

Dottorato di Ricerca in
TECNOLOGIE E SISTEMI DI LAVORAZIONE - XIII CICLO

Politecnico
di Milano

Università degli Studi
di Brescia

Università degli Studi
di Pavia

Università degli Studi
di Lecce

Tube HydroForming:
System Analysis and Process Design

Ph.D. Thesis
(Tesi di dottorato)

Matteo Strano

Coordinator of the Ph.D. program (coordinatore):
prof. Roberto PACAGNELLA (Politecnico di Milano)

Advisor (relatore):
prof. Quirico SEMERARO (Politecnico di Milano)

2002

Acknowledgements

The biggest part of the work described in this dissertation has been carried out by the author at the Engineering Research Center for Net Shape Manufacturing (ERC/NSM), at The Ohio State University, Columbus, Ohio, USA. I wish to thank and acknowledge the Director of the ERC/NSM, professor Taylan Altan, who has been a mentor and a friend throughout the development of this research work.

I also received very precious advices from professor Q. Semeraro and fundamental moral support from ing. B. Colosimo, of Politecnico di Milano.

Finally, I sincerely and gratefully acknowledge the invaluable help, cooperation and friendship of many of the people met at the ERC:

S. Jirathearanat, Y. Aue-u-lan, G. Ngaile, K. Tibari, A. Shr, L. Gao, F. Schroller, S. Motsch, S. Jaeger, G. Aiello, S. Guha, M. Tvaroch, J. Snyder, S. Loesch, H. Mert, H. Sahlan, L. Rathgeber, D. Osborn, S. Awate and all the others...

Table of Contents

Tube HydroForming: System Analysis and Process Design	1
Acknowledgements	2
Table of Contents	3
Nomenclature	5
Acronyms	5
Mathematical symbols	5
Executive Summary	8
PART I The THF System	9
Part I, Chapter 1 Introduction	10
1.1. A brief history of the hydroforming technology	10
1.2. The system approach	11
Part I, Chapter 2 Properties and Quality of Incoming Tubes	12
2.1. The tube bulge test	13
2.2. The hydraulic bulge test for the determination of the stress-strain curve	14
2.3. The energy method	16
2.3.1 Validation of the Model	18
2.3.2 Further application of the model	20
2.3.3 Conclusions	21
2.4. The forming limits	21
2.5. The use of the bulge test for the determination of FLDs	23
2.6. Experimental findings	25
2.6.1 Experiments for the determination of FLDs	25
2.6.2 Experiments for the determination of Damage functions and critical damage value	34
2.6.3 Experiments for the determination of FLSDs	38
2.6.4 Conclusions	39
Part I, Chapter 3 Preforming and bending design and production methods	41
3.1. Bending of tubes	41
3.1.1 Experimental setup	42
3.1.2 FEM simulations with the original bending radius	43
3.1.3 FEM simulations with the modified bending radius	45
3.2. Tube Hydroforming with Macro-Structured Preformed Tubes	46
3.2.1 Potential benefits and drawbacks of using macro-structured preforms	47
3.2.2 The effect of preform shapes on tube formability in free bulging of round tubes	49
3.2.3 Bulging of tubes with complex (round to square) die geometry	54
3.2.4 Summary	62
Part I, Chapter 4 Product, die and tool design	64
4.1. Introduction	64
4.2. Part Design Guidelines	65
4.2.1 Geometrical features of complex THF parts	65
4.2.2 Technological classification of THF parts	67
4.2.3 Modular design of THF parts	68
Part I, Chapter 5 The other components of the THF system	71

5.1. Die-Workpiece Interface: Wear, Friction and Lubrication	71
5.2. Mechanics of the process	72
5.3. Equipment, press and environment related issues	74
5.4. Specifications and requirement of the hydroformed part	76
5.4.1 Crashworthiness	77
Summary	79
PART II Process Analysis and Design	81
Part II, Chapter 1 Introduction	82
1.1. Sequential optimization strategies	84
1.2. Adaptive simulation	84
Part II, Chapter 2 The Self Feeding (SF) Approach	87
2.1. Calculation of process parameters in SF	87
2.1.1 Determination of pressure vs. time	87
2.1.2 Determination of axial feed vs. time	88
2.2. The risk of leaking in feed controlled THF	88
2.3. Calculation of $d_{sealing}$	90
2.4. Example of Application	91
Part II, Chapter 3 The Adaptive Simulation (AS) Approach	93
3.1. Introduction	93
3.2. Wrinkle detection and indication in AS	94
3.2.1 Energy/Stress Approaches	94
3.2.2 The Geometry/Strain Approaches	97
3.3. Mod.1 in the AS algorithm: the ratio (tube surface)/(fluid volume)	98
3.3.1 Formulation of the wrinkle indicator	100
3.3.2 Application of the wrinkle indicator	101
3.4. Mod.2 in the AS algorithm: adaptive control of pressure and feed	103
3.5. Mod.3 in the AS algorithm: determination of time control interval	106
3.6. Example of Application of the AS algorithm	107
Part II, Chapter 4 Post processing the Loading Paths	110
4.1. Smoothing of loading paths	110
4.2. Time rescaling of loading paths	111
4.3. Checking for leaking	112
4.4. Example of Application	112
Part II, Chapter 5 Selection of FEA strategies	114
Summary	116
References	117
Appendix A: Analysis of Variance for Flow Stress Determination	123
Regression analysis between K and n	123
ANOVA and MANOVA analysis.	124
Appendix B: Equations for the calculation of FLSD	125
Conversion from the NADDRG FLD to the FLSD	125
Conversion from the experimental FLD to the FLSD	125
Appendix C: description of the experiments	126
Experimental setup	126
Results of Experiments and FEM Simulations	127
Experiments – tube wall thickness $t_0 = 2$ mm	127
Experiments - tube wall thickness $t_0 = 1.5$ mm	130
FEM simulations	132
Determination of material properties	134
Prediction of fracture	134

Nomenclature

Acronyms

acronym	description
AS	Adaptive Simulation
ERC/NSM	Engineering Research Center for Net Shape Manufacturing
FEA	Finite Element Analysis
FLD	Forming Limit Diagrams
FLSD	Forming Limit Stress Diagrams
LCS	Low Carbon Steel
LP	Loading Path
SF	Self Feeding
SS	Stainless Steel
THF	Tube HydroForming

Mathematical symbols

symbol	unit	description
A	mm	amplitude of wrinkle
A _w	mm	amplitude of preform's wave
BH	mm	outer bulge height from the tube axis, measured at the crown
BH _{in}	mm	inner bulge height from the tube axis, measured at the crown
CDV	several	critical damage value
d _{ax}	mm	axial feed
d _{left}	mm	left side axial feed
d _{right}	mm	right side axial feed
d _{sealing}	mm	minimum feeding value that guarantees sealing
d _{self}	mm	axial feed determined through the SF approach
e	-	engineering strain
f	mm/sec	axial feed rate
F _{ax}	N	axial force
f _{max}	mm/sec	maximum feasible axial feed rate value
K	MPa	strength coefficient of the hardening law
L	mm	wavelength of the initial preformed tube;
l _c	mm	length of the initial preformed tube profile in the plane of the waves;
L _f	mm	length of the final tube spline;
L _i	mm	length of the initial tube spline with uniform round cross section;
l _s	mm	length of the preformed tube profile in the plane of the waves, after free bulging;
MD _{die}	mm	minimum diameter of die;

MD_{tube}	<i>mm</i>	maximum diameter of preform tube;
n	-	strain hardening exponent
n_w	-	number of wave lengths;
OD	<i>mm</i>	original tube diameter
P_i	<i>MPa</i>	internal pressure
P_{max}	<i>MPa</i>	maximum internal pressure
P_{min}	<i>MPa</i>	minimum bulging pressure
r	<i>mm</i>	radial coordinate.
R_{ave}	<i>mm</i>	average radius of the initial preformed tube;
r_c	<i>mm</i>	minimum internal die radius
RI_0	<i>mm</i>	inner initial tube radius
R_{in}	<i>mm</i>	radial coordinate of inner tube profile
R_{in_0}	<i>mm</i>	initial inner tube radius
R_{min}	<i>mm</i>	minimum radius of the initial preformed tube
R_n	-	flow ratio of axial feed
R_o	<i>mm</i>	initial outer tube radius
R_{out}	<i>mm</i>	radial coordinate of outer tube profile
R_{smooth}	<i>mm</i>	Average radius of the free bulged tube
r_z	<i>mm</i>	radius of curvature in the circumferential (hoop) direction
r_θ	<i>mm</i>	radius of curvature in the axial direction
S_t	<i>mm</i> ²	tube surface area
t	<i>sec</i>	time
t_0	<i>mm</i>	initial tube wall thickness
T_0	<i>s</i>	initial simulated time
T_f	<i>s</i>	final simulated time
th	<i>mm</i>	instantaneous tube wall thickness
$th\%_{max}$	-	maximum thinning
th_f	<i>mm</i>	tube wall thickness at fracture
t_{min}	<i>mm</i>	minimum tube wall thickness
t_o	<i>mm</i>	original tube wall thickness
V	-	non dimensional fluid cell volume
V_{fc}	<i>mm</i> ³	volume of water contained inside the deforming tube
w	<i>mm</i>	half bulge width (it's the axial distance from the centerline to the beginning of the die corner)
W	<i>several</i>	wrinkle indicator
W_d	<i>KJ</i>	internal work of deformation
W_{ext}	<i>KJ</i>	external work
z	<i>mm</i>	axial coordinate
Δf	<i>mm</i>	axial feed increment as calculated by AS
ΔP	<i>Mpa</i>	internal pressure increment as calculated by AS
Δt	<i>s</i>	simulation time step
$\Delta \tau$	<i>s</i>	duration of time control interval in adaptive simulation
Γ	<i>mm</i> ³ / <i>s</i>	water flow rate
β	-	stress ratio
$\bar{\alpha}$	-	effective strain
ϵ_f	-	effective strain at fracture
ϵ_o	-	pre-strain
ϵ_r	-	true thickness (radial) strain
ϵ_z	-	true axial strain

