Prediction of forming limit curve for AA6061-T6 at room and elevated temperatures

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Abstract. Forming Limit Curve (FLC) has been widely adopted as a practical criterion for evaluating the formability of sheet metals. Predicting a reliable FLC by a virtual methodology could lead to robust process optimization before expensive tool manufacturing. In order to increase the predictive capabilities of the virtual forming tools, an accurate modeling of the forming limit curve should be considered at room and elevated temperatures. In this work, the isothermal forming limit curves of 6000 series aluminum alloy sheet metal are predicted by performing numerical simulations of Nakajima test. A stress triaxiality and Lode angle based ductile fracture criterion is used to determine the forming limit curve. Also, the ductile fracture criterion is extended to add the impact of temperature on ductile fracture prediction. The hybrid experimental-numerical approach is used to calibrate the ductile fracture criterion. The forming limit curve of AA6061-T6 sheet metal, with a thickness of 1 mm, is predicted using the calibrated ductile fracture criterion at room and elevated temperatures. Numerical simulations are performed in 3D with the finite element code Abaqus. The limit strains are determined for specimens undergoing deformation under different strain paths. Influence of temperature on the predicted forming limit curve is discussed.

1. Introduction
Forming Limit Curves (FLC) are widely used to evaluate the formability of sheet metals. FLC displays in principal strain space in the sheet plane the combinations of strains either at the onset of local necking (FLC at necking) or at the onset of fracture (FLC at fracture). FLC at fracture are very useful to evaluate the forming limits of metals when ductile fracture is induced without onset of localized necking (e.g. material with minor necking, bending [1], incremental sheet metal forming processes [2]). To predict numerically FLC at fracture, accurately calibrated ductile fracture (DF) criterion is strongly recommended.

Jain et al. [3] have predicted FLC at fracture of AA6111-T4 aluminum alloy by means of various ductile fracture criteria. It is found that the maximum shear stress criterion by Tresca predicts reasonably well the fracture limits over a large range of strain ratios. Takuda et al. [4] have successfully predicted the FLC at fracture of aluminum alloy sheets with Oyane’s ductile fracture criterion [5]. Han and Kim [6] proposed a new ductile fracture criterion to better predict the experimental FLC at fracture. Lou et al. [7] have applied a new stress triaxiality and Lode angle based ductile fracture criterion to construct the FLC at fracture. Comparison between the experimental and predicted FLCs demonstrates the accuracy of this new ductile fracture criterion. Yang et al. [8] have effectively obtained FLC at fracture of AA7075-T6 aluminum
alloy with Lou’s ductile fracture criterion. However, the above-mentioned studies refer to FLC at fracture obtained only at room temperature (RT) whereas FLC at fracture for elevated temperature is scarcely reported. Recently, Heidari et al. [9] have obtained experimentally and numerically FLCs at fracture for the AA6063 alloy at RT and 200°C, by using several ductile fracture criteria without considering the dependency of the ductile fracture criterion to the temperature. It is found that increasing the temperature moved the FLC upward, about 15%. In this study, a stress triaxiality and Lode angle based ductile fracture criterion is used to investigate the effect of temperature on the prediction of FLC at fracture for aluminum alloy. The ductile fracture criterion is extended to include the effect of temperature on ductile fracture prediction. Numerical simulations of Nakajima test with various specimen geometries are conducted until fracture to obtain the FLC at fracture of 1 mm thickness AA6061-T6 alloy at RT and 200°C.

2. Material and methods

2.1. Material

AA6061-T6 sheet aluminum alloy with 1 mm thickness is used in this work. The material parameters which have been obtained from uniaxial tensile test are given in Tab.1.

| | YS (MPa) | UTS (MPa) | $r_0$ | $r_{45}$ | $r_{90}$ | $\bar{r}$ | $\Delta \bar{r}$ |
|---|---|---|---|---|---|---|---|
| RT | 270 | 318 | 0.59 | 0.78 | 0.81 | 0.74 | -0.08 |
| 200°C | 206 | 211 | 0.64 | 0.71 | 0.72 | 0.7 | -0.03 |

\[
\bar{r} = \frac{r_0 + r_{90} + 2r_{45}}{4} \quad \Delta \bar{r} = \frac{r_0 + r_{90} - 2r_{45}}{2}
\]

