Numerical Modelling of Sheet Metal Damage in LS-DYNA using GISSMO

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Abstract:

In many engineering applications, as well as in sheet metal forming, there is little interest in studying material behavior until complete fracture. However, when damage and failure of the material is required in order to achieve the purpose of an analysis, it is crucial to understand and predict the material behavior from initial deformation until fracture. Forming limit diagrams (FLD) are commonly used to estimate sheet metal formability but fall short in the sense that they only predict whether or not the material will fracture, but do not describe the fracture behavior. A damage model on the other hand, describes the material degradation until complete fracture.

Modelling of the damage evolution of a sheet metal under deformation is a useful tool within the field of engineering and materials science and can provide information regarding material capabilities and the underlying physical principles of sheet metal forming. With the help of damage modelling, engineers can predict the likelihood of material damage and minimize the need for physical testing as well as time and material waste.

In this paper, a strategy for numerical simulation of cutting during concurrent pressing of stainless steel sheet metal is investigated while accounting for the stress triaxiality and Lode angle parameter. An inverse modelling approach is used, where both a material model and a damage model are calibrated. The damage model is developed using the GISSMO damage model (Generalized Incremental Stress State dependent damage Model). The fracture strain is defined in the stress triaxiality and Lode angle parameter space as a surface, and experiments using different specimens are conducted in an attempt to cover the space of possible stress states. The modified Mohr-Coulomb fracture criterion is used to predict fracture strain in a variety of stress states. The simulation software LS-DYNA is used for numerical modelling and the software LS-OPT is used in conjunction with LS-DYNA to identify damage parameters for use in the GISSMO model.

The results show good agreement with experimental load case data but due to issues with stress state characterization of some of the conducted experiments, further validation is necessary before the damage model can be used in practice. The stress states experienced by a sheet metal under deformation were ultimately found to be widely spread in the stress triaxiality and Lode Angle parameter space, whereas the material tests were limited to a relatively small region. The results from this work show a strong potential for the inverse modelling approach to model the evolution of damage using GISSMO. It was concluded that, in order to accurately describe the material behavior during cutting, experimental data from a wide variety of stress states is necessary and the results from this work highlight that the resulting damage model is highly dependent on the accuracy of the conducted experiments.

1 Introduction

Due to increasing industrial importance, the field of metal fracture has been of interest for many researchers over the last 70 years. There are two fundamental types of fracture in metals, brittle and ductile fracture, where the latter will be discussed here due to its relevance to this work.

Ductile fracture can generally be described as a three-step process: (a) accumulation of damage; (b) fracture initiation; and (c) crack propagation. The fracture initiation occurs due to the accumulation of damage which can be considered microscopically and macroscopically. On a microscopical scale, damage is observed as nucleation, growth and coalescence of voids. On a macroscopical scale however, damage can be observed as the loss of load-bearing capacity of a material in terms of decreased stiffness, strength and reduced ductility [1].

This work is focused on the macroscopical scale of damage, and uses a continuum damage mechanics approach, meaning that the material is idealized as a continuum. The accumulation of damage is described by an internal damage variable coupled with plasticity variables to model the loss of load-bearing capacity of the material. Numerous damage models have been suggested by researchers to describe material degradation which differ in terms of how the internal damage variable evolves with plastic deformation. The use of a continuum damage model can be considered an engineering approach to damage mechanics as opposed to a more scientific model.

The present paper is concerned with investigating a method of modelling the fracture behavior of a punching process during forming of a sheet metal plate for a gasketed plate heat exchanger. The damage modelling is approached using the GISSMO damage model accounting for stress state dependency by means of triaxiality and Lode angle parameter. The space of stress states is defined as a surface, and is obtained using experimental data from several different test specimens, namely uniaxial tension, plane strain, shear stress and a biaxial bulge test for 316 stainless steel. The problem is approached using an inverse modelling technique where the experimental data is used to define the material behavior.

