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Hydroforming Process: Identification of the Material's Characteristics and Reliability Analysis

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1. Introduction

The increasing application of hydroforming techniques in automotive and aerospace industries is due to its advantages over classical processes as stamping or welding. Particularly, tube hydroforming with various cross sectional shapes along the tube axis is a well-known and wide used technology for mass production, due to the improvement in computer controls and high pressure hydraulic systems (Asnafi et al., 2000; Hama et al., 2006; Cherouat et al., 2002). Many experimental studies of asymmetric hydroforming tube have been examined (Donald et al., 2000; Sokolowski et al., 2000). Theoretical models have been constructed to show the hydroforming limits, the material and the process parameters influence on the formability of the tube without failure (buckling and fracture) (Sokolowski et al., 2000). Due to the complexity of the process, theoretical studies up to date have produced relatively limited results corresponding the failure prediction. As for many other metal or sheet forming processes, the tendency of getting a more and more geometric complicated part demands a systematic numerical simulation of the hydroforming processes. This allows modifying virtually the process conditions in order to find the best process parameters for the final product. Thus, it gives an efficient way to reduce cost and time.

Many studies have been devoted to the mechanical and numerical modelling of the hydroforming processes using the finite element analysis (Hama et al., 2006; Donald et al., 2000), allowing the prediction of the material flow and the contact boundary evolution during the process. However, the main difficulty in many hydroforming processes is to find the convenient control of the evolution of the applied internal pressure and axial forces paths. This avoids the plastic flow localization leading to buckling or fracture of the tube during the process. In fact, when a metallic material is formed by such processes, it

experiences large plastic deformations, leading to the formation of high strain localization zones and, consequently, to the onset of micro-defects or cracks. This damage initiation and its evolution cause the loss of the formed piece and indicate that the forming process itself should be modified to avoid the damage appearance (Cherouat et al.,2002). In principle, all materials and alloys used for deep drawing or stamping can be used for hydroforming applications as well (Koç et al.,2002).

This chapter presents firstly a computational approach, based on a numerical and experimental methodology to adequately study and simulate the hydroforming formability of welded tube and sheet. The experimental study is dedicated to the identification of material parameters using an optimization algorithm known as the Nelder-Mead simplex (Radi et al.,2010) from the global measure of displacement and pressure expansion. Secondly, the reliability analysis of the hydroforming process of WT is presented and the numerical results are given to validate the adopted approach and to show the importance of this analysis.

2. Hydroforming process

For production of low-weight, high-energy absorbent, and cost-effective structural automotive components, hydroforming is now considered the only method in many cases.

The principle of tube hydroforming is shown in Figure 1. The hydroforming operation is either force-controlled (the axial forces vary with the internal pressure) or stroke-controlled (the strokes vary with the internal pressure). Note that the axial force and the stroke are strongly interrelated (see figure 1).

Force-controlled hydroforming is at the focus in (Asnafi et al.,2000), where the constructed analytical models are used to show

- which are the limits during hydroforming,
- how different material and process parameters influence the loading path and the forming result, and
- what an experimental investigation into hydroforming should focus on.

The hydroforming operation comprises two stages: free forming and calibration. The portion of the deformation in which the tube expands without tool contact, is called free forming. As soon as tool contact is established, the calibration starts.

Figure 1. The principle of tube hydroforming: (a) original tube shape and (b) final tube shape (before unloading).

During calibration, no additional material is fed into the expansion zone by the axis cylinders. The tube is forced to adopt the tool shape of the increasing internal pressure only.

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2.1. Mechanical characteristic of welded tube behaviour

Taking into account the ratio thickness/diameter of the tube, the radial stress is considerably small compared to the circumferential σ_θ and longitudinal stresses σ_z (see Figure 2). In addition, the principal axes of the stress tensor and the orthotropic axes are considered coaxial. The transverse anisotropy assumption represented through the yield criterion can be written as:

$$\bar{\sigma}^2 = F(\sigma_z - \sigma_\theta)^2 + G\sigma_z^2 + H\sigma_\theta^2 \quad (1)$$

with (F,G,H) are the parameters characterizing the current state of anisotropy.

