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An Evaluation of the Johnson-Cook Model to Simulate Puncture of 7075 Aluminum Plates

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An Evaluation of the Johnson-Cook Model to Simulate Puncture of 7075 Aluminum Plates

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Abstract

The objective of this project was to evaluate the use of the Johnson-Cook strength and failure models in an adiabatic finite element model to simulate the puncture of 7075-T651 aluminum plates that were studied as part of an ASC L2 milestone by Corona et al (2012). The Johnson-Cook model parameters were determined from material test data. The results show a marked improvement, in particular in the calculated threshold velocity between no puncture and puncture, over those obtained in 2012. The threshold velocity calculated using a baseline model is just 4% higher than the mean value determined from experiment, in contrast to 60% in the 2012 predictions. Sensitivity studies showed that the threshold velocity predictions were improved by calibrating the relations between the equivalent plastic strain at failure and stress triaxiality, strain rate and temperature, as well as by the inclusion of adiabatic heating.

ACKNOWLEDGMENTS

We would like to acknowledge Mary Gonzales, who managed the funding for this project, as well as Eliot Fang and Mike Chiesa for suggesting that we continue the puncture work that was started in FY12. Much of the work on which the current report is based was conducted in FY12 as part of an ASC Level 2 milestone. As a result, this report contains ideas and material developed at that time, and we thank our colleagues Nicole Breivik and Jhana Gorman, who were part of the analysis team, Matt Spletzer and Theresa Cordova, who contributed the experimental components as well as Walter Witkowski and Kenneth Hu, who addressed the V&V components of the project. Thanks to Bonnie Antoun for providing recent data on the stress-strain curves of 7075-T651 aluminum alloy at different temperatures. She and Bo Song also provided high-strain-rate stress-strain curves for the same material. The current report also includes recent microscopy work conducted by Lisa Deibler. Her contributions are acknowledged with thanks. We would like to also acknowledge Benjamin Reedlunn for useful discussions we had with him on the modeling of ductile failure, Jake Koester for helping produce the equivalent plastic strain rate contour plots in Figure 33 and Bill Scherzinger for reviewing the report.

CONTENTS

1. INTRODUCTION	11
2. REVIEW OF EXPERIMENTAL RESULTS	14
2.1 Threshold Velocity Measurements	14
2.2 Microscopy Observations.....	18
3. MATERIAL CHARACTERIZATION	22
4. REVIEW OF FY12 PLATE PUNCTURE RESULTS.....	28
5. CALIBRATION OF THE JOHNSON-COOK MODEL	30
5.1 Basics of the Johnson-Cook Model.....	30
5.2 Calibration of the Johnson-Cook model parameters.....	31
5.2.1 Quasi-Static Uniaxial Tension Test at Room Temperature.....	31
5.2.2 Tension Tests on Notched Specimens	32
5.2.3 High Temperature Tests	38
5.3.4 High Strain Rate Tests.....	39
6 Puncture Predictions	42
6.1 Baseline Predictions	43
6.2 Evaluation of Dependence of Plastic Strain to Failure on Triaxiality.	44
6.3 Effect of Element Size.....	45
6.4 Description of Predicted Response and Failure	47
6.5 Evaluation of Thermal Effects.....	55
6.6 Evaluation of Strain Rate Effects.....	56
6.7 Effect of the Value of the Friction Coefficient	57
6.8 Effect of the Failure Evolution Stress Decay	57
6.9 Effect of Hourglass Stiffness	59
6.10 Comments on Sensitivity Analyses.....	59
7 Conclusions.....	59
References	61
Appendix A: Dimensions of tension test specimens	63
A.1 Uniaxial Tension Test Specimens for Quasi-Static Loading.....	63
A.2 Uniaxial Tension Test Specimens for Dynamic Loading	63
A.3 Notched Tension Test Specimens.....	64
Distribution	67

FIGURES

Figure 1: Schematic of the problem of normal impact of a circular plate by a cylindrical punch with a hemispherical nose.	11
Figure 2: Experimental set-up for plate impact	14
Figure 3: Experimental data for the plate impact problem indicating failure or no failure as a function of impact speed. The derived normal probability and cumulative probability distribution functions are also shown.	16
Figure 4: Sequence of high speed video frames showing instances of damage during plate impact for specimen 9 ($v_0 = 11$ ft/s). (a) A crack appears on the surface, (b) scabbing begins, (c) scabs break off plate surface and (d) plug is ejected.	18
Figure 5: Section of specimen 10 through the center of the impact dimple.	19
Figure 6: Section of specimen 13 through the center of the impact dimple.	19
Figure 7: Micrographs showing the localization and fractured regions where the plug was about to form. (a) Left side and (b) right side.	20
Figure 8: Contiguous micrographs showing details of localization region and crack tip on the left side. The element size for the fine and the very fine finite element meshes are shown for comparison.	21
Figure 9: Engineering stress-strain curves obtained from uniaxial tension tests. (a) along the rolling direction and (b) perpendicularly to the rolling direction.	23
Figure 10: Engineering stress-strain curves obtained from uniaxial tension tests of specimens machined with their axes at 45° to the rolling direction.	24
Figure 11: Comparison of the three most ductile stress strain curves for specimens machined with their axes along the rolling direction (wg-16), perpendicularly to the rolling direction (ag-19) and at 45° to the rolling direction.	24
Figure 12: Comparison of high-strain rate to quasi-static engineering stress-strain curves.	25
Figure 13: Load-deflection results for tension tests on notched specimens with four notch radii. Failure is indicated by “x.”	26
Figure 14: Quasi-static strain rate, engineering stress-strain curves for Al 7075-T651 at various temperatures and quasi-static strain rate obtained by Antoun (2013).	27
Figure 15: Cumulative Distribution functions of critical failure velocity and epistemic uncertainty bands at 95% confidence. Experimental data shown in black and numerical prediction data shown in red	29
Figure 16: Comparison between the engineering stress-strain curves for the least and most ductile wg specimens and the prediction of the uniaxial tests obtained with the fit for the Johnson-Cook model. The circles in the true stress-strain curve correspond to the failure strains measured in each of the two tests.	32
Figure 17: Finite element models of two notched tension specimen geometries $r/R = 3.2$ and 032.	33
Figure 18: Comparison of experimentally measured force-displacement responses to the results of numerical predictions.	34
Figure 19: Evolution of equivalent plastic strain vs. triaxiality in the tension tests on notched specimens. The hollow circles correspond to the failure point observed in the experiments. The filled circles represent the failure point in two uniaxial tension tests.	35

Figure 20: Variation of triaxiality and equivalent plastic strain with radial position at the notch at the point at which failure was observed in the experiments. (a) Triaxiality and (b) equivalent plastic strain.	36
Figure 21: Three estimates of the dependence of the equivalent plastic strain at failure used with the Johnson-Cook failure model.	37
Figure 22: Comparison of the load-deflection responses for the tension tests on notched specimens predicted using the low and high estimates for failure. The experimentally obtained curves are included for comparison.	38
Figure 23: Comparison of experimentally measured engineering stress-strain curves at several temperatures with the results of numerical predictions.	39
Figure 24: Comparisons of measured and predicted quasi-static and dynamic uniaxial stress-strain curves.	40
Figure 25: Comparison of predicted engineering stress-strain curves including failure for the tests in Figure 24 comparing quasi-static and high strain rate cases.	40
Figure 26: Model used in the finite element calculations of the plate puncture problem showing the boundary conditions used.	43
Figure 27: Effect of the equivalent plastic strain at failure vs. triaxiality curves on the shape of the plugs. (a) low fit, (b) medium fit, and (c) high fit. Contours of the Johnson-Cook damage parameter $\bar{D}$ are shown. The snapshot times are not equal for the three cases shown.	45
Figure 28: Mode of failure obtained for three different meshes showing the ejection of a plug from the impact region. (a) Coarse mesh, (b) fine mesh and (c) very fine mesh. Contours of Johnson-Cook damage parameter are shown. The snapshot times are not equal for the three cases shown.	46
Figure 29: Contours of the Johnson-Cook damage parameter $\bar{D}$ at various times up to the formation of the plug. (a) 1 ms, (b) 2 ms, (c) 3 ms, (d) 3.15 ms, (e) 3.2 ms and (f) contour scale.	49
Figure 30: Contours of equivalent plastic strain ϵ_e^p at various times up to the formation of the plug. (a) 1 ms, (b) 2 ms, (c) 3 ms, (d) 3.15 ms, (e) 3.2 ms and (f) contour scale.	50
Figure 31: Contours of triaxiality η at various times up to the formation of the plug. (a) 1 ms, (b) 2 ms, (c) 3 ms, (d) 3.15 ms, (e) 3.2 ms and (f) contour scale.	51
Figure 32: Contours of temperature T , in Kelvin, at various times up to the formation of the plug. (a) 1 ms, (b) 2 ms, (c) 3 ms, (d) 3.15 ms, (e) 3.2 ms and (f) contour scale.	52
Figure 33: Contours of equivalent plastic strain rate at various times up to the formation of the plug. (a) 1 ms, (b) 2 ms, (c) 3 ms, (d) 3.15 ms, (e) 3.2 ms and (f) contour scale.	53
Figure 34: Examples of calculated velocity and acceleration histories experienced by the impacting mass. (a) Velocity and (b) acceleration calculated from the filtered velocity traces.	54
Figure 35: Comparison of the impact zones after the plug formation for the adiabatic (a) and the isothermal (b) representations of the material behavior. Contours of Johnson-Cook damage parameters are shown. The snapshot times are not equal for the two cases shown.	55
Figure 36: Comparison of the impact zones after the plug formation. (a) Accounting for strain rate effects and (b) removing the effect of strain rate on the failure model. The snapshot times are not equal for the two cases shown.	56
Figure 37: Schematic showing a linear stress decay after the failure criterion is satisfied. The decay occurs after failure over a strain u_d/L , where L is a characteristic length of the element and u_d is a prescribed displacement.	58

Figure 38: Comparison of impact zones after plug formation. (a) , ft/s and (b) , ft/s. The snapshot times are not equal for the two cases shown.	58
Figure 39: Uniaxial tension test specimen dimensions	63
Figure 40: High strain rate uniaxial tension test specimen dimensions	64
Figure 41: Dimensions of notched tension test specimen with $r/R=3.2$	64
Figure 42: Dimensions of notched tension test specimen with $r/R=1.28$	65
Figure 43: Dimensions of notched tension test specimen with $r/R=0.64$	65
Figure 44: Dimensions of notched tension test specimen with $r/R=0.32$	66

TABLES

Table 1: Plate impact velocities and result, shown in the order they were conducted.	16
Table 2: Johnson-Cook parameters for the three estimates of the dependence of the equivalent plastic strain at failure on triaxiality shown in Figure 21.	37
Table 3: Material property parameter values determined from the calibration exercises.	41
Table 4: Baseline predictions for plate puncture ($d_4 = -0.039$). “Y” indicates failure while “N” indicates no failure.	44
Table 5: Plate failure results for the three estimates of the dependence of material failure on triaxiality. “Y” indicates failure while “N” indicates no failure.	45
Table 6: Dependence of plate failure on the element size. The coarse mesh has elements 0.04 in. on the side, the fine mesh has elements 0.02 in. on the side and the very fine mesh has elements 0.01 in. on the side.	46
Table 7: Effect of including adiabatic heating vs. utilizing an isothermal model on the threshold velocity.	55
Table 8: Effect of including the effect of strain rate on failure vs. utilizing a rate independent model on the threshold velocity.	56
Table 9: Sensitivity of the threshold puncture velocity to the coefficient of friction.	57
Table 10: Sensitivity of the threshold puncture velocity to the value of u_d/L .	58

NOMENCLATURE

A, B, C, n, m	Johnson-Cook strength model parameters
C_p	Heat capacity
D	Plate diameter
$\bar{D}$	Cumulative damage variable
E	Young's modulus
L	Characteristic element length
R	Notched specimen cross-sectional radius
T, T_r, T_m	Current, reference and melting temperature
$\hat{T}$	Homologous temperature
W^p	Plastic work
d	Punch diameter
$d_1, \dots, d_5$	Johnson-Cook failure model parameters
t	Plate thickness
m	Carriage mass
r	Notched specimen notch radius
u_d	Plastic displacement during stress decay after failure
v_o	Impact velocity
β	Fraction of plastic work converted to heat
ε_e^p	Equivalent plastic strain
ε_{ef}^p	Equivalent plastic strain at failure for constant $\eta, \dot{\varepsilon}_e^p, T$
$\dot{\varepsilon}_e^p$	Equivalent plastic strain rate
$\dot{\varepsilon}_{eo}^p$	Reference equivalent plastic strain rate
η	Stress triaxiality
ν	Poisson's ratio
ρ	Initial material density
σ_1	First principal stress
σ_m	Mean hydrostatic stress
ϕ	Wellman's tearing parameter
ϕ_f	Value of ϕ at failure

1. INTRODUCTION

The principal objective of the work presented in this report is to evaluate the use of the Johnson-Cook strength and failure models for puncture predictions of relatively thick 7075-T651 aluminum plates. A schematic of the problem considered is shown in Figure 1. It consists of a circular plate clamped around its periphery. A cylindrical punch with spherical nose and attached to a relatively large mass impacts the plate at velocity v_o . The main item of interest is the prediction of the velocity threshold between impact with no penetration and with penetration of the plate. Penetration is defined as a breach that extends through the thickness from one side of the plate to the other.

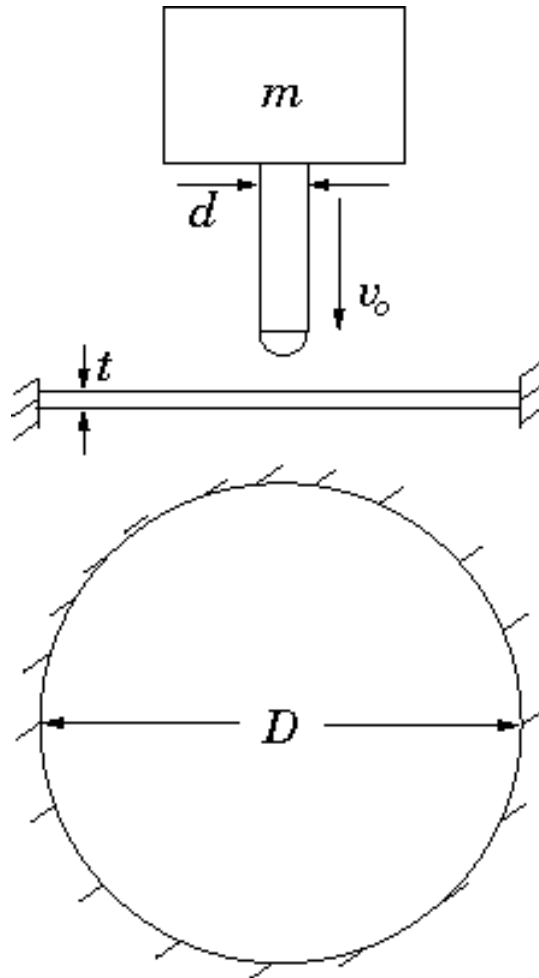


Figure 1: Schematic of the problem of normal impact of a circular plate by a cylindrical punch with a hemispherical nose.

The project is a continuation of an ASC L2 milestone project conducted during FY12 to assess the capability of Sandia's Multi-Linear Elastic-Plastic (MLEP) model with failure (Wellman, 2012) to predict puncture of aluminum plates when impacted by a blunt object (Corona et al, 2012). A series of tests conducted in the Mechanical Shock Laboratory at SNL-NM provided the measurement of the puncture threshold velocity as well as other metrics of the response and failure of the plates. In all tests the specimen dimensions were $D = 6.75$ in. and $t = 0.5$ in. while the punch had a diameter $d = 0.5$ in. The value of the mass was 306 lb.

The work plan for the L2 milestone consisted of a full verification and validation study including experimental (plate tests plus material characterization tests) and computational work as well as documentation in the form of a SAND report. Since all the work had to be completed within a period of one year, the analysis team restricted the scope of the project to include only the material model mentioned above.

The MLEP with failure model has been widely used at SNL-NM for problems combining plastic deformation and ductile failure. It consists of an elastic-plastic constitutive model to predict the deformation of the material and a ductile failure model to estimate when and how the material would fracture. The constitutive model treats the material as an isothermal, rate-independent, elastic-plastic solid. The elastic part of the deformation is taken to be linear and isotropic. Yielding occurs as dictated by the von-Mises criterion, and plastic deformation is calculated using the classical flow theory of plasticity with isotropic hardening. Failure of the material is evaluated using the “tearing parameter” proposed by Wellman (2012), which is calculated as

$$\phi = \int_0^{\varepsilon_e^P} \left\langle \frac{2\sigma_1}{3(\sigma_1 - \sigma_m)} \right\rangle^4 d\hat{\varepsilon}_e^P \quad (1)$$

Here, σ_1 is the maximum principal stress, σ_m is the mean hydrostatic stress and ε_e^P is the equivalent plastic strain. The Macaulay bracket is interpreted as

$$\langle a \rangle = \begin{cases} a, & \text{if } a > 0 \\ 0, & \text{if } a \leq 0. \end{cases}$$

The presence of σ_m in the denominator causes ϕ to accumulate faster when the hydrostatic stress is positive. The tearing parameter is calculated at all integration points in a structure for each load increment and accumulates as long as plastic deformation is occurring. Material failure occurs when

$$\phi = \phi_f \quad (2)$$

where ϕ_f is the value of ϕ at failure, and is usually determined from a uniaxial tension test (The exponent of 4 in (1) was suggested by Wellman (2012) and was used for all the calculations in the milestone project. Recently, the Sierra/Solid Mechanics codes allowed analysts to choose the value of the exponent. For more information on the effect of the value of the exponent see Corona and Reedlunn, 2013).

The principal finding of the L2 milestone project was that the computational results overestimated the experimentally determined puncture threshold velocity by 60%. This result motivated the present work to determine the causes of the discrepancy and improve the results if possible.

The following sections will first review the plate puncture experimental results presented by Corona et al (2012). In this report, they have been augmented with micrographs showing the material flow and damage near the puncture region. This is followed by a description of the material characterization tests conducted in FY12. The material test data are augmented by including results of tension tests on specimens cut at 45° to the rolling direction and recently

obtained stress-strain curves at different temperatures. The comparison between the experimental and numerical results for the threshold puncture velocity obtained in FY12 will be discussed next to set the stage for the current work. Following this, the Johnson-Cook strength and failure models and their calibration based on the material data available will be discussed in detail. The Johnson-Cook model was chosen because it is already implemented in finite element codes, has received significant use in the literature and addresses some of the perceived shortcomings of the MLEP model with failure for the problem at hand. The next section discusses the puncture predictions generated as well as their sensitivity to the parameters of the model. The report concludes with a discussion of the results and insights obtained for the plate puncture problem.

2. REVIEW OF EXPERIMENTAL RESULTS

All plate puncture tests were conducted in the Mechanical Shock Laboratory in SNL-NM using the 20 ft. shock machine DT-67 shown in Figure 2. Briefly, the machine consists of a 306 lb carriage that slides vertically on two polished rods. Accelerations higher than 1 g are achieved by pulling down on the carriage using two long bungee cords. The plates were clamped to a reaction mass at the machine's lower end. The experiments consisted of raising the carriage/punch assembly to the height required to achieve a given impact velocity and then releasing it. The position and acceleration time histories of the carriage/punch during the impact event were recorded using appropriate instrumentation. The velocity history could be obtained by either differentiation of the filtered position data or by integration of the acceleration data. A high speed video camera filmed the surface of the plate opposite to the impact point and programming material was used to stop the carriage after puncture. Relevant details of the experimental set-up and data reduction, including uncertainty estimates, are provided in Corona et al (2012).

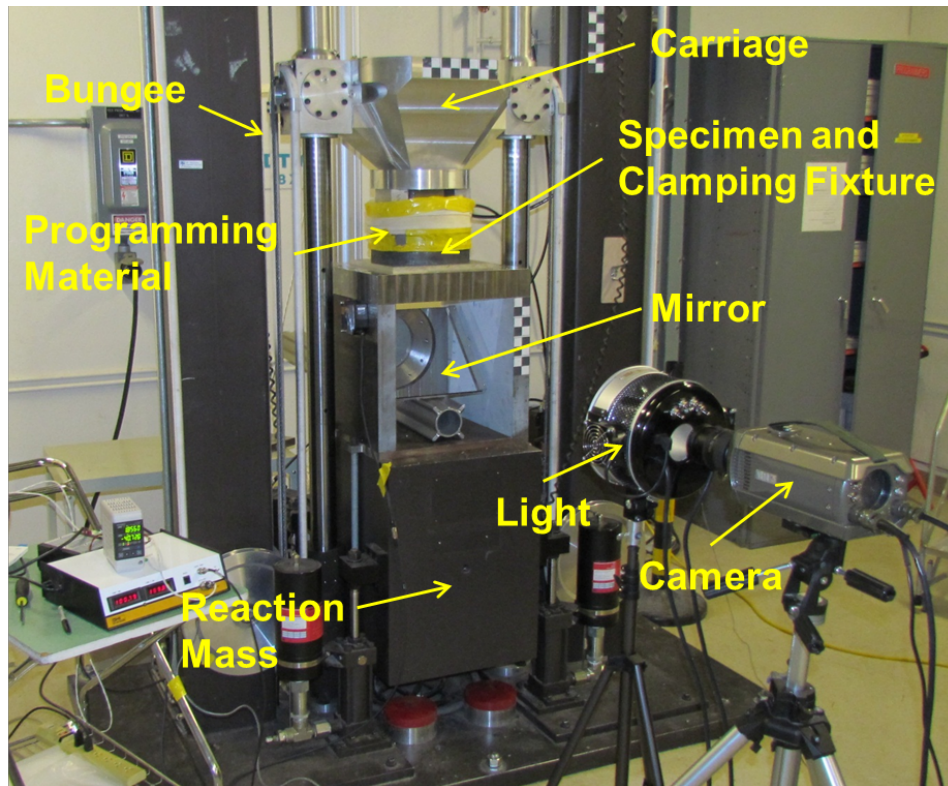


Figure 2: Experimental set-up for plate impact

2.1 Threshold Velocity Measurements

The search for the critical penetration punch velocity was conducted using a one-shot experimental procedure. In this procedure each test specimen was tested only once. A record of the impact speed and whether puncture occurred was kept. The sequence of impact velocities selected in the tests was determined with the help of the Neyer D-optimal test procedure (Neyer, 1994) as implemented in the commercial software product SenTest (Neyer, 1991). In this procedure, the impact velocity chosen for each test is based on the penetration results of all previous tests. Details of the test procedure can be found in Corona et al (2012).

