Prediction of fracture initiation in square cup drawing of DP980 using an anisotropic ductile fracture criterion

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Abstract. This paper deals with the prediction of fracture initiation in square cup drawing of DP980 steel sheet with the thickness of 1.2 mm. In an attempt to consider the influence of material anisotropy on the fracture initiation, an uncoupled anisotropic ductile fracture criterion is developed based on the Lou–Huh ductile fracture criterion. Tensile tests are carried out at different loading directions of 0°, 45°, and 90° to the rolling direction of the sheet using various specimen geometries including pure shear, dog-bone, and flat grooved specimens so as to calibrate the parameters of the proposed fracture criterion. Equivalent plastic strain distribution on the specimen surface is computed using Digital Image Correlation (DIC) method until surface crack initiates. The proposed fracture criterion is implemented into the commercial finite element code ABAQUS/Explicit by developing the Vectorized User-defined MATerial (VUMAT) subroutine which features the non-associated flow rule. Simulation results of the square cup drawing test clearly show that the proposed fracture criterion is capable of predicting the fracture initiation with sufficient accuracy considering the material anisotropy.

1. Introduction
In the automotive industries, the usage of Advanced High-Strength Steels (AHSSs) has been remarkably increasing due to a fact that AHSS sheets show outstanding strength to weight ratio, which allows lightweight construction of the car body structures. AHSS sheets, however, usually comes at expense of ductility resulting in having relatively poor formability compared to that of the conventional steel sheets. This drawback brings about the undesired failure mode of shear fracture during sheet metal forming processes. The shear fracture cannot be predicted by the conventional Forming Limit Diagram (FLD) because it is observed not only in tension but also in shear and compression loading states where the thickness reduction is negligible.

In an attempt to deal with this challenging issue encountered in forming of AHSS sheets, various research works have been reported based on coupled and uncoupled approaches mainly towards modeling of ductile fracture criterion [1–6]. It is because ductile fracture criteria are able to deal with the fracture initiation over a wide range of stress states including shear and compression conditions.
whereas the conventional FLDs can monitor the fracture strain only from uniaxial tension to balanced biaxial tension conditions.

Sheet metals usually exhibit a certain plastic anisotropy on the mechanical properties due to their oriented-crystallographic structures resulting from the conventional rolling process. Material anisotropy is one of the main factors that can affect the fracture initiation according to various loading conditions subjected to a certain loading direction. Prediction of anisotropic fracture initiation in sheet metal forming still remains as a significant challenge for the automotive industry to produce metal components in the desired shape at low cost. Underlying this challenging issue is the difficulty in predicting the fracture initiation with sufficient accuracy according to the loading direction using anisotropic ductile fracture criteria previously proposed. It is, therefore, essential to develop an anisotropic fracture criterion capable of predicting the onset of fracture accurately according to both loading conditions and loading directions.

2. Development of anisotropic ductile fracture criterion

As an effort to predict the anisotropic fracture initiation over a wide range of stress states, Park et al. [7] recently proposed an uncoupled anisotropic fracture criterion as:

$$(\sqrt{T})^{-C_i(\theta_3)} \left( \frac{1}{1+C_o} \left( \frac{\eta_H + \frac{3-L_p}{6\sqrt{T}} + C_o}{2} \right)^{C_i(\theta_3)} \right) \varepsilon_f^+ = C_i(\theta_3)$$  \hspace{1cm} (1)

where $T = F \left( \frac{1+L_p}{2} \cos^2 \theta_3 + \sin^2 \theta_3 \right)^2 + \left( \frac{1+L_p}{2} \sin^2 \theta_3 \right)^2 + \left( H \cos^2 \theta_3 + \frac{N}{2} \sin^2 \theta_3 \right)^2 \left( \frac{1-L_p}{2} \right)^2$,  \hspace{1cm} (2)

$$C_i(\theta_3) = P_i \cos^4 \theta_3 + \left( 4Q_i - R_i - P_i \right) \cos^2 \theta_3 \sin^2 \theta_3 + R_i \sin^4 \theta_3$$  \hspace{1cm} (3)

