Numerical and experimental investigation of the formability of AA6013-T6

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Abstract. AA6013 is an age-hardenable, aluminum alloy with the potential for structural applications in weight-sensitive automotive components. The alloy is at peak strength in the T6 temper, but exhibits relatively low room temperature formability. In an effort to avoid costly post-forming heat-treatments, warm forming of the AA6013 in the T6 temper is investigated in the current work. The experimental formability of the alloy is characterized from room temperature to 250°C through limiting dome height (LDH) tests with a Nakazima hemispherical punch and in-situ stereoscopic digital image correlation (DIC) strain measurement. A forming limit diagram (FLD) is generated from the application of a curvature-based necking criterion to the elevated temperature LDH test results. The evolution of the strain distribution prior to necking is explored using models of the plane strain LDH tests at room temperature and 200°C. At 200°C, strain rate effects are explored relative to the local temporal strain evolution. Modelling efforts include development of a Barlat Yld2000 yield surface to capture the anisotropy of the alloy and use of experimentally obtained isothermal flow-stress curves. The effects of different tensile gage lengths on flow-stress and yield surface calibration for warm tensile testing are discussed. The room temperature model was found to accurately depict the thinning strain distribution consistent with the “safe” thinning strain distribution at room temperature. At 200°C, it is shown that the temporal strain evolution is strongly influenced by strain-rate effects.

1. Introduction

The application of aluminum in vehicle design and fabrication has seen a steady rise over the past two decades in an effort to meet increasingly stringent emissions and fuel economy mandates. To continue the trend of light-weighting, heat-treatable high-strength 6xxx and 7xxx aluminum alloys are under consideration for the fabrication of structural components that have historically been reserved for high-strength low-alloy steels. Heat-treatable aluminum alloys are at peak strength in the T6 temper, but have relatively low room temperature (RT) formability. To improve the formability of heat-treatable alloys without altering the final temper, the warm forming processing route is considered [1].

Before developing a warm forming processing route, the warm formability response of the intended alloy, often characterized in terms of a forming limit curve, must be established under processing parameters comparable to the intended application. So-called time-dependent methods have recently been proposed for use in elevated temperature forming applications [2], [3], [4], [5] and are a focus of the current work.
While the FLD is a useful tool in conjunction with numerical techniques, accurate prediction of the formability of a sheet component is strongly dependent on the ability of the material model to capture the temporal strain evolution within the part [6]. For sheet aluminum alloys subjected to warm forming temperatures, the effects of anisotropy, temperature, and strain rate should all be considered.

In this work, the warm formability of sheet aluminum alloy AA6013-T6 is experimentally characterized through LDH testing at isothermal temperatures of room temperature, 150°C, 200°C, and 250°C. Three dome geometries are used during testing in an effort to produce uniaxial (UX), plane strain (PS), and biaxial (BX) strain paths. A sheet curvature approach is used for detecting the onset of localization in the LDH specimens. The warm formability of the alloy is numerically investigated for the plane strain geometry and compared with experimental observations at room temperature and 200°C. A temperature independent Barlat Yld2000 yield surface is used to capture material anisotropy, while experimentally determined isothermal flow-stress curves are used to model the flow behavior of the alloy. In the 200°C plane strain dome model, the impact of strain rate effects on the local temporal strain evolution is investigated and compared with the experimental results from a representative sample.

2. Experimental Techniques and Results

The 6013-T6 sheet material studied in this work was supplied by Arconic. The nominal thickness of the sheet is 2.0 mm and the nominal composition is included in Table 1. All testing was facilitated with the use of stereoscopic 3d digital image correlation (DIC) techniques for strain measurement.