A total of 19 tests were conducted, with the results shown in Table 1. All but one of the specimens that failed did so by ejecting a plug from the impact site and displayed a through hole of about the same diameter as the punch. In only one test was the failure type identified as “breach,” meaning that no plug was ejected, but a passage from one side of the plate to the other was identified. Assuming that the strength of the plates in terms of the punch velocity required for puncture is normally distributed, one can determine the mean and standard deviation values of the strength distribution (Langlie, 1963). The mean value represents the best estimate of the threshold puncture velocity and the standard deviation is related to its spread. Figure 3 shows all the experimental results plotted with symbols as well as the best-fitting normal probability and cumulative probability distribution functions for the plate strength. Note that the distribution has a mean of 10.04 ft/s and a standard deviation of 0.445 ft/s. Closer consideration of the trends for the evolution of the values of the mean and standard deviation as the test sequence progressed showed that the mean value was nicely converged by the end of the test series, but the standard deviation was still changing with a decreasing trend. Taking the last values for both parameters as the best estimates, one could conclude with 95% confidence that the threshold velocity lies in the range [9.2, 10.9] ft/s. Note that the highest no-failure velocity seen in the tests was 10.5 ft/s while the lowest failure velocity was 9.7 ft/s. Further details regarding the type of damage suffered by each specimen, sample velocity and acceleration records, etc. are shown in Corona et al (2012).

Figure 4 shows four frames taken from the high speed video of specimen 9. The impact speed was 11 ft/s and failure occurred by ejection of a plug. Upon impact, the first sign of damage on the surface being filmed was a crack on the surface opposite to the impact point. This crack appeared 1.5 ms after impact and is shown at 1.8 ms in Figure 4(a). By 2.7 ms after impact, a couple of flakes started breaking off the plate in what is called scabbing as seen in Figure 4(b). Subsequently the scabs broke off the plate as shown in Figure 4(c) at 6.2 ms, and finally the plug was ejected as shown in Figure 4(d), which corresponds to a time of 42 ms after impact. The acceleration record for this experiment suggests that the puncture process had completed approximately 3.45 ms after the initiation of impact. This timing was similar as those in other experiments where a plug was ejected.

Table 1: Plate impact velocities and result, shown in the order they were conducted.

Specimen Number	v_o (ft/s)	Failure (Type)
6	6.7	No
7	11.6	Yes (plug)
8	9.3	No
9	11.0	Yes (plug)
10	8.2	No
11	10.2	Yes (plug)
12	7.8	No
13	10.5	No
14	10.6	Yes (plug)
15	9.4	No
17	9.9	No
18	9.7	No
19	11.0	Yes (plug)
20	9.8	No
21	10.6	Yes (plug)
22	9.7	Yes (plug)
23	10.2	Yes (plug)
24	9.1	No
25	10.4	Yes (breach)

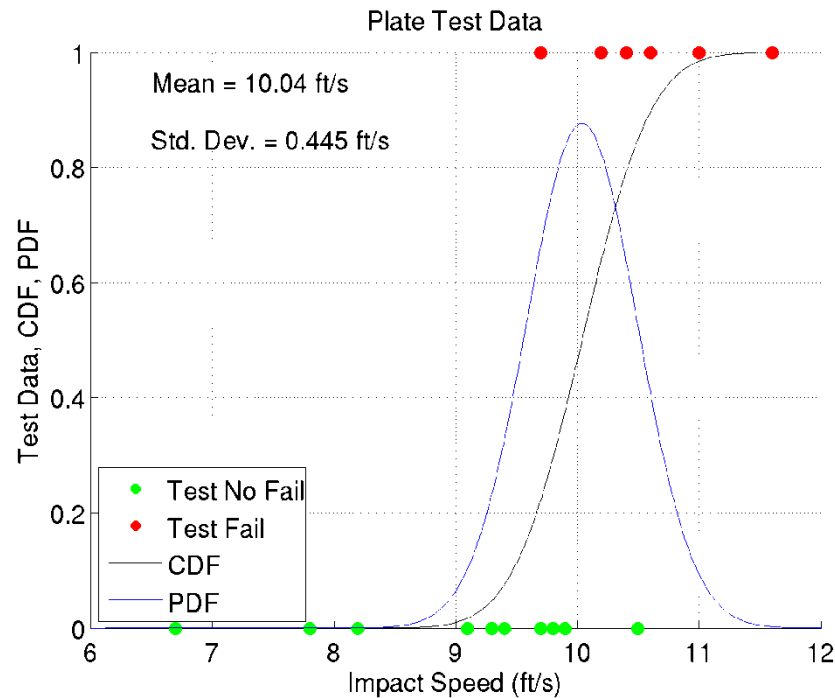


Figure 3: Experimental data for the plate impact problem indicating failure or no failure as a function of impact speed. The derived normal probability and cumulative probability distribution functions are also shown.

Further details of the failure process are presented by Corona et al (2012). It was remarkable that, depending on the impact speed, the damage of the plates would progress up to a certain stage of the events described above. For example, the plate impacted at 6.7 ft/s showed no crack, but plates impacted in the vicinity of 8 ft/s displayed the initial crack but no scabbing. For higher speeds, but still below 10 ft/s, scabbing took place, but no plug was ejected. In most cases with impact speeds above 10 ft/s, the plug was ejected.

Clearly, puncture of the plates tested is a complex process involving a sequence of at least three easily identifiable and distinct failure events:

1. The initial crack that formed at the surface of the plate likely developed under a mostly balanced, biaxial tensile state of stress
2. Scabbing was the result of cracks that ran at a shallow angle to the surface of the plate and may have been the result of the plug pushing out of the plate, even if it had not completely broken off the rest of the plate.
3. The plugs generally had relatively cylindrical shapes which may indicate a shear dominated material failure.

Therefore, the candidate material failure models for this problem should be able to simulate failure under various states of stress, from equibiaxial to shear dominated. The numerical simulations to be presented later will show that the Johnson-Cook model can replicate at least some of the more important aspects of plate puncture if it is calibrated using material test data.

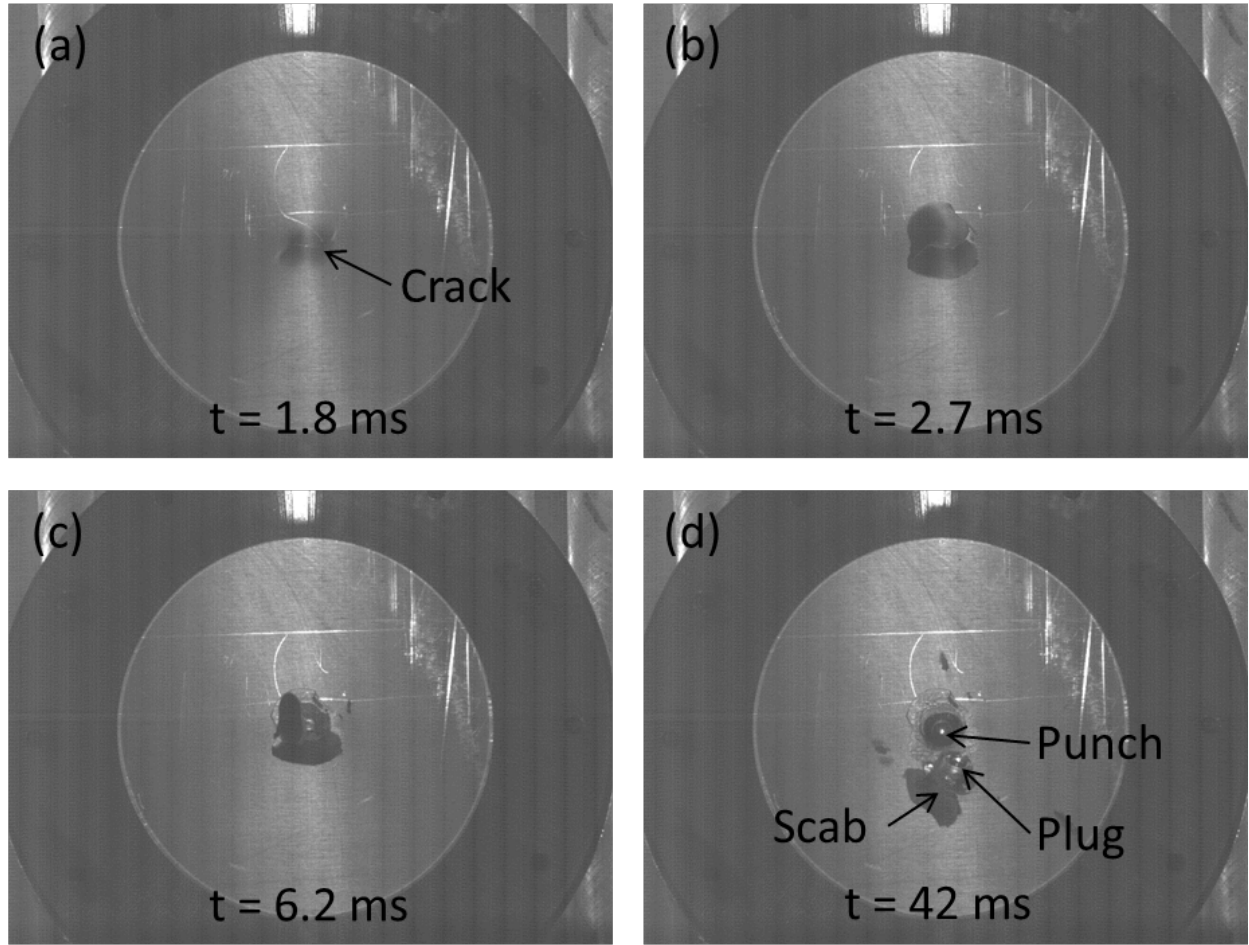


Figure 4: Sequence of high speed video frames showing instances of damage during plate impact for specimen 9 ($v_0 = 11$ ft/s). (a) A crack appears on the surface, (b) scabbing begins, (c) scabs break off plate surface and (d) plug is ejected.

2.2 Microscopy Observations

Two of the plates tested were sectioned and prepared to visualize the flow of the material near the impact region using microscopy. The first was specimen 10. The punch impact velocity was 8.2 ft/s. This plate did not fail, but developed a crack on the side opposite to impact similar to that seen in Figure 4(a). Figure 5 shows a section through the center of the impact dimple. The initial crack is clearly seen to have a main vertical branch that extends approximately one-third of the thickness of the plate. A small horizontal crack is also seen branching off to the left near the lower surface. Interestingly a horizontal internal crack is also visible at the right side of the image.

Figure 6 shows a section through the center of the impact dimple of specimen 13. The impact speed was 10.5 ft/s, which is the fastest that did not lead to failure. Prior to sectioning, the plate was potted in an adhesive to preserve the location of the scabbing fragments. The micrograph clearly shows the extent of scabbing and the plug that was being formed. Micrographs of more magnification showing the edges of the plug are shown in Figure 7. They show that the plug is still attached to the plate, but that areas of very highly localized

deformation occurred. It also shows cracks that are propagating from the lower surface of the plate towards the impact surface. The large deformations in the strain localization regions can be better appreciated in Figure 8, which shows two overlapping micrographs from the left side of the plug. The width of the localization band is in the order of $100\text{ }\mu\text{m}$ (0.004 in.). The two boxes shown correspond to the original finite element dimensions of two meshes that will be discussed later. In preparation for future discussions, it is useful to note that the angle of inclination of the microstructure in the vicinity of the localization band with respect to the horizontal is in the order of 33° to 38° when measured over a length equivalent to the side of the box labeled “Fine Mesh”.

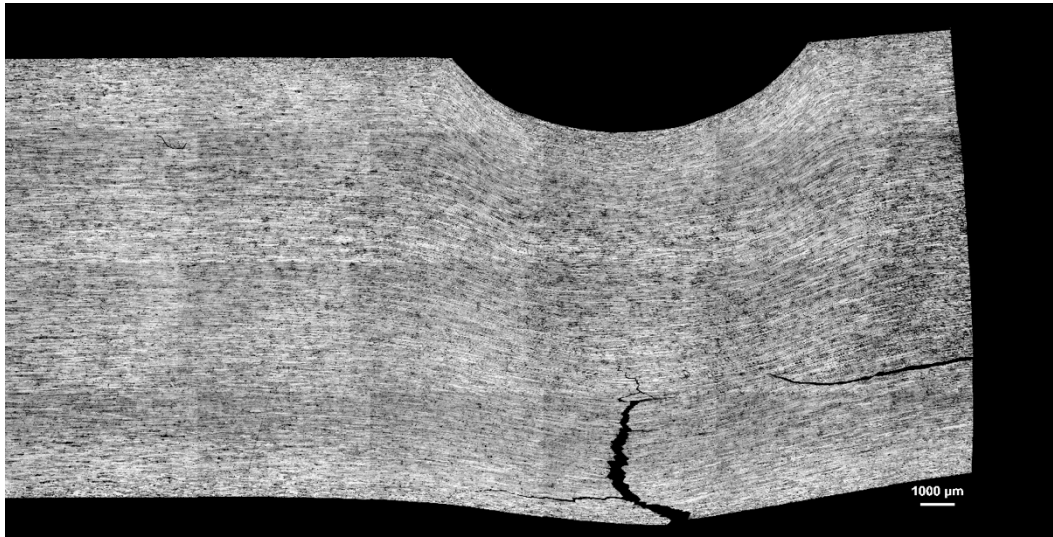


Figure 5: Section of specimen 10 through the center of the impact dimple.



Figure 6: Section of specimen 13 through the center of the impact dimple.



(a) **(b)**
Figure 7: Micrographs showing the localization and fractured regions where the plug was about to form. (a) Left side and (b) right side.

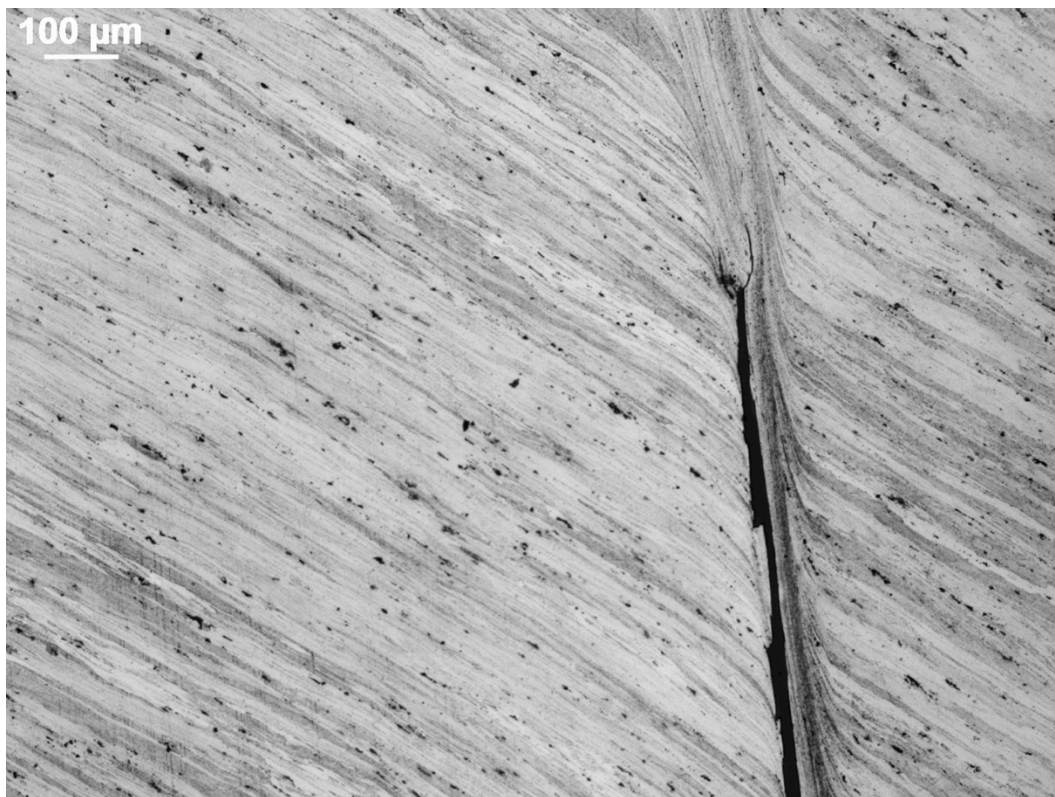
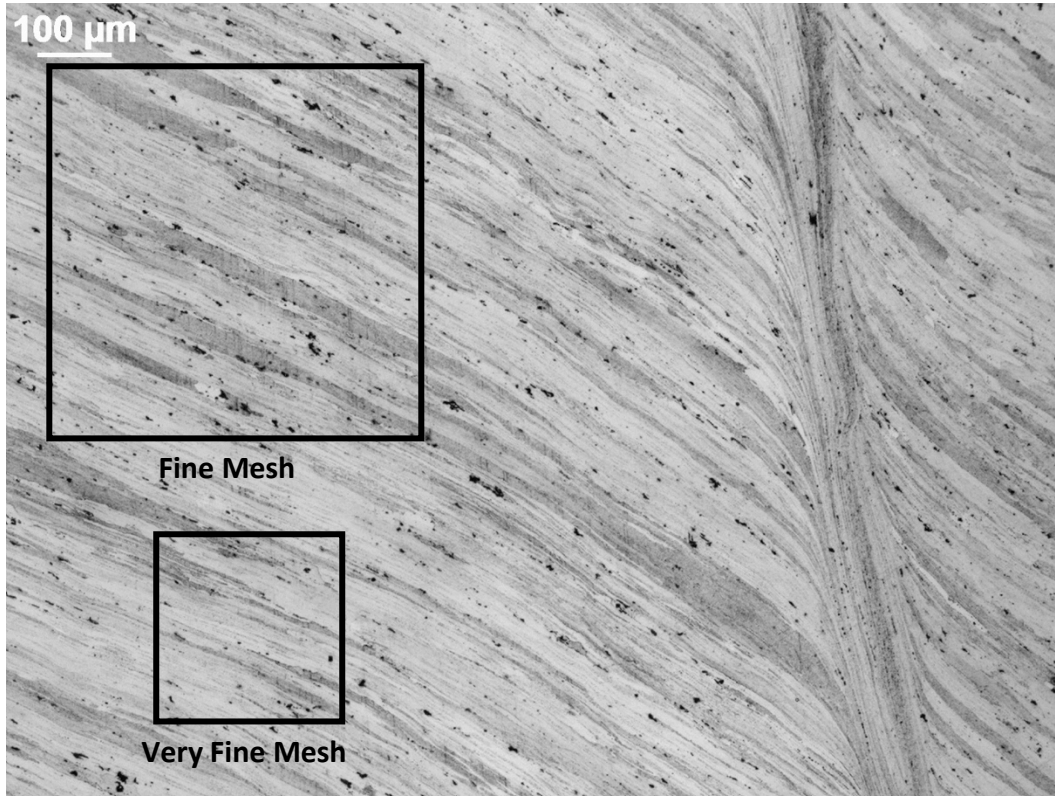


Figure 8: Contiguous micrographs showing details of localization region and crack tip on the left side. The element size for the fine and the very fine finite element meshes are shown for comparison.

3. MATERIAL CHARACTERIZATION

A material characterization study of aluminum alloy 7075-T651 was carried out during the L2 milestone project using specimens machined from the same plate material as the plate puncture specimens. Its objective was to gather material data that could be used to calibrate and test the models used. It consisted of quasi-static uniaxial tension tests in the rolling direction of the plate as well as in the in-plane transverse direction, high strain rate uniaxial tension tests at rates in the order of 1300 1/s and tests on notched tensile specimens with four different notch radii. The procedures and results of these tests were reported in detail in the L2 milestone report (Corona et al, 2012) and will be briefly reviewed in this section.

Quasi-static Uniaxial Tension Tests

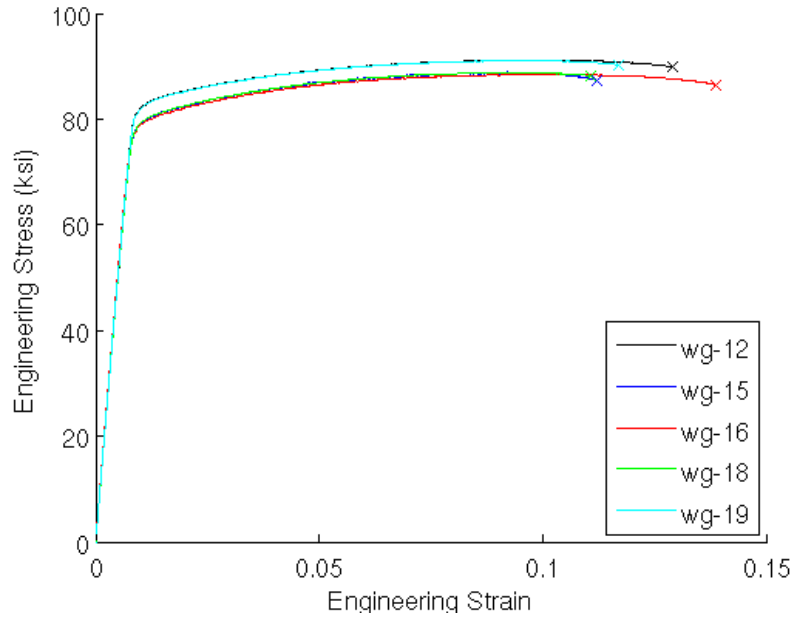
Figure 9 shows the engineering stress-strain curves obtained from the quasi-static tension test specimens cut either along the rolling direction of the plate (Figure 9a) or perpendicularly to it (Figure 9b). The specimens had cylindrical test sections of diameter and length of 0.25 and 1.25 in., respectively. The shop drawings of these specimens are shown in Figure 39 in Appendix A. The tests were conducted at a strain rate of approximately 0.01 1/min. The strain was measured using an extensometer with a gage length of one inch. In all cases, the specimens failed after very little necking and fractured along a surface slanted with respect to their longitudinal axes. Note that the stress-strain curves in the rolling and transverse directions are very similar with respect to their yield stress and strain-to-failure. Also note that specimens cut along the rolling direction exhibited some distinct difference in yield stress. This grouping into two yield stresses was repeatable when more specimens were tested, thus indicating some inhomogeneity in the material properties.

