests conducted for the specimens with metallurgical constraints, namely cup and cone fractures at the center of the specimen and fractures near the weld interface. In the corresponding FE models, the fracture initiations were observed in the same areas. A good agreement between the FE models and experimental tensile tests was thus achieved in the case of the cup and cone fractures. However, the generated FE models underestimated the fracture displacement in the specimens where the fracture occurred near the weld interface.

Keywords: ultra-high strength steel; material modeling; mechanical properties; welded joints; fracture behavior; failure assessment

Nomenclature (symbols are defined in their order of appearance)

- $\sigma_{\text{flow}}$: von Mises flow stress
- $A$: yield limit
- $B$: hardening coefficient
- $\bar{\varepsilon}_p$: equivalent plastic strain
- $n$: hardening exponent
- $C$: viscous effect
- $k^*$: dimensionless plastic strain rate
- $T^*$: homologous temperature
- $m$: thermal sensitivity
- $k$: effective plastic strain rate
- $k^*_r$: reference strain rate
- $T$: deformation temperature
- $T_r$: reference temperature
- $T_m$: melting temperature
- $D$: damage parameter
- $\bar{\varepsilon}_f$: equivalent fracture strain

- $\sigma^*$: ratio of pressure divided by effective stress
- $p$: hydrostatic pressure
- $\sigma_{\text{eff}}$: effective stress
- $\sigma_1, \sigma_2, \sigma_3$: first, second and third principal stress component
- $D_1, D_2, D_3, D_4, D_5$: fracture strain constants
- $R_{0.2}$: 0.2% offset yield strength
- $f_u$: ultimate tensile strength
- $A_1$: elongation at fracture
- $A_3$: elongation at fracture
- $D_i$: instantaneous minimum diameter of a specimen
- $M$: tensile strength mismatch
- $D_s$: original minimum diameter of a specimen
- $L_{\text{FL}}$: weld interface distance
- $\delta_{30}$: displacement at fracture, gauge length 30 mm
1. Introduction

In recent years, novel high and ultra-high strength structural and armor steels have been produced using direct quenching (DQ). In the DQ process, the quenching is performed immediately after the thermomechanical rolling of the plate. After quenching, the plates can be tempered if necessary [1]. According to [2, 3], the DQ process offers a cost-effective way to produce low-carbon martensitic and/or bainitic steel plates. Studies conducted by Chang [4], Meysami et al. [5] and Muckelroy, Findley and Bodnar [2] indicate that the mechanical properties of steels produced using the DQ process differ from those of steels produced using conventional quenching and tempering.

The fabrication of steel structures often requires joining steel parts together. One important joining method is welding, which alters the mechanical properties of the steel due to the chemical and metallurgical changes caused by the thermal cycle of the welding process. The susceptibility to these changes depends on the chemical composition and metallurgical structure of the steel.

According to Porter [6], DQ steels are susceptible to heat affected zone (HAZ) softening due to the low carbon content. However, Amraei et al. [7] showed that by decreasing the width to plate thickness ratio of the softened HAZ in S960 DQ steels, the strength and ductility of the joint can be increased due to the boundary conditions generated by the higher strength of the material around the soft zone. Ran et al. [8] observed the same behavior in various steel grades, but they also noted that ductility decreases due to a tensile strength mismatch generated by the softened zone. Sun et al. [9] and Rodrigues et al. [10] noticed that in addition to the width of the softened zone, the mismatch ratio and the hardening parameters of the adjacent materials have an effect on the degree of the strengthening of the boundary conditions generated by the metallurgical constraints. In addition, Björk et al. [11] demonstrated that in certain welds with a softened zone, a fracture can occur near the weld toe or at the fusion line. This phenomenon can reduce the load-carrying and deformation capacity of the joints. On the other hand, Skriko [12] showed that by using proper welding procedures and parameters, including heat input, working temperature and groove preparation, a good performance quality of weldments made of S960 DQ steel can be achieved despite the softening susceptibility of the steel.

In general, armor steels have high carbon equivalent values (CEV), making them susceptible to hydrogen cracking [13]. In order to minimize cracking, proper heat input and suitable, preferably low strength, filler metal is usually recommended by steel manufacturers. In addition to the mismatch, direct quenched armor steels (DQA) are prone to HAZ softening. Thus, research into the metallurgical constraints is relevant for DQA steels in order to obtain information on defining suitable workshop methods for this type of steel.

As was noted above, in order to investigate the effects of metallurgical constraints, reliable material models must be applied. In this study, the material model developed by Johnson and Cook [14, 15] is applied because it has been successfully used in several studies related to the stress-strain behavior and ductile fracture of conventional armor steels [16, 17, 18, 19, 20, 21, 22].

Welding, especially multipass welding, generates complex microstructural changes in the HAZ [23], and identifying and characterizing the material parameters for all the microstructurally different zones would require substantial effort. On the other hand, using a simplified representation of the zones would require more or less arbitrary assumptions about the importance of each zone to the overall behavior and properties of the weldment. In order to minimize these issues, rotational friction welding was used in this study to manufacture tensile test specimens with metallurgical constraints with clearly definable zones. This procedure enabled the characterization of material parameters for the Johnson-Cook model using separate tensile test specimens. The material parameters were obtained using an optimization routine and were verified by comparing the FE results with results from the tensile specimens that were not used in the parameter identification. Verified and predefined material models were applied in order to investigate the applicability of the Johnson-Cook model to assess the fractures occurring in the softened zone restricted by boundary conditions caused by the metallurgical constraints.