If the circumferential direction is taken as a material reference, the anisotropy effect can be characterized by a single coefficient R and the equation (1) becomes:

$$\bar{\sigma}^2 = \frac{1}{1+R} \left[R(\sigma_z - \sigma_\theta)^2 + \sigma_z^2 + \sigma_\theta^2 \right] \quad (2)$$

The assumptions of normality and consistency lead to the following equations:

$$\begin{cases} d\epsilon_\theta = \frac{d\bar{\epsilon}}{\bar{\sigma}} \left(\sigma_\theta - \frac{R}{1+R} \sigma_z \right) \\ d\epsilon_z = \frac{d\bar{\epsilon}}{\bar{\sigma}} \left(\sigma_z - \frac{R}{1+R} \sigma_\theta \right) \end{cases} \quad (3)$$

where $\bar{\epsilon}$ is the effective plastic strain and $(\epsilon_\theta, \epsilon_z)$ are the strains in the circumferential and the axial directions. The effective strain for anisotropic material can be derived from equivalent plastic work definition, incompressibility condition, and the normality condition:

$$d\bar{\epsilon} = \frac{\sqrt{1+R}}{\sqrt{1+2R}} \sqrt{d\epsilon_z^2 + d\epsilon_\theta^2 + R(d\epsilon_z - d\epsilon_\theta)^2} = \left(\sqrt{\gamma^2 + \frac{2R}{1+R}\gamma + 1} \right) \frac{1+R}{\sqrt{1+2R}} d\epsilon_\theta \quad \text{with} \quad \gamma = \frac{d\epsilon_z}{d\epsilon_\theta} \quad (4)$$

Taking into account the relations expressing strain tensor increments, the equivalent stress (Equation 2) becomes:

$$\bar{\sigma} = \left(\sqrt{1 + \gamma^2 + \frac{2R}{1+R}\gamma} \right) \frac{\sqrt{1+2R}}{1+R+R\gamma} \sigma_{\theta} \quad (5)$$

In the studied case, the tube ends are fixed. As a consequence, the longitudinal increment strain $d\epsilon_z = 0$, and then relations (4) and (5) become:

$$\bar{\sigma} = \left(\sqrt{\frac{2R^2 + 3R + 1}{(1+R)^3}} \right) \sigma_{\theta} \quad d\bar{\epsilon} = \left(\frac{1+R}{\sqrt{1+2R}} \right) d\epsilon_{\theta} \quad (6)$$

The knowledge of the two unknown strain ϵ_{θ} and stress σ_{θ} needs the establishment of the final geometric data linked to the tube (diameter and wall thickness):

$$\epsilon_{\theta} = \ln \left(\frac{d}{d_0} \right) \quad \text{and} \quad \sigma_{\theta} = \frac{Pd}{2t} \quad (7)$$

where P is the internal pressure, (d, d_0) are the respective average values of the current and initial diameter of the sample and (t) is the current wall thickness obtained according to the following relation:

$$t = t_0 e^{-(1+\gamma)\epsilon_{\theta}} \quad (8)$$

Finally, the material characteristics of the tube (base metal) are expressed by the effective stress and effective strain according to the following equation (Swift model):

$$\bar{\sigma} = K(\epsilon_0 + \bar{\epsilon})^n \quad (9)$$

The values of the strength coefficient K , the strain hardening exponent n , the initial strain ϵ_0 and the anisotropic coefficient R in Equations (2) and (9) are identified numerically. For the determination of the stress-strain relationship using bulge test, the radial displacement, the internal pressure and the thickness at the center of the tube are required.