In sheet metal forming, forming limit diagrams are commonly used to evaluate necking and thinning of sheet metal plates. A forming limit diagram only predicts when the material will fracture which is insufficient when the fracture development and crack propagation is of interest. Damage models on the other hand, describe the material degradation on a more detailed level, and for the applications in this work where the fracture is implied, the FLD loses its relevance.

GISSMO is an isotropic, phenomenological damage model, meaning that it is consistent with fundamental theory, but not directly derived from it. The damage model was proposed by Neukamm et al. [2] and is implemented in LS-DYNA, and can be applied to any material model in the software.

2 Stress States

The ductility of a metal is commonly co-analyzed with stress triaxiality which has proven to directly impact fracture strain [3]. Studies conducted by Xue [1] and Bai and Wierzbicki [4] concluded that not only the stress triaxiality has an impact on the fracture strain, but also the Lode angle parameter should be included in the constitutive material description. The stress state of a material can be represented in the principal stress space and is defined using the dimensionless parameters stress triaxiality and Lode angle parameter. The stress state parameters can be obtained from the Cauchy stress tensor, and the state of stress is represented in the Haigh-Westergaard stress space as illustrated in Fig. 1.

The stress triaxiality is defined using the hydrostatic part of the stress tensor that acts to change the volume of the body. The hydrostatic stress can be represented in the Haigh–Westergaard stress space by a position along the hydrostatic axis where $\sigma_{11} = \sigma_{22} = \sigma_{33}$, as illustrated in Fig. 1.

The stress triaxiality is defined as the ratio between the mean stress and the equivalent von Mises stress and can be expressed as

$$\eta = \frac{\sigma_m}{\sigma_{eq}} = \frac{\frac{1}{3}(\sigma_1 + \sigma_2 + \sigma_3)}{\sqrt{\frac{1}{2}[(\sigma_1 - \sigma_2)^2 + (\sigma_2 - \sigma_3)^2 + (\sigma_3 - \sigma_1)^2]}}.$$

The Lode angle parameter is defined using the deviatoric part of the stress tensor which is responsible for the non-volumetric distortion of a body [5], and can be represented as the deviatoric plane in the Haigh-Westergaard stress space, as illustrated in Fig. 1.

The dimensionless Lode angle parameter is defined in terms of the normalized third deviatoric stress invariant, ξ. The Lode angle is illustrated in the principal stress space as an angle in the deviatoric plane. The normalized third deviatoric stress invariant can be expressed using the deviatoric stress invariant, J_3 as

$$\xi = \frac{27}{2}\frac{J_3}{\sigma_{eq}^3} = \cos(3\theta).$$

The Lode angle can then be normalized and expressed as the Lode angle parameter

$$\overline{\theta} = 1 - \frac{2}{\pi}\arccos(\xi).$$

Together with the stress triaxiality, the Lode angle parameter is used to define the three-dimensional stress state of a body.

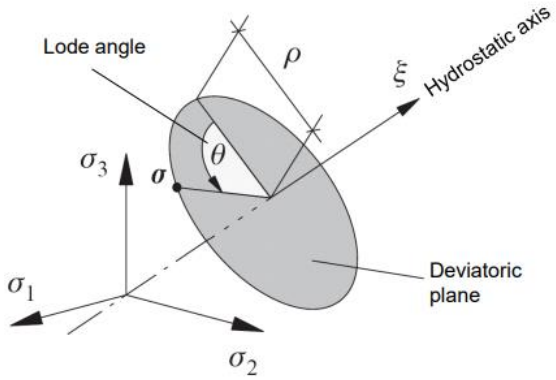

Fig.1: Haigh-Westergaard stress space [6].