ϵ_1	-	true major strain
ϵ_2	-	true minor strain
ϵ_θ	-	true hoop strain
ϵ_{ij}	-	generic strain tensor component
ϵ_{min}	-	minimum thickness (radial) strain
λ	<i>mm</i>	length of wrinkle
λ_w	<i>mm</i>	quarter of wavelength of preformed tubes
v	-	wrinkle aspect ratio
v_w	-	wave aspect ratio
θ	-	circumferential angular coordinate
σ_h	<i>MPa</i>	hydrostatic stress
σ_{ij}	<i>MPa</i>	generic stress tensor component
σ_{max}	<i>MPa</i>	maximum normal stress
σ_z	<i>MPa</i>	true axial (minor) stress
σ_θ	<i>MPa</i>	true hoop (major) stress

Executive Summary

Designing a Tube HydroForming (THF) process means selecting and taking into account several different elements of the technology (see Figure 1).

For this reason, the Tube HydroForming technology will be first analyzed as a system, i.e. each component of the system will be described and the most critical and advanced issues in R&D will be described. When designing a new process, problems and improvements in each area of the THF technology and their interaction should be considered. The discussion of the components of the THF system will be given in Part I.

In Part II, the focus will be on the tools generally used and often required for designing and developing a new Tube HydroForming (THF) process or operation. As a general consideration, these tools are: 1) advanced software packages (such as Finite Element Analysis), 2) design guidelines and methods (including theoretical and empirical models for the description of the process), 3) systems, devices and methods for physical prototyping.

The focus of Part II will be on software tools, and more precisely on the strategies used in *Virtual Prototyping*, i.e. computer simulation using *Finite Element Analysis* (FEA), which plays a major and key role. Both the input data for FEA (all the information coming from the system analysis, described in Part I) and the strategies (described in Part II) must be carefully selected in virtual prototyping, if an accurate and fast response is required.

One of the most effective strategy for selection of process parameters is the Adaptive Simulation approach, which is thoroughly described and tested with different examples.

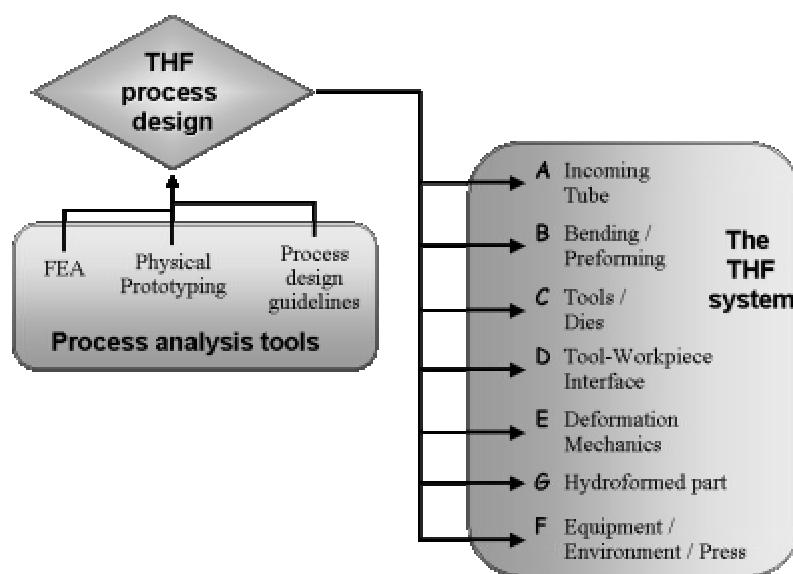


Figure I- 1: the THF system (Part I) and the design tools (Part II)

PART I
The THF System

Part I, Chapter 1

Introduction

1.1. A brief history of the hydroforming technology

Increasing use of hydroforming in automotive applications requires intensive research and development on all aspects of this relatively new technology to satisfy an ever-increasing demand by the industry.

Tube Hydroforming (THF) has been called with many other names depending on the time and country it was used and investigated. Bulge forming of tubes (BFTs) and liquid bulge forming (LBF) were two earlier terms, for instance.

Hydraulic (or hydrostatic) pressure forming (HPF) was another form of name used for a while by some investigators. Internal high pressure forming (IHPF) has been mostly used within German manufacturers and researchers. Throughout this paper, the acronym THF will be used to describe the metal forming process whereby tubes are formed into complex shapes with a die cavity using internal pressure (which is usually obtained by pressurizing water through an intensifier) and axial compressive forces simultaneously.

Even though THF process has been in practical industrial use only more than a decade, development of the techniques and establishment of the theoretical background goes back to 1940s [Grey et al., '39].

In 1960s, experimental and theoretical investigations on instability of thin-walled cylinders were performed by many researchers at different countries. Fundamental investigations on thin- and thick-walled cylinders helped theoretical improvements in LBF operations [Mellor, 1960]. Use of hydrostatic pressure in metal forming processes, in particular, for bulging of tubular parts was first reported in the late 1960s [Fuchs, 1966], [Ogura et al., 1968].

In 1970s, research on different aspects of bulge forming continued both experimentally and theoretically by various authors. New shapes, materials, different tooling configurations and new machine concepts were introduced, whereas the fundamentals remained the same. For instance, instead of polyurethane, rubber and elastomer were used to provide internal pressure [Al-Qureshi, 1970].

Starting from 1980s, researchers in Japan concentrated on determining the material properties and their effects on tube bulging operations [Manabe, 1983], while several theoretical models for the study of the process and appeared [Hashmi et al., 1985].

In the late eighties, the process started to spread industrially, especially in Germany, and a lot of work was conducted, based on the previous theoretical studies, along with real and new industrial applications of this technology [Dohmann et al., 1991].

In the early 1990s, researchers started utilizing the capabilities of continuously developing FEA and computer controls in their experimental and analytical works [Bohm, 1993].

In the late 1990s most of the research work has been addressed towards the selection and control of the process parameters and the investigation of several possible part types, with a heavy use of FEA [Altan, Koc et al., 1999]. Use of FEA for THF process simulations is now a standard development tool. Application of current commercial FEA software, such as LS-DYNA, PAM-STAMP, ABAQUS, MARC, AUTO-FORM, DEFORM, etc., for stamping and forging processes into THF was performed and presented successfully. Consequent and seamless simulation of bending, pre-forming and hydroforming, and sometimes annealing, results in accurate predictions in terms of producibility, formability and thinning of the desired part as well as points out necessary changes in tool

design. In order to shorten the development time and efforts for THF process, supplemental codes and techniques are being developed. Adaptive simulation technique, for instance, iterates between appropriate internal pressure and axial feeding inputs to ensure a part without any fracture and wrinkles. These techniques are still under development and Part II of the present dissertation is focused on them.

1.2. The system approach

A system approach is chosen in the present work for the analysis of THF technology. Indeed, both in research activities and in design and development of a new Tube HydroForming operation, attention must be paid to several aspects and issues of the technology and a system approach to the resolution of problems is highly recommended. In other words, when designing a new process, problems and improvements in each area of the THF technology and their interaction should be considered. The main components and key issues of a complete THF system can be listed as in Figure I- 2.

For each and every one of the mentioned components of the THF system, several issues should be studied and considered for process analysis and design. However, the most critical points for each of the mentioned components, from an industrial point of view, are:

- A.** determination of quality and material properties of incoming tubes (see Chapter I-2);
- B.** design of preform shapes, optimization of pre- (bending, crushing) and post- forming (punching, trimming) operations (see Chapter I-3);
- C.** determination of guidelines for rapid design of dies and tools (see Chapter I-4);
- D.** models for evaluation and prediction of die-workpiece interface conditions (wear, friction and lubrication) (see Chapter I-5);
- E.** numerical and analytical methods for analysis of deformation mechanics (see Chapter I-6);
- F.** design of low cost equipment and press (see Chapter I-7);
- G.** evaluation of performance of the hydroformed part (mechanical resistance, springback) (see Chapter I-8);

The most recent industrial and research trends and activities for each of the listed points will be described in the following chapters. Most of the issues presented in Chapters from I-2 to I-4 derive from original ideas and research work, which are presented in an appropriate reference scientific framework. Chapter from I-5 to I-8 result from a deep and prolonged bibliographic study, although the matter is presented with an emphasis on the activities carried out at the ERC/NSM and the approach is strongly influenced by the research strategy and approach used at the mentioned institute.

