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IMPLEMENTATION OF A PRESSURE AND RATE DEPENDENT FORMING LIMIT DIAGRAM MODEL INTO NIKE AND DYNA

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ABSTRACT

The Forming Limit Diagram (FLD) has been used for decades as an aid to successful sheet metal forming. In this work, we describe the incorporation of the FLD technique into the DYNA and NIKE codes at LLNL along with applications that led to the developments. The algorithm is currently available in the public version of DYNA3D. Several augmentations of the basic technique have been made available due to the necessity of their incorporation to solve programmatic problems of interest at LLNL. Illustration of the use of the FLD model is shown for a dome geometry similar to that used in the Limiting Dome Height (LDH) test. This early example uses the simplest FLD option (analogous to circle grid) and shows the relative merits of this method versus scalar plastic work in prediction of tearing. In a phenomenological extension of the method, a pressure-dependent (FLD+P) method is used to successfully predict the relative design merits of stainless steel forgings. A final application to sheet stamping of a Boeing 737 door frame shows how the scatter plot circle grid option and strain path plots can be used to predict when preforms and intermediate anneals are necessary. The phenomenological nature of the FLD model as implemented is discussed relative to alternative approaches of calculating the FLD and its path dependence.

INTRODUCTION AND MOTIVATION

Historically, our first pass failure analyses at LLNL have been based on approximate levels of effective plastic strain, a scalar measure of plastic work in the material. This method can give useable engineering answers as a guideline for design improvement when a baseline design has been calibrated to experiment already. A classical example of the use of scalar effective strain is in the prediction of limiting draw ratio in cupping (Logan et al., 1986). Even though failure at the punch nose is in a state of near plane strain, we can successfully predict the effect of material, geometry, and process changes using scalar effective strain as a failure measure if we have calibrated to initial experiments that bound the numerical values of strain expected at failure. However, in dome tests and other geometries where failure
is at a more arbitrary location on the flow surface, the correlation of prediction with reality is easily lost if scalar strain measures are used for failure.

It is intuitively obvious from the existence of the Forming Limit Diagram (FLD) that scalar effective strain is not a good measure of proximity to failure for a general class of problem (Keeler, 1968). To illustrate, we show a typical FLD as the heavy line in Fig. 1. This FLD shows a value of scalar effective strain in uniaxial tension of 0.40. Now, we construct an "FLD" based on fixing the scalar (von Mises) effective strain at 0.40, and calculating major and minor strains for other paths. This is shown in Fig. 1 and illustrates quantitatively that the use of scalar effective strain in fact generates a 'backwards' FLD compared to those both experimentally observed or numerically calculated. This in part was the incentive for the implementation into NIKE (Maker et al., 1991) and DYNA (Whirley et al., 1991) of the FLD concept. Further incentive was provided by the frequent cases of rate, pressure, and history dependence of the FLD. These dependencies require the embedding of the FLD framework into the analysis code itself as described below, even though the FLD and its dependencies are input as phenomenological data in this implementation.

Figure 1. Illustration of the FLD concept and the often erroneous use of effective strain to failure on the same plot. For true bulk applications, the lines may coincide.

DEVELOPMENT AND APPLICATIONS IN DYNA3D

For a continuum, the equations of motion may be written

\[ V \cdot \sigma + \rho b = \rho \dot{u} \tag{1} \]

where \( \sigma \) is Cauchy stress, \( b \) is the body force density field, \( \rho \) denotes the current material mass density, \( u \) is the displacement field, and a superimposed dot denotes differentiation with respect to time. Applying the finite element method to spatially discretize Eq. (1) yields a coupled set of nonlinear ordinary differential equations in time,

\[ M \ddot{u} = f^e(t) - f^i(u, t), \tag{2} \]

where \( M \) is a mass matrix. \( u \) is now a vector of nodal displacements, \( f^e \) is a vector of externally applied time-dependent nodal forces, \( f^i \) is a vector of internal nodal forces arising from stresses existing in the elements, and \( t \) is time. Even if higher-order differential operators are included, such as those arising in beam, plate, and shell formulations, the resulting set of ODEs still retains the form of Eq. (2).
Next, the assumptions of explicit analysis are introduced to numerically integrate these ODEs in time. DYNA integrates Eq. (2) using the central difference method. To begin, assume that all quantities are known at time \( t = t_n \) and it is desired to advance the solution to time \( t = t_{n+1} \). The first step is to find the acceleration \( a_n = \ddot{u}(t_n) \) from the discrete version of Eq. (2) at time \( t = t_n \):

\[
Ma_n = f^m_{n+1} - f^m_n,
\]

where \( f_n \equiv f(t_n) \). We now introduce a nodal lumped mass approximation, so \( M \) becomes a diagonal matrix, and the evaluation of \( a_n \) from Eq. (3) is very inexpensive since the equations are now uncoupled and all quantities on the right-hand side are known. The central difference method gives update equations for the nodal velocities \( v \) and displacements \( u \) as

\[
v_{n+1} = v_n + a_n \Delta t
\]
(4)

\[
u_{n+1} = u_n + v_n \Delta t
\]
(5)