where $\eta_H(=\sigma_m/\sigma_H)$, $\bar{\sigma}_H$, $\theta_3$, $C_0$, and $L_p$ denote the anisotropic stress triaxiality, the Hill’s 48 effective stress, the maximum principal stress direction, the sensitivity of the cut-off value for the stress triaxiality and the Lode parameter, respectively. $F$, $G$, $H$, and $N$ are the anisotropic characteristic parameters of the Hill’s 48 yield criterion and $P_i$, $Q_i$, and $R_i$ are the parameters of the fracture criterion. The parameters of the fracture criterion for advanced metals are, in general, calibrated by the fracture strains from certain loading conditions such as the pure shear, the uniaxial tension, and the plane strain tension: those loading states play a significant role in determining the overall shape of the three-dimensional fracture surface. In the case that the non-directionality of the equi-biaxial fracture strain is forced to be held in the calibration of the model parameters, there may arise potential loss of the model performance in predicting the fracture strains for other loading states. In an attempt to enhance the fracture predictability of the anisotropic ductile fracture criterion proposed by Park et al. [6] particularly for the case that non-directionality of the equi-biaxial fracture strain holds, a weight function can be employed into equation (1) as below [8]:

$$(\sqrt{T})^{-C_i(\theta_3)} \left( \frac{1}{1+C_o} \left( \eta_H + \frac{3-L_p}{6\sqrt{T}} + C_o \right) \right)^{C_i(\theta_3)} \left( \frac{\eta_H + \frac{3-L_p}{6\sqrt{T}} + C_o}{2} \right)^{-C_i(\theta_3)} \varepsilon_f^+ = \varepsilon_f^+$$  \hspace{1cm} (4)

where $\varepsilon_f^+$ indicates the equi-biaxial fracture strain. It is simply expected that the fracture predictability of the proposed fracture criterion will be significantly improved due to an increase in the number of model parameters, $C_4(\theta_S)$, because the form of the proposed fracture criterion has a clear linearity between a set of input variables, $(\eta_H, L_p)$, and its corresponding output values, $\varepsilon_f^+$, when it is reviewed in a logarithmic form of
$$- C_i(\theta_i)\ln(\sqrt{T}) + C_s(\theta_i)\ln\left(\frac{1}{1 + C_o}\left(\eta_n + \frac{3 - L_p}{6\sqrt{T}} + C_o\right)\right) - C_s(\theta_i)\ln\left(\eta_n^2 + \frac{5}{9}\right) + \ln \varepsilon_N = \ln \varepsilon_N^F$$  \hspace{1cm} (5)

3. Calibration of the model parameters

Calibration of the parameters of the proposed fracture criterion is carried out with the experimental results obtained by Park et al. \cite{7} as summarized in table 1. In order to evaluate the equi-biaxial fracture strain in aid of the Digital Image Correlation (DIC) method, hydraulic bulge tests were additionally conducted. The model parameters obtained are summarized in table 2. Figures 1 and 2 respectively show the fracture surface predicted by the proposed fracture criterion with the postulate of the plane stress condition and the fracture loci at the three different loading directions of RD, DD, and TD. From these graphical representations, it can be simply concluded that the fracture criterion proposed shows not only the characteristic of non-directionality of the equi-biaxial fracture strain but also the fracture predictability with great accuracy over a wide range of stress states according to the loading direction.

| Loading direction | Uniaxial tension | Pure shear | Plane strain tension |
|-------------------|------------------|------------|---------------------|
| 0°(RD)            | 0.509            | 0.934      | 0.279               |
| Test#1            | 0.545            | 0.962      | 0.313               |
| Test#2            | 0.527            | 0.941      | 0.294               |
| Test#3            | 0.640            | 0.783      | 0.255               |
| Test#1            | 0.691            | 0.822      | 0.269               |
| Test#2            | 0.672            | 0.806      | 0.262               |
| Test#3            | 0.565            | 0.847      | 0.215               |
| Test#1            | 0.603            | 0.887      | 0.246               |
| Test#2            | 0.592            | 0.877      | 0.234               |
| Test#3            | 0.660            | 0.869      | 0.221               |