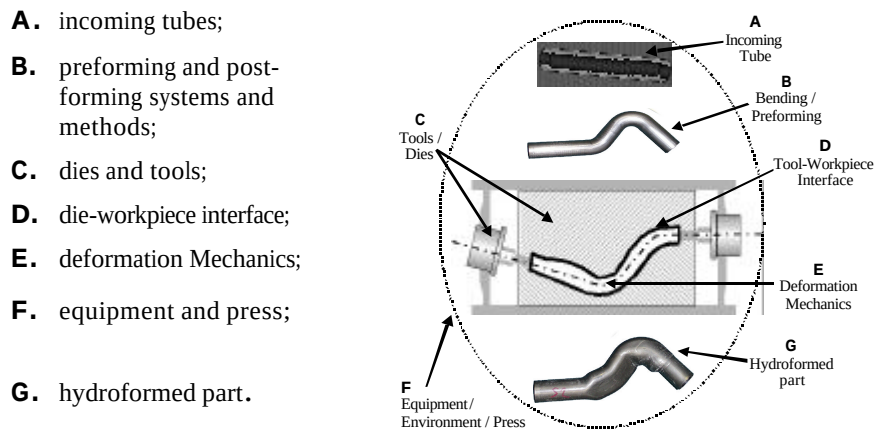


Figure I- 2: The Tube HydroForming System

Part I, Chapter 2

Properties and Quality of Incoming Tubes

The quality of the incoming tube is very critical for the success of any hydroforming process. The basic material properties (i.e., elasticity modulus, ultimate tensile strength, chemical composition, weld type) and dimensions (tube diameter and thickness) of the tube should be determined based on the final part requirements. However, for process simulation and development, more information is needed on the mechanical behavior of the material and, more precisely:

- plastic anisotropy,
- fragility and non-uniformity induced by the weld seam,
- true stress – true strain diagram in the plastic field,
- forming limits,

The plastic anisotropy of sheet metals used to manufacture the tubes can be very important for a successful operation, either as a beneficial or as a detrimental factor, depending on the die geometry and the nature of the anisotropy.

Similarly, the quality of welds can strongly influence the performance of the process. The tubes used in THF usually do not fail in correspondence of the welding seam, but in other regions, unless the quality of the welds is very poor or unless the weld is located in a critical area of tube expansion. Nevertheless, the presence of the weld itself inevitably causes a non-uniform distribution of mechanical properties along the circumference.

The non-uniformity of tubular materials induced by anisotropy and by the welding lines is obviously to be considered when designing a THF process. However when using FEA or other design tools for planning a THF process, issues concerning the true stress – true strain diagram and the forming limits are far more important. For this reason, the following sections of this chapter are focused on these last two points.

In the current industrial practice of tube hydroforming (THF) operations, very often the mechanical properties and the formability of tubes are derived from the tensile test data of the flat sheets used to manufacture the tubes. Alternatively, the material data are determined by running a tensile test directly on the tubes, rather than on the sheets.

In both cases, these practices present some drawbacks, as also stated in previous works (see as an example [Fuchizawa and Narazaki, 1993]). One disadvantage is that the maximum effective strain value achievable with an ordinary tensile test before localized necking occurs is remarkably lower than the effective strain values usually reached during the hydroforming process. Furthermore, when using material data obtained by tensile tests of sheets, they should at least be corrected to consider the straining due to the bending process used to form the tubes.

For the reasons stated above, several alternative testing procedures and tooling have been proposed so far, like the sheet bulge test (extensively described in the literature), the tube bulge test or more complex combined tests [Hora et al., 2000]. The hydraulic bulge test for tubes is gaining always more and more attention from the hydroforming industry. Hydraulic bulge test equipment has been developed by several research institutes, hydroforming press manufacturers and tube suppliers.

2.1. The tube bulge test

In order to obtain reliable data on material properties of the tube, a test procedure should be used, that is as close as possible to the hydroforming process. Although the results of the tensile test can provide information about the stress-strain relationship, they can hardly be used to evaluate formability of tubes for hydroforming, since the tensile test induces a uniaxial state of stress, while the THF process is mainly biaxial. In other words, a test generating a biaxial tensile stress state in the sample (such as a bulging test) would be closer to the real process conditions and this would insure a much more effective evaluation of formability¹.

The principle of the bulge test is very simple: a metal tubular specimen is loaded with internal pressure (usually hydraulic) and expands, undergoing plastic deformation until bursting occurs. By measuring the internal pressure and the tube deformation at the crown of the tube, much information on its mechanical properties can be attained.

Starting from 1980s, researchers in Japan concentrated on determining the material properties and their effects on tube bulging operations. [Manabe and Nishimura, 1983] investigated the influence of the strain-hardening exponent and anisotropy on forming of tubes in hydraulic bulging and nosing processes. They briefly presented the maximum internal pressure as a function of tube radius, thickness, strain hardening exponent, and strength coefficient assuming that there was no axial loading.

[Manabe et al., 1984] published their work on examination of deformation behavior and limits of forming for aluminum tubes under both internal pressure and axial force. Axial cylinders and internal pressure were controlled by a computer-control-system to obtain pre-defined stress ratio during their experiments. They utilized fundamental analysis of thin-walled cylinders in their pre-dictions for internal pressure and axial force.

Also Fuchizawa [Fuchizawa, 1984], [Fuchizawa, 1990], analyzed bulge forming of finite length, thin-walled cylinders under internal pressure using incremental plasticity theory. He presented the influence of strain-hardening exponent on limits of bulge height. Later, he extended his studies to explore the influence of plastic anisotropy on deformation behavior of thin-walled tubes under only internal pressure. He based his analysis on deformation theory and Hill's theory of plastic anisotropy. Longitudinal anisotropy was found to be effective on the critical expansion limit while anisotropy in hoop direction was affecting the maximum internal pressure required. With increasing anisotropy in longitudinal axis, thinning is reduced while obtainable expansion gets larger with less internal pressure requirement. Experimental results were eventually compared with theoretical findings. Different materials including aluminum, brass and copper were tested in their tooling, which only utilized internal pressure in a closed cavity. Assuming that the tube materials obey power law of strain hardening, experimental and calculated results were found to be in good agreement. Studies of Manabe and Fuchizawa on anisotropy effects were mostly found useful in THF applications involving aluminum products.

Hydraulic bulging of tubes was also used in determining the stress-strain characteristics of tubular materials by [Fuchizawa et al., 1993]. Annealed aluminum, copper, brass and titanium tubes were tested under only internal pressure. With the instrumentation and control systems available, tube thickness, radius of curvature in both longitudinal and hoop directions, and internal pressure measured and recorded during formation of the bulge. Using analytical methods by membrane and plasticity theories, stress-strain relations were derived. These findings were also compared with those obtained from tensile tests. Stress-strain relations for aluminum, copper and brass were found to be similar by two tests, whereas that for titanium were different. Since they did not use axial compressive load during bulging, stress-strain relation obtained was limited to low strain values up to 0.7.

At the ERC/NSM, tube properties are currently determined by a hydraulic bulge test. A sketch of this test is shown in Figure I4: the tube is locked on both ends and stretched freely using hydraulic internal pressure. During each experiment, the internal hydraulic pressure and the maximum bulge diameter are measured continuously. These data are used to calculate the flow stress ($\bar{\sigma}$) of the tube material as a function of effective strain ($\bar{\epsilon}$) in the form of the equation $\bar{\sigma} = K(e_0 + e)^n$, under the assumption of isotropic behavior [Altan et al., 1999], [Aue-u-lan et al., 2000].

If a circle grid is etched on the tubular samples before bulging, the test can also be used to determine the experimental Forming Limit Diagrams and Forming Limit Stress Diagrams [Strano et al., 2000a] directly from the tubes, rather than from the original sheet material roll formed to manufacture the tube.

¹ This concept is well established in the scientific literature. See, as an example [Jevons, 1942].

The tube bulge test is a fully established technology, frequently used at the ERC/NSM, and it has been recently accepted by many industrial companies. Currently, efforts for the qualitative and/or quantitative evaluation of anisotropy through the bulge test, are carried out at the ERC/NSM.

2.2. The hydraulic bulge test for the determination of the stress-strain curve

The main problem of using the tube bulge test for determining the stress strain relationship is the measurement of the tube radius of curvature in the axial direction (r_z in Figure I-3). This curvature is required to calculate the stresses, based on stress balance equations².

Therefore, systems for tube bulge testing must be equipped with devices able to measure the bulge height and the tube curvature. This may result in an increase of cost and in difficulty of use of the bulge test tooling, which may prevent its practical use in the press-shops. Furthermore, in some cases it may be difficult to obtain a good precision in the measurement of curvature [Rees, 1995], thus causing a loss of accuracy in the flow stress curve. An alternative would be using a very large ratio of length/diameter of the tube, so that the effect of the curvature becomes irrelevant and so that the test results in plane strain deformation. Unfortunately, in the plane strain tube test (as well as in the tensile test) the amount of effective strain reachable before fracture is limited, and the test does not represent the real conditions of the THF process.