3 Damage Evolution

Material damage can be described as the loss of load-bearing capacity of a material, and from a continuum point of view this manifests as a decrease in material stiffness, strength, and ductility. In GISSMO, the damage evolves with a scalar quantity according to a power law as

$$D = \left(\frac{\epsilon_p}{\epsilon_f}\right)^n,$$

where ϵ_p is the plastic strain, ϵ_f is the fracture strain at a given value of triaxiality and Lode angle parameter and n is the non-linear damage exponent. If a load path is in varying states of stress, it is necessary with an incremental formulation of the above equation which can be expressed as

$$dD = \frac{n}{\epsilon_f(\eta, \xi)} D^{\frac{n-1}{n}} d\epsilon^p = f\left(D, \epsilon_f\right) d\epsilon^p.$$

It can be noted that the damage begins to evolve at the onset of plastic deformation. The damage is coupled to the stress at the point of instability, i.e., the initiation of diffuse necking. Material instability is measured in terms of an instability parameter F which evolves with plastic strain as

$$dF = \frac{n}{\epsilon_i(\eta)} F^{\left(1-\frac{1}{n}\right)} d\epsilon^p, \qquad 0 \le F \le 1.$$

The instability parameter F and the damage parameter D are equal to zero at initial plastic strain and reach critical values $F = 1$ and $\mathrm{D_{CRIT}} \neq 0$ at the point of instability, and at $D = 1$ the material fractures. Beyond the point of instability, the damage is coupled to the stress as

$$\sigma^* = \sigma\left[1 - \left(\frac{D-D_{CRIT}}{1-D_{CRIT}}\right)^{FADEXP}\right],$$

where the fading exponent, FADEXP, is a parameter that impacts the softening behaviour once the damage has been coupled to the stress. GISSMO is activated in LS-DYNA using the keyword ***MAT_ADD_DAMAGE_GISSMO** [7].

4 Fracture Locus

Ductile fracture is dependent on the triaxiality and the Lode angle parameter. Before the Lode angle parameter was introduced however, only triaxiality was used to predict failure strain which is valid under plane stress assumption. In a plane stress load case, where $\sigma_3 = 0$, Xue and Wierzbicki [8] showed that the stress triaxiality can be uniquely related to the Lode angle parameter by

$$\xi = cos\left[\frac{\pi}{2}\left(1-\bar{\theta}\right)\right] = -\frac{27}{2}\eta\left(\eta^2 - \frac{1}{3}\right),$$

meaning that the stress state can be expressed solely in terms of the triaxiality. When the damage model is to be used in three dimensions, such as in a punching process, it is no longer a plane stress case and the triaxiality and Lode angle parameter become independent of each other. The result of adding the Lode angle parameter is that instead of a failure curve, a surface is required to define the failure as illustrated in Fig. 2.

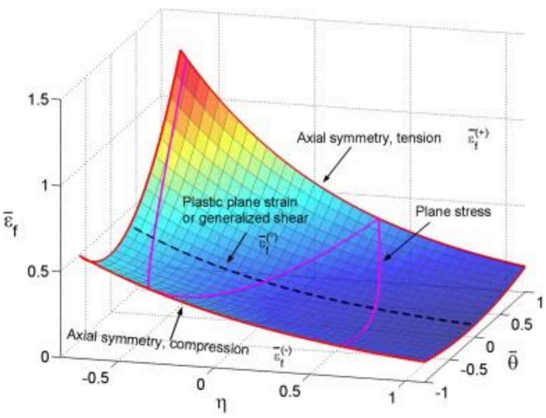

Fig.2: Three-dimensional fracture surface in the triaxiality and Lode angle parameter stress space [4].

Due to the vast number of stress states in the triaxiality and Lode angle parameter space, it is not possible to find the fracture strain for every load case. However, it is possible to find the fracture strain for stress states that describe the general shape of the fracture locus, hence the different experiments that were conducted in this work. In order to create a three-dimensional fracture locus, a fracture criterion is assumed which interpolates and extrapolates fracture strains in the entire space of stress states. The modified Mohr-Coulomb fracture criterion (MMC) is used which was suggested by Bai and Wierzbicki [9] as an extension to the Mohr-Coulomb fracture criterion. Their findings have proven to accurately take into account effects of Lode angle parameter and triaxiality. The plane stress and plane strain stress states can be observed in Fig. 2, showing the necessity of a fracture criterion when the load case is not in plane stress.