A third set of tests was conducted with specimens oriented at 45° to the rolling direction, still in the plane of the plates. The results, however, were not ready in time to be included in the L2 milestone report. These tests were conducted using specimens of similar geometry and at the same strain rates as just described. Five specimens were tested with the results shown in Figure 10. The most ductile responses obtained in each of the three directions tested are shown in Figure 11. Clearly the specimens cut at 45° to the rolling direction showed smaller yield stresses and enhanced ductility when compared to the other two directions. In addition, the mode of failure changed from the relatively smooth fracture in a plane slanted to the specimen axis to one that had similar characteristics to a cup-cone type of failure. The conclusion is that the material is clearly anisotropic in the plane of the plate. No testing has been conducted to date to try to assess the stress-strain curves of specimens with their axes in the through-thickness direction. The lower yield stress along the 45° direction could be accounted using Hill's anisotropic yield surface (Hill, 1950) by reducing the yield stress of the plate material when loaded with pure in-plane shear. Currently available failure models commonly used for engineering analysis, however, do not allow anisotropy with respect to the strain to failure.

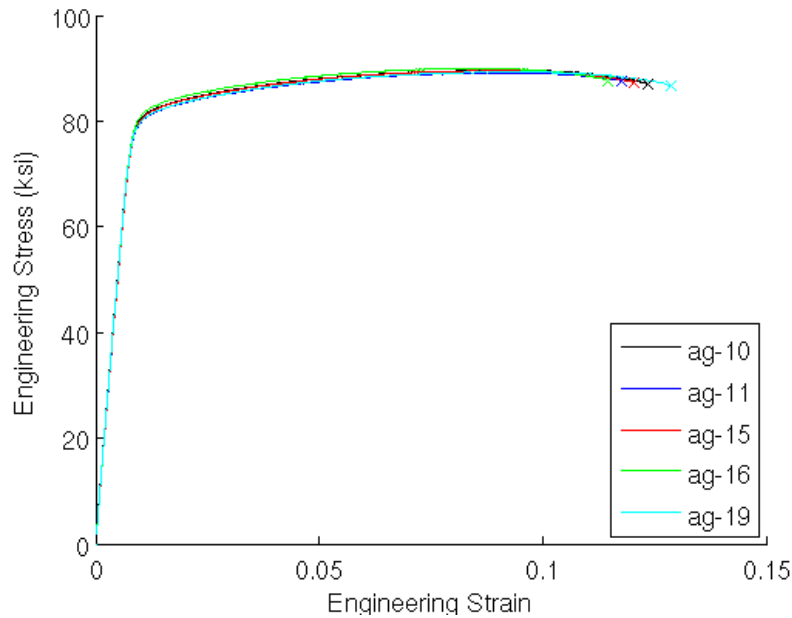
High Strain Rate Uniaxial Tension Tests

Song and Antoun (2012) conducted a set of high-strain-rate uniaxial tension tests for specimens machined perpendicularly to the rolling direction using a Kolsky bar. The strain rates achieved were in the range of 1250 to 1400 1/s. The test and data reduction methods used are those described by Song et al (2013). The shop prints of these specimens are shown in Figure 40 in Appendix A. The measured stress-strain curves are compared to the least and most ductile curves obtained quasi-statically for specimens with similar orientation in Figure 12. Whereas the

peak seen at yield is an artifact of the test method, the high strain rate tests consistently show higher stresses during plastic flow. The strain to failure must be evaluated numerically as will be done later because the specimen size and gage length were different between the quasi-static and the dynamic tests.



(a)



(b)

Figure 9: Engineering stress-strain curves obtained from uniaxial tension tests. (a) along the rolling direction and (b) perpendicularly to the rolling direction.

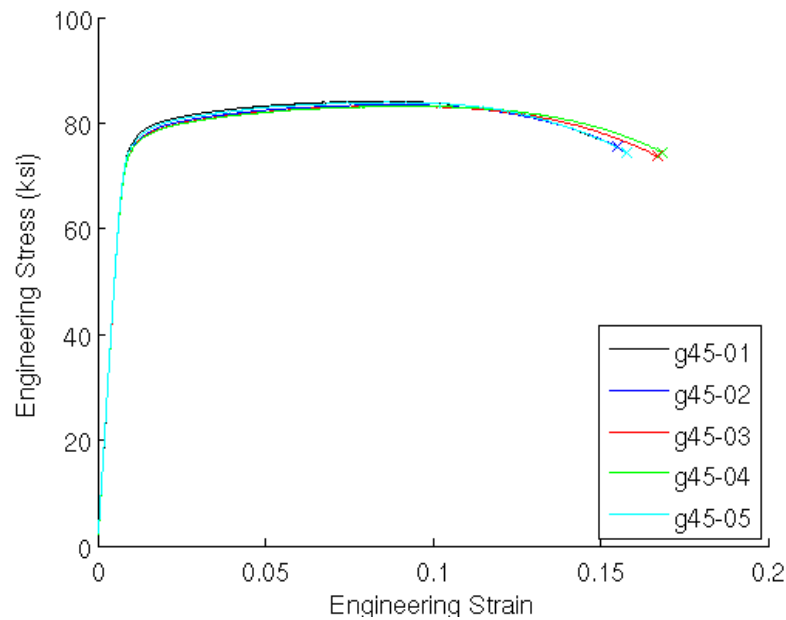


Figure 10: Engineering stress-strain curves obtained from uniaxial tension tests of specimens machined with their axes at 45° to the rolling direction.

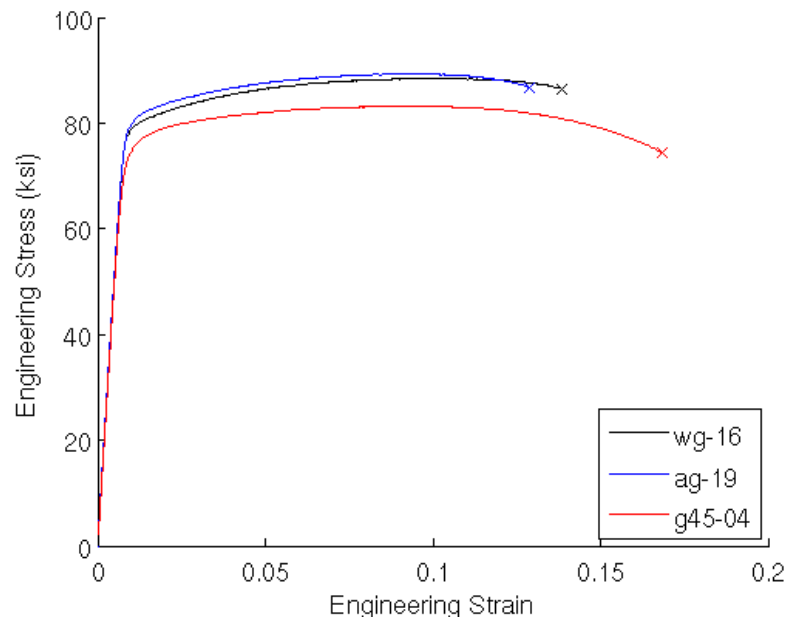


Figure 11: Comparison of the three most ductile stress strain curves for specimens machined with their axes along the rolling direction (wg-16), perpendicularly to the rolling direction (ag-19) and at 45° to the rolling direction.

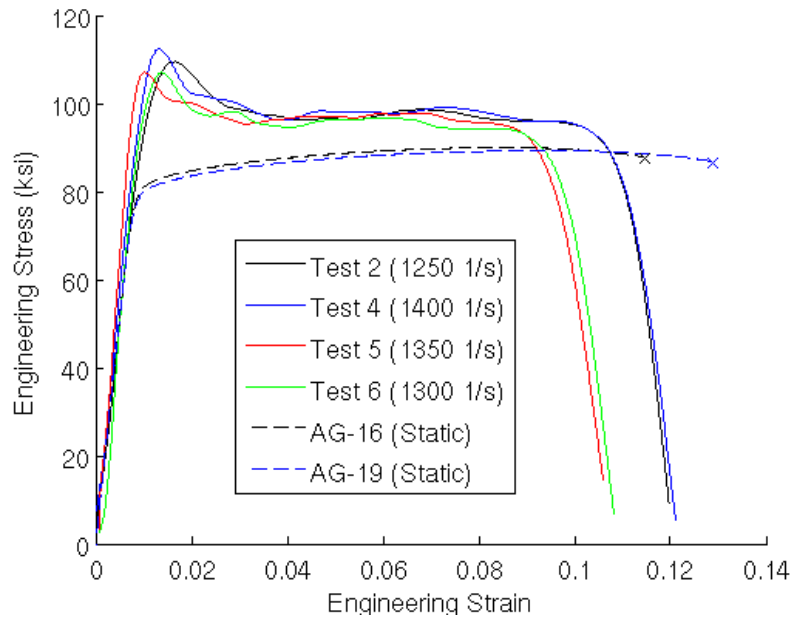


Figure 12: Comparison of high-strain rate to quasi-static engineering stress-strain curves.

Notched Tension Tests

One last set of tests conducted in 2012 consisted of tensile loading of notched test specimens. Four notch radii were considered, and three tests were run for each. The machine shop drawings for these specimens are shown in Figure 41 to Figure 44 in Appendix A. The original purpose of the notch tension tests was to generate quasi-static test data using specimens where the state of stress was not uniaxial. The idea was to compare the measured load and displacement at failure vs. model predictions as shown in section 2.3.3 of Corona et al (2012) to assess the predictive capabilities of the constitutive and failure models. The force-deflection curves obtained in the tests are shown in Figure 13. As the notch radius becomes sharper, the specimens carry higher loads but exhibit less stretch at failure. In the present work, the data will be used to calibrate the failure criterion parameters. A closer look at the stress state at the notch will also be presented in Section 5.2.2.

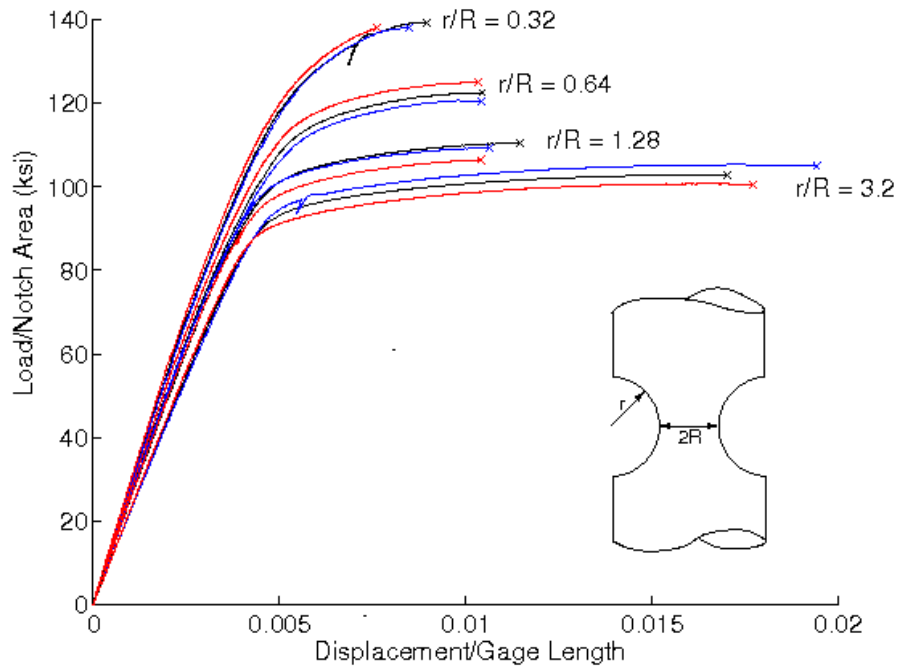


Figure 13: Load-deflection results for tension tests on notched specimens with four notch radii. Failure is indicated by “x.”

High Temperature Tests

The last set of material tests that will be considered for the calibration of the constitutive model consisted of a series of tension tests conducted by Antoun (2013) on Al 7075-T651 at various temperatures as shown in Figure 14. The specimens were machined from a rod with diameter of 3 inches. The test sections were circular with a diameter of 0.35 in. and length of 1.5 in. (Antoun, 2013). The strain was measured using an extensometer with a one inch gage length. Although the alloy and the heat treatment were nominally the same as those used in the plate puncture experiments, the stress-strain curves at room temperature were somewhat different from the ones shown in Figure 9. The trends in Figure 14 show that the flow stress of the material is significantly depressed by increasing the temperature. Simultaneously, the strain to failure increases significantly. It is also interesting to see that the ultimate stress occurs at smaller values of strain as the temperature increases and that for temperatures 200°C and above the ultimate stress occurs upon yielding.

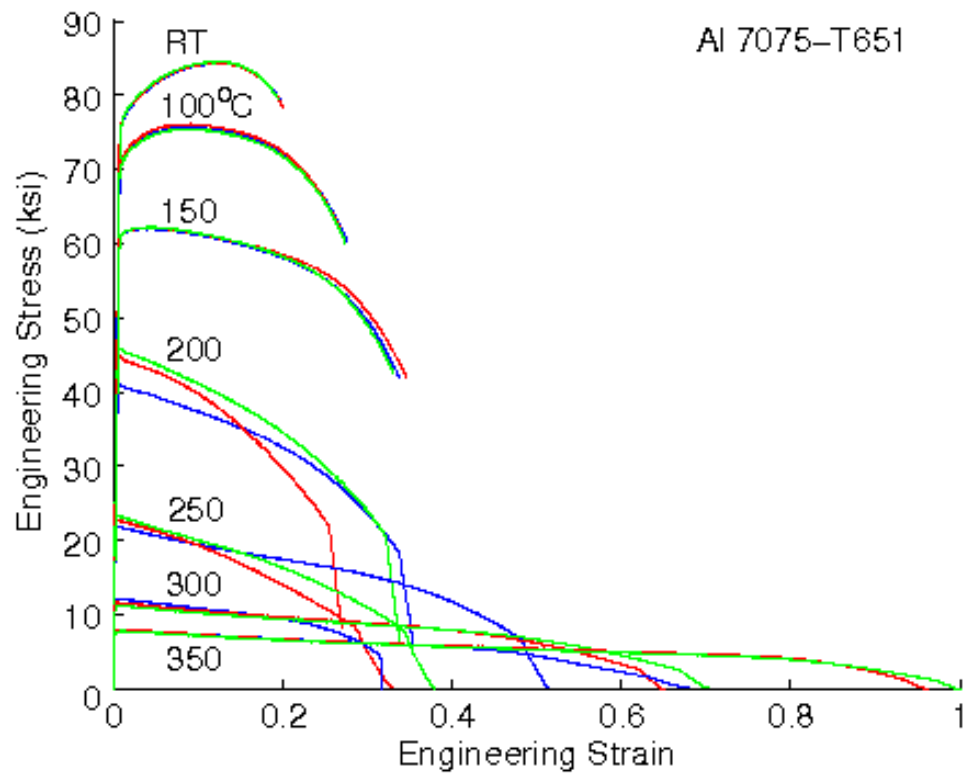


Figure 14: Quasi-static strain rate, engineering stress-strain curves for Al 7075-T651 at various temperatures and quasi-static strain rate obtained by Antoun (2013).

4. REVIEW OF FY12 PLATE PUNCTURE RESULTS

Of all the material tests presented in the previous section, only the quasi-static uniaxial tension tests were required to calibrate the MLEP with failure model. The engineering stress-strain curves shown in Figure 9 were used in a fitting procedure described in Corona et al (2012) to determine the corresponding true stress-strain curves that are needed as input to the finite element analysis of plate puncture. Since the tearing parameter failure criterion only required a single parameter to be determined, the failure point in each tension test provides an estimate of the critical value of the tearing parameter ϕ_f . The fact that variability occurred between the tension tests yielded a distribution of values for both the true stress-strain fit as well as for ϕ_f .

The comparison between the plate puncture experimental results and the numerical predictions is shown in Figure 15 in terms of the cumulative distribution functions for the threshold impact velocity. The solid black line is the same that was presented in Figure 3, whereas the solid red line is the equivalent for the predictions. The shaded regions represent the uncertainty of the distributions at 95% confidence.

Clearly, the numerical predictions overestimated the experimental results by approximately 60%, and the uncertainty regions are not wide enough to account for the difference. At the conclusion of the L2 milestone project, the most likely reasons for the discrepancy were thought to be related to:

1. The isothermal character of the constitutive model. This was based on the observation that the plugs ejected from the plates during the puncture experiments were hot to the touch right after the tests.
2. The apparent insensitivity of the tearing parameter criterion to predict failure under shear-dominated states of stress. Some undocumented historical observations supported this possibility.

As a side note of interest, it is worthy to mention that although the numerical results overestimated the threshold puncture velocity of the plate, the numerical simulation of the notched tension tests gave results that consistently *underestimated* the strength of the material as shown in Figure 46 in Corona et al (2012). This apparent paradox will be addressed in a latter section.

The Johnson-Cook strength and failure models combined with an adiabatic finite element analysis provide the means to explore the two likely reasons for the predicted threshold puncture velocity discrepancy with experiments. It is implemented in various finite element codes and will therefore be adopted in the present work to conduct further exploration of the plate puncture problem.

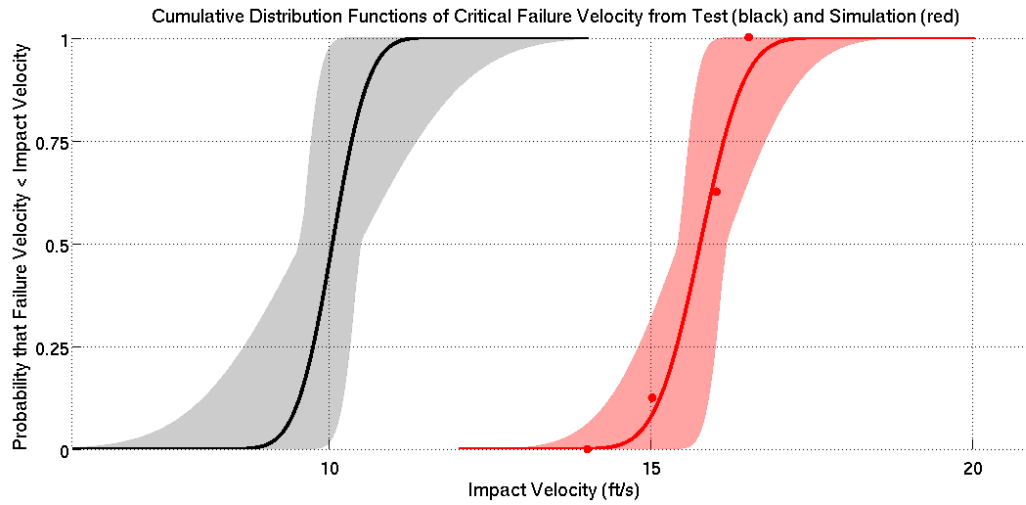


Figure 15: Cumulative Distribution functions of critical failure velocity and epistemic uncertainty bands at 95% confidence. Experimental data shown in black and numerical prediction data shown in red

5. CALIBRATION OF THE JOHNSON-COOK MODEL

Johnson and Cook (1985) proposed plasticity and failure models that account for factors that are important when materials are loaded into the plastic range in shock or impact environments. These factors include large strains, large strain rates, high pressures and high temperatures.

5.1 Basics of the Johnson-Cook Model

The model for plastic deformation (often called the “strength model”) of the material assumes that plastic deformation is governed by the J_2 flow theory of plasticity, but that the expression for the equivalent stress-plastic strain ($\sigma_e - \epsilon_e^p$) curve of the material depends not only on the current plastic strain, but also on the plastic strain rate and the temperature. The influences of each of these parameters are decomposed in a multiplicative manner as follows:

$$\sigma_e = \left[A + B(\epsilon_e^p)^n \right] \left[1 + C \ln \left(\frac{\dot{\epsilon}_e^p}{\dot{\epsilon}_{eo}^p} \right) \right] \left[1 - \hat{T}^m \right]. \quad (1)$$

Here, $(\dot{\bullet}) \equiv d(\bullet)/dt$ where t represents time, $\dot{\epsilon}_{eo}^p$ represents a reference strain rate and

$$\hat{T} = \frac{T - T_r}{T_m - T_r} \quad (2)$$

is the homologous temperature. In the latter expression T , T_r , T_m are the current temperature, a reference (usually room) temperature and the melting temperature of the material. Finally, the five parameters A, B, n, C, m are adjusted to match material test data obtained from experiments as well as possible. Note that testing data under quasi-static and dynamic conditions as well as at temperature are needed to fit this model.

The failure model is also constructed using a multiplicative decomposition of the effect of triaxiality, which is the ratio of the mean hydrostatic stress to the equivalent stress ($\eta = \sigma_m / \sigma_e$), strain rate and temperature on the equivalent plastic strain at failure as follows:

$$\epsilon_{ef}^p = \left[d_1 + d_2 e^{d_3 \eta} \right] \left[1 + d_4 \ln \left(\frac{\dot{\epsilon}_e^p}{\dot{\epsilon}_{eo}^p} \right) \right] \left[1 + d_5 \hat{T} \right]. \quad (3)$$

Here $d_1 - d_5$ are adjusted to best represent the experimentally obtained material failure data. Note that the dependence of the equivalent plastic strain at failure on triaxiality has an exponential form, as suggested by Rice and Tracey (1969) for the enlargement of spherical voids. Since the triaxiality, strain rate and temperature at a material point can vary during the loading history, a cumulative damage variable is defined as

$$\bar{D} = \int \frac{d\hat{\epsilon}_e^p}{\epsilon_{ef}^p(\eta, \dot{\epsilon}_e^p / \dot{\epsilon}_{eo}^p, \hat{T})}, \quad (4)$$

with failure occurring when $\bar{D} = 1$.