2. Theoretical Background

The constitutive and fracture model developed by Johnson and Cook has been used successfully in many studies on the behavior of steels [16, 17, 18, 19, 20, 21, 22]. Some researchers have subsequently modified this model, such as Børvik et al. [24], whose modified model has, in turn, also been used by Banerjee et al. [25] with good results. The Johnson-Cook constitutive model defines the von Mises equivalent flow stress using the equation
\[ \bar{\sigma}_{\text{flow}} = \left[ A + B \bar{\varepsilon}_p \right] \left[ 1 + C \ln \bar{\varepsilon}_p \right] \left[ 1 - (T^*)^m \right] \]  
(1)

The first bracket of the equation describes the hardening behavior, the second bracket takes the strain rate effect into account, and the third bracket considers the temperature sensitivity. In the equation, \( A \) is the yield strength of the material, \( B \) is the strain hardening constant, \( n \) is the strain hardening coefficient, \( C \) is the strengthening coefficient of strain rate, \( m \) is the thermal softening coefficient and \( \bar{\varepsilon}_p \) is the equivalent plastic strain. Dimensionless plastic strain rate \( \dot{\varepsilon}^* \) and homologous temperature \( T^* \) are defined by following equations, respectively

\[ \begin{align*}
\dot{\varepsilon}^* &= \frac{\dot{\varepsilon}}{\dot{\varepsilon}_{\text{ref}}} \\
T^* &= \frac{T - T_{\text{ref}}}{T_m - T_{\text{ref}}}
\end{align*} \]
(2) (3)

where \( \dot{\varepsilon} \) is the effective plastic strain rate, \( T \) is the deformation temperature and \( T_m \) is the melting temperature of the material. \( \dot{\varepsilon}_{\text{ref}} \) and \( T_{\text{ref}} \) are the reference strain rate and the reference deformation temperature, respectively. In the fracture model described by Johnson and Cook, damage to the material is defined using the equation

\[ D = \sum \frac{\Delta\bar{\varepsilon}_p}{\bar{\varepsilon}_f(\sigma^*, \dot{\varepsilon}^*, T^*)} \]
(4)

where \( \bar{\varepsilon}_f \) is the equivalent fracture strain, which is defined

\[ \bar{\varepsilon}_f = \left[ D_1 + D_2 \exp D_3 \sigma^* \right] \left[ 1 + D_4 \ln \dot{\varepsilon}^* \right] \left[ 1 - D_5 T^* \right] \]
(5)

where \( D_1, D_2, D_3 \) constants describe the effect of stress triaxiality on effective plastic strain at fracture. \( D_4 \) and \( D_5 \) represent the material fracture sensitivity to strain rate and temperature. In this model, a fracture occurs when \( D=1 \). In the model, \( \sigma^* \) is defined by following equation

\[ \sigma^* = \frac{p}{\sigma_{\text{eff}}} \]
(6)

where \( p \) is the pressure and \( \sigma_{\text{eff}} \) is the effective stress.

In this paper, the main focus is on the quasi-static behavior of round specimens with different metallurgical constraints at room temperature with the aim of determining the applicability of the Johnson-Cook model to predict failure under the given conditions. In the quasi-static regime, the strain rate and temperature have a minuscule effect on constitutive and fracture behavior [26]. Thus, the parameters governing the strain rate and temperature behavior are not covered in this paper, even though these parameters are usually important in applications related to armor steels, e.g. ballistic analyses.

In recent years, several researchers, such as [27, 28, 29], have noticed that in addition to the stress triaxiality, the Lode parameter \( L \) also determines the amount of effective plastic strain that a material can withstand. The Lode parameter can be defined by the following equation

\[ -1 \leq -\frac{2\sigma_2 - \sigma_1 - \sigma_3}{\sigma_3 - \sigma_1} \leq 1 \]
(7)

where \( \sigma_1, \sigma_2, \sigma_3 \) are the first, second and third principal stress components, respectively. Important special cases are when Lode parameter is equal to -1, 0 and 1, which corresponds to the conditions of axisymmetric uniaxial compression, plane strain and axisymmetric uniaxial tension, respectively. The fracture model described in equation (4) does not take into account the effect of Lode parameter. In this study, the effect of Lode parameter was not studied in detail because the chosen material model could not take into account the effect. However, as can be seen in the Results section, the metallurgical constraint generates a stress state where the Lode parameter is not constant across the specimen.

A common method for the analytical interpretation of tensile tests is to use the formulas developed by Bridgman [30]. Alves and Jones [31] and Bao [32] studied the accuracy of Bridgman’s equations using FE calculations, and they found that the equivalent strain at the minimum cross-section after necking and in notched specimens is not constant. In the center of the specimen, Bridgman’s solution overestimates the value of the equivalent strain and underestimated it on the surface of the specimen, especially when the notch radius is small. Alves and Jones [31] concluded that the difference between the numerical solution and Bridgman’s solution is negligible when the notch radius is large.

Bao [32] also reported that equivalent strain is not constant in the minimum cross-section. The study compared the stress values obtained using Bridgman’s equations and the FE calculations. According to Bao’s [32] results, there is a 30-50 % difference in the stress values between the numerical solution and Bridgman’s solution.

In addition, Alves and Jones [31] also studied the effect of strain hardening on the stress and strain state of a notched tensile specimen using numerical calculations. They noticed that the development of the stress
triaxiality at the center of the specimen depends on the strain hardening behavior of the material. Bridgman does not take this effect into account in his equations.

Because of these reported issues, analytical calculations to determine the material parameters were not used in this study. Instead, the identification was based on measured values of the force, displacement and cross-section of the experimental tensile test specimens. The procedure used is discussed in detail later in this paper.

3. Experimental Procedure and Numerical Calculations

In this study, specimens were manufactured from DQA steel plate with a thickness of 14 mm as well as from cold drawn bar (CDB) S355J2C+C with a diameter of 16 mm. The chemical compositions and mechanical properties according to the material certificates for both steels are presented in Table 1 and Table 2, respectively. The CEVs for DQA and CDB steels are 0.61 and 0.41, respectively.

Table 1. Chemical weight percent compositions according to the material certificates.