Figure 2. Stress state at bulge tip

3. Identification process

The parameters (K, ϵ_0, n) are computed in such a way that the constitutive equations associated to the yield surface reproduce as well as possible the following characteristics of the sheet metal. The problem which remains to be solved consists in finding the best combination of the parameters damage which minimizes the difference between numerical forecasts and experimental results. This minimization related to the differences between the m experimental measurements of the tensions and their numerical forecasts conducted on tensile specimens.

Due to the complexity of the used formulas, we have developed a numerical minimization strategy based on the Nelder-Mead simplex method. The identification technique of the material parameters is based on the coupling between the Nelder-Mead simplex method (Matlab code) and the numerical simulation based finite element method via ABAQUS/Explicit© of the hydroforming process. To obtain information from the output file of the ABAQUS/Explicit©, we use a developed Python code (see Figure 3).

Figure 3. Identification process

4. Results and discussion

A three dimensional finite element analysis (FEA) has been performed using the finite element code ABAQUS/Explicit to investigate the hydroforming processes.

4.2. Welded tube (WT) hydroforming process

In this case, the BM with geometrical singularities found in the WT is supposed orthotropic transverse, whereas its behaviour is represented by Swift model. The optical microscope observation on the cross section of the wall is used to build the geometrical profile of the notch generated by the welded junction. By considering the assumptions relating to an isotropic thin shell ($R=1$) with a uniform thickness, the previously established relations (6), (7) and (9) allow to build the first experimental hardening model using measurements of internal pressure/radial displacements. This model is then proposed, as initial solution, to solve the inverse problem of required hardening law that minimizes the following objective function:

$$\xi_F = \frac{1}{m_p} \sqrt{\sum_{i=1}^{m_p} \left(\frac{F_{\text{exp}}^i - F_{\text{num}}^i}{F_{\text{exp}}^i} \right)^2} \quad (10)$$

where F_{exp}^i is the experimental value of the thrust force corresponding to i^{th} nanoindentation depth H_i , F_{num}^i is the corresponding simulated thrust force and m_p is the total number of experimental points.

Different flow stress evolutions of isotropic hardening (initial, intermediate and optimal) are proposed in order to estimate the best behavior of the BM with geometrical singularities found in the WT. Figures 5 and 6 show the effective stress versus plastic strain curves and the associate pressure/radial displacement for these three cases. As it can be seen, there is a good correlation between the optimal evolution of Swift hardening and the experimental results. Table 1 summarizes the parameters of these models.

Hardening model	ε_0	K (MPa)	n
Initial	0.025	1124.6	0.2941
Intermediate	0.055	692.30	0.2101
Optimal	0.080	742.50	0.2359

Table 2. Swift parameters of different hardening evolution

The anisotropy factor R is determined only for the optimal hardening evolution. In the problem to be solved there is only one parameter which initial solution exists, that it corresponds to the case of isotropic material ($R = 1$). The numerical iterations were performed on the WT with non-uniformity of the thickness (see Figure 7), and the obtained results are shown in Figure 8. A good improvement in the quality of predicted results is noted if R corresponds the value of 0.976.

Figure 5. Stress-strain evolutions for different hardening laws

Figure 6. Internal pressure versus radial displacement

Figure 7. Radial displacement for different values of anisotropy coefficient R

4.3. Thin sheet hydroforming process

Sheet metal forming examples will be presented in order to test the capability of the proposed methodology to simulate thin sheet hydroforming operation using the fully isotropic model concerning elasticity and plasticity (Cherouat et al., 2008). These results are carried out on the circular part with a diameter of 300mm and thickness of 0.6mm. During hydroforming of the blank sheet, the die shape keeps touching the blank, which prevents the deformed area from further deformation and makes the deformation area move towards the outside. The blank flange is drawn into the female die, which abates thinning deformation of deformed area and aids the deformation of touching the female die and uniformity of deformation. Compared with the experiments done before, the limit drawing ratio of the blank is improved remarkably.