An additional set of parameters are needed to calculate the temperature rise in the material that is generated in response to plastic deformation. This temperature rise will be calculated on the assumption of adiabatic heating. In essence, it is assumed that the impact event is so fast that sufficient time is not available to conduct heat away from the regions with large

plastic deformation. Under these conditions, the rise of temperature $\Delta T = T - T_r$ is directly related to the plastic work done at a material point by

$$\Delta T = \frac{\beta W^p}{\rho C_p} \quad (5)$$

Where β represents the fraction of plastic work that is converted into heat, ρ is the density of the material and C_p is the heat capacity of the material. The parameter β is generally taken to be a constant in the order of 0.90 to 0.95, but it can depend on strain and strain rate as discussed by Mason et al (1994). It appears, however, that assuming constant values in the order of 0.90 to 0.95 is a good approximation, especially as the strains become larger, at least for the Al 2024 and 4340 steel specimens that they tested. The values for the parameters ρ and C_p are available in the literature for many metals.

5.2 Calibration of the Johnson-Cook model parameters

The data presented in Section 3 provides information that allows the determination of the parameters of the Johnson-Cook model for the Al 7075-T651 material of the plate specimens used in the impact tests.

All work that used the Johnson-Cook model was conducted using the finite element code Abaqus version 6.12 (Dassault Systemes, 2012). All calculations were conducted in the SI system of units and the results then converted to the lb/in/s/°R system to be presented in this document. Because of this, the values of all parameters will be presented in both sets of units.

The adiabatic heating calculation in (5) is not part of the Johnson-Cook model but is needed if the analysis is going to include an evaluation of the effect of adiabatic heating on the plate response. The required parameters were obtained from the Aerospace Specification Metals website (asm.matweb.com/search/SpecificMaterial.asp?bassnum=MA7075T6). Other sources listed similar values as follows: $\rho = 2810 \text{ kg/m}^3$ ($2.63 \times 10^{-4} \text{ slug-ft/in}^4$) and $C_p = 960 \text{ J/(kg K)}$ ($826 \times 10^3 \text{ lb-in/(slug-ft/in-}^\circ\text{R)}$). The melting temperature was taken as the solidus temperature with a value $T_m = 750 \text{ K}$ (1350°R). Finally, the reference temperature and plastic strain rate were taken to be $T_r = 293 \text{ K}$ (527°R) and $\dot{\epsilon}_{eo}^p = 0.00016 \text{ 1/s}$ to agree with the test conditions in the quasi-static uniaxial tension tests.

5.2.1 Quasi-Static Uniaxial Tension Test at Room Temperature

The first step in the calibration of the Johnson-Cook model is the fitting of the reference stress-strain curve. In the present work, curves obtained under quasi-static loading conditions were used to determine the parameters for the reference. Figure 16 shows the experimentally obtained engineering stress-strain curves for specimens WG-16 and WG-18, which were the least and most ductile specimens aligned with the rolling direction of the plate. The dashed red line corresponds to the true stress-strain curve with $A = 517 \text{ MPa}$ (75.0 ksi), $B = 405 \text{ MPa}$ (58.7 ksi) and $n = 0.41$. These parameters were determined using an educated trial and error process and visually comparing the results of simulations to the experimentally obtained engineering stress-strain curve. The predicted engineering stress-strain curve corresponding to the parameters chosen is shown in solid red line and agrees well with the measured data.

The engineering stress and strain values at which the specimens failed are shown by an “x” in the figure, and the corresponding values of true stress and strain at failure are shown as circles. These points will be used later to help establish some of the parameters of the failure model.

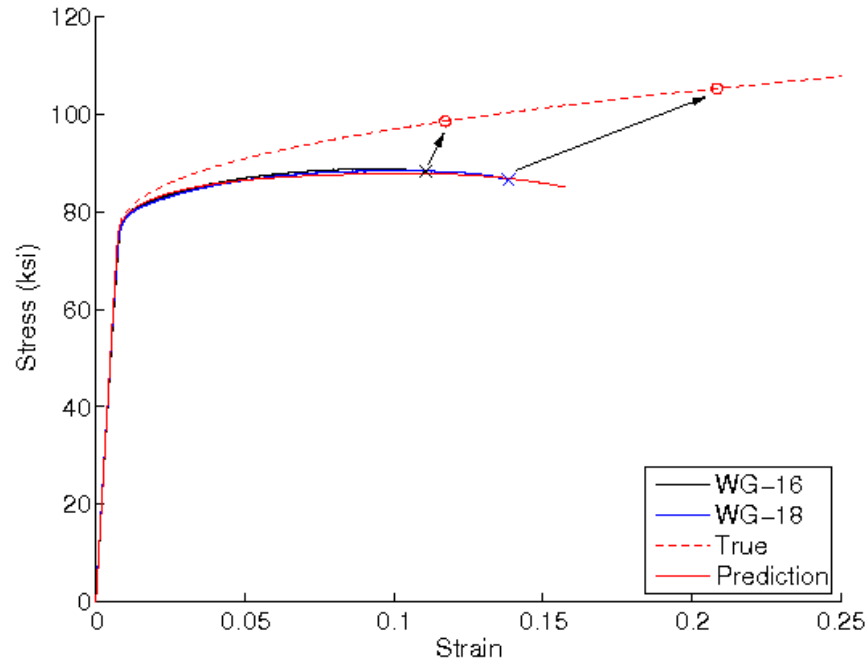


Figure 16: Comparison between the engineering stress-strain curves for the least and most ductile wg specimens and the prediction of the uniaxial tests obtained with the fit for the Johnson-Cook model. The circles in the true stress-strain curve correspond to the failure strains measured in each of the two tests.

5.2.2 Tension Tests on Notched Specimens

The data from the quasi-static tension tests conducted on smooth and notched specimens provide a means to calibrate the dependence of the equivalent strain at failure on triaxiality. Given that the fit for the reference uniaxial tension curve is known, the next step of the calibration process was to simulate the response of notched tension tests. Models of each nominal specimen geometry were constructed. Finite element meshes for models with the bluntest and sharpest notches are shown in Figure 17. The models take advantage of three planes of symmetry and, as a result, only one-eighth of the notch region needs to be considered as shown. The length of the models is 1/2 inch to match the gage length of the extensometer used in the tests. Symmetry conditions are implemented on the surfaces at the notch cross-section and the two longitudinal flat surfaces, while the axial displacement at the surface of the end of the thick part of the specimen is prescribed. The first set of predictions was conducted using the implicit solution method in Abaqus/Standard.

The predicted load-deflection results are shown in dashed line and compared to the experimental measurements in Figure 18 (these calculations were conducted using a multi-linear fit of the true stress-strain curve obtained at the reference strain rate. The predictions are almost

indistinguishable from those obtained with the Johnson-Cook strength model, as will be demonstrated shortly). Clearly, these predictions do not include failure, but the curves are very close to the experimental ones prior to failure indicating that J_2 flow theory yields appropriate predictions.

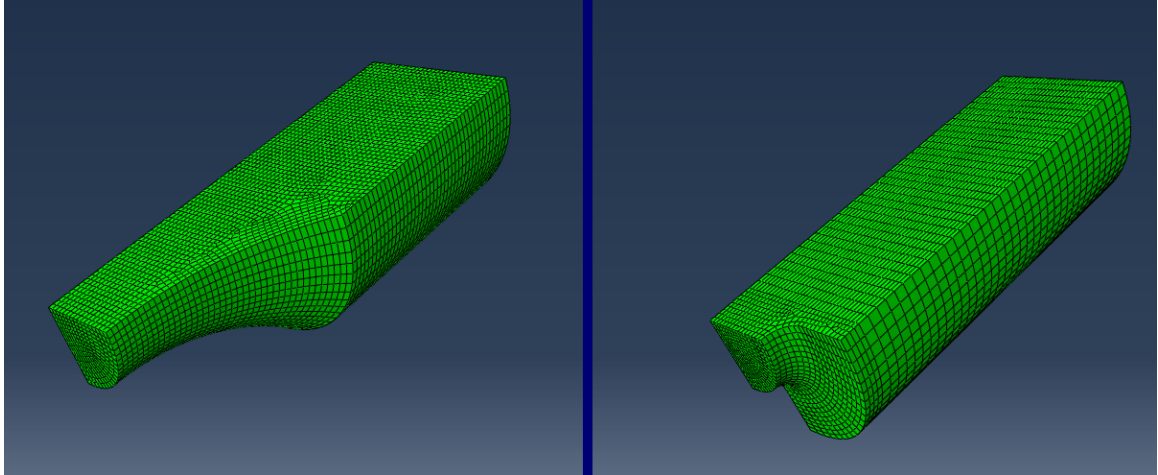


Figure 17: Finite element models of two notched tension specimen geometries $r/R = 3.2$ and 032.

From the results of the calculations, one can easily extract the equivalent plastic strain and triaxiality histories at any point in the specimen. Two points are of particular interest for failure because they usually have the highest values of either of these parameters. These points are located at the narrowest cross section, at the center of the specimen cross-section ($x = 0$) and at the surface ($x = R$). Figure 19 shows the evolution of equivalent plastic strain vs. triaxiality at these two points for all four notch radii. Based on the largest value of displacement to failure for each notch radius obtained experimentally, the combinations of equivalent plastic strain and triaxiality at failure have been identified with open circles at each of the two locations of interest. The two solid symbols correspond to the combinations at failure calculated for the smooth-specimen tension tests WG-16 and WG-18 at $x = 0$, which was the most critical location in the uniaxial tension tests.

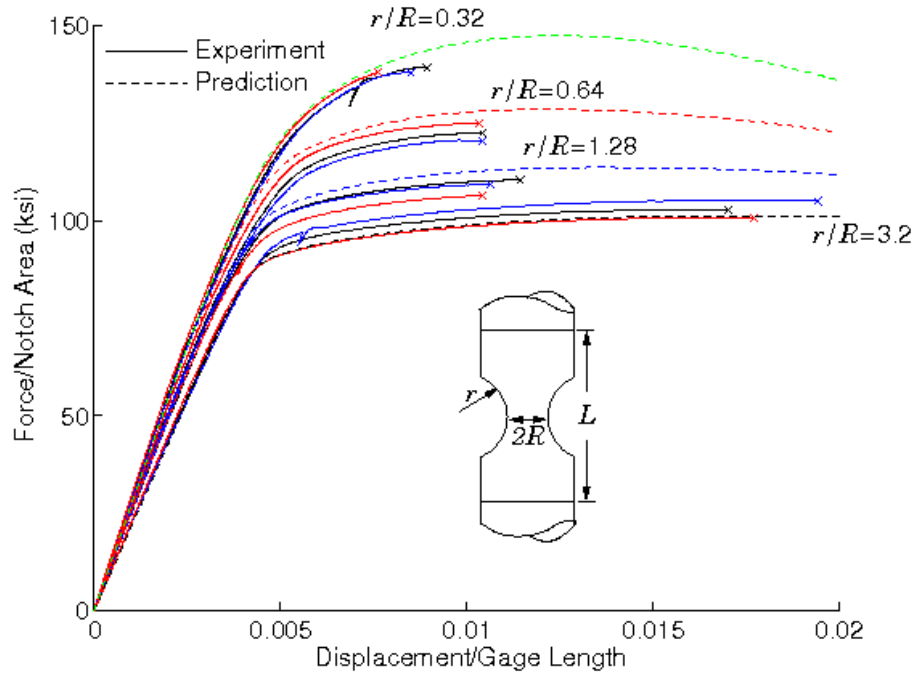


Figure 18: Comparison of experimentally measured force-displacement responses to the results of numerical predictions.

Several trends can be observed from Figure 19:

- Triaxiality does not remain constant neither at $x = R$ nor $x = 0$, although it does tend to remain close to constant as the loading progresses and the material goes further into the plastic range.
- As expected, the triaxiality at $x = 0$ is always larger than at $x = R$. The sharper the notch, the higher the triaxiality at both locations. The increase at $x = 0$ is much more pronounced because the tensile radial stresses induced by the notch are highest there. The rate at which the equivalent plastic strain accumulates, increases faster at $x = R$ for specimens with $r/R = 1.28$, 0.64 and 0.32 due to the stress concentration induced by the notch, which becomes more severe as the notch radius decreases. Only the case with $r/R = 3.2$ (least severe notch) shows a faster growth of equivalent plastic strain at $x = 0$.
- Assessing whether the most critical point as far as failure is concerned is at $x = 0$, $x = R$ or somewhere in between requires further thought. An expectation is that the equivalent strain at failure decreases with increasing triaxiality. Based on this, the most critical location for the case with $r/R = 3.2$ is at $x = 0$ because the equivalent plastic strain at failure is higher for the point with higher triaxiality. Further evidence for this can be seen from Figure 20, which plots the variation of triaxiality and equivalent plastic strain as functions of radial position (x) at a time just prior to where failure was detected in the tests. Indeed, for the case with $r/R = 3.2$ both the largest triaxiality and equivalent plastic strain occur at $x = 0$. Similar arguments based on the data shown in Figure 19 and Figure 20 could be made for the case with $r/R = 1.28$. Here, the most critical point is at $x = 0$ as well since the equivalent plastic strain at failure is relatively constant along the radius but the triaxiality is highest at $x = 0$. For the cases with $r/R = 0.64$ and 0.32 , the character of the distribution

of equivalent plastic strain shown in Figure 20(b) changes, and the highest values now occur at $x = R$, where the triaxiality is lowest. For the case with $r/R = 0.64$ it is possible for failure to start at either $x = 0$ or 1. For the case with $r/R = 0.32$, the equivalent plastic strain at $x = 0$ is so small that it seems unlikely that that location is where failure would start. In summary, the following points have been eliminated from consideration as failure points: $x = R$ for $r/R = 3.2$ and 1.28 as well as $x = 0$ for $r/R = 0.32$.

Two more points, shown as magenta circles, are shown in Figure 19. They correspond to the equivalent plastic strain and triaxiality combinations at failure for the two uniaxial tension test specimens shown in Figure 16. The values of triaxiality are in the vicinity of 1/3, thus reflecting the relatively small neck that developed in these tests.

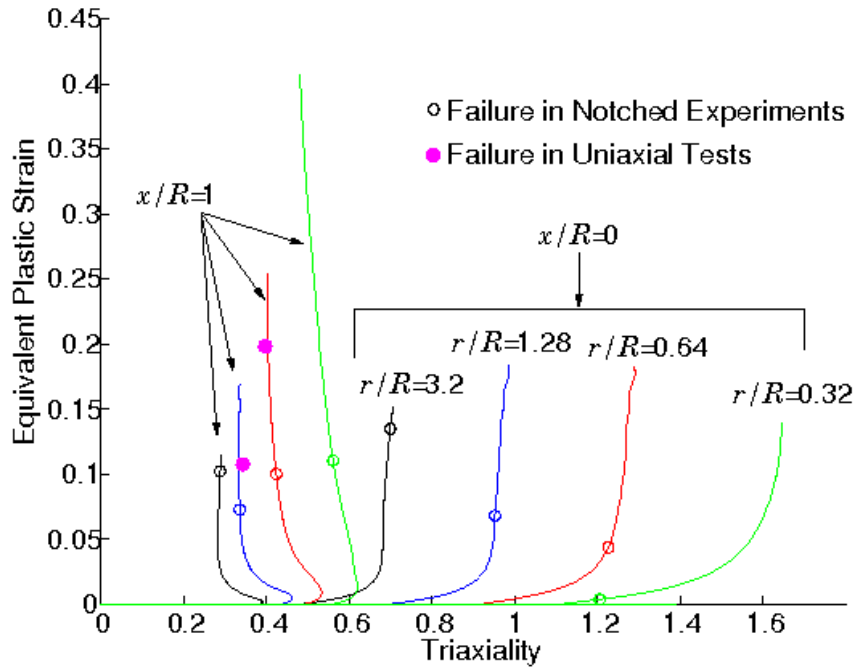


Figure 19: Evolution of equivalent plastic strain vs. triaxiality in the tension tests on notched specimens. The hollow circles correspond to the failure point observed in the experiments. The filled circles represent the failure point in two uniaxial tension tests.

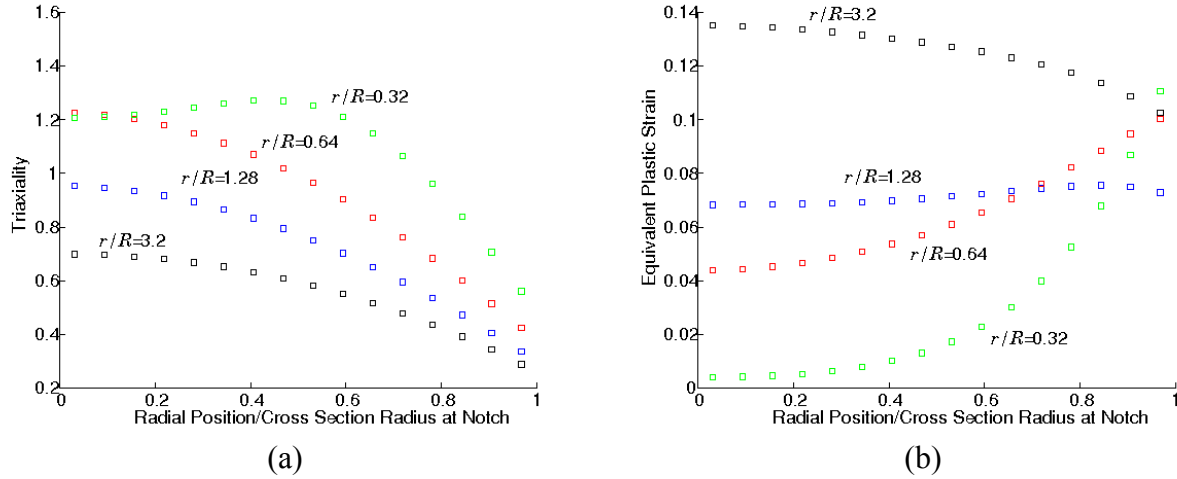


Figure 20: Variation of triaxiality and equivalent plastic strain with radial position at the notch at the point at which failure was observed in the experiments. (a) Triaxiality and (b) equivalent plastic strain.

Although the triaxiality clearly does not remain constant as the notched specimens are pulled, an attempt will be made to fit the dependence of the equivalent plastic strain at failure on triaxiality by picking the parameters (d_1, d_2, d_3) in (3) such that we obtain curves that fit the failure data as plotted in Figure 19. Three such fits are shown in Figure 21, and they represent low, medium and high estimates of the equivalent plastic strain at failure. The fit parameters are shown in Table 2. As a final check, the low and high fits were used in the finite element analysis of the notched tension tests to investigate where the failure points would fall. In this analysis, the Johnson-Cook strength model and the explicit version of Abaqus were used. Since the tests were quasi-static, but the analysis time was much faster, the heat generation and strain rate dependences of the strength and failure models were removed for these calculations.

Figure 22 shows the results in the form of load-deflection responses. The first point to notice is that the response curves are very similar to those in Figure 18 obtained using a quasi-static analysis with a piece-wise linear fit of the true stress-strain curve. The second is that the predicted displacements at failure by the low and high failure estimates, which are identified by precipitous drops in load, bracket the experimental data in most cases. The exception is the case with $r/R = 3.2$, for which the high estimate predicts the displacement at failure well, and the low estimate gives a much lower displacement at failure. Based on the results shown, it appears that the high estimate in Figure 19 should be preferred.

One very important item to note is the fact that all the failure data available lie in the range of triaxiality larger than $1/3$. In the impact tests described earlier, it seems likely that the crack that first appears opposite to the impact point developed in this triaxiality range. The fact that a plug is ejected, however, suggests a shear dominated failure event with triaxiality likely in the vicinity of zero, and perhaps even somewhat negative. The proposed fits, however, are extrapolations in this regime. Therefore, it is highly desirable to develop shear-dominant tests to allow for more accurate fits and reduce the uncertainty in the prediction of the failure threshold velocity of the plates.

Table 2: Johnson-Cook parameters for the three estimates of the dependence of the equivalent plastic strain at failure on triaxiality shown in Figure 21.

	Low	Medium	High
d_1	0.025	0.015	0.005
d_2	0.15	0.24	0.34
d_3	-1.5	-1.5	-1.5

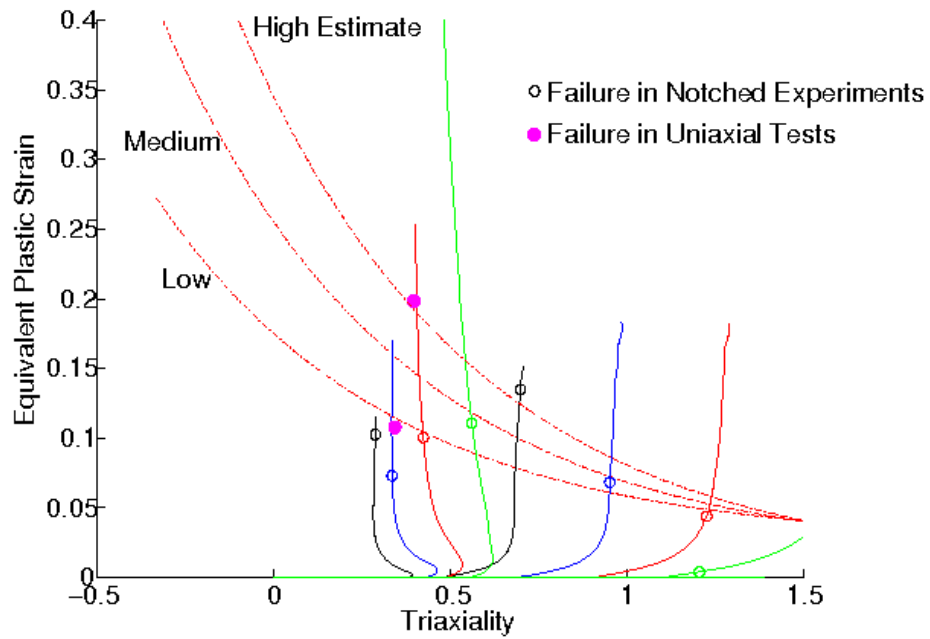


Figure 21: Three estimates of the dependence of the equivalent plastic strain at failure used with the Johnson-Cook failure model.