<table>
<thead>
<tr>
<th>Steel</th>
<th>C [%]</th>
<th>Si [%]</th>
<th>Mn [%]</th>
<th>P [%]</th>
<th>S [%]</th>
<th>Al [%]</th>
<th>N [%]</th>
<th>Cu [%]</th>
<th>Cr [%]</th>
<th>Ni [%]</th>
<th>Mo [%]</th>
<th>Sn [%]</th>
<th>Fe [%]</th>
</tr>
</thead>
<tbody>
<tr>
<td>DQA</td>
<td>0.185</td>
<td>0.31</td>
<td>0.95</td>
<td>0.009</td>
<td>0.001</td>
<td>0.045</td>
<td>0.003</td>
<td>0.017</td>
<td>0.61</td>
<td>1.46</td>
<td>0.197</td>
<td>0.003</td>
<td>balance</td>
</tr>
<tr>
<td>CDB</td>
<td>0.15</td>
<td>0.22</td>
<td>1.29</td>
<td>0.012</td>
<td>0.022</td>
<td>0.022</td>
<td>-</td>
<td>0.25</td>
<td>0.09</td>
<td>0.09</td>
<td>0.03</td>
<td>-</td>
<td>balance</td>
</tr>
</tbody>
</table>

Table 2. Mechanical properties according to the material certificates.

<table>
<thead>
<tr>
<th>Steel</th>
<th>(R_{p0.2}) [MPa]</th>
<th>(f_u) [MPa]</th>
<th>(A_t) [%]</th>
<th>(A_5) [%]</th>
<th>Charpy-V -40 °C [J]</th>
<th>Hardness [HB]</th>
</tr>
</thead>
<tbody>
<tr>
<td>DQA</td>
<td>1230</td>
<td>1497</td>
<td>9</td>
<td>-</td>
<td>71</td>
<td>462-472</td>
</tr>
<tr>
<td>CDB</td>
<td>650</td>
<td>695</td>
<td>-12.8</td>
<td>-</td>
<td>-</td>
<td>209</td>
</tr>
</tbody>
</table>

Round notched tensile specimens and specimens with metallurgical constraints were used in this study. The geometries of the notched specimens are presented in Figure 1. For specimens with geometry presented in Figure 1 (a), UN is used as the abbreviation, and the notch radii presented in Figure 1 (b) and (c) are used hereafter as the abbreviation for notched specimens.

Figure 1. Geometries of the notched specimen. (a) geometry of specimens UN. In (b) geometry of specimens with notch radiiuses 30, 15, 7.5 and 5 mm and in (c) geometry of specimen with notch radiiuses 1.5, 1 and 0.5 mm.

One specimen was manufactured from each material, respectively, for each notch radius except in the case of DQA, from which two additional UN specimens were manufactured. In the case of DQA, square billets with a length of 150 mm were saw-cut from a plasma-cut plate parallel to the rolling direction. The two additional specimens, which are hereafter identified using the abbreviations UN45 and UN90, were manufactured with an orientation of 45 and 90 degrees, respectively, to the rolling direction. A schematic representation of the billet orientations is presented in Figure 2. Subsequently, the billets were turned to the given dimensions using a numerically controlled (NC) lathe. The CDB specimens were saw-cut to a length of 150 mm and then turned using an NC lathe. For both steels, specimens UN, R30, R7.5, R5, R1.5 and R0.5 were used to determine the Johnson-Cook model material properties.
Specimens R15 and R1 were not used in the material parameter identification process described later and were instead used to verify the generated material model. Correspondingly, the DQA specimens UN45 and UN90 were used to study the degree of anisotropy of the DQA plate.

Specimens with metallurgical constraints were manufactured by joining DQA billets and CDB billets using rotational friction welding. The welding parameters and measured cooling time are presented in Table 3. The parameters were chosen in order to minimize the heat input and thus minimize the metallurgical changes occurring in the joint without compromising the strength of the weld interface. The DQA billets were kept stationary during welding. A schematic representation of the manufacturing phases is presented in Figure 3, whereby each number inside a square denotes a single phase. The abbreviation FWXX, whereby the XX is replaced with the measured length between the weld interfaces, is used hereafter to identify the specimens.

First, a DQA billet was friction welded to CDB. After welding, the burr was removed by turning using a manual lathe. After removal of the burr, the total length was measured to compensate for the changes generated by the welding process. After the measurement, the specimen was saw-cut to the desired length in the second phase of the manufacturing process. In the third phase, a second DQA billet was welded to the DQA-CDB billet. A numerically controlled lathe was used in the fourth phase to turn the specimens to the final dimensions.

Nine tensile specimens with different degrees of metallurgical constraint, achieved by varying the length of the CDB zone, were manufactured for the study. After the fourth phase, the lengths between the weld interfaces were measured from images acquired using a macroscope. A macroscopy image with the measurements for FW9.1 is presented in Figure 5. The weld interfaces were easy to identify from the specimens due to the different degrees of surface roughness, caused by the hardness change between the materials, after the turning. The distance between the weld interfaces was determined based on the average of two length measurements.

In addition to the specimens described above, six specimens were manufactured to test the tensile strength of the weld interface and to conduct the hardness measurements. In this study, the Vickers hardness measurement method with 5 kg load (HV5) was used. Two of these specimens were manufactured by joining two DQA billets, two specimens by joining two CDB billets, and two specimens by joining DQA and CDB billets using the given friction welding parameters. These specimens are hereafter referenced using the abbreviations DQA-DQA, CDB-CDB and CDB-DQA.