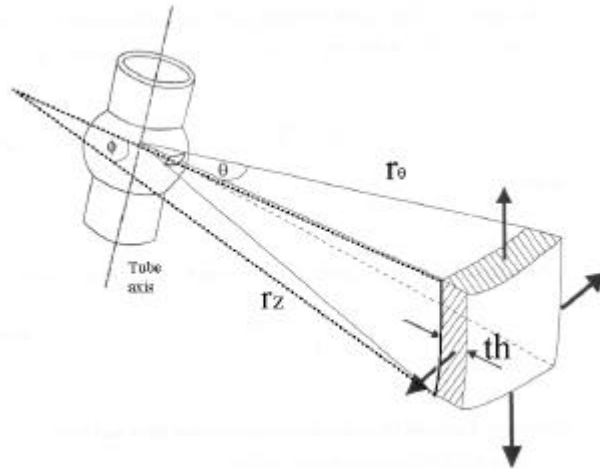


Figure I-3: r_z , radius of curvature in the circumferential (hoop) direction;
 r_θ , radius of curvature in the axial direction; th , instantaneous wall thickness.

For all the reasons stated in the previous section, a very simple hydraulic tube bulge test procedure has been developed at the Ohio State University's Engineering Research Center (ERC), which does not require the measurement of the radius of curvature in the axial direction and which is able to produce formability evaluations as well as stress-strain data. Several methods for building the true stress / true strain curves have been implemented.

A simple and fast method is based on Stress Balance Equations, very similar to the traditional bulge test approach. In this case the radius of curvature is not measured, but calculated by assuming an analytical shape function for the tube profile.

A more accurate approach is based on an Inverse Finite Element Iterative Technique. The inverse FE method has been used also by other authors for different applications [Gavrus et al., 1996], [Kusiak et al., 1996]. In this case, the parameters of the assumed flow stress law are iteratively adjusted until the results of the FE simulations

² An example of the equations suitable for the tube bulge tests and of a tooling provided with a curvature measuring is given in [Fuchizawa and Narazaki, 1993].

meet the experimental data. The method is more accurate, but it may require a relatively long computational time before it converges to a solution.

These two methods have already been briefly described in Sokolowski et al., 2000 and Altan et al., 1999 and with more detail, in ERC internal reports [Aue-u-lan et al., 2000].

A third method is hereby presented, that has been developed in order to reduce the computational time required by the FE technique, trying at the same time to improve on the accuracy given by stress balance equations. The method is based on an energy balance and will be described later. Details on this technique are also given in [Strano, Aue-u-lan, Schroller et al., 2000]. It may be now useful to summarize the main benefits and drawbacks of the proposed method.

DRAWBACKS OF THE PROPOSED METHOD

The test (as any other bulging test) is not able to give information about the anisotropy of the material, which must still be evaluated from the tensile test data. Nevertheless, it can be adjusted to consider the anisotropy parameters as input data. A second drawback is that the accuracy in determining the flow stress curve is reduced by the assumptions required by the mathematical model. For the last two reasons, the stress-strain data obtained from this tube bulge test should not be considered as general properties of the material, but as useful input data for FEM simulation of the THF process only.

ADVANTAGES OF THE PROPOSED METHOD

The proposed approach is very simple and therefore suitable to be used on any hydroforming press, as well as in simple bulging tooling as described below. In fact, it does not require any other sensors than a pressure-recording device: several tubes of the same material are tested at different pressure levels, and the deformations (strains and bulge height) are measured after each tube is extracted from the tooling. The proposed test can be used as a quality control test on incoming materials, giving a reasonable estimation of the hardening parameters and useful information on the formability limits. Furthermore, the accuracy of the flow stress data can always be improved by means of the FE inverse technique described in [Altan et al., 1999].

DESCRIPTION OF THE TOOLING

A sketch of the tooling used at the ERC is shown in Figure I-4. It is a stand-alone tool set used for the bulging of tubular specimens. The test is applicable to tubes with different material, wall thickness and diameter. Different bulge width can be tested. With the tube locked at the ends, the unsupported center section is free to expand under the effect of the internal pressure. A small die radius (6.35 mm) is used in an effort to reduce material flow over the corner. The air assisted hydraulic pump, used at the ERC, has a maximum pressure of 70 MPa. The data acquisition system for this tooling consists of a pressure sensor and a computer recording data system. The tooling is also equipped with an on-line sensor that can measure the bulge diameter continuously.

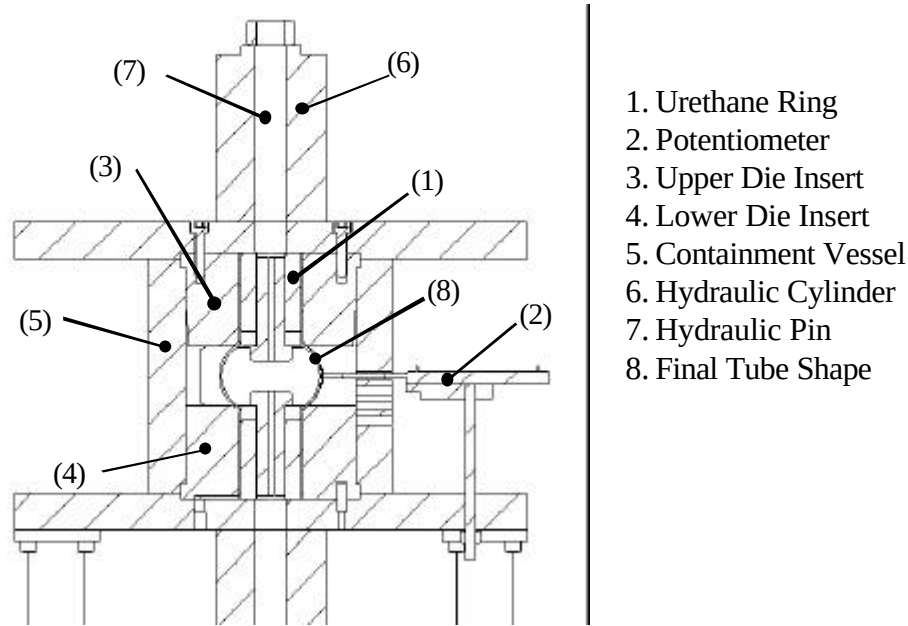


Figure I-4: Bulge test tooling at the ERC/NSM, the Ohio State University

2.3. The energy method

The method presented in this section has been developed for the determination of the three unknown parameters (K , e_0 , n) of the flow stress equation:

$$\bar{s} = K(e_0 + \bar{e})^n, \quad (I-1)$$

where $\bar{s}$ and $\bar{e}$ are the effective stress and strain. The model, implemented with a commercial software package for mathematical calculus, can be described as follows.

Initially, geometrical data of tube and die are given to the model: the initial thickness t_0 , the tube outer diameter OD (or the initial outer radius R_0), the half bulge width w .

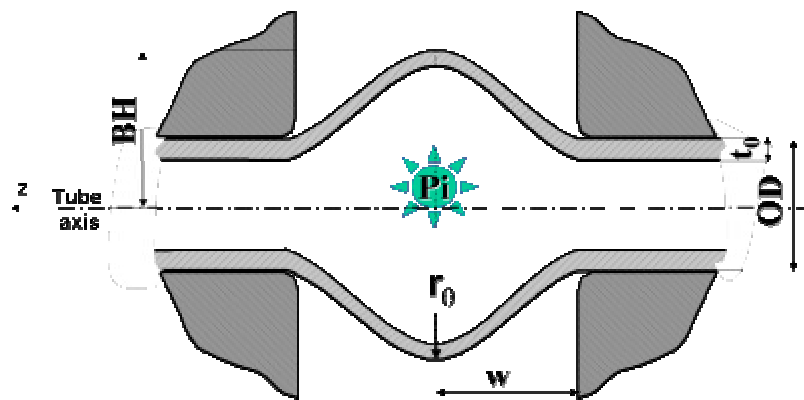


Figure I-5: scheme of bulge test

Then, experimental values of pressure and of corresponding measured outer bulge heights vs. time t ($P_i(t)$, $BH(t)$) are inputted. A typical pressure vs. bulge height curve is given in Figure I- 6.

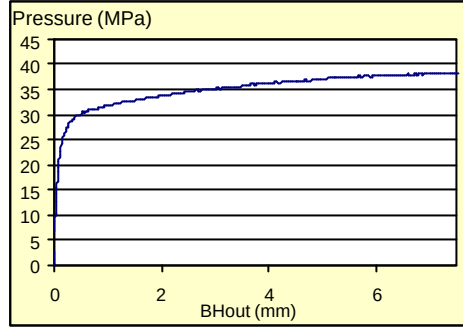


Figure I- 6: Example of pressure P vs. bulge height BH curve.