5 Experimental Investigation

Physical experiments have been conducted to lay the foundation for the damage model in this work. In the experiments, the specimens had a thickness of 0.5 mm and were designed such that they each represented a different stress state described by the Lode angle parameter and the stress triaxiality. From each experiment a fracture strain could be found, which then acted as the foundation of the three-dimensional fracture locus. The tests were conducted in a quasi-static manner in the rolling direction of the material and were performed to provide a plane stress state. Since the experiments were only conducted in the rolling direction, material anisotropy cannot be accurately accounted for. However, a previously obtained material model was used to scale the missing parameters based on the yield stresses in the rolling direction. Furthermore, strain rate effects were not considered in the experimental testing.

The experimental investigation consisted of four different tests as illustrated in Fig. 3.

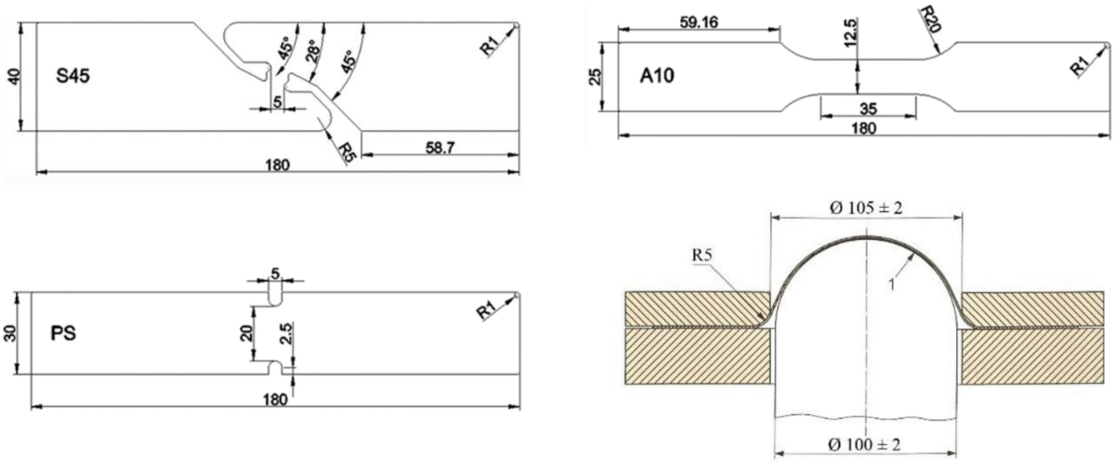

Fig.3: Experimental geometries for material- and damage model calibration [10].

The stress state for each specimen was measured as an average value obtained from the center region of each specimen using Digital Image Correlation (DIC), as presented in Table 1.

Load case	Specimen	Triaxiality, η	Lode angle parameter, $\bar{\theta}$
Shear	S45	0.139	0
Uniaxial	A10	0.361	1
Plane strain	PS	0.574	0
Biaxial	Nakajima	0.667	-1

Table 1: Stress state parameter values for each test geometry.

6 Numerical Setup

To numerically investigate the physical measures from the test specimens, they were modelled using solid elements with reduced integration. In the case of a plane stress problem, shell elements can be used but due to the three-dimensionality of the punching process, solid elements are necessary to describe the different stress states. As opposed to the pre-necking state, the damage model is element size dependent. Due to this, the element size used in the future application of the damage model should coincide with the element size used in the damage model calibration. As the damage model is to be applied to forming simulations with an element length as short as 0.1 mm, the same element length should be used in the damage model calibration. Due to computational limitations and lack of time however, an element length of 0.125 mm is used. Only the part of the specimen that is affected by the damage model, i.e., the part that experiences large enough plastic strains, had a refined mesh where the damage model is active. It is possible and recommended to do a mesh regularization, meaning that the resulting damage model can be scaled based on element size which is a method of making finite element models mesh independent.