### Table 3. Welding parameters.

<table>
<thead>
<tr>
<th>Rotational speed [RPM]</th>
<th>Friction time [s]</th>
<th>Friction pressure [MPa]</th>
<th>Forging time [s]</th>
<th>Forging pressure [MPa]</th>
<th>Cooling time $t_{5.5}$ [s]</th>
</tr>
</thead>
<tbody>
<tr>
<td>1400</td>
<td>1.0</td>
<td>138</td>
<td>4</td>
<td>138</td>
<td>5</td>
</tr>
</tbody>
</table>

Figure 2. Schematic presentation of the DQA billet orientations.

Figure 3. Schematic presentation of the manufacturing phases for specimens with metallurgical constraints.
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Tensile tests were conducted for the specimens described above using a 150 kN servo-hydraulic test rig with displacement control. The test setup is presented in Figure 6 (a). Cross head velocities used in the tensile test and numerical calculations are presented in Table 4. In addition to a force sensor and a 50 mm strain extensimeter, digital image correlation (DIC) 3D strain measurement equipment, known as ARAMIS, was used to record the displacement data during the experiment. Prior to testing, the ARAMIS equipment was calibrated using the procedure recommended by the manufacturer. The procedure involves a use of calibration panel. The calibration was done for 75 mm lens with measuring volume of 105/80/60 mm. Single snap mode was utilized with ellipse quality of 0.4. Calibration deviation was 0.03 pixels and the limit value according to the manufacturer is 0.05 pixels. The scale deviation limit is 0.008 and the calibration result was 0.000 mm. During the measurement, subsets or edge tracing was not used. A speckle pattern was applied to the surface of each specimen. Firstly specimen was spray painted using a white color after that a black spray paint was used in order to create a shadow to the edges, as can been seen from Figure 6 (d). Due to the round geometry the light provided by the measurement system creates a shadow to the edges, thus creating problems and possible some noise to deformation data at the edges.

GOM Correlate software was used in order acquire the data from the experiments. A data point of DQA UN specimen and a data point of FW4.9 is presented in Figure 6 (b) and (c), respectively. Firstly, a coordinate system was aligned to the images captured from experiments. The Y-axis was designed to represent the axial dimension of the specimen. In the case of notched tensile specimens, a fitting circle was added by selecting the elements in the center line of the notch. A Gaussian best-fit method was applied and the normal of the generated circle was forced to align with the Y-axis of the coordinate system. This circle, named minCC_YMP 1 in the Figure 6 (b), was used to measure the minimum cross-section of the specimen. Five additional fitting circles, 1mm_YMP 1, 2mm_YMP 1, 3mm_YMP 1, 10mm_YMP 1 and 15mm_YMP 1, were also generated. The location of these circles were measured from the minCC_YMP 1 in the undeformed state. As the specimen deformed, the length between five additional circles and minCC_YMP 1 circle increased. The applied force was inputted to the software as a voltage signal from the force sensor. The deformation data and force data was used in order to generate force-displacement plots for the specimens.

After the alignment of the coordinate system, the next step in the post processing of the ARAMIS data of the friction welded specimens was to determine the center line of the soft zone. This was done by analyzing the Y-strain data. Symmetric deformation was assumed and thus, by knowing the length between the weld interfaces, the center could be determined. A Gaussian best-fit circle, named WM_center_YMP 1 in Figure 6 (c), was generated to that location. In addition, 15mm_YMP 1 was generated and the length between these circles and the voltage data from the force sensor was used to generate the force-displacement data.

The numerical calculations were conducted using the FE software LS-Dyna. The axisymmetric model of specimen UN is presented in Figure 7 (a). An examples of the models of the notched specimen and friction welded specimen are presented in Figure 7 (b) and (c), respectively. All models were generated using nominal
diameters and notch radii. The lengths for the FWXX/2 of CDB in the models were adjusted according to the measurements conducted for the specimens. In the FWXX models, the material parameters obtained using the optimization routine described below were used. According to the metallographic examinations (for more details, see the Results section), a straight weld interface was modeled. The HAZs of both materials were neglected in the analysis models based on the hardness measurements and tensile tests conducted for the DQA-DQA, CDB-CDB and CDB-DQA specimens; for more details, see the Results section.

Figure 6. (a) Tensile test setup. Examples of data acquisitions from (b) a notched tensile specimens and from (c) specimens with metallurgical constraint by utilizing GOM Correlate software. Example of the speckle pattern applied to the surface of specimens for DIC. Color contours in (b) and (c) represent the axial strains of the specimens.

Symmetrical constraints were used in the symmetry planes, represented by the dotted lines in Figure 7. Displacement as a function of time, represented by the arrows in Figure 7, was added to simulate the movement generated by the tensile test machine. The mesh was generated using fully integrated (four integration points through thickness) volume weighted axisymmetric solid elements (ELFORM 15 in LS-Dyna) with LS-Dyna default viscous hourglass control.

The size of the fine mesh at the axial symmetry end of the models was 0.1*0.1 mm. It is clear that the mesh size and the shape of the elements had an effect on the results, especially beyond uniform elongation. When necking begins, strain localizes to a certain region. Since the strain values after localization are dependent on the gauge length, the values differ depending on the mesh size. The authors acknowledge this fact, but since the dependency of the mesh size is not the focus of this paper, only the stated mesh size was used in the calculations.

An explicit solver was used to solve the models. The displacement speed used in the models was equal to the crosshead speed of the tensile test for the given specimen. Mass scaling (in LS-Dyna setup, DT2MS value was set to 5) was used to increase the stable time step of the explicit formulation in order to reduce the computational time without compromising the results. The energy balance of each simulation was analyzed to ensure that the amount of kinetic or hourglass energy was negligible.
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31.3.2020

Figure 7. (a) FE model used for UN specimens. (b) Example of the FE model for notched specimens and (c) example of FE model for friction welded specimens.

Table 4. Cross head velocities used in the experiments and in FE calculations.