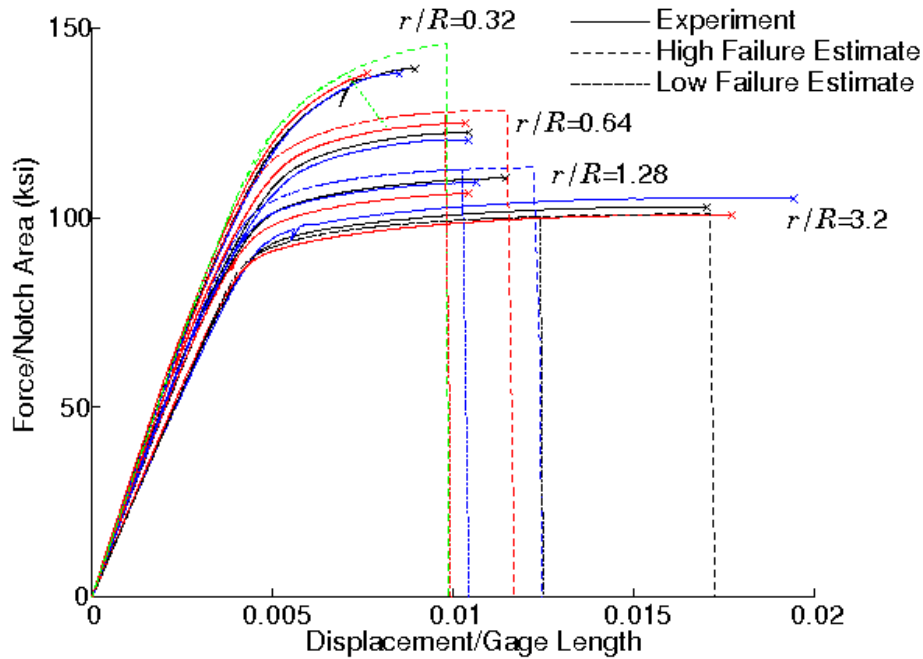


Figure 22: Comparison of the load-deflection responses for the tension tests on notched specimens predicted using the low and high estimates for failure. The experimentally obtained curves are included for comparison.

5.2.3 High Temperature Tests

The test data presented in Figure 14 were used to calibrate the parameters m and d_5 in Equations (1) and (3) respectively. The calibration of m was attempted first. Recall that the specimens for the thermal testing came from another batch of material, different from the ones calibrated previously. Therefore, values for the parameters A , B and n in Equation (1) had to be determined for these specimens. The values found were 440 MPa, 400 MPa, and 0.3 respectively. The value of m was picked to match the dependence of the ultimate stress on temperature observed in the test as well as possible in an attempt to match the overall flow stress levels in the experimental curves. This was followed by the calibration of d_5 to match the prediction of the engineering strain at failure. The values determined for these two parameters were 1.1 and 8.

Since the dependences of the Johnson-Cook strength and failure models on temperature are governed by relatively simple relations, an exact match to the experimentally observed trends was not possible as demonstrated in Figure 23. The figure shows comparisons only up to 200°C, which will be shown later to be about the upper temperature limit of interest in the plate finite element model. Note that the shape of the predicted curves is reasonably similar for all temperatures shown. This was not true in the tests. As was mentioned previously, the test data shows a significant decrease in the strain at the ultimate stress, and this is not captured by the model. The trend in the experiments is as if the parameter n in Equation (1) decreases with temperature as well, but the model does not allow this. The model predicts the strain to failure reasonably well in the temperature range shown in the figure. In spite of the shortcomings

shown, the model does provide a first order representation of the effect of temperature on the material response and failure and will be used in subsequent calculations.

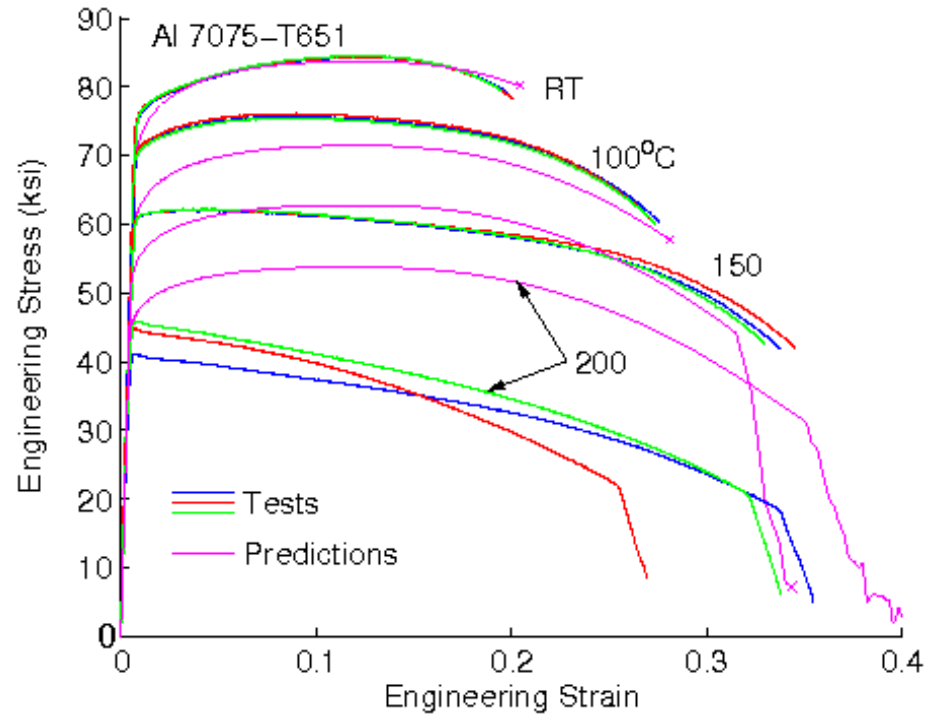


Figure 23: Comparison of experimentally measured engineering stress-strain curves at several temperatures with the results of numerical predictions.

5.3.4 High Strain Rate Tests

After determining the dependence of the Johnson-Cook model parameters on plastic strain, triaxiality and temperature, the high strain rate uniaxial tension tests provide the means to calibrate the strain rate dependence of the model. Ideally tests should be conducted at several strain rates to assess the quality of the fit. At the present, data at only two strain rates are available, approximately 0.00016 1/s and 1250-1400 1/s. The curves in Figure 12 indicate trends where increasing the strain rate results in higher flow stress. Note that just as in the quasi-static tests in Figure 9 and Figure 10, the high strain rate tests also displayed a range of strain-to-failure values. In order to calibrate the constant C in the strength model, a first estimate was made based on the ratio of the flow stresses in the quasi-static and dynamic tests. Subsequently, finite element simulations of the dynamic tests were conducted to adjust the value of C to obtain the best match between predictions and test results for the engineering stress-strain curves as shown in Figure 24, giving a value of $C = 0.0075$. The figure includes the true stress-strain curves for both strain rates for reference.

The circles plotted on the true stress-strain curves correspond to the estimated failure strains seen for the specimens that displayed the most ductile behavior in their respective groups. Based on the ratio of these strains, the value of the failure parameter accounting for the effects of strain rate was estimated. Subsequently, matching the results of measurements and numerical simulations of the high strain rate tests yielded a final estimate of $d_4 = -0.039$

that was adopted for the rest of the work. We note that the value of d_4 that was calibrated by Brar et al (2009) for Al 7075-T651 was positive (0.016), indicating an increase in ductility with strain rate and therefore opposite to what was measured from the samples used here.

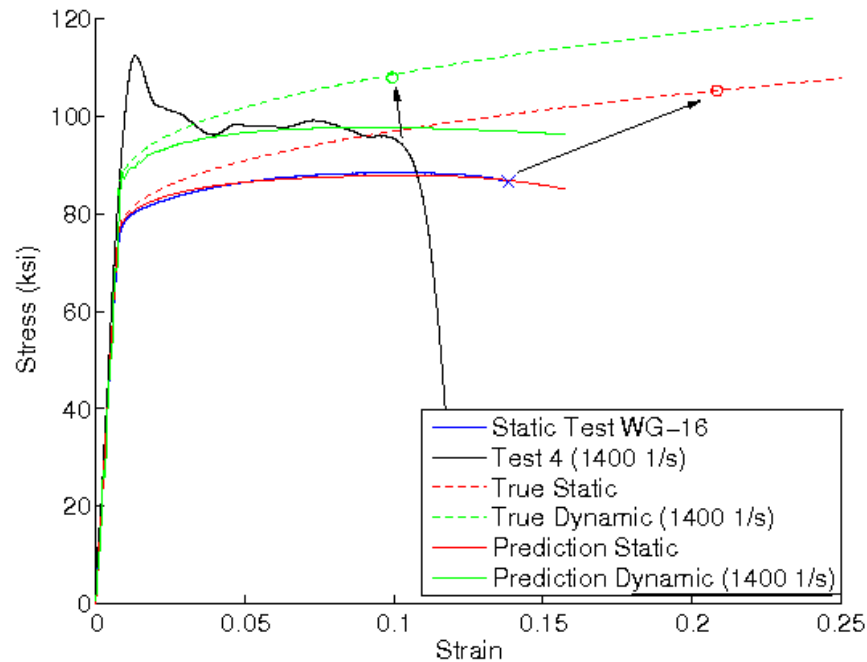


Figure 24: Comparisons of measured and predicted quasi-static and dynamic uniaxial stress-strain curves.

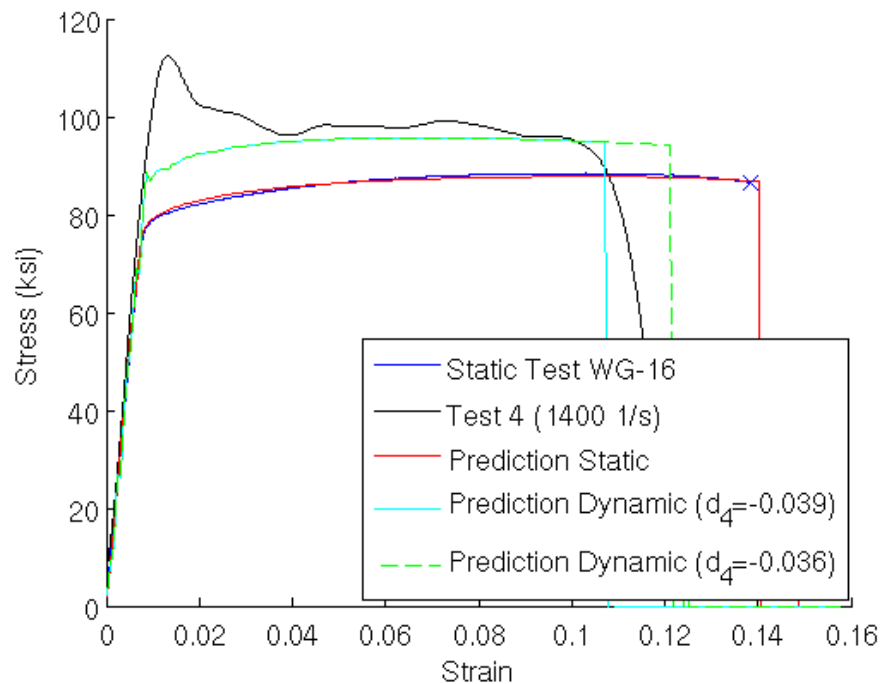


Figure 25: Comparison of predicted engineering stress-strain curves including failure for the tests in Figure 24 comparing quasi-static and high strain rate cases.

To conclude the comparison between measured and predicted material responses, Figure 25 shows the quasi-static and dynamic stress-strain curves as measured and as predicted, including failure, with the fully calibrated model ($d_4 = -0.039$). The predictions reproduce the measured response, including failure reasonably well. An earlier calibration with $d_4 = -0.036$ that was used to generate many of the results that will be shown later is also included for comparison. The effect of this small change in the value of d_4 in the plate puncture predictions will be demonstrated to be negligible.

A summary of all the parameters that are included in the model is shown in Table 3.

Table 3: Material property parameter values determined from the calibration exercises.

Strength Model Parameters				
A , MPa (ksi)	B , MPa (ksi)	n	C	m
517 (75.0)	405 (58.7)	0.41	0.0075	1.1
Failure Model Parameters				
d_1	d_2	d_3	d_4	d_5
See Table 2	See Table 2	-1.5	-0.039	8.0
Elastic Parameters				
E , GPa (ksi)	ν			
71.7 (10.4x10 ³)	0.33			
Thermal Parameters				
c_p , J/(kg-K) (lb-in/(slug-ft/in °R))	T_m , K (°R)	T_r , K (°R)	β	
960 (827x10 ³)	750 (1350)	293 (527)	0.95	
Other Parameters				
$\dot{\epsilon}_e^p$	ρ , kg/m ³ (slug-ft/in/in ³)			
0.00016	2810 (2.63x10 ⁻⁴)			

6 PUNCTURE PREDICTIONS

Once the material model has been calibrated and its parameters determined (see Table 3), predictions of the response and failure of the plates can be conducted and compared against the experimental results. Figure 26 shows the finite element model of the plate impact problem. It consists of the plate, shown in red, the punch in yellow and a block representing the mass of the carriage in the experiments that is shown in green. A single plane of symmetry is taken in the model, so only one-half of the plate, punch and mass need to be modeled. Although the initial geometry is axisymmetric, the experiments showed that the failure was not. Figure 3 shows that the first expression of material failure was a crack mostly aligned with the rolling direction. Although the material model is isotropic, the mesh lines in the model break the axisymmetry condition and allow discrete cracks to appear in the model.

In the tests, the plate was clamped around the periphery over a flange with a circular opening with diameter of 6.75 in. This is represented in the model of the plate by a fully clamped condition on the outer cylindrical surface located at 3.875 in. from the center as well as an adjacent 0.5 in. wide band where the lower surface of the plate is restricted from downward displacement. Loading is applied by prescribing an initial downward mass/punch velocity v_0 just prior to impact. In addition, gravity and a point load in the mass to represent the bungee forces, which were estimated to be 200 lb for the half model, are also prescribed.

The elements used in the analysis were of the type C3D8R which are 8-node linear hexahedral bricks with reduced integration and hourglass control. In the first set of calculations to be conducted below, the element size in the impact region was 0.02 in. on each side. Meshes with this element size will be referred as “fine” meshes in the remainder of this report. Later the effect of element size will be assessed by considering “coarse” meshes with element size of 0.04 in. and “very fine” meshes with element size of 0.01 in. In all cases, the element size increases radially outside the impact zone to reduce the number of degrees of freedom in the models.

One aspect of the failure model that has not been discussed so far needs to be mentioned prior to showing the results. It concerns how material failure is implemented in the model once the failure criterion has been satisfied. Two processes need to be addressed. The first concerns how the load capacity of points where the failure criterion has been satisfied is eliminated while the second concerns how the geometry of the model is modified to reflect failure and emulate tearing.

Regarding the first process, it is important to first clarify that no material degradation occurs prior to the time at which the failure criterion is satisfied. Once it is, however, the stress can be reduced to zero over some further straining to at least partially represent the energy dissipated by the propagation of fracture. In this work, however, no data is available to calculate the amount of energy dissipated during fracture. As a result, this aspect of the problem will be treated as a parameter whose influence can be studied via sensitivity analyses. Initially, it will be assumed that the stress in the material drops to zero immediately upon satisfying the failure criterion. The sensitivity of the numerical simulations to the amount of energy dissipation will be addressed in a latter section.

A few techniques have been developed to address the second process, which involves updating the finite element discretization of the problem geometry to represent the fracture process. In this work the element deletion (or element death) approach will be used. In this approach, elements where the material has failed and the stress has decayed to zero are removed from the model to account for tearing.

A final aspect of the problem that needs to be prescribed concerns the friction between the punch and the plate. Here, a simple Coulomb friction model is used. This model has a single parameter: the coefficient of friction (μ). The value of μ depends on a variety of factors such as the materials involved, their surface finish, lubrication conditions, etc. The Engineering Handbook (Dorf, 1995) lists a value for the dry kinetic coefficient of friction of 0.47 for aluminum on mild steel. In the current work a value of 0.4 was adopted for most of the simulations, and their sensitivity to the value chosen will be the subject of a latter section.

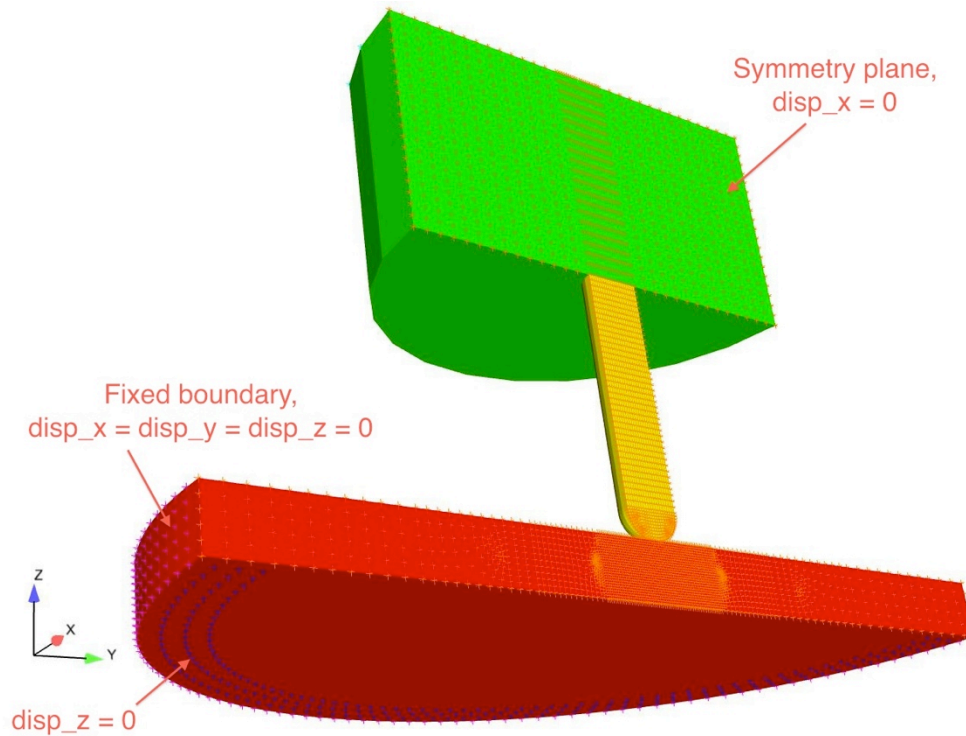


Figure 26: Model used in the finite element calculations of the plate puncture problem showing the boundary conditions used.

6.1 Baseline Predictions

A baseline case was chosen to obtain a first estimate of the puncture threshold velocity of the plates using the parameters listed in Table 3, the “high” parameters in Table 2, the geometry described above and the fine mesh. The choice to use the fine mesh was made to agree with the baseline case used in Corona et al (2012).

The results are shown in Table 4 and indicate that the predicted threshold velocity is between 10.4 and 10.5 ft/s, which closely agrees with the experimental results presented in Figure 3.

The following sections present the dependence of the results on different choices of model parameters. Note that all results presented in those sections were obtained with a value of the failure parameter $d_4 = -0.036$ obtained with an older calibration instead of $d_4 = -0.039$ as listed in Table 3. It will be shown that this discrepancy has a very minor effect on the results, and as a result should not affect the conclusions that will be drawn in the remainder of this report.

Table 4: Baseline predictions for plate puncture ($d_4 = -0.039$). “Y” indicates failure while “N” indicates no failure.

v_o , ft/s	Puncture Prediction
10.30	N
10.35	N
10.40	N
10.45	Y
10.50	Y
10.55	Y
10.60	Y

6.2 Evaluation of Dependence of Plastic Strain to Failure on Triaxiality.

The first exercise was to assess the choice of the parameters that dictate the dependence of the equivalent plastic strain to failure on triaxiality. The three fits described in Table 2 were used for this purpose and the results are shown in Table 5. To make this table, calculations were conducted in impact velocity increments of 0.5 ft/s. Recall that the best estimate of the threshold velocity from the test data was 10 ft/s.

The threshold velocity when using the “low” fit is between 6.5 and 7.0 ft/s and therefore too low compared with the tests. In addition, the mode of failure predicted is somewhat different from what was seen in the tests as can be seen in Figure 27(a). In the simulations, the plug is relatively thin, and many plate elements that came in contact with the punch were deleted. Using the “medium” fit gave a threshold velocity between 9.5 and 10 ft/s, which is definitely in the range of the experiments. The mode of failure shown in Figure 27(b) involved a plug and some scabbing in the lower surface of the plate as can be seen (the scab is the lowest fragment while the plug is still next to the punch in the figure). This scabbing is similar to what was observed experimentally, but that is thought to be a coincidence. We believe that scabbing is strongly dependent on the ductility of the material when pulled through the thickness, but it was not measured. The “high” estimate gave a threshold velocity between 10.5 and 11 ft/s, which is at the upper range of the experimental results. The mode of failure is shown in Figure 27(c) and shows a well-formed plug, but no scabbing. Therefore, both the “medium” and “high” property estimates produce results that are close to experimental observations. The “high” estimate was chosen to conduct further studies that will be presented next.

Table 5: Plate failure results for the three estimates of the dependence of material failure on triaxiality. “Y” indicates failure while “N” indicates no failure.

v_o , ft/s	Low	Medium	High
6	N		
6.5	N		
7	Y	N	
7.5	Y		
8	Y	N	
8.5	Y		N
9	Y	N	N
9.5	Y	N	N
10	Y	Y	N
10.5		Y	N
11		Y	Y
11.5		Y	Y
12			Y

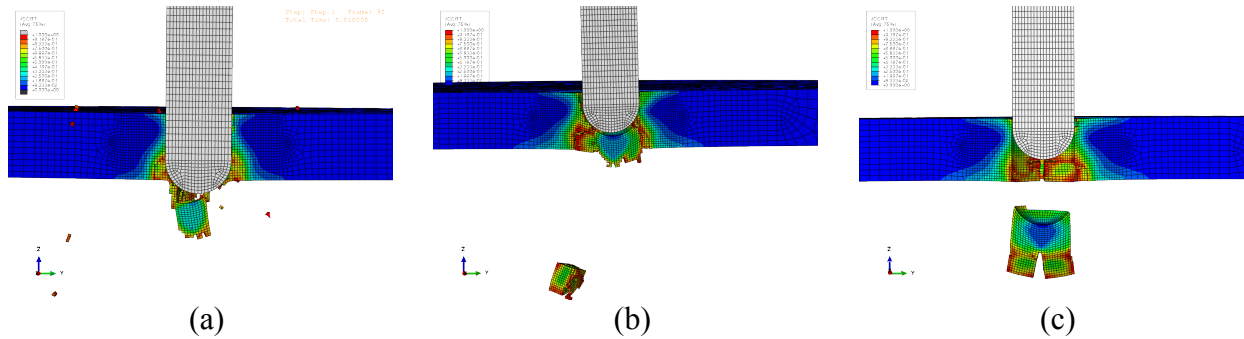


Figure 27: Effect of the equivalent plastic strain at failure vs. triaxiality curves on the shape of the plugs. (a) low fit, (b) medium fit, and (c) high fit. Contours of the Johnson-Cook damage parameter $\bar{D}$ are shown. The snapshot times are not equal for the three cases shown.