It should be noted that all reported values that are not indicated to be single measurements are median values. Furthermore, all error measurements and error bars were calculated using the median absolute deviation (MAD), \[ MAD = \text{median}(|X_i - \text{median}(X)|) \]. The MAD is a more resilient metric to outliers and a preferred measure of the real deviation within a sample group [7].

| Alloy   | Mg    | Si     | Cu     | Mn   | Fe   | Others | Al   |
|---------|-------|--------|--------|------|------|--------|------|
| 6013-T6 | 0.8-1.2 | 0.6-1.0 | 0.6-1.1 | 0.2-0.8 | 0.5 | 0.15 | Bal. |

2.1. Digital Image Correlation Parameters

Two cameras were utilized for stereoscopic image acquisition during tensile and LDH testing. The cameras were operated in the range of 5 to 15 frames per second (fps) during LDH testing and up to 100 fps during tensile testing. A total of 300 or more images were typically acquired for any given experimental test. The Correlated Solutions software program Vic-Snap was used for image acquisition, while Vic-3D 7 was used for image processing. A white base and black random speckle pattern was used for all DIC work. Temperature resistant white paint was used when required.

Relevant image processing parameters in the Vic-3D 7 software package include the subset size, strain filter size, and step size. The product of the step size and strain filter size may be thought of as a type of spatial averaging parameter [9], [10], while the subset size is primarily used for tracking pixel changes between images. In this work, the step and strain filter sizes were maintained at 3 pixels and 5 pixels, respectively, while the subset size was generally maintained between 27 and 37 pixels. The average pixel density in the DIC images was approximately 11 pixels per mm.

2.2. Tensile Testing

2.2.1. Testing Parameters

Warm tensile testing of the 6013-T6 sheet was completed in the rolling (RD), diagonal (DD), and transverse (TD) directions at isothermal temperatures of room temperature, 150°C, 200°C, and 250°C for a nominal strain rate of 0.01/s. At 200°C, warm tensile testing was also completed at nominal strain rates of 0.1/s and 0.001/s. The tensile geometry used was consistent with the ASTM E8
standard size geometry [11], with a gage length of 50 mm and a gage width of 12.5 mm. At room temperature, the specimens were clamped and immediately pulled to fracture. The strain evolution was captured over the duration of testing with DIC techniques. The tensile testing apparatus used for all tensile testing was an MTS Exceed 45, rated for up to 100 kN.

Testing at elevated temperatures was completed using a 651 Series Environmental Chamber [12] attachment for the MTS tensile frame. The chamber was retrofitted with optical grade glass panels to allow for high quality image acquisition during tensile testing for DIC analysis.

2.2.2. Tensile Results

At elevated tensile temperatures, the ultimate tensile stress (UTS) values were achieved at plastic strain levels as low as 0.01 over the 50 mm gage length, as determined from the Considere Criterion. With so little plastic strain to the UTS, extrapolation of the tensile curve for modelling purposes was difficult. To overcome this limitation, the instantaneous area true stress-strain (SS) curve was computed by calculating the thickness and width reductions at the localization area from the DIC data, as shown in Figure 1. The resulting true stress-strain curves are plotted in Figure 2 which also considers different specimen gage lengths.

From Figure 2, it can be seen that as the gage length used for stress-strain calculations decreases, the true stress-strain curve approaches the area reduction stress-strain curve. The area reduction SS curve qualitatively appears to follow the hardening behaviour of the smaller gage length tensile curves and shows positive hardening over the practical range of true strains. Thus, the area true SS curve appears appropriate for use in developing extended flow-stress curves for modelling purposes, and will be used throughout the remainder of this work.

Stress ratios between RD, DD, and TD were found to be relatively insensitive to temperature, with average stress ratios of 1.0, 0.992, and 0.996 for RD/RD, DD/RD and TD/RD, respectively. Likewise, Lankford coefficients (R-values) were not found to be significantly dependent on temperature, with most median values being within the scatter of the measurements. Measured R-values as a function of temperature are summarized in Figure 3. Four or more tests per direction per temperature were completed to estimate R-values.

Extrapolated flow-stress curves were generated using Hockett-Sherby fitting of the area true SS curves. The extended flow-stress curves as a function of temperature are included in Figure 4. Observe that the 200°C flow stress curve at a nominal strain rate of 0.1 saturates at almost the same stress as the 150°C curve at a strain rate of 0.01. This behaviour begins well below 0.1 plastic strain, where the Hocket-Sherby fit matches with the experimental data.