Now that the updated kinematic variables are known, the next step is to evaluate the forces on the right-hand-side of Eq. (3) at time \( t = t_{n+1} \). Since external loads (including those due to prescribed motions of the rigid tooling) are usually prescribed functions of time, the evaluation of \( f^m_{n+1} \) is straightforward. The bulk of the computations within a time step are expended to evaluate the internal force \( f^m_{n+1} \). Computation of \( f^m_{n+1} \) begins with the calculation of the rate of deformation

\[
d_{n+1} = \frac{1}{2} \left[ \nabla v_{n+1} + (\nabla v_{n+1})^T \right]
\]
(6)

where \( \nabla v \) denotes the gradient of the velocity with respect to the geometry at time \( t = t_{n+1} \), and \( B \) is the "strain-velocity operator." Next, the updated Cauchy stress \( \sigma_{n+1} \) is found from

\[
\sigma_{n+1} = \sigma_n + \int_0^{t_{n+1}} \dot{\sigma} \, dt,
\]
(7)

where \( \dot{\sigma} \) is computed from an objective stress response function using the rate of deformation \( d_{n+1} \) and material history variables. This incremental formulation easily accommodates material nonlinearities such as elastoplasticity and viscoplasticity. Finally the new internal force vector for an element \( e \) is found from the updated stresses using

\[
f_{n+1} = \int_{\Omega_e} B^T \sigma_{n+1} \, d\Omega_e,
\]
(8)

and the global force vector \( f^m_{n+1} \) is found by assembling contributions from all elements. This completes the update of all quantities from time \( t = t_n \) to time \( t = t_{n+1} \).

During this process, the effective stress, both trial and updated, is calculated from the von Mises eqn:

\[
\overline{\sigma}^2 = \frac{(\sigma_1 - \sigma_2)^2 + (\sigma_2 - \sigma_3)^2 + (\sigma_3 - \sigma_1)^2}{2}
\]
(9)

Implementation of the FLD

For elastoplasticity, the stress update is performed using the radial return method. The strain increments,

\[
de_{ij} = d_{ij}^{n+1} \Delta t
\]
(10)

are summed to provide accumulated total strains in the element coordinate system. These may be rotated to obtain the principal strains, from which the Forming Limit strains \( \varepsilon_{major}, \varepsilon_{minor}, \) and \( \varepsilon_{normal} \) are chosen. The FLD method (Keeler, 1968) involves plotting the major (largest principal) strain \( \varepsilon_{maj} \) versus the minor (next largest) principal strain \( \varepsilon_{min} \). For shells, both \( \varepsilon_{maj} \) and \( \varepsilon_{min} \) are assumed to lie in the plane of the shell (or sheet material). For bricks, the code chooses its own coordinate system by finding the largest and next-largest principal strains, and taking normal to that plane as the 'sheet normal.'
This happens automatically if an input parameter $INORM = 0$, but may not always be what is desired (i.e. in a drawing operation where $\varepsilon_{\text{min}}$ is actually the smallest principal strain.)

For brick elements only, $INORM > 0$ overrides the automatic choice of 'normal direction', and forces the code to pick, in the element coordinate system, the direction 1, 2, or 3 as the sheet 'normal' if $INORM = 1$, 2, or 3 respectively. The orientation for $\varepsilon_{\text{maj}}$ and $\varepsilon_{\text{min}}$ is then taken within the plane defined by the normal given by $INORM$. This feature is nearly essential when using brick elements for stamping or hydroforming. When the major strain reaches the FLD limiting value, which is a function of the minor strain, material failure is predicted. Once failure is detected in this model, the yield stress $\sigma_Y$ and the tangent modulus $E_T$ are reduced by the factor $SCLDEV$. In addition, a failure-fraction parameter $F$ is set to one to indicate that failure has occurred at a material point. The parameter $F$ represents the fractional distance from the origin to the limit line of the FLD.