Symmetry conditions were used such that the uniaxial tensile test and the plane strain test were modelled as 1/8 of the full geometry. The shear stress test and the biaxial bulge test as 1/2 and 1/4 of the full geometry, respectively. This greatly reduced the size of the models and therefore also the computational time. The model of the uniaxial tensile test is illustrated in Fig. 4.

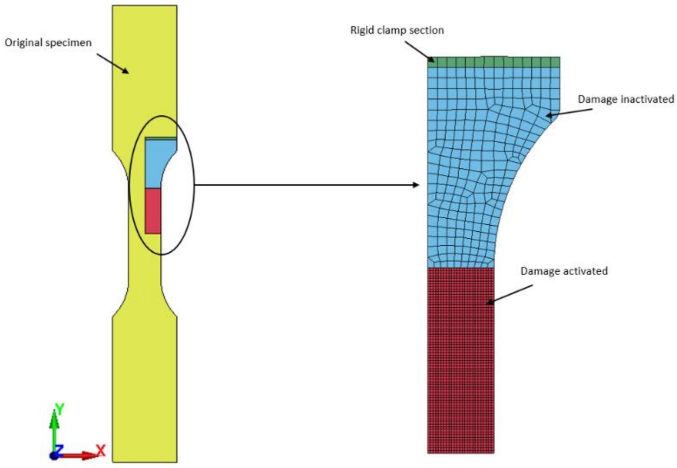

Fig.4: Original and symmetric model of the uniaxial tensile test.

7 Material Model Calibration

The material model used in this work is the Barlat YLD2000 model, which was developed by Barlat et. Al. [11], and has proven to provide accurate results in sheet metal forming processes. It is a frequently used material model for forming simulations due to its flexibility regarding input parameters and applicability on different models using different element types.

The material model is used to uniquely describe the material behavior until the point of instability and is calibrated using experimental data from the uniaxial tensile test by determining a hardening curve. The yield stress is assumed to be at 0.2% plastic strain and the hardening curve is determined by fitting a curve to the experimental stress-strain data. The data used for the curve fitting is every measured experimental point from the yield stress up until the point of necking. The hardening behavior of the material is represented by a power law model, defined as $\sigma = K\epsilon^n$, which is determined using experimental data from the uniaxial tensile test. In the power law, K is a material constant and n is the strain hardening exponent. In Fig. 5 the experimental true stress as a function of plastic strain for the uniaxial tensile test is presented together with the fitted hardening curve. The hardening curve is validated using the four experimental test cases and is directly used in the material model in LS-DYNA as a load curve consisting of the true stress values at given true plastic strains. The points on the hardening curve beyond the necking point are extrapolated stress values that are coupled to the damage as previously described.

In Fig. 6, the force displacement response for each test using the material model is illustrated and compared with experimental data.

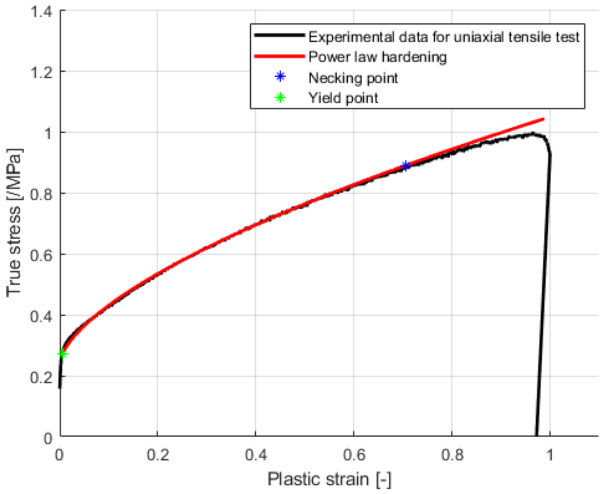

Fig.5: Hardening curve fitted to experimental data for uniaxial tensile test.