2.2.3. Yield Surface Calibration

A Barlat Yield 2000 yield surface was calibrated to capture the anisotropy of the 6013-T6 sheet alloy. A yield exponent of 8 was chosen for the Yld2000 yield surface which corresponds to the FCC crystal structure of the alloy. The yield surface was calibrated using median R-values and stress ratios at each direction over the range of temperatures discussed in the previous section. The corresponding yield surface is temperature independent over the range of temperatures examined in this work. These
values are summarized in Table 2. A planar R-value was computed from the median RD, DD, and TD values R-values, while a biaxial stress ratio of 1.0 was assumed, as summarized in Table 2. The coefficients for the resulting fit are included in Table 3, while the normalized yield surface is shown in Figure 5 with the overlay of a Von Mises yield surface for comparison. Note the sharper nose on the Yld2000 surface relative to the Von Mises formulation.

Table 2: AA6013-T6 Median R-Values and Stress Ratios

| R-Values  | Stress Ratios |
|-----------|---------------|
| RD        | DD  | TD  | Biaxial | RD  | DD  | TD  | Biaxial |
| 0.654     | 0.729| 0.623| 0.684   | 1.000| 0.992| 0.996| 1.000   |

Table 3: AA6013-T6 Barlat Yld2000 yield surface parameters for a yield surface exponent of 8

| Barlat Yield Coefficients for m = 8 |
|-------------------------------------|
| a0       | a1   | a2   | a3   | a4   | a5   | a6   | a7   |
| 0.978    | 0.950| 0.895| 1.004| 1.028| 1.045| 0.978| 1.060|

2.3. Formability Testing

2.3.1. Experimental Testing
The warm formability of AA6013-T6 was evaluated along the rolling direction (RD) at RT, 150°C, 200°C, and 250°C through LDH testing under isothermal conditions with a 100 mm hemispherical
Nakazima punch operated at 0.25 mm/s. Three geometries were used to simulate conditions of uniaxial, plane strain, and biaxial loading. A minimum of four specimens per geometry and temperature condition were tested for this work. The readers are referred to references [3] and [4] for further details on the testing procedures, such as the lubrication scheme, clamping loads, and heating methods.

2.3.2. Forming Limit Analysis Procedures
Forming limit strains were determined from each LDH test using the extracted DIC parameters and a localization detection scheme which evaluates local changes in the curvature of the specimens. In the curvature approach, DIC data including principal strains and displacements is extracted from LDH DIC datasets perpendicular to the maximum failure location, similar to the ISO12004-2:2008 [13] standard, as shown in Figure 6. The local curvature is then computed from the dome height profile for every image at the peak strain location. When the sheet curvature switches from negative to positive, the material is assumed to have localized. Once the onset of localization is detected, the maximum principal surface strains on the prior image are taken as the “safe” limit strains. A simple example of this approach is shown in Figure 7. When the solid line switches from negative to positive, a visible neck can be detected from the DIC data. Here, a negative curvature corresponds to a concave surface (no localization) and a positive curvature corresponds to a convex surface (observable localization). This may be considered analogous to the “finger test” originally implemented by Keeler and Backhofen [14]. Although not discussed in this paper, the rate-based linear best fit approach by Volk and Hora [15] and the ISO12004-2:2008 approaches, amongst others, are under consideration in a more general study of formability tests to understand differences in formability predictions using these various approaches.

In this work, a zero curvature criterion is used in conjunction with the curvature approach to determine the “safe” image, i.e. the last test image before the onset of detectable necking. The authors concede that a zero curvature trigger may, in reality, already indicate the presence of some necking; however, this criterion can serve as a maximum upper bound in Nakazima testing, given that if a positive curvature if observed, a physical neck must be present.

To determine the curvature from the discrete DIC dome-height points, a b-form smoothing spline is used [3]. For more details on this approach, the curvature method is discussed in varying levels of detail in [3], [5], and [2].

2.3.3. 6013-T6 Temperature Dependent FLD
The FLD of AA6013-T6 for all temperatures is included in Figure 8, while Figure 9 compares the major principal safe strains in the plane strain condition for all tested temperatures. In Figure 9, it can be seen that the major plane strain limit strains increase by approximately 25% from RT to 200°C before decreasing or plateauing at 250°C. The UX and BX geometries show similar trends, although the median absolute deviation on the BX safe strains indicates a large degree of scatter in the resulting limit strains.