The shape of the forming limit diagram is determined by the left-hand and right-hand load curves, $LCLH$ and $LCRH$. The first column of each these load curves gives the ratio of minor true strain to major true strain, $R_{mm}$, and the second column gives the major true strain, $\varepsilon_{\text{maj}}$. The left-hand load curve should cover the minor/major strain ratio range of -0.5 to 0.0, and the right-hand curve should cover the strain ratio range from 0.0 to 1.0, as shown in Figure 2. Note that the load curves input to describe the FLD are input by the user; thus this implementation is not an attempt to calculate the FLD but rather to track the (changing) FLD as input by the user.

The pressure dependence of the forming limit diagram is determined by the pressure load curve, $LCPX$. Strictly speaking, the FLD is a plane-stress sheet forming concept. Thus, for any monotonic path on the FLD, there is an associated value of normalized pressure $\tilde{p}$, where $\tilde{p}$ is given by

$$\tilde{p} = \frac{p}{\sigma_Y}$$

where $p$ is the hydrostatic component of the stress tensor and $\sigma_Y$ is the current yield stress. For uniaxial tension, $\tilde{p} = -1/3$, and for biaxial tension $\tilde{p} = -2/3$. Any additional normalized pressure, $\tilde{p}$, beyond these standard values for plane stress conditions may cause the FLD to grow or shrink (Figure 3) by an increment $d\varepsilon^p_{\text{maj}}$ on each strain path $R_{mm}$. Load curve $LCPX$ should contain pairs giving additional normalized pressure in the first column (example, the hypothetical value -0.40 in Fig. 3) and the corresponding increment $d\varepsilon^p_{\text{maj}}$ in the second column (example, the corresponding value -0.025 in

Figure 2. Generation of the Forming Limit Diagram using load curves $LCLH$ and $LCRH$.

Figure 3. Effect of added hydrostatic pressure on the shape of the Forming Limit Diagram ($LCPX > 0$).
Fig. 3. Figure 3 thus illustrates the operation of the pressure-dependence load curve, assuming $LCPX > 0$. If $LCPX < 0$, then instead of the increment, the second column of the load curve ($ILCPX$) is a multiplier on the value of $S_{maj}$ instead of being added to it. Thus, if $LCPX > 0$, load curve entries of 0.0 will have no effect. If $LCPX < 0$, load curve entries of 1.0 will have no effect.

The material constitutive behavior differs from the elastic-plastic model, Material Type 3, in that the yield stress may be multiplied by a strain rate dependent factor found from load curve $LCEDM$. The first column of this load curve should contain total strain rate values, and the second column should give the scale factor to be multiplied times the current yield stress, $\sigma_Y$. Note that this strain rate scaling is applied after strain hardening is taken into account. Use of this feature of the FLD+P model as implemented in NIKE2D (Engelmann and Hallquist, 1991), has been reported previously (Logan et al., 1993).

The determination of failure or proximity to failure is made in one of three ways, depending on the value of the forming limit failure criterion option, $IFLD$. For the TOTAL FLD approach ($IFLD = 1$ as used in this work), the total major and minor strains are compared to the limiting values given by the forming limit diagram, and the failure fraction parameter $F$ is determined. The pressure used in pressure dependence option with this criterion is a "damped" pressure, which is a running average of the instantaneous pressures. The strain rate is similarly damped. This procedure approximates the "total" forming limit diagram method used in press shop practice.

**Use of the FLD in Formability Testing**

Implementation of the FLD concept based on total strain is a matter of tracking the principal strains. This is especially simple for shell elements since we make the assumption that one of the principal strains is in the direction of the sheet normal. It should be pointed out that this is indeed an assumption albeit a good one. The examples shown here represent the use of the total FLD method (the $IFLD=1$ option in the input for DYNA3D).

One obvious application where it is crucial to use an FLD-related failure criterion instead of a scalar effective strain is in the forming of domes as in (Hecker, 1974). The stretch forming process used to form these domes can be simulated with DYNA3D using the same SVE (Selective Velocity Enhancement) technique described previously (Whirley et al., 1992), wherein the actual punch velocity is increased by a factor of about 100 decreasing the DYNA3D run time accordingly. Use of an SVE greater than about 100 often leads to erroneous results, and SVE must be used with due caution.