ϵ_0 is assumed equal to the plastic pre-strain, due to the roll forming process used to bend the tube, approximately calculated as in [Tirosh et al., 1996]:

$$e_0 = \sqrt{\frac{1}{12} \left[\ln \left(\frac{RO_0}{R_{med}(0,0)} \right) - \ln \left(\frac{RI_0}{R_{med}(0,0)} \right) \right]} \quad (I-2)$$

$$R_{med}(z,t) = \frac{R_{out}(z,t) + R_{in}(z,t)}{2} \quad (I-3)$$

The instantaneous outer and inner tube profiles (R_{out} and R_{in}) can be described by cosine-like functions, as in the following equations (4), (5) and (6):

$$R_{out}(z,t) = R_0 + BH(t) \cos\left(\frac{pz}{2w}\right) \quad (I-4)$$

$$R_{in}(z,t) = R_{in0} + BH_{in}(t) \cos\left(\frac{pz}{2w}\right) \quad (I-5)$$

$$BH_{in}(t) = BH(t) + t_0 - th(t) \quad (I-6)$$

The assumption made in generating equations (4) and (5) is that the geometry of the bulge is entirely defined, once the maximum bulge height $BH(t)$ is known. A very similar mathematical representation of the tube profile has already been effectively used in [Tirosh et al., 1996], although for a different experimental setup. In equation (6), $th(t)$ is the minimum wall thickness as a function of time, and it can be calculated according to volume constancy considerations. Therefore, once the inner and outer profiles of the tube have been defined, all the strains are completely defined and they can be calculated as follows:

$$\text{Radial (thickness) true strain} \quad (I-7)$$

$$e_q(z,t) = \ln \left(\frac{R_{med}(z,t)}{R_{med}(z,0)} \right) \quad (I-8)$$

Hoop (circumferential) true strain

$$e_z(z,t) = -e_r(z,t) - e_q(z,t) \quad (I-9)$$

Axial true strain

$$e_{zr}(z,t) = \frac{1}{2} \frac{\partial R_{med}(z,t)}{\partial z} \quad (I-10)$$

$$\bar{e}(z,t) = \sqrt{\frac{2}{3} [e_q(z,t)^2 + e_r(z,t)^2 + e_z(z,t)^2 + 2e_{zr}(z,t)^2]} \quad (I-11)$$

The internal work of deformation is finally calculated as:

$$W_d(t, K, n) = \frac{K}{n+1} \int_0^w \int_{Rin}^{Rout2p} \{r[e_0 + \bar{e}(z, t)]^{n+1} - e_0^{n+1}\} dq dr dz, \quad (I-12)$$

and the external work is calculated as:

$$W_{ext}(t, K, n) = \int_0^t P(t, K, n) \frac{\partial V_{fc}(t)}{\partial t} dt, \quad (I-13)$$

where $V_{fc}(t)$ is the instantaneous volume of water contained inside the tube.

The flow stress curve parameters can now be determined by minimization of a least square function:

$$LSF(K, n) = \sum_{i=0}^m [W_d(t_i, K, n) - W_{ext}(t_i, K, n)]^2, \quad (I-14)$$

where m is the number of experimental points.

2.3.1 Validation of the Model

The model has been used and tested with several tube materials, tube dimensions and die widths. Some of the results are presented in the following. As an example in Figure I- 7, flow stress curves determined through the energy method are shown, obtained testing tubes from stainless steel AISI 304 with different dimensions (see Table I- 1).

The following remarks can be done on the test:

- the stress-strain curves obtained by bulge test considerably differ from flow stress curves obtained by the tensile tests on the sheets, for all strain ranges, especially for low carbon steels (see, as an example, Figure I- 8);
- the n -values are higher than traditional n -values obtained by tensile tests.

The K and n values obtained by the proposed method should not be considered as accurate and universal mechanical properties of the materials, but as parameters obtained by fitting of experimental points, therefore useful as input to FEM simulation of the THF process.

Table I- 1: Tube dimensions and flow stress parameters for curves plotted in Figure I- 7. Material AISI 304.

	<i>material A</i>	<i>material B</i>	<i>material C</i>	<i>material D</i>
RO₀ (mm)	28.6	24.3	25.4	25.4
th₀ (mm)	1.65	2.13	2.00	1.50
K (MPa)	1350	1305	1143	1145
n	0.565	0.598	0.459	0.489
ε₀	0.017	0.026	0.018	0.024

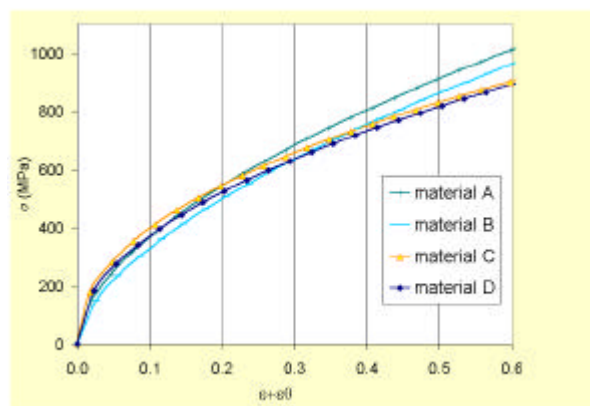


Figure I- 7: Flow stress curves determined through the energy method for different AISI 304 tubes.

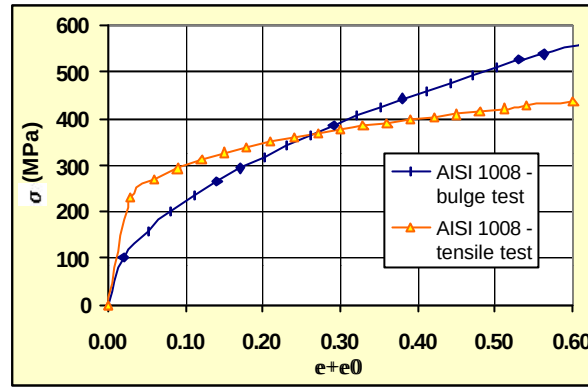


Figure I- 8: Flow stress curves determined trough the energy method, compared with tensile test data for low carbon steel AISI 1008-galvanized. $t_0=2$ mm , $R_0=28.58$ mm.

$$\text{Bulge test data: } \bar{s} = 722(0.041 + \bar{e})^{0.52} \quad \text{Tensile test data: } \bar{s} = 484\bar{e}^{0.21}$$

In fact, FEM simulations of free bulging have been run with both sets of flow stress data obtained from tensile test and bulge test. In general, when simulations are run with flow stress data obtained from the bulge test, the bulge height (BH) can be predicted with better accuracy. Simulations have been run with DEFORM-2D™, a commercially available implicit FEM code. Isotropic behavior is assumed for all of the materials.

Indeed, the percentage difference between experimental and calculated BHout is usually between 5 % and 15 %, except for very low values of pressure and at fracture. At low-pressure values, the deformations are small and the elastic behavior of the material may affect the accuracy of the model, which is based on a rigid-plastic approach. This hypothesis is confirmed by some tests run on aluminum alloys. In this case, fracture occurs at very small values of effective strain and, as a consequence, the proposed model failed in producing accurate flow stress data. Also in proximity of fracture, the bulge test seems to be less accurate: in some of the simulated experiments the difference can be up to 30 %. However, this effect is not very significant, since the measure of bulge height at fracture is affected by a large variance.

In FEM simulation runs with flow stress obtained by the tensile test of sheet, the accuracy is remarkably low, especially for higher values of pressure.

As an example, in Figure I- 9 the simulated and experimental bulge height are shown for the data given in Figure I- 8. In this particular case, if using tensile test data, the accuracy of the simulation is extremely low.

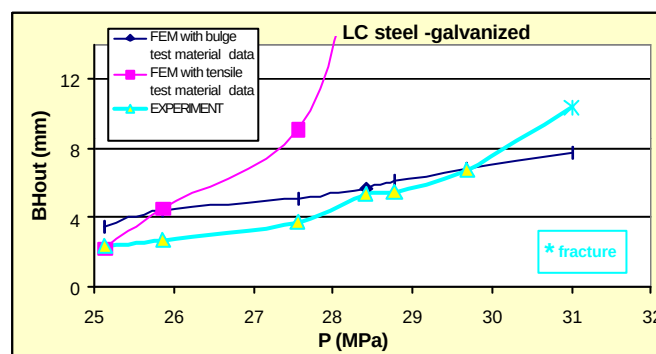


Figure I- 9: Experimental and predicted bulge height (BHout) for low carbon steel 1008-galvanized.

The analysis described so far is only concerned with comparison between experimental and FEA data of free bulging, i.e. expansion in an open die. However, the data obtained by the tube bulge test have been used at the ERC/NSM in several finite element analyses of real tube hydroforming operations in a closed die. The agreement between experimental and simulations results is usually satisfactory. This can be considered an indirect confirmation of the reliability of stress-strain data obtained by bi-axial tube expansion.

2.3.2 Further application of the model

In addition to the experiments described in the previous section 1.2.2, several other tube samples have been tested, through the bulging test available at the ERC/NSM, in order to determine flow stress curves. The goal of the present study was to determine whether there is any significant difference in the flow stress behavior of 304 stainless steels coming from different suppliers, or with different diameters and thickness.

The flow stress rule used is:

$$\bar{s}(\bar{e}) = K(\bar{e}_0 + \bar{e})^n, \quad 15$$

In Table I- 2, the complete range of tubes tested is given, along with the average K, n and e_0 values and the number of specimens used for each test.