By considering the assumptions relating to an isotropic thin shell with a uniform thickness, the previously established relations allow to build the first experimental hardening model using measurements of force/displacement. This model is then proposed, as initial solution, to solve the inverse problem of required hardening law that minimizes the following objective function:

$$E_{\text{error}} = \frac{1}{m} \sqrt{\sum_{i=1}^m \left(\frac{P_{\text{exp}}^i - P_{\text{num}}^i}{P_{\text{exp}}^i} \right)^2} \quad (11)$$

where P_{exp}^i is the experimental value of the thrust pressure corresponding to i^{th} displacement δ_i , P_{num}^i is the corresponding predicted pressure and m is the total number of experimental points.

The controlled process parameters are the internal fluid pressure applied to the sheet as a uniformly distributed load to the sheet inner surface and is introduced as a linearly increasing function of time with a constant flow from approximately 10 ml/min. The comparator is used to measure the pole displacement. The effect of three die cavities (D1, D2 and D3 see Figure 8) on the plastic flow and damage localisation is investigated during sheet hydroforming. These dies cavities are made of a succession of revolution surfaces (conical, planes, spherical concave and convex). The evolution of displacement to the poles according to the internal pressure during the forming test and sheet thicknesses are investigated experimentally. The profiles of displacements are obtained starting from the deformations of the sheet after bursting. Those are reconstituted using 3D scanner type Dr. Picza Roland of an accuracy of $5\mu\text{m}$ with a step of regulated touch to 5mm. In addition, two measurement techniques were used to evaluate the thinning of sheet after forming; namely a non-destructive technique using an ultrasonic source of Sofranel mark (Model 26MG) and a destructive technique using a digital micrometer calliper of Mitiyuta mark of precision $10\mu\text{m}$ (see Figure 9).

Figure 8. Geometry of die cavities (D1, D2 and D3)

Experiments results of circular sheet hydroforming are shown in Figure 10 (Die D1), Figure 11 (Die D2) and Figure 12 (Die D3). For the die cavities D1 and D3, fracture appeared at the round corner (near the border areas between the conical and the hemispherical surfaces of the die). For the die cavity D2, the fracture occurred at the centre of the blank when the pressure is excessive. This shows that the critical deformation occurs at these regions. It is noted that the rupture zone depends on the die overflow of the pressure medium from the pressurized chamber and the reverse-bending effect on the die shoulder were not observed in the experiment. In this part we are interested in the comparison between experimental observations of regions where damage occurred and numerical predictions of areas covered

by plastic instability and damage. Figures 10, 11 and 12 present as such, the main results and simulations of all applications processed in this study. The predicted results with cavity dies show that the equivalent von Mises stress reached critical values high and then subjected to a significant decrease in damaged areas. This decrease is estimated for the three die cavities D1, D2 and D3, respectively 29%, 14% and 36%.

Figure 9. 3D scanner G Scan for reconstitution

Comparisons between numerical predictions of damaged areas and the experimental observations of fracture zones led us to the following findings:

1. The numerical calculations show that increasing pressure, the growing regions marked by a rise in the equivalent stress followed by a sudden decrease can be correlated with the damaged zones observed experimentally. In this context, the results of the first die cavity D1 show that instabilities are localized in the central zone of the blank, limited by a circular contour of the radius 72mm. The largest decrease in stress is located in the area bounded by two edges of respective radii 51 and 64mm. While the rupture occurred at the border on the flat surface with the spherical one located on a circle of radius 60mm. With the die cavity D2, the largest decrease is between two contours of radii 10 and 19mm, the rupture is observed at a distance of 17mm from the revolution axis of deformed blank. Finally with the die cavity D3, the calculations show that the damaged area is located in a region bounded by two edges of respective radii 54 and 73mm, the rupture occurred in the connection of the flat surface with the surface spherical one.
2. The pressures that characterize the early instabilities are respectively the order of 4.90MPa (for D1), 2.85MPa (for D2) and 5.1MPa (for D3). For applications with die cavities D1 and D3, regions where the beginnings of instability have been identified (see Table 2).