![a) & b)](image)

Fig. 4. DYNA3D simulations of Dome Stretching. First predicted failure sites would be (a) center of dome as predicted with scalar effective strain, or (b) further down the dome as predicted with FLD.
When this is done, effective strain will be the highest under the center of the dome, as shown in the output plot of Fig. 4a. However, this is not generally where the failure site is observed, especially for real-world conditions with friction $\mu >> 0$. Instead, the failure site is observed further down, but generally still on the punch, in a condition closer to plane-strain. This is captured accurately in the plot of FLD failure fraction $F$, which ranges from $0 \leq F \leq 1$, in Fig. 4b. The failure region shown here using the FLD method closely approximates the area of failure commonly observed in the Limiting Dome Height (LDH) test, which has a very similar geometry as developed in (Ghosh, 1975).

For first-pass studies such as these, we can often estimate the shape of the FLD as a function of strain-hardening and sheet thickness, using relations as in (Hogarth et al., 1991) and elsewhere. In fact, this procedure was used to re-examine some of the early data on dome stretching obtained in (Hecker, 1974). The experimental data from this study is reproduced in Fig. 5a, plotted as dome height to failure as a function of the strain exponent $n$ for the various sheets used. The dome forming of each set of sheet metal was modeled, and dome height to failure plotted as lines to compare to the data obtained. When failure height is assumed to be proportional to the strain exponent $n$, as used successfully in studies with flat bottomed punches (Logan et al., 1986), a very poor correlation to the data is obtained. When failure height is based on FLD's estimated from sheet thickness, strain exponent $n$, and rate exponent $m$, good correlation is achieved as shown in Fig. 5b. Numerous compilations have been done on the effect of these parameters on the FLD. For typical sheet of thickness $t < 3$mm, we have found the following estimates to work reasonably well in the absence of experimental data, and they have been employed for the FLDs used in constructing Fig. 5b, where $\varepsilon_{min}^*$ is the plane-strain intercept on the FLD:

$$n^* = n \quad (n \leq 22)$$

$$n^* = 22 + 0.65 (n - 22) \quad (n \geq 22)$$

$$m^* = m \quad |m| \leq 0.01$$

$$\varepsilon_{ef} = 52 n^* m^* t$$

$$\varepsilon_{mf} = \exp(4m) - 1$$

$$\varepsilon_{min}^* = n^* + \varepsilon_{mf} + \varepsilon_{ef}$$

(12)

Thus, the importance of the FLD for failure prediction in stretching is shown qualitatively in Fig. 4 and quantitatively in Fig. 5.

Figure 5. Correlation of predicted dome heights with strain exponent, compared to data from (Hecker, 1974). (a) using scalar effective strain, (b) using FLD for failure criterion.
The FLD+P Method for Bulk Forging Analysis

Material failure in forgings can be addressed using scalar effective strain, but often with the same drawbacks as shown above. Other failure models for bulk deformation generally involve the calculation of damage accumulation due to plastic deformation accounting for microstructure and mean stress, and thus retain a strong component of the scalar measure described above. Further, the input for such models is often difficult to generate. An alternative employed with success here involves the extension of the FLD concept for use with thicker blanks, and then to the general case of the forming (even back extrusion) of a preform. It is not clear that this methodology is the best possible algorithm when plate thickness is extended beyond a certain point, and indeed beyond a certain thickness the shape of the "FLD" as shown in Fig. 1 may indeed look more like the fracture limit line so that scalar effective strain (or an FLD so constructed) may be more appropriate. However, the method showed remarkable success here in duplicating a forging case history on the first attempt. The (uncorrected) scalar effective strain methodology was unable to account for observed events.

Using the HERF (High Energy Rate Forge) procedure, our desire was to produce a dimpled hemisphere shape from tooling already being used to make simple hemi shapes. The original HERF process (Method A) for this 21Cr-6Ni-9Mn stainless steel involved several steps as shown in Fig. 6. However, since the final forging steps took place at a relatively low temperature (about 873 K) to impart strength to the final forging, ductility began to limit the process and failures were observed in the dimple region. A second procedure (Method B), as outlined in Fig. 7, involved imparting the same amount of deformation at the final 873 K temperature, but using a different preform process. Successful parts were made in this manner.