6.3 Effect of Element Size

After choosing a fit for the dependence of plastic strain to failure on triaxiality, the next step was to investigate the effect of element size on the predicted threshold velocity. Here, the increment in impact velocity between neighboring cases was decreased to 0.1 ft/s. The results for the three element sizes discussed previously are shown in Table 6. Changing from the “fine” mesh to the “coarse” mesh resulted in an increase in the threshold velocity, from 10.45-10.55 ft/s to 11.25-11.35 ft/s. Interestingly, changing from the “fine” to the “very fine” mesh also increased the threshold by about the same amount. Although some of the details of the geometry of the failure region were different between the three cases, all generated a plug as shown in Figure 28. Note however, that the case with the very fine mesh also produced a scab. The CPU times to run the analyses for 10 ms simulation time were in the vicinity of 15 minutes for the “coarse” mesh, 1.5 hours for the “fine” mesh and 12 hours for the very fine mesh, all using eight processors. Although the change of threshold velocity with element size was not monotonic, the

difference between the three meshes was about 8%, which is relatively mild. In other words, all three meshes gave results that were within 10% of the threshold velocity measured experimentally. In order to keep the CPU time required for the simulations at a reasonable level, the “fine” mesh was chosen to continue further work in this problem.

At this point it is useful to compare the threshold velocity obtained here with the fine mesh, between 10.45 and 10.55 ft/s, to that of the baseline case in Table 4, which was between 10.40 and 10.45 ft/s. Recall that the only difference between these predictions is the value of d_4 , 0.036 vs. 0.039. Clearly the small difference in this parameter made less than one percent difference in the estimation of the threshold velocity.

Table 6: Dependence of plate failure on the element size. The coarse mesh has elements 0.04 in. on the side, the fine mesh has elements 0.02 in. on the side and the very fine mesh has elements 0.01 in. on the side.

v_o , ft/s	Coarse	Fine	Very Fine
10.35		N	
10.45		N	
10.55		Y	
10.65		Y	
10.75		Y	
10.85	N	Y	N
10.95	N	Y	N
11.05	N	Y	N
11.15	N	Y	N
11.25	N		N
11.35	Y		N
11.45	Y		Y
11.55	Y		Y
11.65	Y		Y

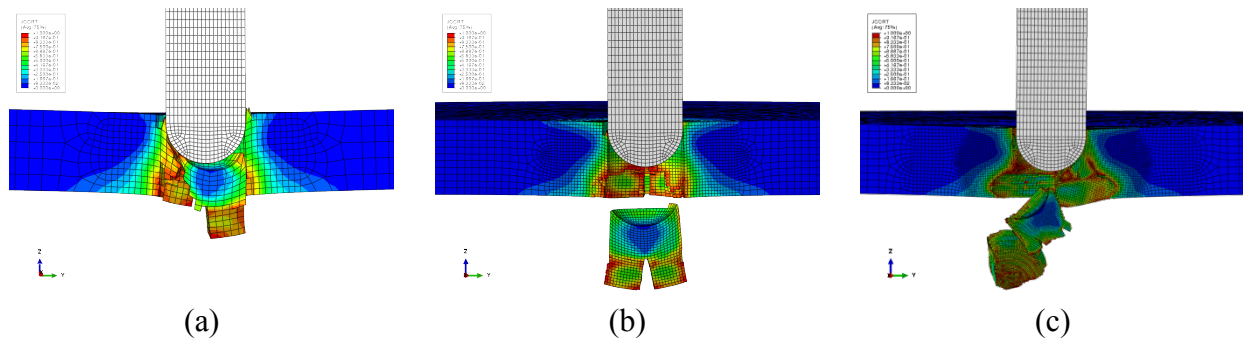


Figure 28: Mode of failure obtained for three different meshes showing the ejection of a plug from the impact region. (a) Coarse mesh, (b) fine mesh and (c) very fine mesh. Contours of Johnson-Cook damage parameter are shown. The snapshot times are not equal for the three cases shown.

6.4 Description of Predicted Response and Failure

Figure 29 through Figure 33 present contour plots of several quantities of interest at five times (1, 2, 3, 3.15 and 3.2 ms after the onset of impact) when the impact velocity was 11 ft/s. These are: the Johnson Cook parameter $\bar{D}$ in Figure 29, the equivalent plastic strain in Figure 30, the triaxiality in Figure 31, the temperature in Figure 32 and the equivalent plastic strain rate in Figure 33. The punch was removed from the visualization to show the contours on the contact surface. The fine mesh and the “high” failure properties are used. Together, these help show the physics of failure that the finite element model predicts to be at work in the plate puncture problem. The following observations can be made:

- At most times, the equivalent plastic strain is highest in the vicinity of the contact between the plate and the punch. It reaches values of one at some locations, or more than four times the equivalent plastic strain at failure in the uniaxial tension tests. The triaxiality in the same region, however, is strongly negative with values significantly lower than -1. The Johnson-Cook model predicts failure at much higher equivalent plastic strains at those triaxiality levels, and hence failure does not occur under the punch. This is also reflected by the values of $\bar{D}$, which are not highest under the punch.
- At 1 ms, prior to any failure occurring, $\bar{D}$ is highest at the lower surface of the plate. Even though the equivalent plastic strain is not very high there, the triaxiality is in the order of 2/3 thus causing the value of $\bar{D}$ to rise relatively quickly. Indeed, by 2 ms two perpendicular cracks are forming on the lower surface under the impact point. This is somewhat similar to what was seen in the tests where the first sign of failure was a crack on this surface. In the tests the crack was mostly oriented along the rolling direction of the plate. This suggests that the crack direction is likely determined by plastic anisotropy, which was not accounted for in the model. Therefore, the crack in the model propagates along perpendicular mesh lines.
- The scabbing seen in the tests was not predicted by this model. Scabbing was observed with the fine mesh when the “medium” failure properties were used and with the very fine mesh and the “high” failure properties. It is likely, however, that the scabbing is sensitive to the value of strain at failure of the material when pulled in the through-thickness direction, which was not measured. Therefore, not much significance can be attached to the prediction of scabbing by the model.
- Temperature increases are essentially restricted to the region that will become the plug. The temperature increase can be as much as 200K in some regions, but is in the order of 50 to 120K over most of the plug. The predicted temperature rise is consistent with the experimentalists’ observations that the ejected plugs were too hot to touch right after the tests.
- Figure 29 shows most clearly that the plug is predicted to form by failure propagating upwards from the lower surface of the plate, as was observed in the micrographs in Figure 7. This propagation occurs through regions where the value of triaxiality is in the vicinity of zero or is somewhat negative. This indicates a shear-dominated failure. Looking at Figure 21, it should be noted that the value of the equivalent plastic strain at failure in the region around zero triaxiality is an extrapolation by the Johnson-Cook

model that is based on data obtained for triaxiality values above 1/3. Therefore a need exists to conduct low triaxiality experiments to increase the confidence that the calibration is appropriate for the problem at hand.

- The equivalent plastic strain rate contours in Figure 33 show that just prior to failure the strain rate increases significantly along bands where failure is occurring, thus indicating strain localization. As was mentioned when discussing Figure 8, however, the element side is significantly longer than the width of the localization band seen experimentally. The angle of rotation of the originally horizontal mesh lines at 3.15ms, just prior to plug separation, on the elements undergoing localization is in the order of 55° to 58° on the right side of the plug and 40° to 42° on the left side. These numbers are slightly larger than the values measured from the micrographs on the left side (up to 38°). The point could be argued that although the element size is too large to capture the localization seen in the micrographs, the plug formed in the analysis when the material rotation in the neighborhood of failure was about the same as in the experiments. Such a statement, however is made with great reservation because it is just an observation and can not be proven.
- Finally, note that the response of the plate is not symmetric about the centerline of the punch. The punch, which is modeled as a linearly elastic body with properties of steel can deflect in the plane shown and induce asymmetries in the plate behavior. In fact, Figure 33(d) shows that the shear strain localization is more pronounced on the right side of the model.

Figure 34(a) shows examples of the velocity histories of the mass block in the model during impact for cases where penetration occurred (red line) and did not occur (blue line). The black lines represent the same data but passed through a low-pass filter with a cut-off frequency of 500 Hz. Figure 34(b) shows the corresponding acceleration plots obtained by calculating the derivative with respect to time of the filtered velocity data. Both the velocity and acceleration plots closely resemble those obtained from the tests and shown in Figure 9 of Corona et al (2012). The times at which the maximum acceleration occurred and when the impact event finished match the test data very well. The predicted peak accelerations are in the order of 100g in both cases, but those in the tests were slightly smaller, at about 80g. In spite of all the approximations of the model, the predictions match the test results reasonably well.

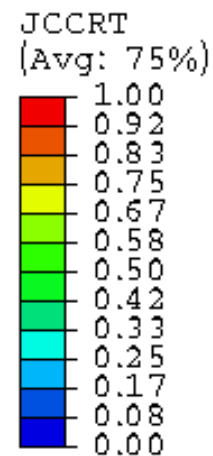
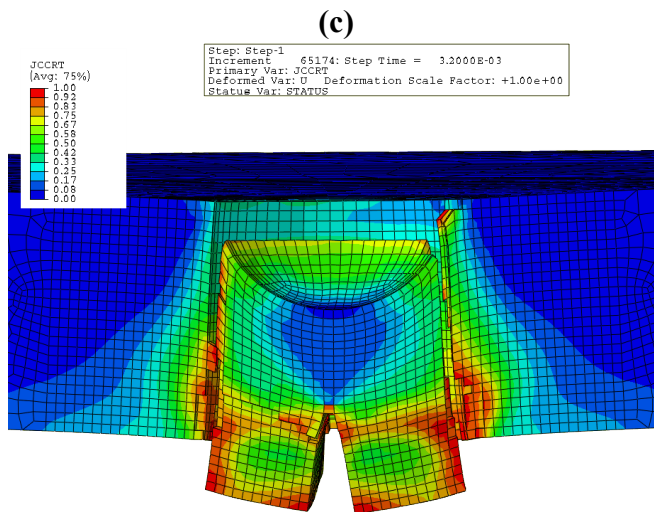
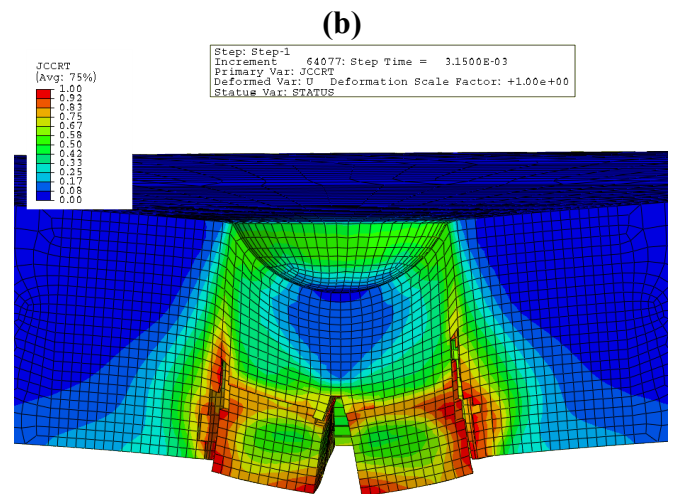
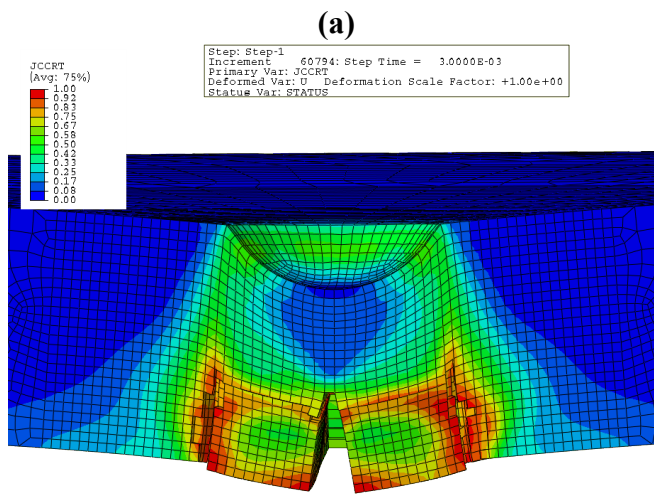
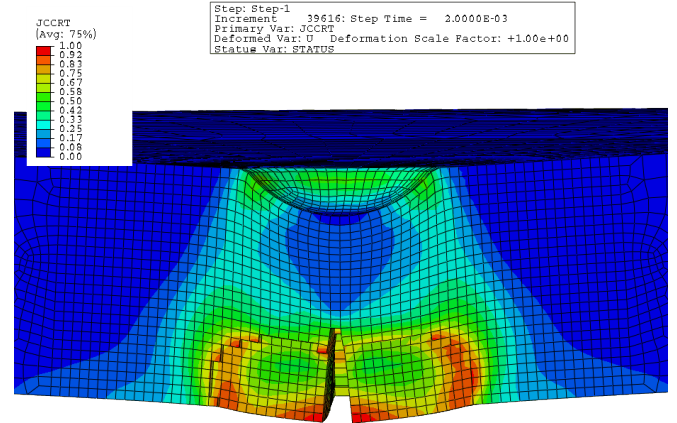
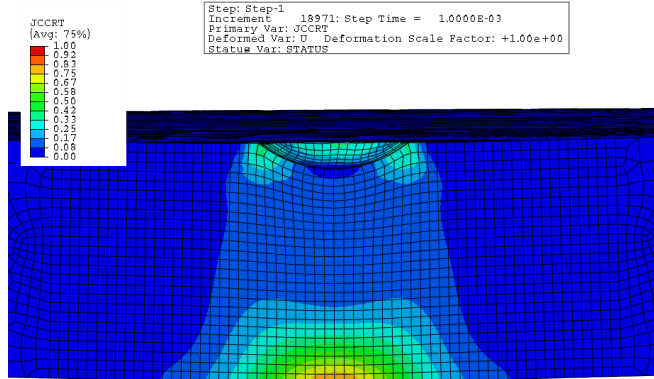
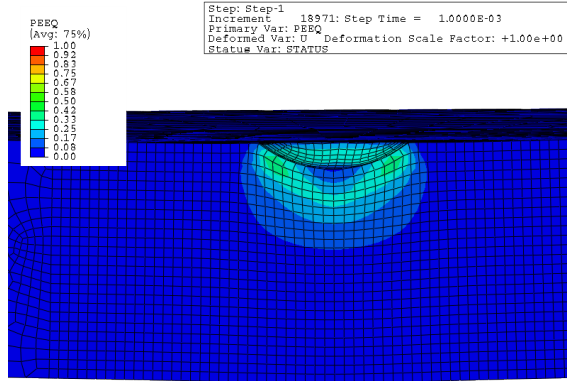
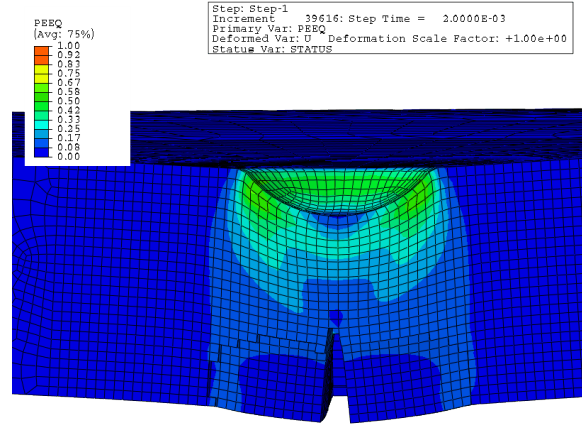


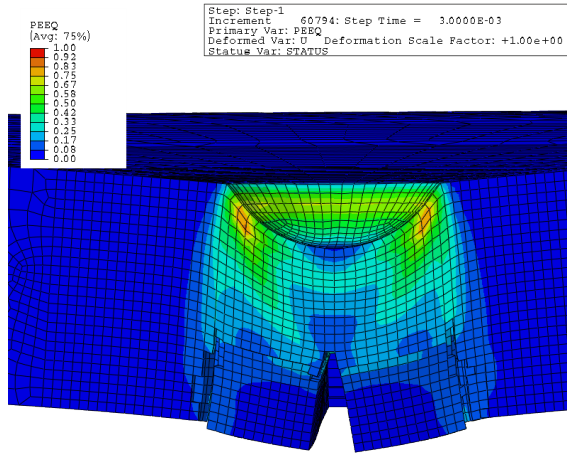
Figure 29: Contours of the Johnson-Cook damage parameter $\bar{D}$ at various times up to the formation of the plug. (a) 1 ms, (b) 2 ms, (c) 3 ms, (d) 3.15 ms, (e) 3.2 ms and (f) contour scale.



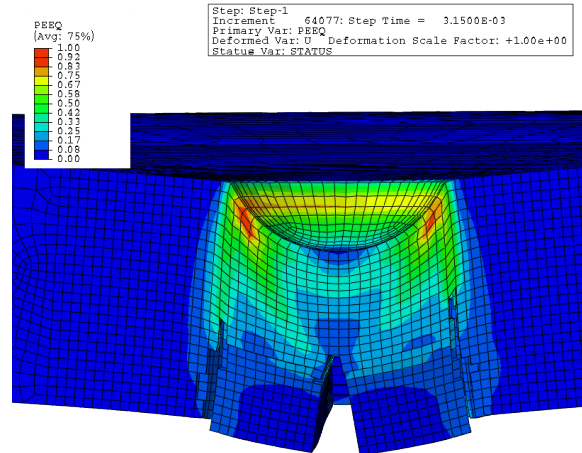
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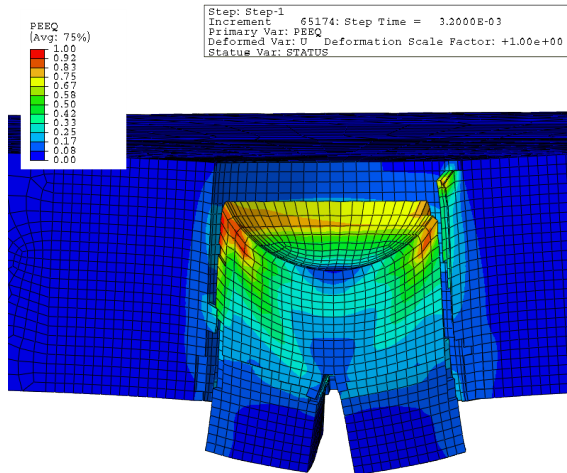
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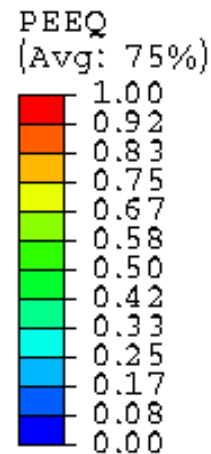
(c)



(d)



(e)



(f)

Figure 30 Contours of equivalent plastic strain ε_e^p at various times up to the formation of the plug. (a) 1 ms, (b) 2 ms, (c) 3 ms, (d) 3.15 ms, (e) 3.2 ms and (f) contour scale.