<table>
<thead>
<tr>
<th>Specimen</th>
<th>Cross head velocity [mm/s]</th>
<th>Specimen</th>
<th>Cross head velocity [mm/s]</th>
<th>Specimen</th>
<th>Cross head velocity [mm/s]</th>
</tr>
</thead>
<tbody>
<tr>
<td>DQA UN</td>
<td>0.006</td>
<td>CDB UN</td>
<td>0.01</td>
<td>FW19.1</td>
<td>0.005</td>
</tr>
<tr>
<td>DQAUN45</td>
<td>0.006</td>
<td>CDB R30</td>
<td>0.008</td>
<td>FW14.6</td>
<td>0.005</td>
</tr>
<tr>
<td>DQA UN90</td>
<td>0.006</td>
<td>CDB R15</td>
<td>0.006</td>
<td>FW10.9</td>
<td>0.005</td>
</tr>
<tr>
<td>DQA R30</td>
<td>0.004</td>
<td>CDB R7.5</td>
<td>0.004</td>
<td>FW9.1</td>
<td>0.004</td>
</tr>
<tr>
<td>DQA R15</td>
<td>0.0035</td>
<td>CDB R5</td>
<td>0.004</td>
<td>FW6.8</td>
<td>0.004</td>
</tr>
<tr>
<td>DQA R7.5</td>
<td>0.003</td>
<td>CDB R1.5</td>
<td>0.003</td>
<td>FW4.9</td>
<td>0.004</td>
</tr>
<tr>
<td>DQA R5</td>
<td>0.003</td>
<td>CDB R1</td>
<td>0.003</td>
<td>FW4.0</td>
<td>0.003</td>
</tr>
<tr>
<td>DQA R1.5</td>
<td>0.003</td>
<td>CDB R0.5</td>
<td>0.003</td>
<td>FW2.8</td>
<td>0.0025</td>
</tr>
<tr>
<td>DQA R1</td>
<td>0.003</td>
<td></td>
<td></td>
<td>FW2.2</td>
<td>0.0025</td>
</tr>
<tr>
<td>DQA R0.5</td>
<td>0.0025</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

The material parameters for both steels used in this study were obtained using an optimization routine generated for the optimization software LS-Opt. In this study, metamodel-based optimization using a sequential optimization strategy with domain reduction was used. The routine determines the values for the chosen continuous variables used in the FE models and compares the numerical results to the imported experimental results. The routine is iterative and after each iteration loop, the values used for the variables are adjusted according to the previous results until the converged solution fulfills the inputted termination criteria. For the first iteration round, the user must input the initial values for the variables in addition to the limiting values. Material model parameters to be determined were inputted into the optimization routine as continuous variables.

The parameter identification was done for both applied materials in two phases using the tensile test results and FE models of specimens UN, R30, R7.5, R5, R1.5 and R0.5. In the first phase, the parameters $A$, $B$ and $n$ were determined. In this phase LS-Dyna material model MAT_098 was used. The experimental force-displacement curves obtained from the DIC measurements were used as input data for the optimization. An example of the input data obtained from the tensile test of the specimen CDB UN used in optimization of parameters $A$, $B$ and $n$ is presented in Figure 8 (a). The curves represent the different gauge lengths. The numerical values of force and displacement were adjusted for the axisymmetric condition of the FE models.

The parameters $D_1$, $D_2$ and $D_3$ were determined in the second optimization phase. Since in LS-Dyna MAT_098 ignores failure the material model was changed to MAT_015 in LS-Dyna. MAT_015 was used in analysis where parameters $D_1$, $D_2$ and $D_3$ was utilized. In these calculations, the constitutive parameters determined in the first optimization phase were used as material constants. In order to identify the fracture parameters, the optimization routine requires input data regarding the fracture. In this study, the instantaneous radius of the minimum cross-section displacement curves presented in Figure 8 (b) was used as the input data for the optimization. The curves were constructed by combining the FE results using the optimized constitutive model parameters with the measured fracture radii of the tensile specimens. The diameter was constant after the fracture; thus, after the ultimate displacement, the diameter was set to the measured value. This procedure was chosen.
because the exact point of fracture initiation could not be determined from the tensile tests and because the FE results were in good agreement with the experimental data; for more details, see the Results section.

Figure 8. Example of the input data for the optimization of the material parameters. Experimentally determined values have been adjusted for the axisymmetric condition.

4. Results

The hardness measurements conducted for near-surface and middle of the specimen CDB-DQA are presented in Figure 9 (a) and (b), respectively. According to the measurements, the HAZ in DQA has a distinct softened zone, which is typical for DQ steels. The lowest hardness is approximately 300 HV5, which is in the order of 75% of the pure/unaffected base material hardness. The measured hardness values of the CDB bar in welded condition varied by approximately ±10% compared to the hardness value given in the material certificate. Since hardness was nearly constant in the CDB, it was assumed that the CDB part of the specimen with metallurgical constraints could be modelled using the material properties obtained from the notched CDB specimens.

The tensile test results for the DQA-DQA, CDB-CDB, and CDB-DQA specimens are presented in Figure 10. In the DQA-DQA specimens, the fracture occurred in the softened HAZ. Both the CDB-CDB and CDB-DQA specimens fractured at the base material outside the gauge length. Thus, the fracture occurred more than 25 mm from the weld interface, clearly outside the HAZ, which indicates that the HAZ has a strength that is at least equal to that of the base material. Due to the higher hardness and tensile strength of the DQA HAZ compared to the CDB, the softened HAZ was also modelled using DQA material properties obtained from the optimization.

The weld interface was examined in detail using scanning electron microscopy (SEM) and energy-dispersive X-ray spectroscopy (EDS) analyses. The CDB and DQA can be distinguished from each other by the percentage of elements Cr and Ni. Figure 11 presents the EDS analysis of these elements at the weld interface. A distinct weld interface and a clear difference in the amounts of these elements can be observed in Figure 11. The width of each contour map is approximately 60 µm. The FE model was generated with a 100 µm mesh size. Thus, the existence of a clear and sharp interface between the materials in the FE model was assumed.