Table I- 2: Table of experiments for the determination of flow stress (material: SS304)³.

OD (mm)	48.6			57.2				50.8	
material	A		B	C			E	D	
t_0 (mm)	1.5	2.0	2.1	1.7	1.3	0.61	1.7	2.0	1.5
K (MPa)	1207	1401	1555	1361	1654	1363	1648	1362	1357
n	0.351	0.406	0.615	0.559	0.589	0.503	0.627	0.504	0.470
e_0	0.021	0.028	0.030	0.034	0.026	0.007	0.034	0.027	0.020
# of specimens	2	1	2	1	1	1	1	2	2

A statistical analysis of the data (see Appendix A) proved the following conclusions:

- Flow stress curves of materials B, C, D and E do not significantly differ;
- K and n values are significantly smaller for material A (lower formability) than for the other four materials (Figure I- 10);
- Since the variability of the results of the bulge test is considerable, t_0 and OD do not induce any statistically significant differences on the flow stress curves. See, as an example Figure I- 11.

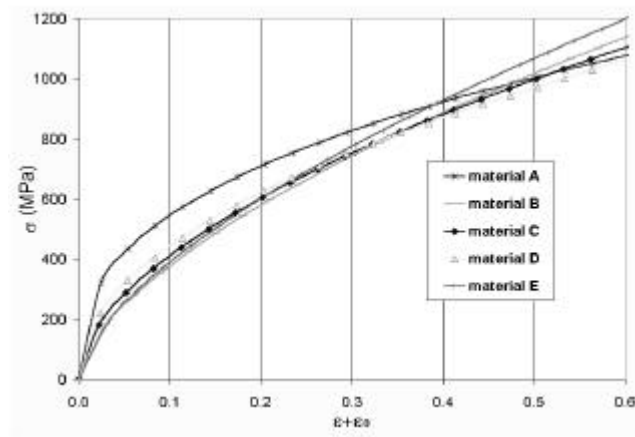


Figure I- 10: Effect of material AISI 304 supplier on the flow stress curves, with the ERC/NSM tube bulging test

³ Materials A and B have been supplied by a Japanese company, mat. C by a Canadian company, mat. D by a German company, mat. E by a supplier in the USA.

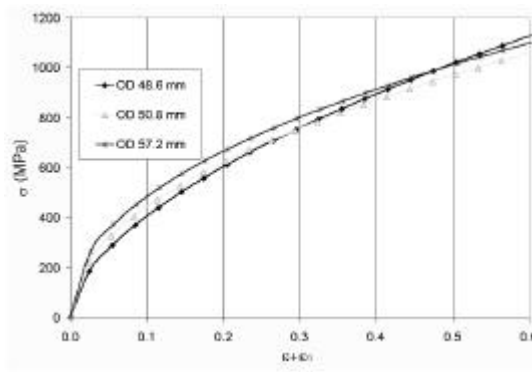


Figure I-11: Effect of tube outer diameter OD on the flow stress curves (AISI304)

2.3.3 Conclusions

The proposed inverse energy method is suitable to provide approximated flow stress data, by means of a very simple bulge test procedure. The results show that the flow stress obtained with the energy method seem to ensure better accuracy, when used as input of FEM simulations, if compared with the flow stress determined by tensile test.

The proposed energy method is suitable to provide approximated flow stress data, by means of a very simple bulge test procedure. The method is relatively simple, computationally inexpensive and, at present, it is implemented through a commercial mathematical software package (Mathematica 4).

- The hardening parameters produced by the method have been tested in FEM simulations and proved effective in predicting the bulge height, especially for larger degrees of deformation (i.e. larger bulge heights and therefore larger values of effective strain).
- The results show that the flow stress obtained with the energy method seem to ensure better accuracy, when used as input of FEM simulations, if compared with the flow stress determined by tensile test.

As a future work, an improvement of the model will be attempted, in order to take into account the anisotropy of the tubes.

2.4. The forming limits

Currently, a fully reliable and practical methodology to predict fracture in Tube Hydroforming (THF) processes is not yet available. This is mainly due to the uncertainty of the forming properties of the tubular materials. In common industrial practice, designers often use thinning of the material as a criterion to indicate when and where fracture occurs. This method roughly predicts the location of fracture and is not reliable with complex and non-linear straining paths. Therefore, other methodologies to predict the occurrence of fracture are needed.

Several methods are available for the determination of formability of sheet metals. They will be briefly described in the following sections: Forming Limit Diagrams, Damage models, Forming Limit Stress Diagrams, Numerical Perturbation methods. Since the literature on the subject is very extensive, only the most representative publications are given as a reference for each subject touched by this section.

When forming limits are determined experimentally, as for the evaluation of the stress-strain diagram, in order to obtain reliable data on material properties of the tube, a test procedure should be used, that is as close as possible to the hydroforming process. Therefore, the bulge test should always be the method of choice.

FORMING LIMIT DIAGRAMS (FLD)

The most common method for the representation and prediction of fracture in sheet metals is the Forming Limit Diagram (FLD), i.e. the locus on the plane $([\epsilon_{axial}, \epsilon_{hoop}]$ for THF) where localized or diffused necking occurs. The FLDs can be determined both experimentally [Keeler, 1965], empirically [Levy, 1996] or theoretically. In all cases, the main problem of FLDs is that they are strongly strain path dependent. This concept has been proven several

times for sheet forming, and it is also true for tube hydroforming [Davies, 2000]. Besides, the forming limits determined directly on the tube appear to be considerably different from the forming limits of the sheets, at least for high values of the ratio outer radius/wall thickness (R_0/t_0) [Kergen, 2000] and for small negative values of the minor strain. One of the main goals of this report is to confirm this result for positive values of the minor strain.

There are two main analytical methods for the determination of FLDs:

- the M-K method [Marciniak, 1967], based on the assumption of the presence of a defect (groove) on the deforming sheet;
- the plastic instability method [Storen, 1975].

These two methods, as initially proposed, present several limitations regarding their assumptions and validity, but an extensive literature has contributed to extend and improve their use. It is common believe that instability method is not very accurate [Yoshida, 1995]. This is probably the reason why the M-K method has been used most often in recent studies on FLDs. However, the M-K method still presents several problems, listed as follows.

- It requires different approaches for the left (compression-tension) and right (tension-tension) sides of the FLD [Hutchinson, 1978].
- It appears that it overestimates the experimental forming limits in balanced biaxial loading conditions and underestimates the experimental forming limits on plane strain condition [Chan, 1989],
- The size f of the assumed defect has to be iteratively determined, if a good match with the experimental data is to be found [Davies, 2000].

It appears that the M-K method, at least in its original formulation, is more suitable as a theoretical tool for the evaluation of the phenomenon rather than a way to reduce the need for experimental fracture data. In order to reduce the dependence on strain path and to improve the accuracy of the M-K model, many authors have worked on its extensions. Including a micro-structural damage effect on the constitutive equations can reduce the strain path dependence [Chan, 1989]. However, these modifications usually increase the complexity of experimental testing in order to determine the damage properties of materials and they increase the complexity of the numerical computations, too.

DAMAGE MODELS

Many authors have tried to avoid the use of the M-K method for the prediction of fracture and to base their analysis only on the growth of damage in the material. In this case, a damage function is included in the constitutive law and a numerical simulation is run. When the damage reaches a Critical Damage Value (CDV), which is supposed to be a property of the material, fracture is predicted. The method, in respect of the M-K and plastic instability methods seems to have the following advantages: it reduces the strain path dependency [Chow, 2001], [Tang, 1999] and it helps in the localization of fracture [Brunet, 1998]. Moreover, the damage method can still be used to build FLDs and it usually provides a unique approach both for the right (tension-tension) and left (compression-tension) sides [Chow, 2001].

The damage properties (and the critical damage value) have to be determined experimentally, and usually these tests are more complex than a standard tensile test. Moreover, it has not been completely proved whether the CDV is a material property or a function of the specific process. For these reasons, in some cases the damage function (used to predict the localization of necking) is coupled with a criterion used to predict the occurrence of necking, based on stresses [Brunet, 1996] or strains [Brunet, 1998].

An alternative approach is to correlate the micro-structural damage to macroscopic variables, without incorporating it into the constitutive equations. Macroscopic criteria for ductile fracture are commonly used in bulk forming to predict the forming limits [Gouveia, 2000]. Usually the damage function is an integral function of stresses and strains, calculated for each element in the FEM mesh. The damage function has to be used together with a constant value (Critical Damage Value, CDV). In other words, the simulation can proceed as long as no element in the workpiece reaches the value CDV :

$$f(s_{ij}, e_{ij}) < CDV$$

Several studies proved that the value of CDV is often dependent on the deformation path [Groche, 1991], though the method seems to be effective within a single process [Takuda, 1999].

The method is one of the simplest available, and requires only a few experimental tests to determine the *CDV*. Therefore, goal of the present study is to evaluate the feasibility of this method for tube hydroforming, by the free bulging test.