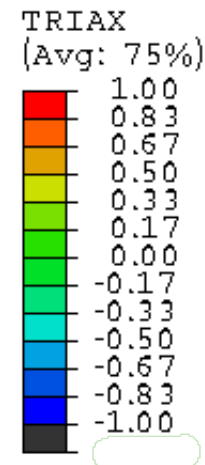
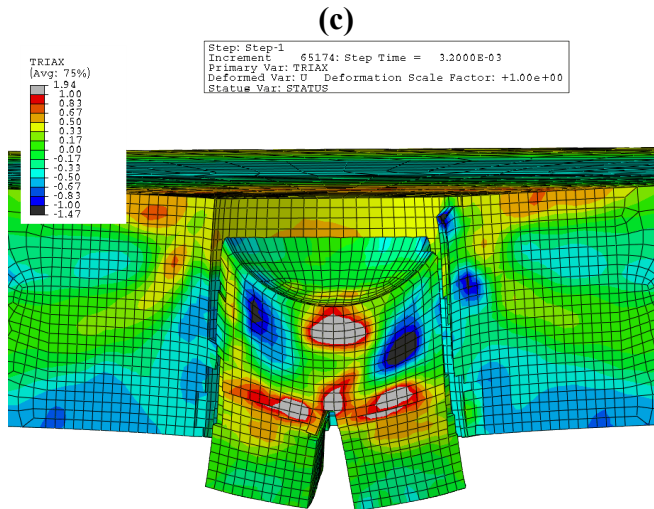
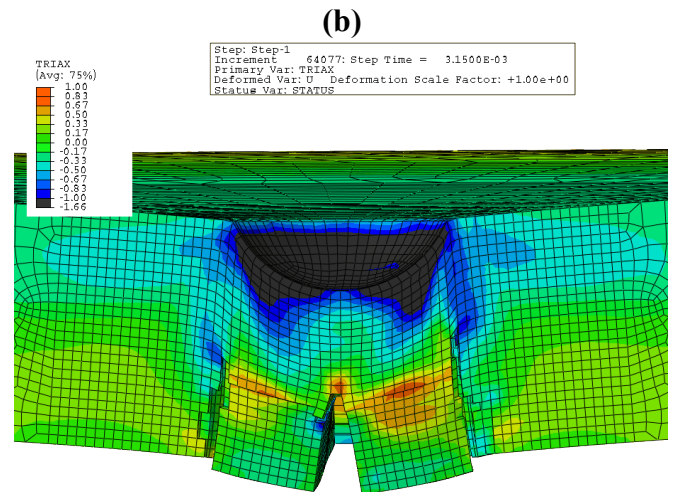
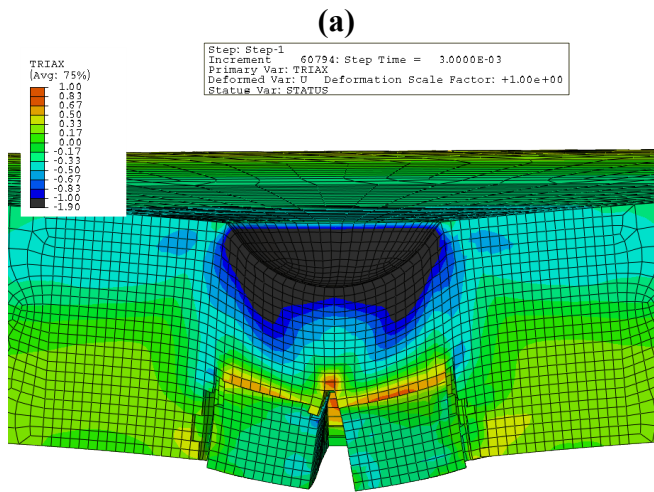
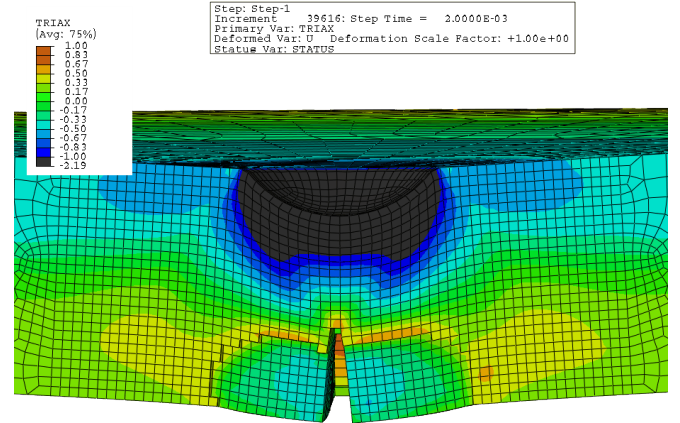
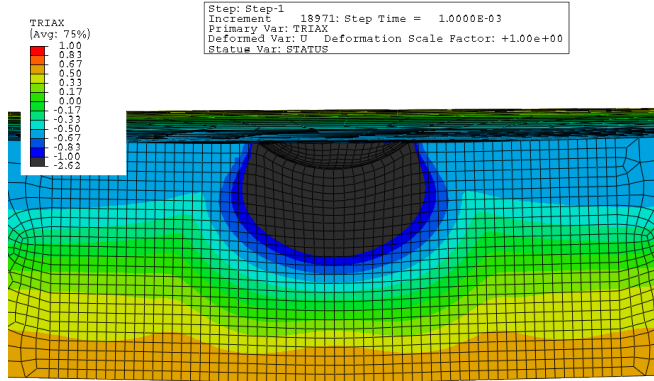
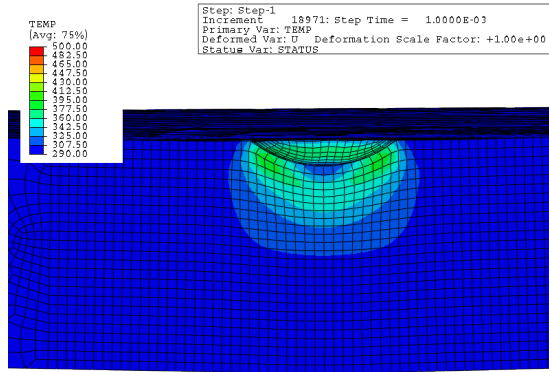
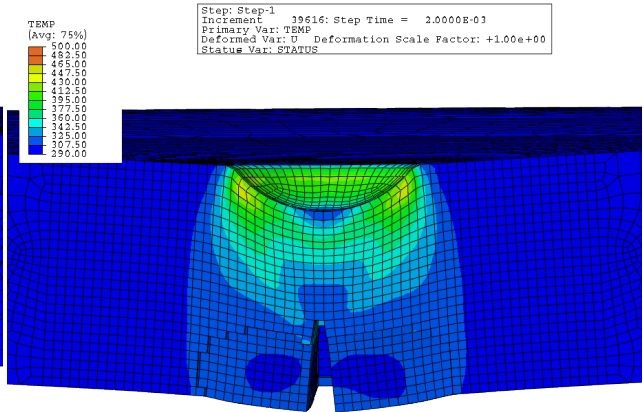


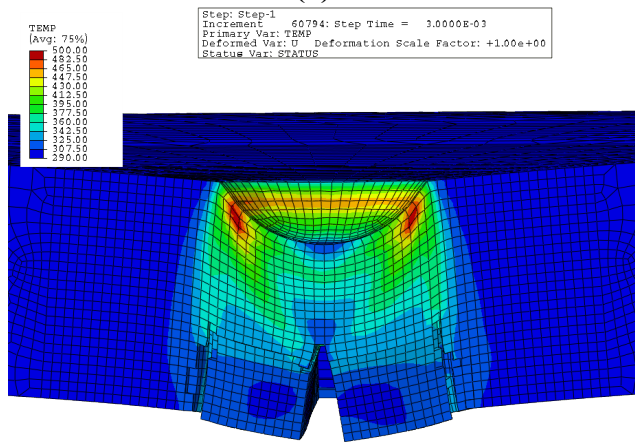
Figure 31: Contours of triaxiality η at various times up to the formation of the plug. (a) 1 ms, (b) 2 ms, (c) 3 ms, (d) 3.15 ms, (e) 3.2 ms and (f) contour scale.



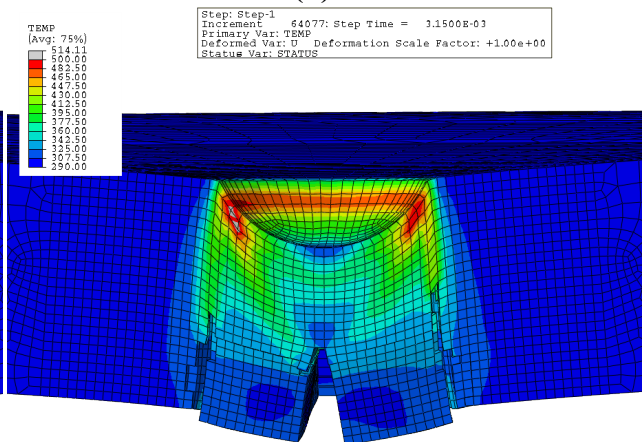
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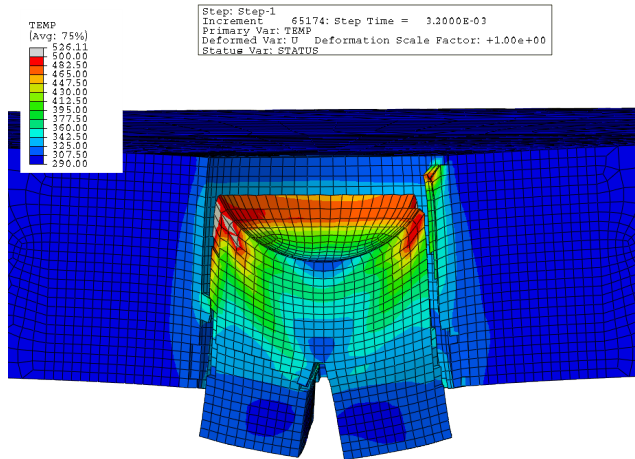
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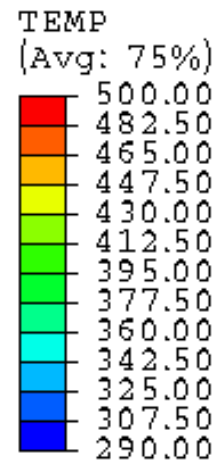
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(d)

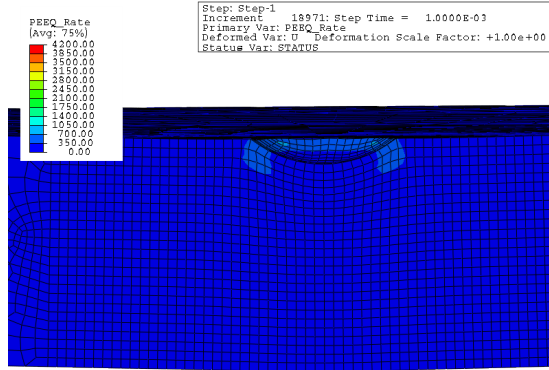


(e)

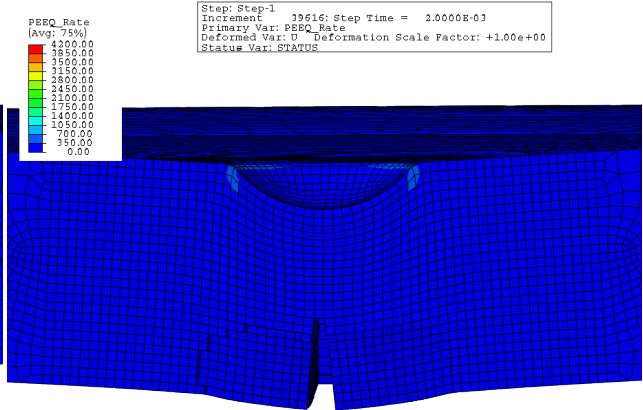


(f)

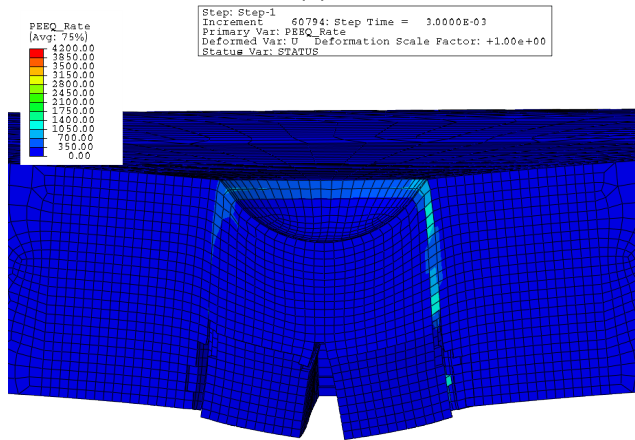
Figure 32: Contours of temperature T , in Kelvin, at various times up to the formation of the plug. (a) 1 ms, (b) 2 ms, (c) 3 ms, (d) 3.15 ms, (e) 3.2 ms and (f) contour scale.



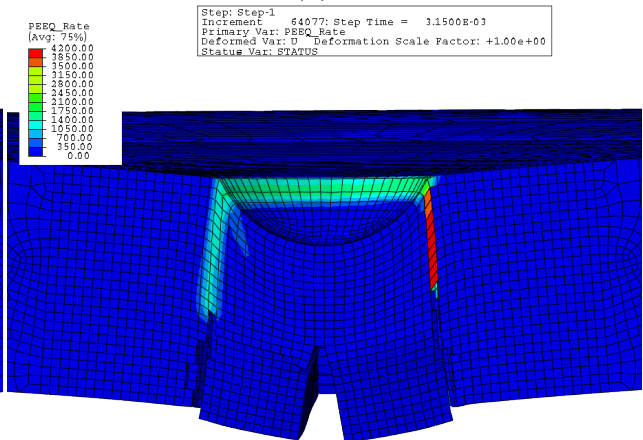
(a)



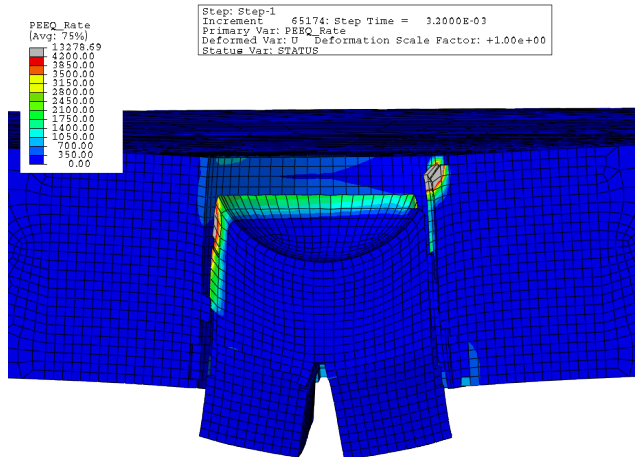
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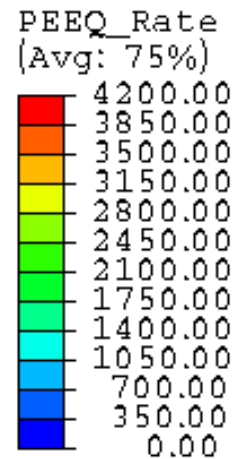
(c)



(d)



(e)



(f)

Figure 33: Contours of equivalent plastic strain rate at various times up to the formation of the plug. (a) 1 ms, (b) 2 ms, (c) 3 ms, (d) 3.15 ms, (e) 3.2 ms and (f) contour scale.

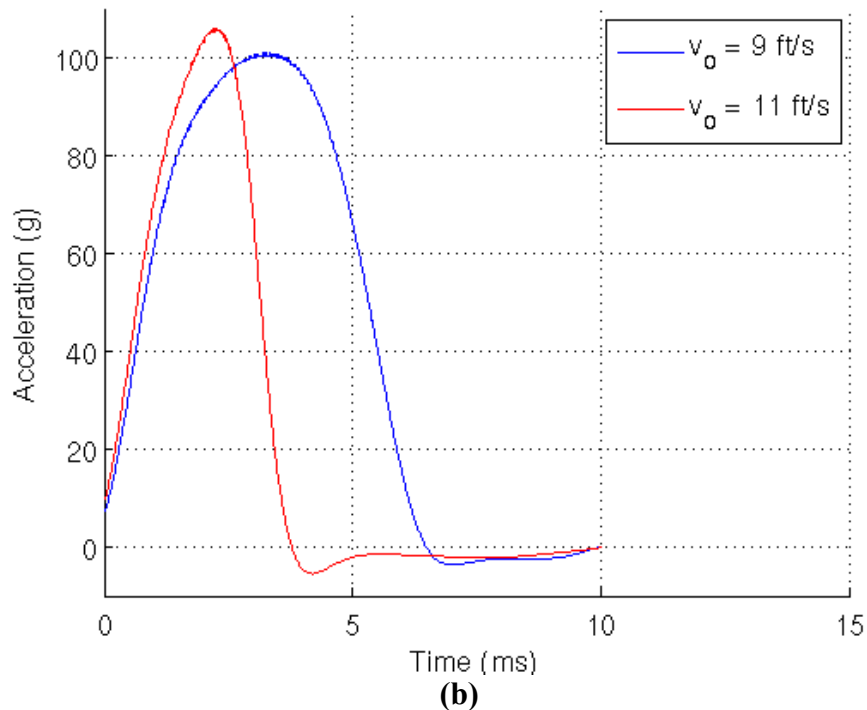
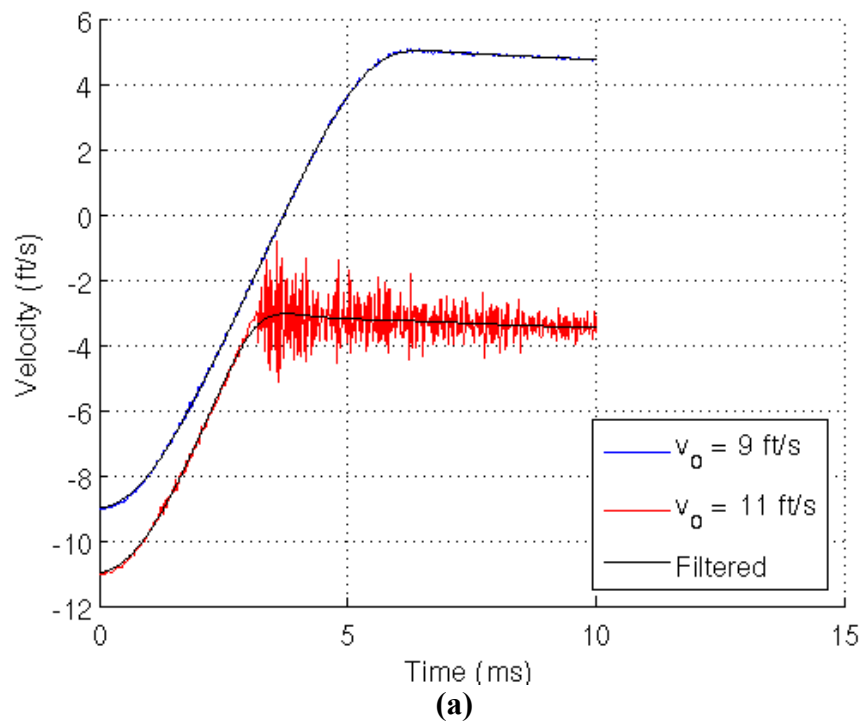


Figure 34: Examples of calculated velocity and acceleration histories experienced by the impacting mass. (a) Velocity and (b) acceleration calculated from the filtered velocity traces.

6.5 Evaluation of Thermal Effects

After assessing the performance of the model, the next important step is to evaluate the influence that adiabatic heating could have on the response and failure of the plates. Recall that adiabatic heating is the result of plastic work being converted to heat, thus locally raising the temperature of the material. Since the response of the material depends on the temperature, then one could expect that thermal effects could be important.

The evaluation was conducted in a very simple manner by setting the fraction of plastic work (β) to zero to force isothermal conditions. Then, the threshold velocity was evaluated again. The results are shown in Table 7. Failure still occurred by the formation of a plug, but the geometry of the plug was less well defined. This can be seen by comparing the shape of the deformed models in Figure 35. Note that a good number of elements next to the punch were deleted at the time the plug was formed in the isothermal case. Hence, making the simulation isothermal reduced the threshold velocity and changed the details of failure and plug formation. This is likely due to the fact that the strains to failure of the material were smaller in the isothermal case. High temperature tends to make the material more ductile, as was seen in Figure 14.

Table 7: Effect of including adiabatic heating vs. utilizing an isothermal model on the threshold velocity.

v_o , ft/s	$\beta = 0.95$	$\beta = 0$
9	N	N
9.5	N	Y
10	N	Y
10.5	N	Y
11	Y	Y
11.5	Y	Y

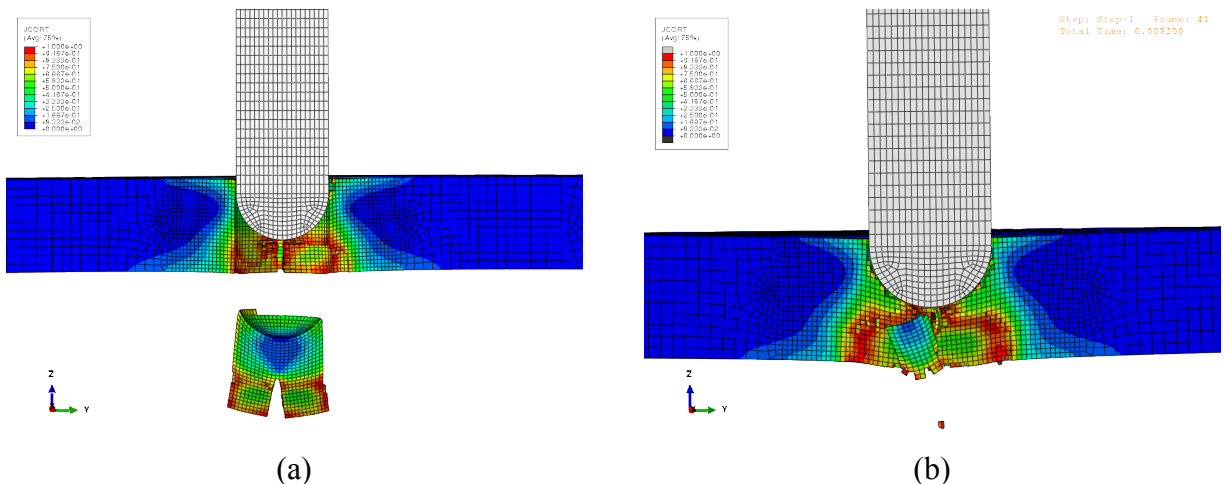


Figure 35: Comparison of the impact zones after the plug formation for the adiabatic (a) and the isothermal (b) representations of the material behavior. Contours of Johnson-Cook damage parameters are shown. The snapshot times are not equal for the two cases shown.

6.6 Evaluation of Strain Rate Effects

The failure model also has a term that controls the dependence of the equivalent plastic strain to failure on the strain rate. The test results in Figure 24 showed that the true strain to failure was smaller when the strain rate was in the order of 1300 1/s than when the material was strained quasi-statically. Furthermore, the results in Figure 33, show that the equivalent plastic strain rate in front of the leading edge of the failure that cuts the plug can be even higher than what was achieved in the Hopkinson bar tests but is within the same decade.

The effect of strain rate was evaluated by removing the dependence of the failure model on it, but leaving the strength model untouched. In summary, the parameter d_4 was set to zero. The results obtained showed that the threshold impact velocity increased to between 14 and 14.5 ft/s from the previously found 10.5 to 11 ft/s as shown in Table 8. Hence, the effect of strain rate is significant in this problem. Failure still occurred by the ejection of a well-defined plug when the effect of strain rate on material failure was removed. A comparison of the plug configurations with and without the effect of strain rate is shown in Figure 36. Removing the strain rate effect makes the material more ductile, and that is why the threshold velocity increased.