The results presented in Fig. 9 show that the relative differences between predicted and experiment results of pole displacement are in the limit of 7% while the pressure levels are below a threshold characterizing the type of application.

Figure 10. Experimental and numerical results of hydroforming using die cavity D1

Figure 11. Experimental and numerical results of hydroforming using die cavity D2

Figure 12. Experimental and numerical results of hydroforming using die cavity D3

Figure 13. Pole displacement versus internal pressure

Die cavity	Beginning instability (MPa)	Critical (MPa)	Experimental (MPa)
D1	4,90	6,74	5,2
D2	2,85	2,85	3,0
D3	5,10	6,86	5,3

Table 3. Levels of pressure for different dies **Figure 14.** Optimisation of complex shape part

4.4. Optimization of sheet shape

Optimization is the action of obtaining the preferable results during the part design. In the CAE-based application of optimization, several situations can cause the numerical noise (wrinkling). When the numerical noise exists in the design analysis loop, it will create many artificial local minimums. In this case, the minimization of local thinning condition in the blank sheet metal was tested with a cost function of the optimization system was chosen to minimize the thinning ratio of 20% thinnest element.

i.e. Cost function: $f = \sum_{i=1}^n \left\| \frac{t - t_0}{t_0} \right\|^2$

where t_0 is the initial thickness and t the final thickness.

In this case a significant design variable for formability of blank during hydroforming process and the design (D and d) constraints were defined:

$50 \leq D \leq 250\text{mm}$ $20 \leq d \leq 100\text{mm}$. The experimental final shape is shown in Figure 10a. The comparison of the force versus the maximum displacement with the initial and optimized blank shape is present in Figure 10b. Good agreement between the optimum shape and the experimental values. Figures 10c and 10d compare the initial and the optimum blank shape. Successfully decreased the cost function (thinning ratio) from 50% to 20% is obtained without wrinkling (Figure 10e and 10f) (see Ayadi et al., 2011).

5. Reliability analysis

Recently, RBDO has become a popular philosophy to solve different kind of problem. In this part, we try to prove the ability of this strategy to optimize loading path in the case of THP where different kind of nonlinearities exist (material, geometries and boundary conditions). The aim of this study is to obtain a free defects part with a good thickness distribution, decrease the risk of potential failures and to let the process insensitive to the input parameters variations. For more detailed description of the RBDO methodology and variety of frameworks the reader can be refer to the following references (Youn et al., 2003; Enevoldsen et al., 1994; El Hami et al., 2011). The RBDO problem can be generally formulated as:

$$\begin{cases} \text{Min } f(d, X) \\ \text{subject to } P[G_i(d, X) \leq 0] - \Phi(-\beta_{ti}) \leq 0 \quad i = 1, \dots, np \\ d^L \leq d \leq d^U, \quad d \in R^{ndv} \text{ and } X \in R^{nrv} \end{cases} \quad (12)$$

where $f(d, X)$ is the objective function, d is the design vector, X is the random vector, and the probabilistic constraints is described by the performance function $G_i(X)$, np , ndv and nrv are the number of probabilistic constraints, design variables and random variables, respectively, β_{ti} is the prescribed confidence level which can be defined as $\beta_{ti} = -\Phi^{-1}(P_f)$ where P_f is the probability of failure and Φ is the cumulative distribution function for standard normal distribution.

The process failure state is characterized by a limit state function or performance function $G(X)$, and $G(X)=0$ denotes the limit state surface. The m -dimensional uncertainty space in thus divided into a safe region ($\Omega_s = \{X : G(X) > 0\}$) and a failure region ($\Omega_f = \{X : G(X) \leq 0\}$) (see Radi et al., 2007).