Figure 9. Hardness distributions from (a) near-surface and (b) middle of the CDB-DQA friction welded joint.
Use of the Quasi-Static Johnson-Cook Model in the Failure Assessment of Tensile Specimens With Metallurgical Constraints

Riku Neuvonen, Tuomas Skriko, Timo Björk

31.3.2020

1. Figure 10. Tensile test results of the additional welded specimens.

2. Figure 11. EDS contour map of the Cr and Ni contents at the weld interface of the CDB-DQA friction welded joint.

The tensile test results of the DQA UN specimens with different orientations are presented in Figure 12. The ultimate strength of the three specimens varied by 3.2 % and the difference in the minimum diameter after fracture was 10.7 %. The difference in the tensile behavior was small. According to these results, the DQA material might be slightly anisotropic. The specimens with metallurgical constraints were manufactured from billets cut parallel to the rolling direction, and the material parameters for DQA were defined using specimens with the same orientation. In addition, the results from the FE calculations were in good agreement with the test results without applying the anisotropic material model. Thus, anisotropy was neglected in this study. The same argumentation was applied for the CDB material.

3. Figure 12. Experimental tensile test results of the DQA UN specimens with different orientation.

4. 4.1. Notched tensile specimens

The fracture surfaces of all notched specimens are presented in Figure 13. Ductile fractures can be observed in all specimens. The material parameters obtained from the optimization routine are presented in Table 5. Strain rate and temperature effects were suppressed in the material model by using suitable predetermined parameters presented in Table 5. Graphical presentation of constitutive and fracture models for the used steels are presented in Figure 14 (a) and (b), respectively. In Figure 15 (a) and (b), a comparison between the diameter reduction in CDB and DQA specimens and the FE models is presented, respectively. The FE results are in good agreement with the experimental data. As the fracture model parameters do not influence the diameter reduction prior to element erosion, the calculated results combined with the experimentally determined fracture radii can be used in the identification of the damage parameters.

The stress-strain results of CDB specimens used in the optimization and the corresponding numerical results are presented in Figure 16 (a). In Figure 16 (b), the stress-strain results of CDB specimens not used in optimization and the corresponding numerical results are presented. The stress-strain results obtained from tests with DQA specimens and the corresponding FE results used in optimization are presented in Figure 16 (c) and the stress-strain results of DQA specimens not used in optimization and the corresponding FE results are presented.
in Figure 16 (d). A detailed comparison of the percentage difference in the ultimate tensile strength and diameter after fracture is presented in Table 6. In the table, the dashed lines separate the specimens not used in the optimization. The FE modelling results are in good agreement with the experimental data.

Figure 13. Fracture surfaces of notched specimens. Row (a): DQA specimens in ascending order from R0.5 to R30. Row (b): DQA UN, UN45 and UN90. Row (c): CDB specimens in descending order from R0.5 to R30. Row (d): CDB UN.

Table 5. Predetermined material parameters and parameters obtained from the optimization.

<table>
<thead>
<tr>
<th>Steel</th>
<th>A [MPa]</th>
<th>B [MPa]</th>
<th>n</th>
<th>( D_1 )</th>
<th>( D_2 )</th>
<th>( D_3 )</th>
</tr>
</thead>
<tbody>
<tr>
<td>CDB</td>
<td>636.679</td>
<td>180.122</td>
<td>0.325815</td>
<td>0.299891</td>
<td>305.29</td>
<td>-8.85566</td>
</tr>
<tr>
<td>DQA</td>
<td>1030.61</td>
<td>648.548</td>
<td>0.13464</td>
<td>0.075977</td>
<td>156.687</td>
<td>-6.78667</td>
</tr>
<tr>
<td>Predetermined material parameters</td>
<td></td>
<td></td>
<td>( C=0 )</td>
<td>( m=1 )</td>
<td>( D_2=0 )</td>
<td>( D_3=0 )</td>
</tr>
</tbody>
</table>

Figure 14. (a) Constitutive and (b) fracture models for the used steels.
Figure 15. Experimental (EXP) and FE results of the diameter reduction in (a) CDB and (b) DQA specimens.

Figure 16. Comparison of the stress-strain curves from the experimental tests and FE modellings.

Table 6. Percentage difference between the experimental values and FE results.

<table>
<thead>
<tr>
<th>Specimen</th>
<th>Tensile strength</th>
<th>Diameter after fracture</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>CDB</td>
<td>DQA</td>
</tr>
<tr>
<td>UN</td>
<td>-4.3</td>
<td>-0.5</td>
</tr>
<tr>
<td>R30</td>
<td>-4.2</td>
<td>-2.0</td>
</tr>
<tr>
<td>R15</td>
<td>-3.4</td>
<td>-1.6</td>
</tr>
<tr>
<td>R7.5</td>
<td>-3.8</td>
<td>-1.1</td>
</tr>
<tr>
<td>R5</td>
<td>-3.3</td>
<td>-1.7</td>
</tr>
<tr>
<td>R1.5</td>
<td>-1.0</td>
<td>-0.8</td>
</tr>
<tr>
<td>R1</td>
<td>2.0</td>
<td>-0.2</td>
</tr>
<tr>
<td>R0.5</td>
<td>-2.7</td>
<td>-1.2</td>
</tr>
</tbody>
</table>