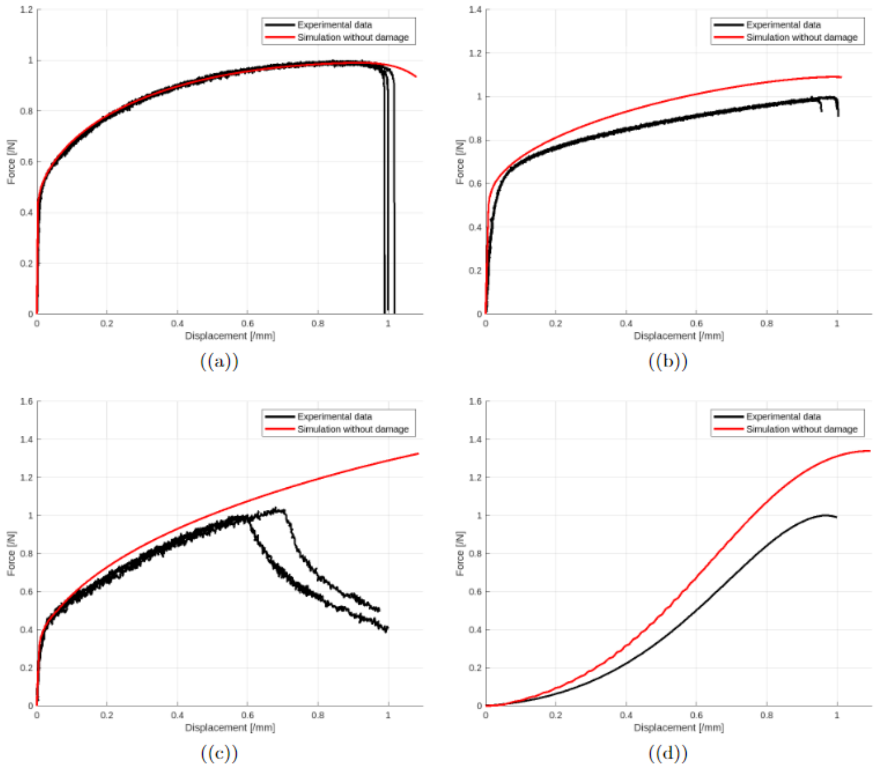

Fig.6: Experimental data(black) and simulated force displacement response(red) from the (a) uniaxial tensile test, (b) plane strain test, (c) shear stress test and (d) biaxial bulge test without an active damage model.

8 Damage Model Calibration

To model damage and failure using GISSMO, several input parameters are required which affect the onset of necking as well as the post necking behavior. The damage and fracture are dependent on the stress state of the material defined in the Lode angle parameter and triaxiality stress space. Depending on the stress state, the material will have different points of instability onset and fracture strain and to obtain these values, the simulation data is recorded as force and displacement and is matched against experimental data.

8.1 Parameter Identification

The parameters necessary to calibrate the fracture locus are the damage exponent, the fading exponent, the instability strain and the fracture strain. To identify the parameters of the finite element models an optimization algorithm is run using LS-OPT where parameters are allowed to vary within an interval in order to find the best curve fit using a least square method. The points used for the curve fitting are taken from the softening part of the experimental force displacement data where the material degradation takes place, as illustrated in Fig. 7. The damage exponent and fading exponent are global parameters and are obtained from the uniaxial tensile test, which was also used to calibrate the material model.

Each test specimen is simulated independently in LS-OPT to find instability strain and fracture strain in their respective stress states before combining them in one general **`*Keyword`**.

