Damage mechanics modelling of material separation in self-pierce riveting (SPR) process

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Abstract. In numerical simulation of self-pierce riveting process (SPR) material separation criteria plays a critical role. In most of published works, material failure is simulated using numerical techniques, such as erosion criteria, that are calibrated on available experimental results. The lack of material based criteria strongly limits the use of numerical simulation as effective investigative tool for manufacturing process parameters assessment. In this work, damage mechanics is used to determine failure conditions in SPR considering dissimilar material sheets. In particular, the extended Bonora Damage Model (XBDM), which account for both void growth and void sheeting, was used. Damage model parameters for metal sheets have been determined independently and successively used in the simulation of SPR process. Results were compared with available experimental data.

1. Introduction

Self-pierce riveting (SPR) is a high-speed mechanical fastening technique for point joining of sheet materials consisting in a cold forming operation used to fasten two or more sheets of material by driving a tubular rivet through the top sheet, piercing the bottom sheet and flaring the rivet skirt under the guidance of a suitable die [1]. As the process relies on a mechanical interlock rather than fusion, it can be used on combinations of materials with different mechanical characteristics. Presently, improvements of the structural behavior of SPR joint structures are still mainly based on trial-and-error tests or knowledge-based procedures. Alternatively, finite element simulation can be used to investigate, before testing, the possibility to obtain an acceptable joint between two or more materials and to optimize the process parameters such as the sheet thicknesses or the die shape [2]. This requires both computational capabilities and knowledge of material behavior. The first involves the capability to account for multiple contact between deformable and rigid bodies, automatic remeshing, and large plastic deformation. The latter requires an accurate description of the material flow curve at large plastic strain, the possibility to account for strain rate and temperature effects but most of all the possibility to predict the occurrence of fracture under complex stress states. In the last two decades, the possibility to simulate the formation of SPR joint was demonstrated. In these studies, the piercing of the upper sheet was obtained either as a result of the automatic remeshing algorithm or by means of a separation criterion that splits the mesh when the ligament become smaller than a prescribed value [3]. It is evident that this kind of approach, although suitable to reproduce a specific SPR process, once calibrated on available experiment, cannot be used to investigate different configuration or conditions in which process parameters or materials are varied. Alternatively, material failure can be described by means of damage models. Among all possible approaches and model formulations, continuum damage
mechanics (CDM) has shown to be very versatile in predicting material failure under different stress triaxiality conditions. Recently, it was recognized that some classes of metals and alloys fail at much lower strain when deformed under shear, although the stress triaxiality being low or negative. Aluminum, magnesium and titanium alloys, which are main candidate materials for SPR, are among those materials in which shear fracture sensitivity has been reported. Bonora et al. [4] modified the Bonora damage model (BDM) [5] in order to account for the effect of the third invariant of the deviatoric stress on material ductility. This new model formulation extended the BDM predicting capabilities and its possible use in application in which materials are subjected to stress state with negative stress triaxiality dominated by shear deformation. In this work, the extended Bonora damage model (XBDM) was used to predict the piercing phase in the SPR process showing the possibility to anticipate critical behavior for a non-acceptable joint.

2. Extended Bonora Damage Model (XBDM)

In CDM, the set of constitutive equations for the damaged material are derived in the context of irreversible thermodynamics. Assuming isotropic damage, the state variable for damage $D$, is a scalar with an associated state variable $Y$ that can be derived from the state potential [6],

$$Y = -\frac{\bar{\sigma}}{(1-D)} \frac{R_v}{2E}$$

(1)

In this expression, $Y$ is the damage strain energy release rate, $E$ is the material Young’s modulus $\bar{\sigma}$ is the von Mises stress and $R_v$ is the term that accounts for stress triaxiality effect given as

$$R_v = \frac{2}{3}(1+\nu)+3(1-2\nu)T^2$$

(2)

where the stress triaxiality $T$ is defined as the ratio between the mean and Von Mises stress. Damage is assumed to be coupled with the equivalent plastic strain $\dot{\rho}$ accumulated under positive stress triaxiality state of stress. Testa et al. [7] modified the expression of the damage dissipation potential in the original Bonora damage model as follows,

$$F_D = \frac{1}{2} \left( \frac{Y}{S_0} \right)^2 \frac{S_0}{1-D} \left[ \left( \frac{D_{\alpha\alpha} - D}{\alpha} \right)^{\alpha^{-1}} \right] + \left( \frac{Y}{S_0} \right) \frac{S_0}{1-D} A_\omega \omega^\beta D^\beta$$