Table 8:. Effect of including the effect of strain rate on failure vs. utilizing a rate independent model on the threshold velocity.

v_o , ft/s	$d_4 = -0.036$	$d_4 = 0$
10.5	N	
11	Y	
14		N
14.5		Y

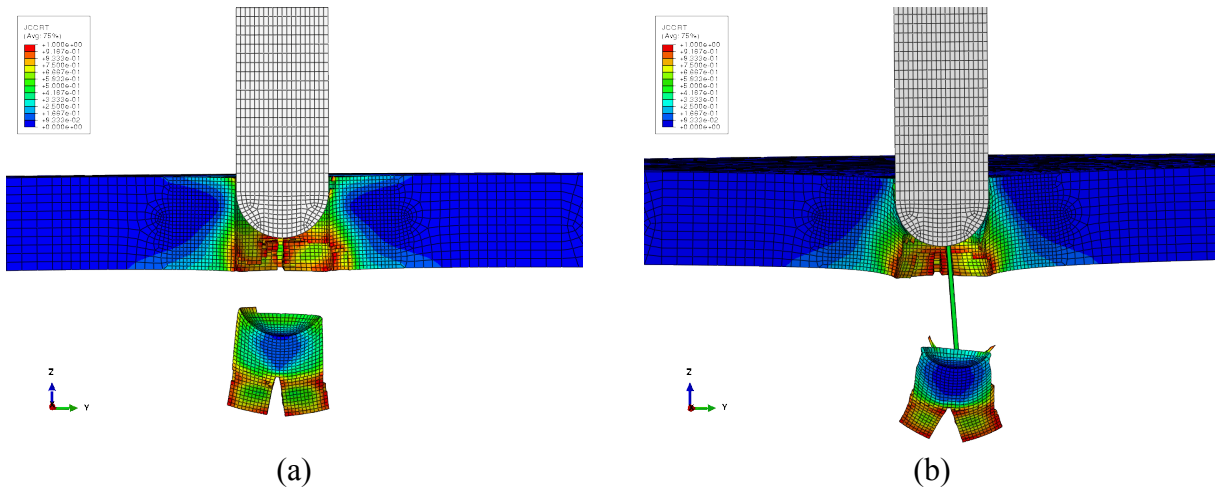


Figure 36: Comparison of the impact zones after the plug formation. (a) Accounting for strain rate effects and (b) removing the effect of strain rate on the failure model. The snapshot times are not equal for the two cases shown.

6.7 Effect of the Value of the Friction Coefficient

In all calculations presented so far, the value of the coefficient of friction was kept at 0.4. In order to assess the sensitivity of the plate threshold puncture velocity and the failure mode to this parameter, additional calculations were conducted to determine the threshold velocity for values of $\mu = 0.6$ and 0.2. The results are shown in Table 9 and show no effect in the range considered.

Table 9: Sensitivity of the threshold puncture velocity to the coefficient of friction.

v_o , ft/s	$\mu = 0.6$	$\mu = 0.4$	$\mu = 0.2$
9	N	N	N
9.5	N	N	
10	N	N	N
10.5	N	N	N
11	Y	Y	Y
11.5	Y	Y	Y
12	Y	Y	Y

6.8 Effect of the Failure Evolution Stress Decay

In all the calculations conducted up to this point, material failure and element deletion occurred instantaneously after the failure criterion was satisfied. It is generally recommended that the stress of a failing element be reduced gradually over some strain range, so that some additional energy dissipation occurs. In the current work, a strain decay that is linear with strain is adopted as shown in Figure 37. In this work, the stress decay is prescribed via a displacement u_d as required by Abaqus to reduce mesh dependency of the results. The results in Table 6, however, indicated that little mesh dependence occurred in the cases considered for the prediction of the threshold velocity when $u_d = 0$. Since no test data is available to fit u_d , it will be treated as a parameter of the problem. In other words, it will be varied while keeping the rest of the problem parameters, including the mesh, constant to see its effect on the puncture predictions.

The values used were chosen so that u_d/L would be approximately 2.3×10^{-4} and 2.3×10^{-3} , assuming L to be the length of an element diagonal. These values correspond to approximately 0.1% and 1% of the strain-to-failure under uniaxial conditions, which was 21%. Little difference in the results was observed as seen in Table 10. The shape of the plugs also remained unaffected as can be seen in Figure 38. As mentioned previously, whether the range chosen is appropriate or not remains unknown.

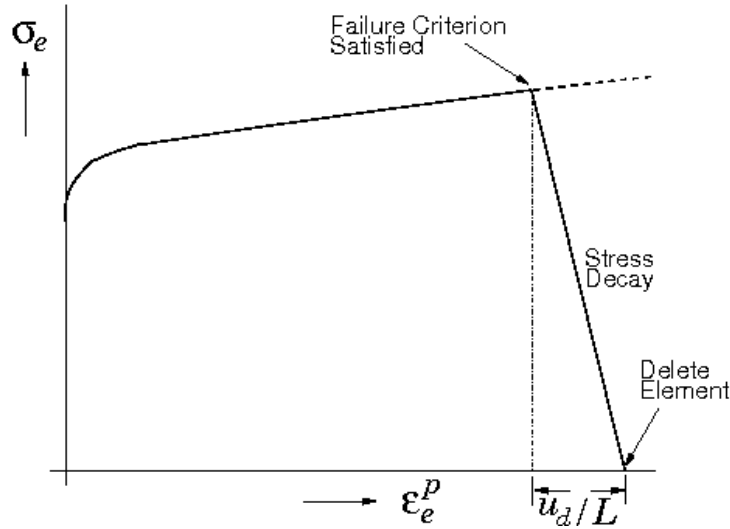


Figure 37: Schematic showing a linear stress decay after the failure criterion is satisfied. The decay occurs after failure over a strain u_d/L , where L is a characteristic length of the element and u_d is a prescribed displacement.

Table 10: Sensitivity of the threshold puncture velocity to the value of u_d/L .

v_o , ft/s	$u_d / L = 0$	$u_d / L = 2.3 \times 10^{-4}$	$u_d / L = 2.3 \times 10^{-3}$
9	N	N	N
9.5	N	N	N
10	N	N	N
10.5	N	N	Y
11.	Y	Y	Y
11.5	Y	Y	Y

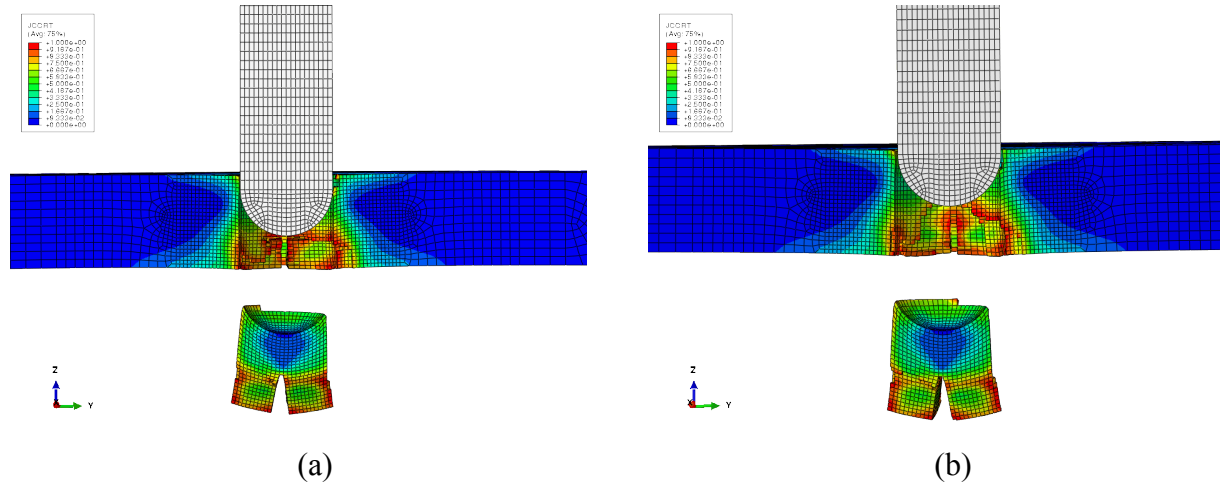


Figure 38: Comparison of impact zones after plug formation. (a) $u_d / L = 0$, $v_o = 11$ ft/s and (b) $u_d / L = 2.3 \times 10^{-3}$, $v_o = 10.5$ ft/s. The snapshot times are not equal for the two cases shown.

6.9 Effect of Hourglass Stiffness

The single integration point elements used in this analysis are susceptible to zero-energy hourglass modes. As a result, an artificial hourglass stiffness is introduced in the element formulation to control these modes. A recommended default stiffness of 0.5% of the shear modulus was used to generate the results presented up to this point. Since the hourglass stiffness can affect the results of the simulations, the code manual used in this work (Dassault Systemes, 2012) suggests that the hourglass stiffness be kept in the range between 0.2 and 3 times the default value.

Additional calculations were conducted at these extremes to assess the effect of the value chosen on the predicted puncture threshold velocity. When the hourglass stiffness was set to 0.2 of the default value, the calculated threshold velocity was between 10.1 and 10.2 ft/s, or about 3% lower than when the default value was used. Using an hourglass stiffness 3 times of the default value gave a threshold velocity between 10.4 and 10.5 ft/s, which was the same as when the default value was used. Therefore, the effect of hourglass stiffness seems to be rather minor when the input value is kept within the recommended range.

6.10 Comments on Sensitivity Analyses

The results presented in this report give an indication of the sensitivities of the main quantities of interest of the problem to various parameters. For example, it has been shown that the sensitivities of the puncture threshold velocity to whether the coarse, fine, or very fine meshes are used, or to what value of friction coefficient is used (within the range considered) are relatively small. On the other hand, the sensitivity of the threshold velocity is more significant to the choice of the curves of equivalent plastic strain to failure vs. triaxiality, or to whether adiabatic heating or the dependence of the equivalent plastic strain to failure on strain rate are included in the analysis.

Clearly, more complete sensitivity analyses can be conducted by choosing a reasonable range of values for the parameters of the problem and varying them according to some methodology like Latin Hypercube Sampling as was done by Corona et al (2012). This approach ensures a more representative sampling of the parameter space and can give an indication of the influence of the interaction of the input parameters on the quantities of interest. Determining the range over which each of the input parameters varies is an important step in this type of sensitivity analysis. Generally, the range of variation is based on estimates of the uncertainty associated with the calibration of the material properties or with the measurement of geometric quantities, but these steps were not carried out in this study.

Future work on similar problems should include a reasonable estimate of the range of variation of the input parameters, especially those associated with the constitutive and failure models of the material to allow more systematic sensitivity studies to be conducted.

7 CONCLUSIONS

In summary, the results presented showed that using the Johnson-Cook model yielded numerical results that were close to experimental measurements and observations for the puncture of the plates tested in 2012. In particular, the threshold velocity between no puncture and puncture measured in the experiment was predicted reasonably well; a great improvement

over the results obtained by Corona et al (2012). It appears that the elements that allowed for the improvement in the predictions were:

- Used experimentally obtained material data to calibrate all the parameters of the Johnson-Cook strength and failure models. Most of the data came from the same stock as the plates tested.
- The Johnson-Cook failure model includes dependency on triaxiality, strain rate, and temperature, all of which contributed to close the gap between experimental and numerical results.
- Included adiabatic heating in the calculations.

Although the results presented for the threshold velocity seem reasonable, one must keep in mind that many aspects of the response of the material and/or their effect on the response remain unknown or have not been modeled. For example:

- The anisotropy on the yield stress and on the strain to failure seen in Figure 11 could not be used in the model because it did not allow them.
- The response of the material to tension/compression in the through-thickness direction has not been investigated.
- Experiments have not been conducted in the shear-dominated triaxiality regime.

Although a lot of questions remain regarding the suitability of the Johnson-Cook model to represent the plastic deformation and failure behavior of the material, as well as of the finite element model to capture the actual failure mechanisms, the methods used seem promising when used to predict the puncture of Al 7075 plates. Obviously, the model needs to be exercised in other similar problems to assess how robust it is, but it is noted that other authors (Borvik et al, 2005 and Teng and Wierzbicki, 2006) have also reported good results in plate puncture problems using the Johnson-Cook model.

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APPENDIX A: DIMENSIONS OF TENSION TEST SPECIMENS

A.1 Uniaxial Tension Test Specimens for Quasi-Static Loading

Figure 39 shows the dimensions of the uniaxial tension test specimens. The strain was measured with an extensometer with a gage length of one inch.

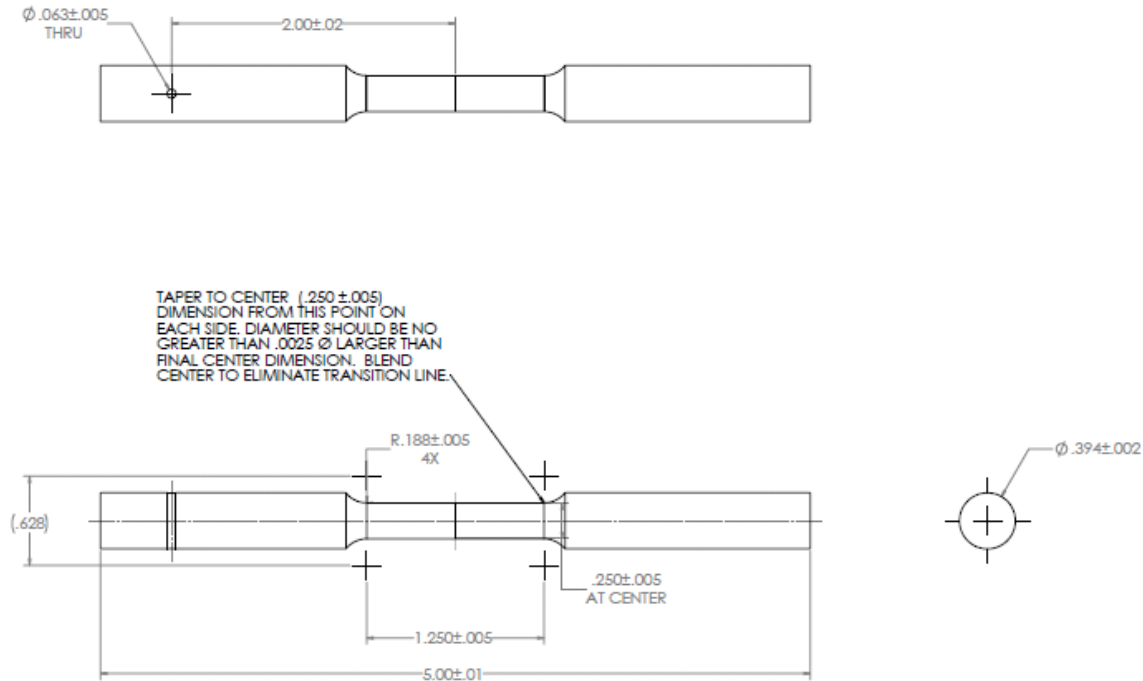


Figure 39: Uniaxial tension test specimen dimensions

A.2 Uniaxial Tension Test Specimens for Dynamic Loading

Figure 40 shows the dimensions of the uniaxial tension test specimens tested in the Kolsky bar by Song and Antoun (2012).

MATERIAL : 7075-T651
 QUANTITY: 6EA

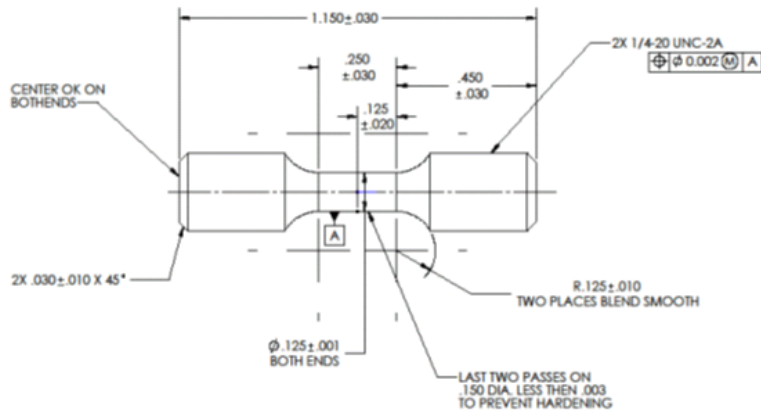
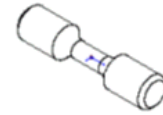


Figure 40: High strain rate uniaxial tension test specimen dimensions

A.3 Notched Tension Test Specimens

Figure 41 through Figure 44 show the dimensions of the notched tension test specimens.

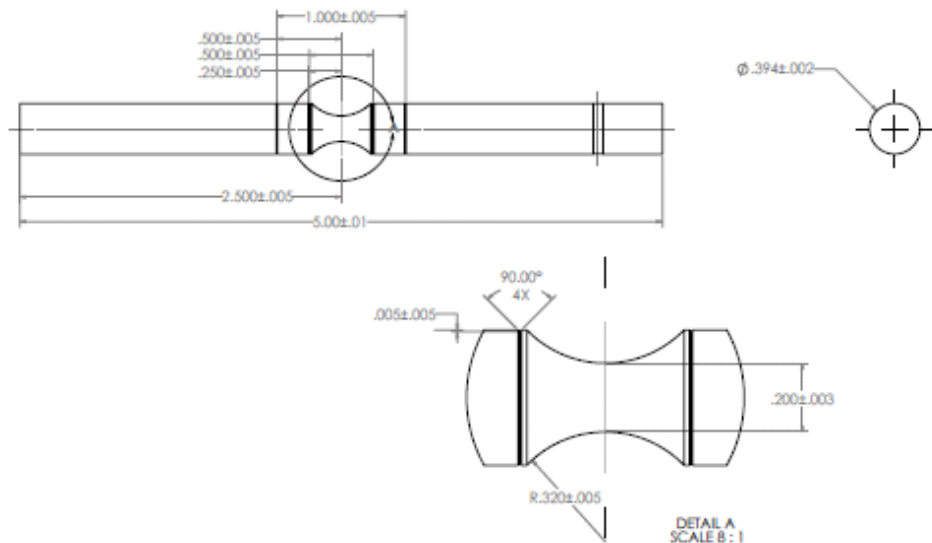


Figure 41: Dimensions of notched tension test specimen with $r/R=3.2$

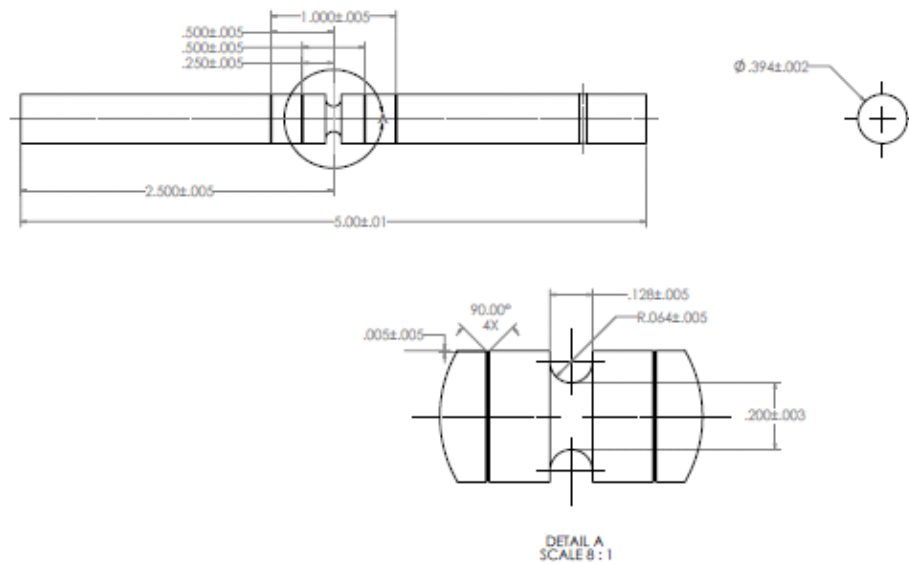


Figure 42: Dimensions of notched tension test specimen with $r/R=1.28$

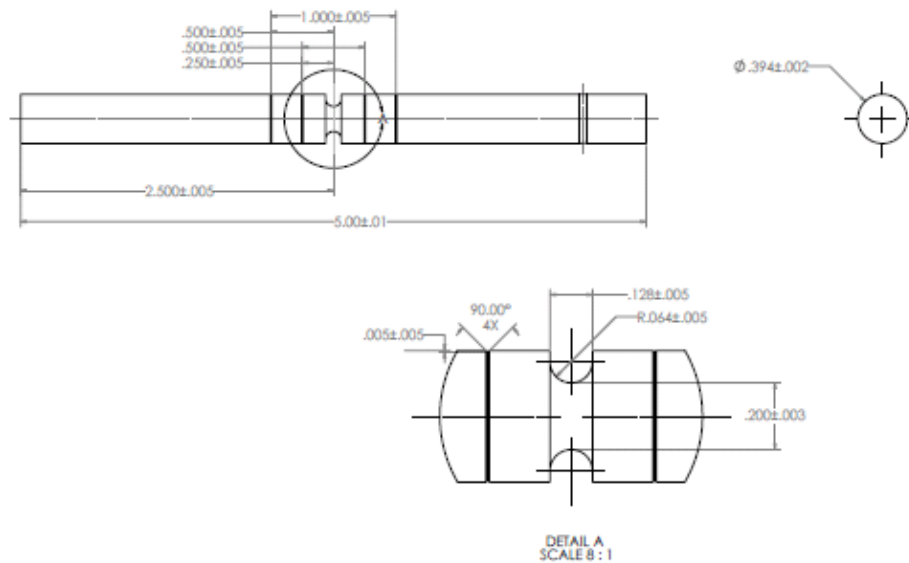


Figure 43: Dimensions of notched tension test specimen with $r/R=0.64$

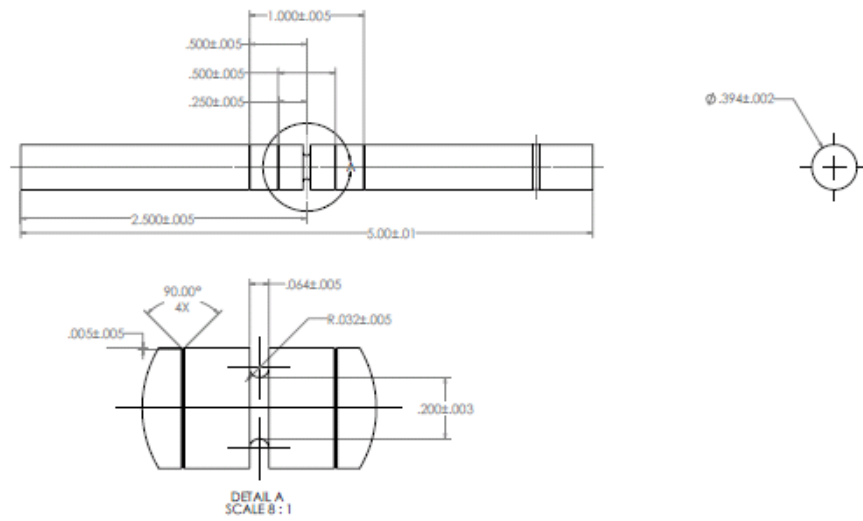


Figure 44: Dimensions of notched tension test specimen with $r/R=0.32$

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