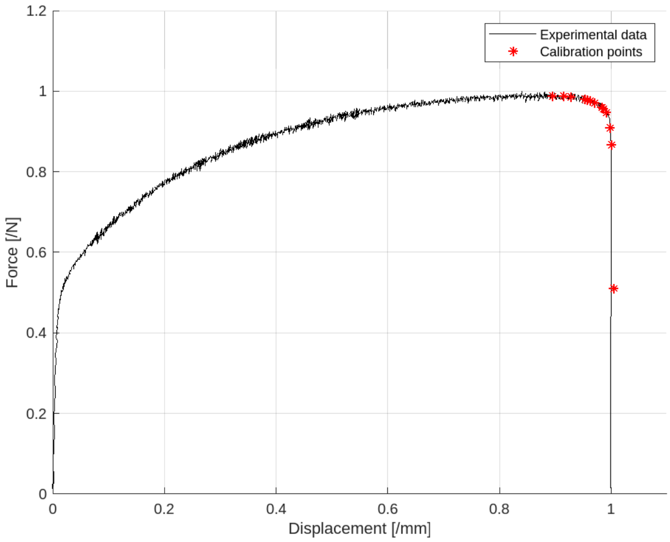

Fig.7: Experimental data with chosen damage calibration points for the damage parameters for the uniaxial tensile test. Similar points are chosen for the other models.

Case	Damage exponent	Fading exponent	Instability strain, ϵ_i	Fracture strain, ϵ_f
S45	2.28	2.3	0.29	0.66
A10	2.28	2.3	0.296	0.67
PS	2.28	2.3	0.26	0.62
Nakajima	2.28	2.3	0.31	0.73

Table 2: Damage parameters obtained from LS-OPT. The damage exponent and fading exponent are obtained from the uniaxial tensile test.

8.2 Fracture Locus

To create the fracture locus, the points corresponding to the measured fracture strains obtained using LS-OPT were used in a python script in which a curve fit function generates a surface according to the MMC fracture criterion. The points that make up the surface were written to an input file as load curves in a table definition that could be used in LS-DYNA using the **`*INCLUDE`** keyword. The fracture locus is illustrated in Fig. 8, indicated by the damage parameter values in Table 2. The calibration point at triaxiality $\eta = -0.5$ and Lode angle parameter $\overline{\theta} = -1$ was not found through calibration but was used to avoid limitations for the damage model in compressive stress states where there was a lack of experimental data.

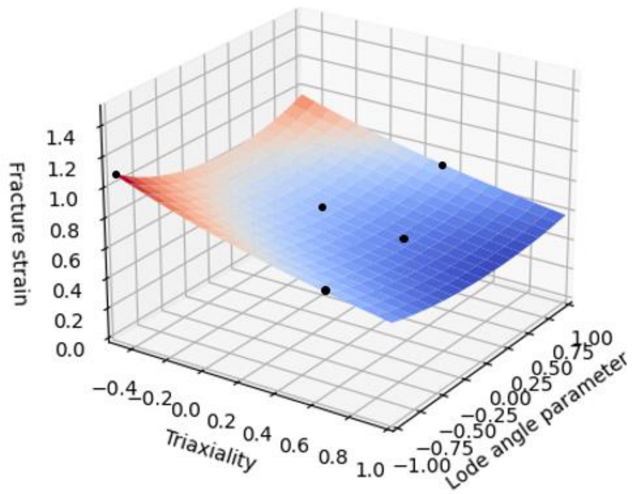

Fig.8: An illustration of the fracture locus found using the identified damage parameters, indicated by the black dots, using the modified Mohr-Coulomb fracture criterion.

In Fig. 9 the force displacement response for each of the models is presented where the fracture locus has been used.