(3)

Here, the first term on the right hand side account for void nucleation and growth damage [8] while the latter term account for void sheeting damage though the parameter $\omega$ defined as a function of the Lode parameter $L$ as

$$\omega = 1 - \left[ -\frac{27}{2} J_3 \frac{1}{\bar{\sigma}^3} \right]^2$$

(4)

where $J_3$ is third invariant of the deviatoric stress tensor and $\bar{\sigma}$ is the von Mises stress. Here, $S_0, A_\omega, D_{\alpha\alpha}, \alpha, \beta, k$ are material constants. Therefore, the damage evolution law can be obtained from the normality rule as,

$$\dot{D} = \dot{\lambda} \frac{\partial F_D}{\partial Y} = \dot{D}_T + \dot{D}_\omega$$

(5)

Here, the first term on the right hand side accounts for stress triaxiality governed damage while the latter term accounts for the shear controlled damage. Under the assumption of constant stress
triaxiality (proportional loading) and constant \( \omega \) deformation path, eqn. (5) can be integrated analytically to obtain the following damage evolution law,

\[
D = D_{cr} \left[ 1 - \left( 1 - R_v \frac{\ln \rho}{\ln \varepsilon_f / \varepsilon_t} \right)^{\frac{\omega}{\beta}} + \left( \frac{\omega^k}{\gamma_f} \rho \right)^{\frac{1}{1-\beta}} \right]
\]  

(6)

where \( \rho \) the total accumulated plastic strain. Here, when \( D=D_{cr} \), \( \rho \) becomes \( \rho_f \) and this provides the expression of the failure locus. The model requires the determination of seven parameters. \( \varepsilon_t \) and \( \varepsilon_f \) can be determined by fitting failure strain vs stress triaxiality data obtained on tensile tests of round notched bar with different notch radii. Similarly, \( \gamma_f \) and \( k \) can be determined fitting on the same \( \rho \) vs \( T \) plot fracture data obtained for zero and negative stress triaxiality (torsion and shear plus compression test) for which the first damage contribution is zero.

### 3. Numerical simulation of SPR

#### 3.1. Geometry

In this work, the SPR of DP600 and AA 5182-O sheets was investigated. Numerical simulations were performed with the commercial code MSC MARC v2018. In Figure 1, the sketch of the reference configuration for SPR is shown. The holder and the punch are simulated as rigid bodies while deformable-deformable contact is considered between the rivet, the upper, and the lower metal sheets. The rivet type is the self-pierce H4 C-SKR with dimensions given in Figure 2. The upper and lower sheet thicknesses are 2.0 mm and 1.6 mm, respectively. Since the problem is axisymmetric, all simulations have been performed using 2D four node axisymmetric element with four Gauss points and bilinear shape functions. Analyses have been carried out using large displacement, finite strain and Lagrangian updating. Automatic remeshing was used to avoid severe element distortion and analysis interruption due to the impossibility to reach convergence. Remeshing was performed using advanced front quadrilateral prescribing the average (0.04 mm) and minimum element size (0.01 mm) for the new created elements.

![Figure 1. Reference configuration for SPR simulation.](image)

**Figure 1.** Reference configuration for SPR simulation.

![Figure 2. H4 C-SKR rivet type. D=5.0 mm, H=5.45 mm, B=7.67 mm.](image)

**Figure 2.** H4 C-SKR rivet type. D=5.0 mm, H=5.45 mm, B=7.67 mm.

#### 3.2. Materials

The materials considered are the dual phase steel DP600 and aluminium alloy AA5182-O. The DP600 is a dual phase steel consisting of a ferrite matrix containing a hard second phase, usually islands of martensite, widely used in the automotive industry for different parts and safety cage components (B-pillar, floor panel tunnel, engine cradle, front sub-frame package tray, shotgun, seat). AA5182-O has good formability and corrosion resistance and is used for packaging products such as containers, beverage cans, automotive body panels and reinforcement members. Testa et al. [9] determined the
flow curve and the XBDM model parameters for DP600. For AA5182-O, the flow curve as reported in [10] was refitted using a Voce law, successively validated simulating tensile test on thin sheet in order to ensure the onset necking. Figure 3. Bonora et al. [11] showed the possibility to identify damage model parameters fitting fracture formability limit (FFL) and shear fracture formability limit (SFFL). Using this procedure, the damage parameters for AA5182-O were identified. In Figure 4, the fitted FFL over available data is shown. Unfortunately, shear fracture data were not available. Therefore, the solution proposed by Isik et al. [12] to represent the SFFL in the principal strains space as a perpendicular straight line to FFL, was made to estimate the damage parameter for shear controlled fracture, Figure 4. Finally, the XBDM parameters are summarized in Table 1.