4.2. Specimens with metallurgical constraints

The fracture surfaces of specimens with metallurgical constraints are presented in Figure 17. A cup and cone fracture mechanism at the center of the CDB region was observed in specimens FW19.1, FW14.6, FW10.9, FW9.1 and FW6.8. Specimens FW4.9, FW4.0, FW2.8 and FW2.2 fractured near the weld interface. In Figure 18, the cross-sections of the fractured specimens FW9.1, FW4.0 and FW2.2 are presented. The orientations of the cross-section images are represented by dashed lines in Figure 17.
The experimentally and numerically obtained stress-strain curves of specimens fractured via cup and cone mechanism are presented in Figure 19 (a) and (b). In Figure 19 (c) and (d), the corresponding results from specimens fractured near weld interface are presented. According to the figure, the tensile strength of the specimen increases as the width of the CDB zone decreases. The nominal mismatch of the friction welded joint is $M_n = f_u.CDB/f_u.DQA = 0.46$. According to the tensile test results of the UN specimens, the mismatch is $M_e = f_u.CDB_{UN}/f_u.DQA_{UN} = 0.50$. The mismatch for each specimen can be defined as $M_{FWXX} = f_u.FWXX/f_u.DQA_{UN}$. Figure 20 presents the values of $M_{FWXX}$ versus the length between the weld interfaces ($L_{FL}$) divided by the original diameter ($D_a$) of the specimen. The increase in the tensile strength is due to the increase in the metallurgical constraints as the $L_{FL}/D_a$ value decreases.

Figure 17. Fracture surfaces of the specimens with metallurgical constraints.

Figure 18. Cross-sections of the fractured specimens FW9.1, FW4.0 and FW2.2.

The percentage difference between the experimental results and the FE modelings for tensile strength and displacement at fracture measured by 30 mm gauge length are presented in Table 7. In the tests, the actual fracture initiation points could not be determined. Thus, the last recorded displacement value was used to represent the values. In the FE results, the fracture point was determined when the value of $D$ in the first element was equal to 1. The FE model contours of parameter $D$ at the instance of the erosion of the first elements are presented in Figure 21. The red dashed lines represent the weld interfaces and the white arrows point to the location of fracture initiation. The fracture initiated at the center of the axial symmetry plane (center of the CDB region) in models FW19.1, FW14.6, FW10.9, FW9.1 and FW6.8. The fracture initiated at the weld interface at the surface in models FW4.9, FW4.0, FW2.8 and FW2.2. Thus, the experimental results of the two distinct fracture locations were matched by the FE models. The numerically obtained results are in good agreement with the experimental results for the specimens fractured by the cup and cone mechanism.

In Figure 22 (a) and (b), the effective plastic strain contours of FE models are presented for cup and cone fracture and fracture near the weld interface, respectively. In the case of cup and cone fracture, the effective plastic strain has the highest value at the center of the model. As $L_{FL}/D_a$ ratio decreases, the effective plastic strain starts to accumulate to CDB side of weld interface at the surface of the specimen.

Figure 23 (a) and (b) present the contours of stress triaxiality of FE models. In addition to the increased stress triaxiality at the center of the notch, a concentration of stress triaxiality can be observed at the weld interface
in the surface of the soft zone. The location of increased stress triaxiality occurs in the same location as maximum
effective plastic strain in specimens fractured near weld interface, see Figure 22 (b).

The Lode parameter contours of FE models fractured via cup and cone mechanism are presented in
Figure 24 (a). In the Figure 24 (a), the Lode parameter contour indicates that stress state in the center of specimens
correspond to axisymmetric uniaxial tension. At the surface, the $L$ value is slightly lower than 1. In the surface of
the soft zone at the weld interface, there is a region of plane strain, i.e. $L=0$. Plane strain stress state can be observed
also in the FE models fractured near the weld interface, see Figure 24 (b). In the center of the notch, the area under
axisymmetric tension seems to be reduced as the ratio $L_{FL}/D_a$ decreases and the stress state at surface is shifted
towards the plane strain condition.

![Cup and cone fracture](image1)

![Fracture near weld interface](image2)

![Fracture near weld interface](image3)

Figure 19. Comparison between the stress-strain curves from the experimental tests and the FE modellings of the
specimens with metallurgical constraints.

![Strengthening caused by the metallurgical constraint](image4)

Figure 20. Tensile strength mismatch of each specimen versus $L_{FL}/D_a$.

Table 7. Percentage difference between the experimental values and FE results of the specimens with metallurgical
constraints.

<table>
<thead>
<tr>
<th></th>
<th>Cup and cone fracture</th>
<th>Fracture near weld interface</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>FW19.1</td>
<td>FW14.6</td>
</tr>
<tr>
<td>Diff. $f_u$ [%]</td>
<td>-8.4</td>
<td>-8.5</td>
</tr>
<tr>
<td>Diff. $\delta_30$ [%]</td>
<td>-0.6</td>
<td>0.0</td>
</tr>
</tbody>
</table>
Figure 21. Damage contours of the FE models of the specimens with metallurgical constraints. Images are taken after the first elements have been eroded.

Figure 22. Effective plastic strain contours of FE models with (a) cup and cone fracture and (b) fracture near weld interface. Images are taken a single time step prior the first elements have been eroded.

Figure 23. Stress triaxiality contours of FE models with (a) cup and cone fracture and (b) fracture near weld interface. Images are taken a single time step prior the first elements have been eroded.
5. Discussion

A good agreement between the experimental tensile test results and FE calculations was achieved using the Johnson-Cook model with material parameters obtained using the optimization routine. Thus, the model can estimate the behavior of DQA and CDB steels in the quasi-static region for round specimens. In the introduction section, the issues concerning the soft zone in weldments of DQ steels were discussed. The material models were calibrated to study the possibility of using the Johnson-Cook material model to assess the effects of a softened zone on the load-carrying and deformation capacity under the given circumstances. Because metallurgically induced changes in the mechanical properties of the HAZ in DQ welds can be very complex, simplified specimens were manufactured using friction welding. A distinct weld interface was observed between the materials. The hardness values of the CDB only changed by ±10 % and thus, it was assumed that the CDB region can be modelled using the identified parameters.