From a process model constructed with DYNA2D (Whirley et al., 1992), we were able to simulate the HERF process for both Method A (Fig. 8), and the three-hit process of Method B (Fig. 9). To model the three-hit process, we meshed the punch and two additional 'ghost' punches to represent the 2nd and 3rd hits. All three punches were given the initial impact velocity but timed in space to allow for impact

Figure 6. HERF process for dimpled hemisphere forging, Method A.
Figure 7. HERF process for dimpled hemisphere forging, Method B.
and rebound of the previous punch. After the 1st hit (Fig. 9b), the 1st punch was rezoned out of the problem, leaving the 2nd punch to impact (Fig. 9c). After rebound of the 2nd punch, it too was rezoned out and the 3rd punch left to impact (Fig. 9d). The DYNA2D estimates of total HERF energy agreed with estimates from the HERF press, and the final shapes obtained agreed with the actual parts.

![Figure 8. DYNA2D analysis of the Method A HERF process. Failure has already occurred at t=0.8 ms so model is not run to complete die closure.](image)

![Figure 9. DYNA2D analysis of the Method B HERF process. (a) Before impact. (b) After first punch impact. (c) After 2nd punch impact. (d) After 3rd and final impact.](image)

However, with scalar effective strain as our only measure, we were unable to quantify numerically why the Method A procedure had failed, as shown in Fig. 10a, while Method B produced successful parts. In fact, simulations of both processes indicated that an effective strain (plastic work) of $\tilde{\varepsilon}^* > 1.00$ would be imparted to the dimple area, with a higher value in fact for Method B. The inconclusive nature of this observation led us to search for a more methodical way of predicting failures under these HERF conditions. One clue that we had was to note that the hydrostatic pressures observed in Method A ranged from -500 MPa to +500 MPa (P/X of about -0.8 to +0.8) whereas the pressures in Method B in the dimple area were near +2000 MPa (P/X of about +3.0). These were the extremes of pressure histories that were highly variable with time. To account for this, we used an estimated FLD for the material to be HERF'ed, with a plane-strain intercept $n^*$ taken from the uniaxial strain-hardening exponent $n$ of the material. An FLD was constructed that was in fact a cross between a typical FLD for sheet and a 'fracture strain' FLD. However, the effect of pressure on the FLD was folded in to exploit the capability of Model 35, as described above. The slope, or pressure dependence, was calculated by comparing approximate strains to failure in uniaxial compression versus estimates of biaxial tension, giving a pressure dependence slightly more conservative than discussed by Hosford and Caddell (1983). Alternative implementations of this philosophy for forging analysis have been given elsewhere (Chen et al., 1990), (Frater and Penza, 1989). Use of this model in the Method A simulation immediately indicated failures in the dimple region, as shown in Fig. 10b-c.
Figure 10. (a) Photo of failed dimpled hemi, Method A. (b) DYNA with FLD+P indicates failure onset (unshaded region) at t=0.8 ms, with (c) progression of failed elements at t=1.0 ms.

These compared well with the actual HERFed parts. In the Method B simulations, no failures were indicated with DYNA2D and the FLD+P method. These results were obtained on the first try with our best estimated properties and a * values for Method A which contained prior warm-work, and Method B, which was fully annealed material but with the total warm-work imparted in the final HERF step. A sensitivity study showed that the same results (failure in "A", success in "B") were obtained using .20 ≤ n ≤ .66 in Method A, and .50 ≤ n ≤ .66 in Method B. Thus, this was an example that did not require meticulous data on the materials to be HERFed. Rather, any reasonable estimates for the properties, combined with use of the FLD+P estimate, allowed an adequate predictive capability.

Large model application. Door Frame Stamping

The utility of the FLD method and the outputs available from DYNA3D's Model 35 are illustrated in this example of a large model application, where an overview of the information about proximity to failure is used to focus on potential trouble sites in the stamping. Figure 11 is a picture of a Boeing 757 door frame in the sub-assembly process. This part is currently formed by a matched die forming process using five hits of increasing depth with partial and full anneals where necessary. The current process was developed by a trial and error method which consumed considerable time and material. As a part of Boeing's effort to reduce cycle time, finite element analysis has been applied to a number of sheet forming processes.

This specific effort was to improve the current process and investigate how analysis could be used to predict producibility and to reduce the cost and time required to develop a matched die process. The model was built from an IGES translation of a CAD dataset. Figure 12 shows the mesh of the punch, sheet and die. The section of the part that has been magnified in Fig. 12 illustrates an area that causes problems during forming operations. This area is referred to as a "bullnose" region, because of the small diameter drawn cup configuration that tends to tear during forming.