3. Numerical Model Development and Validation
A quarter-symmetry numerical model (Figure 10) was developed and simulated in LS-DYNA using the tooling geometry used during experimental LDH testing. The model was used to simulate the experimental plane strain dome tests at room temperature and 200°C. A fully integrated shell element formulation was used to model the blank with an average element size of 0.2 mm near the punch apex.

The Barlat Yld2000 material model was used to model the AA6013-T6 sheet material with the flow-stress curves shown in Figure 4. Strain-rate dependent flow curves were implemented using a tabular approach. When discussing the 200°C models, NR will signify the model without strain rate effects, while R signifies the model with strain rate effects. The friction coefficient was taken as 0.04 [16]. An implicit formulation was used in consideration of the slow punch speed.
A comparison of experimental and numerical plane strain load-displacement curves is shown in Figure 11. At room temperature, the experimental failure load from a representative test was 37.75 kN, while the model predicted 39.44 kN for the sample level of displacement, corresponding to an approximate absolute error of 4.5%. In the representative test, the dome height at the onset of localization occurred less than 0.4 mm prior to dome height failure, at which point the absolute error in loading predictions was approximately only 6.0%. At the experimental failure location of a representative dome test at 200°C, the failure load was 31.0 kN, while the model loads at this displacement were 32.26 (4.1%) kN and 31.36 kN (2.77%), respectively, for the NR and R models, respectively. Similar levels of error were seen at the dome height corresponding to the onset of localization (~1.5 mm prior). Generally, the models were found to accurately predict the experimental load-displacement behaviour of the plane strain dome samples.
Figure 10: Major strain distribution in RT PS dome during localization (scale: 0 – 0.244)

A plot of the thickness strain distribution (thinning taken as positive) at the onset of localization from the representative PS dome test at 200°C is shown in Figure 12. Thickness strain distributions from the surfaces of the NR and R models at are also included in Figure 12, taken at the dome height corresponding to the experimental “safe” dome height. In Figure 12, it can be seen that the R model closely matches the experimental strain distribution, while the NR model appears to already be significantly localized. This observation may indicate that strain-rate effects play a large role in delaying the onset of localization at elevated temperatures in the 6013-T6 alloy.

While the thickness strain distribution for a comparable dome-height is within 0.03 strain for the N model, it should be noted that selection of the correct dome height for comparison when at the cusp of localization can be difficult. Furthermore, while the strain distributions show similar shapes and values, in the experimental dome specimen, only a single neck was observed, whereas the use of this quarter symmetry models implies that a double neck might exist. This may indicate the presence of non-uniform friction or potential imperfections in the 6013 sheet which might trigger necking.

Figure 11: Experimental and model load-displacement curves

Figure 12: Strain-rate effects on thickness strain distribution at 200°C (thinning taken as positive)

4. Conclusions
The experimental formability of AA6013-T6 has been characterized from room temperature to 250°C in the rolling direction for a punch speed of 0.25 mm/s. A curvature-type approach was applied to determine the onset of localization in the sheet material. It was found that the sheet material has a positive response to warm forming from RT to 200°C, but becomes stagnant at elevated temperatures.

Elevated temperature testing was completed on the 6013-T6 sheet material and it was found that the Lankford coefficients and stress ratios are not sensitive to temperature over the range of temperatures tested. A temperature invariant Barlat Yld2000 yield surface was calibrated based on the experimental measurements and implemented within the developed numerical models. The elastic modulus was observed to vary with temperature; however, further testing is required to quantify this behaviour.
Through warm forming simulations of plane strain domes, it was found that the elevated temperature strain-rate sensitivity of the AA6013-T6 alloy plays a large role in delaying the onset of localization in the material and is necessary for predicting the temporal strain evolution. On the other hand, strain-rate effects had little impact on the load-displacement response of the models in this work.

Acknowledgments
The authors greatly appreciate financial support from the Honda R&D Americas, Promatek Research Centre (Cosma International), Arconic Technical Center, the Natural Sciences and Engineering Research Council of Canada, the Canada Research Chairs Secretariat, and the Ontario Research Fund.

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