For the finite element (FE) simulation, isotropic hardening with Hill48 yield criterion is chosen to model the mechanical behavior of this material. The yield function is given by:

\[
f = \bar{\sigma} - \sigma_y(\bar{\varepsilon}_p) \tag{1}
\]

where $\bar{\sigma}$ is the Hill48 equivalent stress that is defined as follow, from the components of the Cauchy stress tensor:

\[
\bar{\sigma} = \sqrt{F(\sigma_{yy} - \sigma_{zz})^2 + G(\sigma_{zz} - \sigma_{xx})^2 + H(\sigma_{xx} - \sigma_{yy})^2 + 2L\sigma_{yz}^2 + 2M\sigma_{xz}^2 + 2N\sigma_{xy}^2} \tag{2}
\]

$F$, $G$, $H$, $L$, $M$ and $N$ are the Hill’s coefficients. Due to the low value of the planar anisotropy coefficient $\Delta \bar{r}$, transverse isotropy is assumed. Therefore, the values of Hill’s coefficients are calculated considering only $\bar{r}$ (i.e. assuming $r_0 = r_{45} = r_{90} = \bar{r}$) according to these equations:

\[
F = G = \frac{1}{1 + \bar{r}} \quad H = 1 - G \quad N = \frac{1 + 2\bar{r}}{1 + \bar{r}} \tag{3}
\]

Due to the lack of available data regarding the mechanical behavior in the thickness of the sheet, $L$ and $M$ are kept equal to their isotropic value (i.e. $L = M = 1.5$).

$\sigma_y(\bar{\varepsilon}_p)$ is the hardening function that is chosen with a saturation form of Hockett-Sherby modified to include the strain rate sensitivity at elevated temperature [10].

\[
\sigma_y(\bar{\varepsilon}_p, \dot{\varepsilon}) = [\sigma_0 + Q(1 - \exp(-b(\bar{\varepsilon}_p)^n))] \left[ \frac{\dot{\varepsilon}}{\dot{\varepsilon}_0} \right]^m \tag{4}
\]

where $\bar{\varepsilon}_p$ is the equivalent plastic strain, $\sigma_0$ is the initial yield stress, $Q$ represents the maximum change in the size of the yield surface, $b$ defines the growth rate of the yield surface, and $n$ is
the strain hardening coefficient, $\dot{\varepsilon}$ is the strain rate, $\dot{\varepsilon}_0$ is a constant strain rate normalization factor and $m$ is the strain rate sensitivity coefficient.

Lou’s ductile fracture criterion [7, 11] is used to predict the onset of fracture. This original criterion is expressed as follows:

$$D = \frac{1}{C_3} \int_0^{\bar{\varepsilon}_f} \left( \frac{2}{\sqrt{L^2 + 3}} \right)^{C_1} \left( \frac{(1 + 3\eta)}{2} \right)^{C_2} d\bar{\varepsilon}_p$$  \hspace{1cm} (5)

where $D$ is a failure indicator that gives the onset of fracture when it reaches unity. $\bar{\varepsilon}_f$ is the equivalent plastic strain at fracture. $C_1$, $C_2$ and $C_3$ are material parameters that need to be calibrated. $\eta$ and $L$ are the stress triaxiality and the Lode parameter, respectively.

An extended form of Lou’s ductile fracture criterion which includes the impact of temperature on ductile fracture criterion is also used. The extended form recently proposed by Kacem et al.[12] is expressed as:

$$D(T) = \frac{1}{C_3(1 + C_4 T^*)} \int_0^{\bar{\varepsilon}_f} \left( \frac{2}{\sqrt{L^2 + 3}} \right)^{C_1} \left( \frac{(1 + 3\eta)}{2} \right)^{C_2} d\bar{\varepsilon}_p$$  \hspace{1cm} (6)

where $C_4$ is an additional material parameter to include the influence of temperature $T$. $T^*$ is the homologous temperature, defined by:

$$T^* = \frac{T - T_{ref}}{T_m - T_{ref}}$$  \hspace{1cm} (7)

$T_{ref}$ and $T_m$ are respectively the reference and melting temperature ($T_{ref} = 293 \text{ K}$ and $T_m = 873 \text{ K}$).