FORMING LIMIT STRESS DIAGRAMS (FLSD)

An alternative way to represent the forming limits, is to do it in terms of principal stresses ($[\sigma_z, \sigma_\theta]$ for THF), rather than in principal strains. Many authors proved that the forming limit stress diagrams (FLSDs) are more intrinsic than the conventional FLDs, i.e. they are less strain-path dependent. As well as the FLDs, the FLSDs can be determined experimentally or theoretically [Arrieux, 1995], [Arrieux, 1997], [Sing, 1997].

However, it appears that FLSDs determined through different techniques tend to overlap [Zhao, 1996], therefore the stress representation seems to be much more robust than the strain representation. For this reason, additional goals of the present report are:

- to determine an experimental procedure for the determination of the FLSDs from the tube free bulging bulge test;
- to evaluate their suitability for tube hydroforming applications.

NUMERICAL PERTURBATION METHODS

This technique is a numerical method, suitable for integration into FEM codes. The method consists in perturbing, at each step, the solution given by the standard FE code, in order to evaluate its stability. The method is based on the assumption that local necking is an instability condition of the local mechanical equilibrium. The method seems to be effective for different sheet forming applications, [Boudeau, 2000] and it may be implemented [Boudeau, 1996] without the need for additional material parameters (unlike the damage method).

The main drawback of the method is the extra-computational time required in order to carry out the perturbation analysis. Furthermore, the method is not suitable to be tested by a commercially available FEM code, but it requires a special purpose code. Therefore, although the method seems promising, it will not be tested in the present study.

2.5. The use of the bulge test for the determination of FLDs

The bulge test can be easily used for the experimental determination of FLDs. Before conducting the test, it is necessary to etch (mechanically, chemically or electrochemically) a grid on the outer tube surface; the grid can be made up of circles, as shown in Figure I- 12. Figure I- 12(a) shows the circle grid of undeformed tube, while Figure I- 12(b) shows the circle grid of deformed tube. As the internal pressure increases and the tube bulges, the shape of the grid changes, and the circles turn into ellipses. By measuring the diagonals of these ellipses along the hoop direction and the axial direction (Figure I- 13), strains can be calculated at each pressure level.

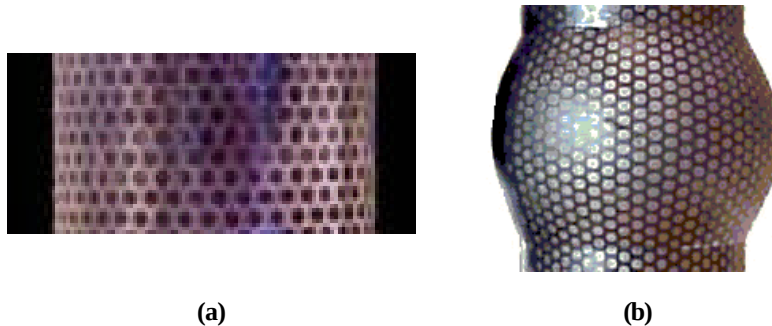


Figure I- 12: electrochemically etched circle grid of undeformed (a) and deformed (b) tubes. The standard size (diameter) of circle grid used in this study is 2.54 mm

When the tube is fractured, the strains are measured along the two edges of the fracture (refer to Figure I- 14(a)), at the centerline. The average of strains on the right and left sides of the fracture is taken. The same measuring procedure is applied also if the tube is not fractured yet, but a localized necking has already occurred, as in Figure I- 14(b). The grid allows to measure deformations in axial and circumferential directions be along the edges of the fracture and therefore to select one point on the FLD in the e_1 - e_2 plane. e_1 is the major strain ($e_1 = \epsilon_\theta$); e_2 is the major strain ($e_2 = \epsilon_z$).

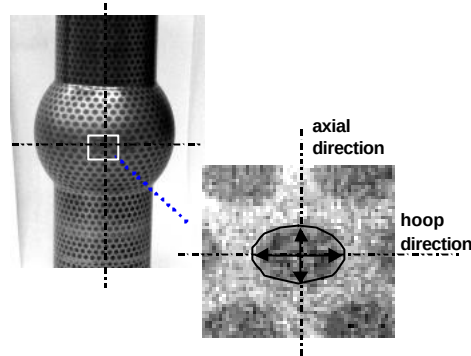


Figure I- 13: circle grid measured strains in the hoop and axial directions

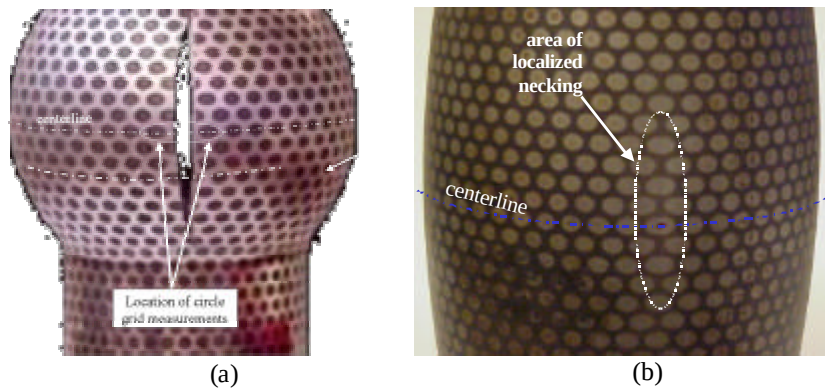


Figure I- 14: location of circle grid measurement when the tube is fractured (a) or necked (b).

The right side of the FLD can be explored by clamping a tube both ends and bulging it with internal pressure (Figure I-5). In this case, both the stresses in hoop and axial directions (see Figure I- 13) are tensile. As a consequence, both resulting strains will be positive (zone A in Figure I- 15). By changing the value of the bulge width (w in Figure I-5), the material follows different strain paths on the e_1 - e_2 plane, and therefore fractures at different locations on the FLD, which can be determined experimentally. As w increases, the test tends to induce a plane state of strain and to move the point on the FLD downwards and leftwards (Figure I- 16).

In order to obtain strain states in which the hoop strain is positive, but the axial strain is negative (Zone B in Figure I- 15), the specimens have to be burst by the simultaneous action of internal pressure and axial compression. This kind of test requires more complex and costly equipment than in the previous case. Several research labs worldwide have developed such instruments. As an example, in [Kergen, 2000] the equipment shown in Figure I- 17 is used. Another example can be found in [Davies et al., 2000].

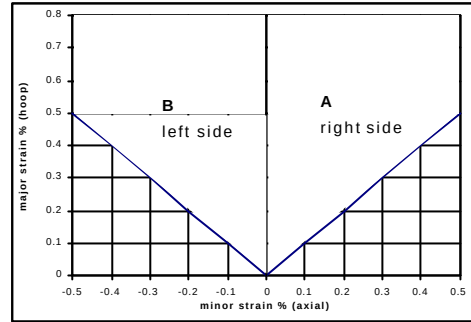


Figure I- 15: 2 zones in Forming Limit Diagram;
Zone A: without axial feeding, and Zone B: with axial feeding

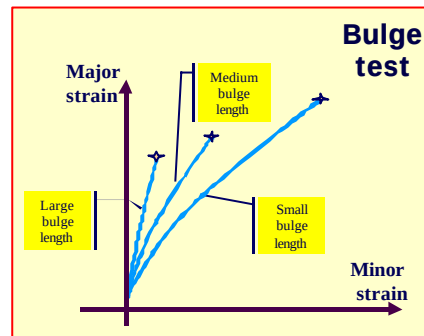


Figure I- 16: strain paths with fixed tube ends

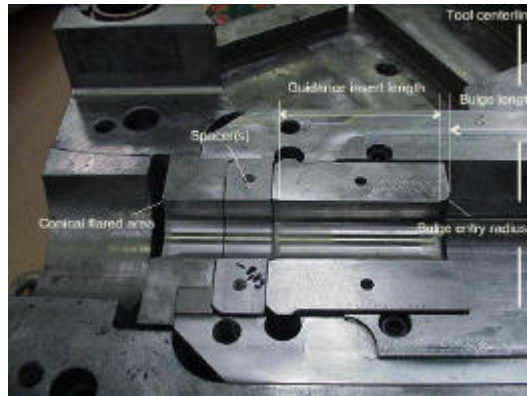


Figure I- 17: a photo of a disassembled tooling for bulge test with axial compression
(courtesy of Usinor, France)

2.6. Experimental findings

In this study, experiments have been conducted only to cover the right side of the FLD, because the tooling that was used at the ERC/NSM does not allow feeding.

2.6.1 Experiments for the determination of FLDs

In order to evaluate a valid procedure for the prediction of fracture in Tube Hydroforming, three different approaches have been considered in this study. The first approach is the possibility of using Forming Limit Diagrams (FLDs) obtained directly from the tubes. The second one is the possibility of employing a ductile fracture criterion (i.e. a damage function) together with a critical damage value. The third one is the conversion of

experimental (FLDs) into Forming Limit Stress Diagrams (FLSDs). The objectives of the present study can be summarized in bullets as follows.