| Table 1. Fracture strain along the three loading direction in the three loading cases (after Park et al. \cite{7}). |

| \( i \) | \( P_i \) | \( Q_i \) | \( R_i \) | \( C_o \) |
|--------|--------|--------|--------|--------|
| 1      | 2.901  | 4.616  | 4.857  |        |
| 2      | 2.898  | 2.542  | 3.098  |        |
| 3      | 0.669  | 0.669  | 0.669  | 1/3    |
| 4      | 0.553  | 0.262  | 0.425  |        |

| Table 2. Parameters of the proposed fracture criterion. |

Figure 1. Fracture surface predicted by the proposed fracture criterion with the postulate of the plane stress condition.

Figure 2. Fracture loci at the three different loading directions of RD, DD, and TD.
4. Additional tensile tests

In order to validate the predictability of the proposed fracture criterion, additional tensile tests were carried out with various specimen geometries including the pure shear, the dog-bone, and the flat grooved specimens fabricated along 22.5° and 67.5° to the rolling direction using the same material and the test conditions used for the experiments by Park et al. [7]. The objective of this experimental program is to investigate the model performance in general loading states not involved in the calibration of the model parameters, which lies in a fact that more weight in the fracture prediction will be given to the experimental data involved in the calibration of the model parameters.

Equivalent plastic strain distribution on the specimen surface was measured in aid of the DIC method. The fracture strain was then obtained by measuring the maximum equivalent plastic strain just before the onset of fracture for each loading case which is induced by the specimen geometries. It is worth to note that those specimen geometries were designed to have the same dimensions for each type considered in the previous experiments so as to disregard the possible influence of specimen geometry changes on the experimental data for the reliable comparison with the previous ones. Tensile tests were conducted three times to confirm the reproducibility of the fracture strains evaluated. In the comparison of the experimental data with the ones predicted from the proposed fracture criterion, it is clearly confirmed that the proposed fracture criterion is capable of predicting the fracture initiation over a wide range of stress states with sufficient accuracy.

![Figure 3](image_url)

**Figure 3.** Comparison of the experimental data with the ones predicted from the proposed fracture criterion: (a) Pure shear; (b) Uniaxial tension; (c) Plane strain tension.

5. Validation of the model performance at a structural level

5.1. Square cup drawing test

A simple square cup drawing test was conducted so as to validate the model performance at a structural level. The test condition given in table 3 was designed to make the specimen being fractured during the test. The dimensions of a square cup drawing system are schematically explained in figure 4. From the square cup drawing tests, it was observed that a crack was propagated at the punch edge of the sheet with a presence of non-symmetric crack initiation as shown in figure 5, which may result in non-uniform distribution of blank-holding force during the drawing process. The punch stroke to fracture was evaluated as 21 mm.

| Specimen size [mm] | Punch speed [mm/s] | Blank-holding force [kN] |
|--------------------|--------------------|-------------------------|
| 299 x 299          | 10                 | 1,400                   |
5.2. Numerical simulation

Finite Element Analysis (FEA) was conducted with the commercial software of ABAQUS/Explicit V6.10. The proposed fracture criterion was incorporated into the FEA by programming the Vectorized User-defined MATerial (VUMAT) subroutine. In the numerical simulation, a linear integration rule for damage accumulation was adopted in consideration of the non-proportional loading that the material undergoes during the cup drawing process:

\[ D(\mathbf{z}^* r) = \frac{1}{C_1} \int_0^r \left( \sqrt{t} \right)^{-C_1(t_0)} \left( \frac{1}{1+C_0} \left[ \eta + \frac{3-L_p}{6\sqrt{t}} \right]+C_0 \right)^{C_1(t_0)} \left( \eta + \frac{5}{9} \right)^{-C_1(t_0)} \, d\mathbf{z}^* (6) \]