2.2. Nakajima test
Nakajima forming method is used to obtain the FLC. Fig.1 shows the forming tools and the specimen geometries used to obtain three different strain paths. A hemispherical punch of $\phi 33 \text{ mm}$ diameter is used to deform the specimen with an outer diameter of $\phi 60 \text{ mm}$ into the die cavity with $\phi 35.25 \text{ mm}$ diameter corresponding to a ratio of 1/3 to the standard dimensions [13]. A blank-holding force of 100 kN is set to clamp firmly the sheet between the die and blank-holder. The punch speed is set at 1.5 $\text{m.s}^{-1}$ according to the standard ISO 12004-2 [13].

2.3. Numerical model
Isothermal numerical simulations of Nakajima test are conducted in 3D using Abaqus/Standard. The tools are modeled as rigid surfaces whereas the workpiece is modeled as a deformable body as shown in Fig.2. One quarter of the workpiece is meshed with 3D solid hexahedral elements with reduced integration (C3D8R). A fine mesh size of approximately 0.2 mm is used in the local zone where fracture is likely to occur and the size is increased to approximately 0.6 mm at sections away from this zone. Moreover, 4 elements are used through the thickness. The workpiece is assumed to have elastic-plastic behavior with the material properties specified in Section 2.1 and given below. Since, experimentally, an optimal lubricant system is strongly recommended to meet the requirement of a fracture on the top of the dome [13], the friction between the punch and the workpiece is neglected for the sake of simplicity. For the other tool-workpiece interfaces, Coulomb’s friction law is used with a friction coefficient of 0.2. In a first step, the force is applied to the blank-holder. Then, the punch is constrained to move in the axial direction. The ductile fracture criterion is implemented in the FE code through UVARM subroutine which calculates the damage indicator $D$ (Eq.6) at each increment. The major and minor values in the sheet plane of the first element reaching the critical value of $D = 1$ (critical
element) are recorded to plot the FLC at fracture. For a first approach in this study, isothermal conditions are considered and the blank temperature is supposed uniform. FLC at fracture are predicted at RT and 200°C.

![Forming tool](image1)

**Figure 1.** Geometry and dimensions (in mm) of forming tool and specimens for Nakajima test.

![FE model of Nakajima test and meshing for specimens W15 and W60.](image2)

**Figure 2.** FE model of Nakajima test and meshing for specimens W15 and W60.

### 3. Results

#### 3.1. Calibration of material parameters

In a recent study conducted by the same authors [12], experiments and simulations of tensile tests up to fracture on dog-bone specimens, notched specimens with different radius (NR5 and NR15), specimens with a central hole (HR4) and shear specimens (SH) are used to calibrate the hardening and fracture parameters of AA6061-T6 at RT and 200°C. Hardening law parameters have been identified manually through several trial and error iterations until the numerical and experimental load-displacement curves obtained with all fracture specimens show good agreement. Hybrid experimental-numerical approach have been used to determine the fracture strain and the corresponding stress state parameters for each specimen geometry as described by Kacem et al. [14]. The fracture parameters are then optimized by minimizing a cost function. Though the process is considered isothermal, the two DF criteria are used out of comparison’s sake. Tabs.2 and 3 show the identified hardening law parameters and calibrated fracture parameters, respectively. Figs.3 and 4 show the hardening curves predicted by the modified Hockett-Sherby model and the fracture locus for AA6061-T6, respectively.

|   | $\sigma_0$ (MPa) | Q (MPa) | b | n | $\dot{\varepsilon}_0$ ($s^{-1}$) | m |
|---|---|---|---|---|---|---|
| RT | 244 | 135 | 7 | 0.68 | – | – |
| 200°C | 130 | 76 | 7 | 0.36 | $10^{-4}$ | 0.04 |

**Table 2.** Hardening law parameters identified at RT and 200°C.
Table 3. DF criterion parameters calibrated at RT and 200°C.

|                  | Lou's original DF criterion | Lou's extended DF criterion |
|------------------|----------------------------|----------------------------|
|                  | \(C_1\) | \(C_2\) | \(C_3\) | \(C_1\) | \(C_2\) | \(C_3\) | \(C_4\) |
| RT               | 0.640  | 0.250  | 0.612   | 0       | 0.201  | 0.573  | 1.321   |
| 200 °C           | 0      | 0.166  | 0.801   | 0       | 0.201  | 0.573  | 1.321   |

Figure 3. Hardening curves of AA6061-T6 predicted at RT and 200°C. Symbols represent experimental points obtained from uniaxial tension of dog-bone specimen at \(\dot{\varepsilon} = 10^{-3}\) s\(^{-1}\).