According to Porter [6], DQ steels are prone to softening in the HAZ. This behavior was also observed in this study. Experimental tensile tests conducted for the CDB-CDB, CDB-DQA and DQA-DQA specimens showed that the tensile strength of the DQA-DQA specimens is approximately 500 MPa greater than the tensile strength of CDB. Thus, the softened zone of the DQA in the FE models generated for the specimens was neglected and the model was generated using the obtained parameters.

Sun et al. [9] used analytical and FE calculations based on experiments conducted by Ran et al. [8] to predict the strength increase caused by the metallurgical constraints in the axisymmetric condition. In this study, the strengthening phenomenon was observed directly from the tensile experiments; see Figure 20. However, the results obtained by Sun et al. [9] and achieved in this study are not directly comparable because of the difference in the strength values of the used materials. Both studies indicate that if the numerical value of the width of the soft zone divided by the diameter is less than 1.0, the effect of the metallurgical constraints starts to increase, and when the value is in the order of 0.5, the effect intensifies.

Ran et al. [8] reported that mechanical properties and the width of the soft zone govern the overall behavior of the joint. They noticed that as the width of the soft zone decreases, the strength increases. In the current study, the tensile strength of the specimens with metallurgical constraints increased and the ductility decreased as the length of the soft region decreased. In this study, two different fracture behaviors were observed. The specimens with an $L_{FL}/D_A$ ratio greater than 0.8 fractured from the center of the CDB zone by the cup and cone mechanism. The specimens with an $L_{FL}/D_A$ ratio smaller than 0.6 fractured near the weld interface. Fusion line fracture of ultra-high strength steel weldments has previously been reported by Björk et al. [11] in fillet welded joints. As the $L_{FL}/D_A$ ratio decreased from 0.85 to 0.61 mm, the fracture location shifted from the center of the softened region to the weld interface, the ultimate strength of the specimens increased by 5 %, and the elongation at fracture decreased by 54 %.
In order to design and manufacture safe steel structures, the ductility and strength of welds must be at a sufficient level. Skriko [12] noted that on the basis of proper manufacturing parameters, e.g. heat input and groove preparation, joints with suitable performance quality can be fabricated from S960 DQ steel. The results of this study indicate that, at least under the given conditions, there is a certain cut-off value where the ductility of the joint decreases suddenly while the strength increases only slightly. Despite the limited amount of specimens and stress states covered in this study, it seems that the softening phenomenon must be considered in order to manufacture joints with suitable strength and ductility from DQA steels. Further studies must be conducted to gain more information on this phenomenon before any firm conclusions can be drawn.

Both Rodrigues et al. [10] and Sun et al. [9] concluded that the width of the softened zone, the mismatch ratio, and the hardening parameters of the adjacent materials affect the metallurgical constraints and the tensile properties of the joint. According to Figure 16 and Table 6, the use of the optimization routine generated reliable hardening parameters for the materials used in the specimens with metallurgical constraints. Also, both the width and the geometry of the softened zone and the mismatch ratio were clearly defined in this study. This seems to indicate that these parameters govern the mechanical behavior of specimens with metallurgical constraints.

The FE calculations of specimens with metallurgical constraints were in good agreement with the experimental results for the specimens with a cup and cone fracture, but they underestimated the fracture displacement when the fracture occurred near the weld interface. The difference between the predicted and tested tensile strengths shows a clear trend, namely it increased as the length of the soft region decreased. A reason for this could be that the models overestimated the metallurgical constraints because the softened HAZ of the DQA was neglected. The decision to omit the soft HAZ was made based on the hardness and tensile test results for the DQA-DQA, CDB-CDB and DQA-CDB specimens. However, in the DQA-DQA specimens, there is also the possibility that the metallurgical constraints strengthened the soft zone and thus, affected the results by increasing the tensile strength.

Since the DQA HAZ was not studied in detail, no information about the constitutive behavior of HAZ is currently available. The hardness distribution in HAZ is not constant; thus, the discretization of the mechanical properties for the whole zone is not a trivial task. In addition, as has been discussed, reliable material parameters need to be used when studying the metallurgical constraint effect. In order to improve the results, the different regions of HAZ must be included in the models, and consequently, further research in terms of this topic is needed.

As mentioned before, the fracture model used in this study does not include the influence of the Lode parameter. According to Figure 24 (b) there is a local plain strain condition in the location of fracture initiation in addition to an elevated stress triaxiality value. According to literature [27, 29, 28, 33], the Lode parameter value might have an effect on fracture locus of the material. On the other hand, the referenced studies seems to indicate that the effect of Lode parameter on the fracture is high at the low value of triaxiality and low at the high triaxiality state for structural steels. The effect of Lode parameter was not studied in this paper and based on authors’ knowledge, it has not been studied for the materials used in the current research. Thus, a study concerning the Lode parameter effect should be conducted in order to investigate it’s effect in the case of fracture near the weld interface.

6. Conclusions

In this research, the behavior and properties of round specimens subjected to tensile loading with and without metallurgical constraints were studied. Based on the conducted experiments, measurements and FE analyses, the following conclusions can be drawn:

1. Using notched tensile test specimens and the optimization routine, reliable material parameters can be obtained for different steel grades.
2. The ultimate tensile strength increases but ductility decreases in the soft zone due to the metallurgical constraint effect generated by the adjacent zones of higher hardness.
3. The Johnson-Cook material model can be used in the assessment of fractures in specimens with metallurgical constraints.
4. The created material model can estimate the two different fracture initiation locations observed in the experimental tests conducted for specimens with metallurgical constraints.
5. Value of the Lode parameter near the weld interface at CDB material is not constant. The model used in the study does not take into account the effect of Lode parameter. The effect of Lode parameter on the fracture in specimen with metallurgical constraints should be studied in more detail.

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Data availability

The raw/processed data required to reproduce these findings cannot be shared at this time as they also form part of an ongoing study.
References