![Figure 3. Flow curve of AA5182-O, comparison with simulated tensile test on sheet sample.](image)

![Figure 4. Damage parameters identification for AA5182-O using FFL and SFFL data.](image)

| Table 1. Summary of damage parameters for AA5182-O |
|-----------------------------------------------|
| $\varepsilon_{th}$ | $\varepsilon_f$ | $D_s$ | $\alpha$ | $\gamma$ | $\kappa$ | $\beta$ |
| 1.97          | 2.01         | 0.1  | 0.3     | 0.72      | 0.3    | 0 |

### 4. Results and discussion

The importance of modelling material failure in SPR simulation, the joining of AA5182-O and DP600 sheets was simulated using the Bonora damage model and its extended version. In the first case, because the stress triaxiality during plastic deformation is always negative, no damage is predicted to develop. Piercing of upper metal sheet occurs because of the plastic strain redistribution due to global remeshing. Remeshing smooths plastic strain gradient and the redefinition of new contact surface force the material to flow around the rivet. In Figure 5, the comparison of the predicted punch force vs vertical displacement for calculated with BDM and XBDM is shown. Here, for the XBDM, two solutions obtained varying the shear damage parameter $\gamma_f$ for DP600 are reported. It can be noted that when piercing is driven by remeshing (no damage), the predicted punch force vs displacement is approximately 30% higher than that predicted considering material failure. Changing the shear resistance of the lower sheet does not change the response of the system until damage initiates here. In Figure 6, sketches of the predicted deformed configurations at 0.4 mm, 2.57 mm and 4.63 mm (4.0mm for the BDM case) are shown. These punch displacement values correspond to the end on the clamping stage, the end of the piercing stage and the stage in which the rivet interacts with the lower metal sheet. It is interesting to note that when piercing occurs because of remeshing (case A), the rivet is predicted to flare simultaneously with piercing. This behaviour is contradicted by experimental observations that clearly show that piercing occurs by material failure and not by accommodation of plastic deformation [13]. In this case the use of damage models that account for void nucleation and
growth process only with unilateral condition for damage, would not be able to anticipate the occurrence of material failure.

\[ \Delta = 0.4 \text{ mm} \quad \Delta = 2.57 \text{ mm} \quad \Delta = 4.0 \text{ mm} - 4.63 \text{ mm} \]

**Figure 5.** Predicted punch force vs displacement considering shear damage.

**Figure 6.** Deformed mesh showing different stage of rivet deformation for: A) BDM, B) XBDM DP600 \( \gamma = 0.9 \) and C) XBDM DP600 \( \gamma = 1.8 \).

The fact that piercing occurs by material failure is a clear indication that shear damage has to be accounted for to simulate correctly the SPR process. Case B and C show that with XBDM, at the end of the clamping phase, piercing initiates as a result of the development of material failure which is controlled essentially by the shear damage contribution. When the piercing of the upper sheet is
completed, mechanical interlock can occur if the lower sheet material has enough resistance to shear damage. In Figure 6-C, in the case of DP600, using the right damage parameter for shear damage, results in the full development of the interlock as also observed in the experiments for the AA5182-O/DP600 joint. Interestingly, if the material would exhibit a lower shear damage resistance, as for the Figure 6-B, the model predicts the occurrence of fracture development in the lower sheet. This result is also consistent with experimental observations of fatigue resistance of SPR joints[14]. Here, the difference observed in the life of joints was ascribed to the damage induced by the SPR process.

5. Conclusions
In this work, the extended formulation of the Bonora damage model was used to simulate self-piercing rivet process. Numerical simulations confirmed that the piercing stage cannot be simulated correctly without accounting for shear damage. The use of remeshing, which is indeed necessary to avoid excessive element distortion, can artificially cause the piercing of the upper sheet but the predicted behaviour of the rivet would differ considerably from that observed in interrupted SPR process tests. The proposed damage model formulation demonstrated to be suitable to predict the effective behaviour of materials and to anticipate the quality of the resulting joint.

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