Figure 4. Fracture locus of AA6061-T6 constructed in the space of stress triaxiality and equivalent plastic strain under plan stress condition at RT and 200°C.

3.2. Ductile fracture prediction

The parameter \(D\) is used to indicate the onset of ductile fracture when it reaches \(D = 1\) and as an indicator to assess the damage state in the specimen. Fig.5 shows the distribution of the parameter \(D\) in the deformed specimen at fracture initiation (i.e. when a first element reaches the critical value \(D = 1\)) for both ductile fracture criteria at RT and 200°C.

As expected, the critical zone where fracture initiates is found on the top of the dome for all specimens. The critical zone is more localized for W15 and W30 specimens. However, W60 specimen shows no signs of localized necking prior to fracture making the determination of necking point required to establish the FLC at necking very difficult for this kind of specimen. The original and extended fracture models show a similar distribution of the parameter \(D\). To compare more quantitatively between both fracture criteria, Tab.4 shows the equivalent plastic deformation at fracture \(\bar{\varepsilon}_f\) predicted by both models at RT and 200°C.

It can be seen that the values of \(\bar{\varepsilon}_f\) predicted by the original DF criterion are very close to the ones predicted by the extended form, especially at elevated temperature where the maximum difference is found about 0.5 %. At room temperature, the maximum difference between both models is about 3.7 %. Therefore, it is better to use the extended form instead of the original form to predict the forming limits since the extended form is more useful to predict ductile fracture initiation under isothermal and non-isothermal conditions whereas the original can be only used under isothermal conditions.
Figure 5. Damage distribution at the onset of fracture at RT (left hand side) and 200°C (right hand side)

Table 4. Equivalent plastic strain at fracture $\bar{\varepsilon}_f$ predicted at the critical element when $D=1$ by the original and extended DF criteria at RT and 200°C.

| Specimen | Condition | Lou's original DF criterion | Lou’s extended DF criterion |
|----------|-----------|-----------------------------|-----------------------------|
| W15      | RT        | 0.542                       | 0.550                       |
|          | 200°C     | 0.775                       | 0.777                       |
| W30      | RT        | 0.521                       | 0.539                       |
|          | 200°C     | 0.765                       | 0.764                       |
| W60      | RT        | 0.550                       | 0.530                       |
|          | 200°C     | 0.750                       | 0.746                       |

3.3. Forming limit curves at fracture
The histories of strains (i.e. major and minor strains) are extracted in the critical element to obtain the strain path and the fracture point as shown in Fig.6. Only results obtained from the extended DF criterion are considered since both criteria give similar results.

It can be seen that the FLC at fracture tends to have an approximately linear shape as was also found experimentally at RT in the work of Takuda et al. [4] for AA1100-O and AA5182-O aluminum alloy sheets with a thickness of 1 mm. As can be observed, the temperature has a high effect on the major strain limit with an increase from RT to 200°C of about 40% that is higher than that found by Heidari et al. [9] for AA6063.

The same linear strain path is obtained for W60 specimen for RT and 200°C corresponding to equi-biaxial tensile state. However, for W15 and W30 specimens due to the punch geometry, the strain path are non-linear as was widely reported in previous works, e.g.[15, 16]. The deformation starts with bending, leading to an initial balanced biaxial strain path, which is the major cause of the non-linear strain path in Nakajima test. It can be seen that the non-linear strain path effect is more pronounced at 200°C.
4. Conclusion
To investigate the effect of temperature on the forming limits of aluminum alloy, FLC at fracture of AA6061-T6 are determined numerically at RT and 200°C using Nakajima test. To predict the onset of fracture, Lou’s DF criterion is used in numerical simulation. An extended form of the DF criterion which include the influence of temperature on ductile fracture prediction is also used. Comparison between the original and extended forms shows almost similar results making the use of the extended form more recommended especially in non-isothermal conditions. The same approximately linear shape of FLC at fracture is obtained at RT and 200°C. It is found that the temperature has a strong influence on the forming limits. The limit strain may increase by 40% with the raise of temperature from RT to 200°C. It is also found that the non-linear strain path effect inherent to Nakajima test increases with the raise of temperature.

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