The crack initiation and propagation are simulated using the element deletion technique. Elements are deleted when the damage index predicted by the proposed fracture criterion reaches unity at each integration point. In order to describe the non-symmetric crack propagation, simulations were performed with two different FE models for the blank: a quarter model; and a half model. The schematic description of each square cup drawing simulation is depicted in figure 6. In the case of the half model, a bias of 5 mm was imposed to generate non-uniform blank-holding force during the cup drawing simulation. The geometries of both FE models were discretized using three-dimensional brick elements with reduced integration (C3D8R in ABAQUS element library). Each FE model was made with various mesh sizes to confirm the dependence of mesh size on the fracture initiation. Six element layers were made through thickness direction for all cases to compensate the computational efficiency. The friction coefficient of 0.1 was assigned in the FE simulations and the non-associated flow rule was adopted to incorporate with the effect of a different directionality of the \( r \)-value and the yield stresses. In this approach, both the plastic potential and the yield function are simply assumed to have the same form of the Hill’s 48 criterion. A projection method proposed by Stoughton and Yoon [9] was taken into consideration to compensate possible numerical violation of the stress–strain relationship defined by the constitutive model during the space–time integration approximation. The material behavior was predicted by the hardening curves evaluated from the combination of the tensile test and the hydraulic bulge test as shown in figure 7. The test conditions for the hydraulic bulge test are explained in table 4.

| Specimen size [mm] | Punch speed [mm/s] | Blank-holding force [kN] |
|--------------------|-------------------|--------------------------|
| 200 x 200          | 10                | 800                      |

Simulation results of both cases demonstrate that the punch stroke to fracture decreases as the mesh size decreases. This tendency is quite reasonable because the decrease in the mesh size of the FE model implies the increase in the number of numerical integration points. In other words, numerical analysis using the FE model with the coarse mesh demonstrates the deformation behavior in an
average sense. Figure 8 shows distributions of the localized equivalent plastic strain through the thickness with the variation of the mesh size along the punch radius. The non-symmetric crack propagation was predicted particularly for the simulation group using a half FE model as represented in figure 9(b). This result mainly comes from the non-uniform blank-holding force induced by setting the punch location with a bias of 5 mm along the transverse direction of the blank. Meanwhile, both simulation groups appropriately predicted the fracture location in the blank considering the material orientation. This result can reveal that the proposed fracture criterion has a good performance in predicting the fracture initiation for a certain stress state which the material mainly undergoes according to the material orientation.

![Figure 6. Two different FE groups: (a) a quarter model; (b) a half model.](image)

![Figure 7. Flow stress curves of the DP980 1.2t steel sheet.](image)

![Figure 8. Localized equivalent plastic strain distribution before the fracture initiation with different mesh sizes of: (a) 0.5 mm; (b) 3 mm.](image)
In order to evaluate the dependence of the mesh size on the fracture initiation, the relationship between the punch stroke to fracture and the mesh size normalized by the punch radius was investigated because the fracture takes place at the location near the edge of punch radius during the drawing process. The tendency of the punch stroke to fracture was demonstrated for each FE group using an exponential function given in equation (7) as shown in figure 10.

\[ P_f = P_0 + P_1 \exp \left( \frac{x}{P_2} \right) \]  

(7)

where \( x \) denotes the normalized mesh size and \( P_0, P_1, \) and \( P_2 \) denote the model parameters.

![Diagram](image)

**Figure 9.** Crack predicted in numerical simulation: (a) A quarter FE model; (b) A half FE model.

![Bar chart](image)

**Figure 10.** Punch stroke to fracture for each FE group: (a) A quarter FE model; (b) A half FE model.
6. Conclusion
A new uncoupled ductile fracture criterion is developed to provide a phenomenological model in consideration of non-directionality of the equi-biaxial fracture strain as well as the effect of anisotropy at the macroscopic level particularly for sheet metal forming application. Experimental validations were performed in the viewpoint of a structural and a specimen level through the square cup drawing and the tensile test at additional loading directions. In comparison of the experimental results with the ones predicted from the proposed fracture criterion, it was clearly concluded that the proposed fracture criterion has a sufficient capability to predict the fracture initiation over a wide range of stress states in consideration of the material anisotropy as well as non-directionality of the equi-biaxial fracture strain.

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