A rectangular shaped blank was used for this analysis, even though the production blank has a shape closer to the final part configuration. The rectangular blank was chosen to simulate a new part producibility analysis, to see if the finite element modeling and analysis would predict the same shape as trial and error development had produced. Subsequent simulation effort for this process used a curved blank or a sub-model of the region of interest. This model contains approximately 36,000 elements describing the punch, sheet and die. This model was run without a blankholder, commonly referred to as crash mode, and consumes well over 3 days of cpu time and 20 megabytes of RAM on a Hewlett Packard HP-735 workstation. The 2024-0 aluminum properties were obtained from (Dorward, 1994), detailing work performed at the Kaiser Aluminum & Chemical Corp. Center for Technology. Figure 13 shows the variation of the state of strain and failure factor as a function of punch travel (25mm and 50mm).
The failure factor $F$ is the degree to which the elemental strains have exceeded the FLD for the current state of strain. At 25mm of punch travel, the bullnose has already exceeded the FLD for 2024-T3. The strain ratio plot at the top of Fig. 13 shows that different regions of the part are experiencing very different states of strain. Regions of black are experiencing biaxial strain while white regions are either in plane-strain compression (no thinning or thickening of the part) or have not yet gone plastic. At 50mm of punch travel, the compressive strains due to the rectangular blank are apparent (the middle lower edge of the part). Gray regions are near plane-strain tension, as seen around the bullnose area. As these regions approach the FLD, the failure fraction increases toward $F=1.0$ as shown in the middle plots of Fig. 13. The lower plots in Fig. 13 show development of thinning strain, and appear qualitatively similar to the failure fraction plots, since most of the localization is occurring in the plane strain areas of the stamping.

Figure 14a and 14b are plots of circle grid data taken in the bullnose region of the part compared with the major and minor strains taken from 50 elements in a similar region of the part. Two different punch travels are shown and the correlation is generally good, considering the use of the isotropic von Mises flow criteria. Figure 14c plots the failure factor $F$ for each of the 50 elements plotted above. These diagnostics are readily available in post-processing and are useful here as noted in previous works also (Sklad et al., 1991). Currently, the actual first hit in the forming process is 25mm of punch travel which correlates nicely with the predictions in Fig. 14b-c. For certain elements, a predicted failure factor $F$ of 1.0 is reached at just under 25mm of travel, indicating we have reached the FLD and thus danger of localized necking as discussed above. Note that the strains tend to localize on further displacement of the punch, leading to (fictitious) values of $F$ of up to 7.0 at 125mm punch travel.

This material model and the information available allows the results of a finite element simulations to be used to develop or improve a sheet forming process.
Figure 12. DYNA3D model and meshing used to analyze the 757 door frame stamping process. Close-up of circled region is the "bullnose".
1. Strain Ratio, $\varepsilon_{\text{min}}/\varepsilon_{\text{maj}}$

2. Failure Fraction, $F$

3. Thinning Strain, $\varepsilon_t$

Figure 13. Upper: Strain ratio ($\varepsilon_{\text{min}}/\varepsilon_{\text{maj}}$) in door frame stamping at 25mm and 50mm of punch travel. Middle: Corresponding failure fraction, $F$. Lower: Corresponding thinning strain.

The plots of state of strain can be used by process developers to efficiently design the blank shape to understand what is happening to the material as the part is formed. The ability to plot the elemental strains on an FLD allows an early assessment of number of preforms and anneals necessary to form the part. This assessment can be used immediately to influence part design for producibility instead of releasing the drawing and then determining in the shop whether the part is formable. DYNA3D with post-processing of FLD parameters available from Model 35 allows fast finite element simulation turn around time which can make process development and design a concurrent event.

CONCLUSIONS

We have illustrated the implementation and use of the Forming Limit Diagram (FLD) model in LLNL's DYNA/NIKE finite-element code family. Quantitative examples of its use were provided for sheet formability testing, bulk forging analysis, and large-scale sheet stamping applications. Although pressure augmentation was used in the forging example, this is only one of many optional features of this FLD implementation. Further, only the Total FLD option was demonstrated in this work. For highly non-proportional loadings, the other phenomenological options (Incremental FLD and Damage FLD) might be employed with success. In addition, we are exploring the implementation of theoretically-based calculated FLD's into the code for future studies.
Figure 14. (a) Comparison at 12.5mm punch travel, with FLD (solid line), DYNA3D scatter plot in bullnose (crosses) and circle-grid analysis (diamonds) in same area of actual stamping. (b) Comparison at 25mm punch travel. (c) Growth of failure factor $F$ in bullnose.
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