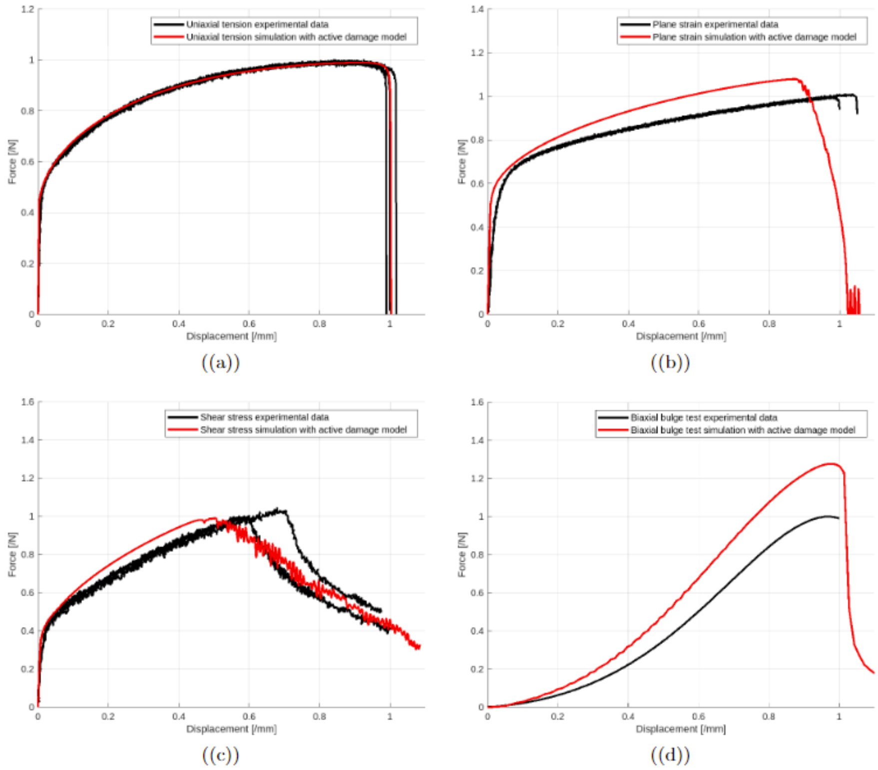

Fig.9: Experimental(black) and simulated force displacement response(red) from the (a) uniaxial tensile test, (b) plane strain test, (c) shear stress test and (d) biaxial bulge test with an active damage model using the fracture locus.

9 Discussion and Conclusion

The industrial importance of damage modelling has brought necessary attention to the subject and the influences of triaxiality and Lode angle parameter on fracture strain have been highlighted by many researchers. This paper has investigated a method of incorporating the triaxiality and Lode angle parameter in a three-dimensional space of stress states.

The use of the MMC fracture criterion assumes that the damage in every stress state can be described by experiments in a plane stress state and therefore also that the fracture criterion accurately estimates the fracture strains in other stress states. The choice to use the MMC fracture criterion is based on successful results in previous research regarding ductile fracture. However, further testing in a wider variety of stress states would be necessary to confirm that the MMC criterion is accurate in this case.

The damage model can be further improved by additional experimental data in the pure shear and compressive stress states where the accuracy of the current model cannot be determined. Conducting punching and compressive tests would provide a wider spread of experimental stress states which would improve and validate the damage model in shear and compression.

In order to accurately assess a damage model, accurate experimental data is crucial. In this work it proved to be difficult to characterize the stress states for the plane strain and shear stress tests. The use of DIC to assess the stress state in the center of each specimen is accurate in that specific region. The plane strain and shear tests, however, were not homogeneous and the fracture was not initiated in the region where the stress state was measured. This issue raises the question of which stress state the specimens should be characterized with and where the fracture strain is valid. The effect of this can be seen in Fig. 9, especially for the plane strain model where the fracture response significantly deviates from the experimental data. The response for the uniaxial tensile model agrees very well with the experimental data which is due to the homogeneous nature of the experiment and ease of characterizing its stress state.

The present approach for investigating a strategy for numerical simulations of punching during concurrent pressing of stainless steel sheet metal appears promising for use with other materials. Despite the lack of incorporation of physical mechanisms, the phenomenological GISSMO damage model shows good experimental agreement. The material model can be more carefully calibrated by additional testing in different directions in the sheet metal in order to more accurately assess the material anisotropy. As can be seen in the resulting force displacement curves, the material model response is stiff compared to the experimental data. Further improvements can perhaps be made if experiments of inhomogeneous nature are avoided to the fullest extent possible.

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