- To determine the Forming Limit Diagrams (FLDs) of different tubular materials directly from the free bulging test, on the tension-tension quadrant of the FLD graph.
- To determine if and how the tube FLD differ from the sheet FLD.
- To evaluate the feasibility of applying macroscopic empirical damage function for the determination of forming limits during FEM simulation of THF.
- To determine an experimental procedure for the determination of the Forming Limit Stress Diagrams (FLSDs) directly from the tube free bulging test.
- To evaluate the suitability of FLSDs for tube hydroforming applications.

RESULTS OF PRELIMINARY EXPERIMENTS

The results presented in this section have been verified experimentally on different materials and different tube geometries (outside diameter and wall thickness). The tests performed are summarized in the following list.

1. Stainless Steel (SS AISI 304): a preliminary experiment has been conducted with 50.8 mm bulge width ($w=25.4$ mm). The stainless steel used was roll-formed and laser-welded, as common practice for the THF processes. Dimensions and material properties of this tubing can be found in Table I- 3. The wall thickness of this material ($t_0 = 0.6$ mm) is the lower range of thickness commonly used in tube hydroforming.
2. Low Carbon Steel (LCS AISI 1008): a set of experiments has been carried out for 2 different values of the Outside Diameter (63.5 mm and 89 mm) and 2 different values of the thickness (2 mm and 3 mm).

Table I- 3: Material properties and geometry of 304 Stainless Steel specimens. The material properties were obtained directly from tube specimens in hydraulic bulging test, using Krupkowsky's Law ($\bar{s} = K(e_0 + \bar{e})^n$)

Parameter	Value
Diameter	57.15 mm
Thickness	0.6 mm
Strength Coefficient (K)	1503 MPa
Strain-Hardening Coefficient (n)	0.716
Pre-strain (ϵ_0)	0.06

Before the test, each tube has been electrochemically etched as shown in Figure I- 12. The dimension of a standard etched circle grid is 2.54 mm.

Tubes have been pressurized until bursting to determine the maximum allowed pressure of each setup. Then the bursting pressure has been reduced randomly (for example to 95 %, 92 %, etc.) in order to determine the strain paths at the center line. For this set of experiments, 5 pressure levels have been tested (Table I- 4). Each pressure level has been replicated 3 times.

Figure I- 18 shows the deformation at different pressure levels. Figure I- 19 shows the measured strains along major (hoop- direction) and minor (axial- direction) axis at the middle of bulging area. Also, Figure I- 19 shows the plot of measured strains obtained from the tubing and forming the limit curve obtained from sheet metal. In this case, it can be seen that the fracture occurs a little above the sheet metal's FLD. The straining path appears to be slightly non-linear.

Table I- 4: Pressure level used to run the experiment for this FLD study.

Pressure Level	Values (MPa)
	11.72
P_1	
P_2	12.76
P_3	14.48
P_4	16.55
P_B	18.28*

* bursting pressure

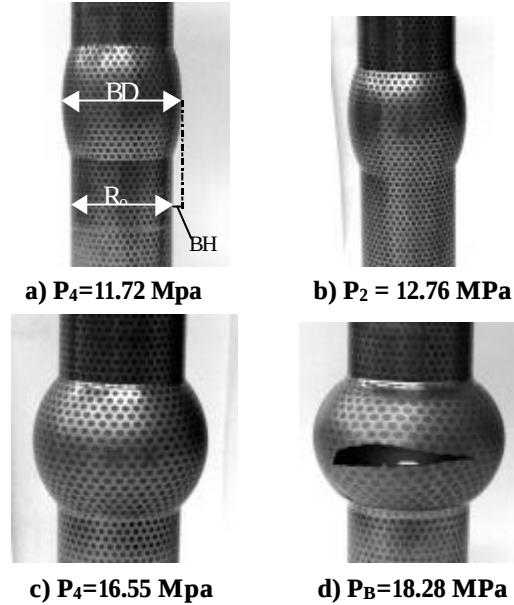


Figure I- 18: Deformation and circle grid changes at different pressure levels .

The material used in the second set of preliminary experiments is commercially available Low Carbon Steel tubing, intended for hydroforming applications. The tubes are made from cold rolled sheets, electric-resistance seam welded, normalized, and rotary straightened. Dimensions and material properties of this tubing can be found in Table I- 5.

Only 3 pressure levels have been tested due to a limitation on the number of samples. The strains along hoop and axial direction have been measured at the middle of bulging area. Figure I- 20(a) shows the measured strains compared to the FE –simulated strain path at different pressure levels. The FEM strain path appears to be strongly non-linear and it is reasonably close to the measured values. Indeed, DEFORM-2D, as well as any 2D-axisymmetric FEM model is not able to simulate properly the behavior of the material when approaching necking. In fact, in the real process, necking occurs as a strip of localized thinning along the axial direction (see Figure I- 20(b)). Therefore during necking, the process is no longer axisymmetric.

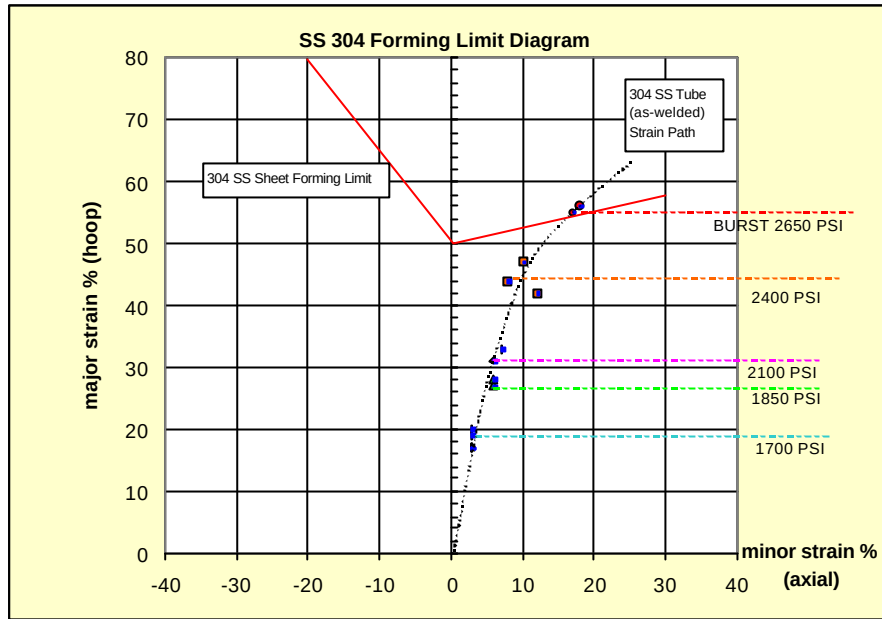
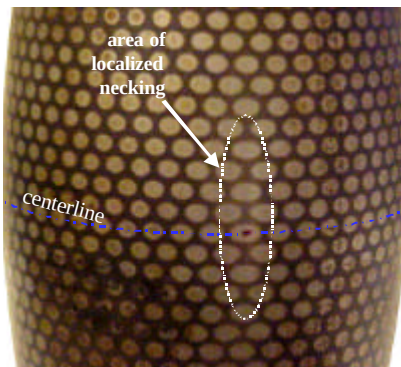
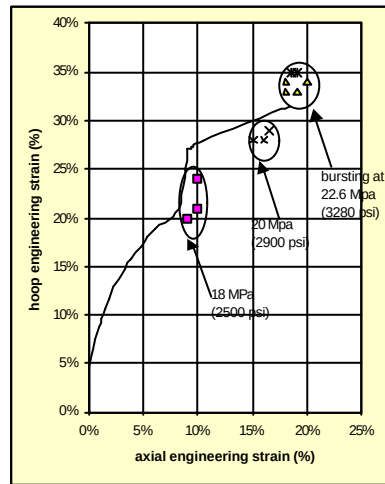


Figure I- 19: Measurement of major and minor strains at the middle of bulging area of SS304: $t_0 = 0.6$ mm, $OD = 57.15$ mm, $w = 25.4$ mm

Table I- 5: Material properties and geometry of Low Carbon Steel 1008 specimens. The material properties were obtained directly from tube specimens in hydraulic bulging test, using the Krupkowsky's Law ($\bar{s} = K(e_0 + \bar{e})^n$)

Parameter	Value
Diameter	88.90 mm
Thickness	2.00 mm
Strength Coefficient (K)	563 MPa
Strain-Hardening Coefficient (n)	0.323
Pre-strain (ϵ_0)	0.01



(a).....(b)

Figure I- 20: (a) Comparison between the FEM deformation path and the experimental observation for LCS1008 $t_0 = 2.0$ mm, $OD = 88.90$ mm, $w = w = 25.4$ mm (b) necking in tube bulging

The main conclusions of the preliminary experiments are described as follows.

- The bulge tooling at the ERC/NSM can be used, together with the circle grid method, to evaluate FLDs for tubes, but only on the right side (tension-tension) of the FLD.

- The FEM software (DEFORM 2D) can be used to predict the strain path in deformed tubes with reasonable accuracy, but only before necking occurs. It cannot be used to predict necking.
- As expected, the maximum deformation of this process occurs at the middle of bulge width, and both major (hoop –direction) and minor (axial –direction) engineering strains are in the positive side.
- The strain path of this process appears to be non-linear.

FURTHER EXPERIMENTAL INVESTIGATIONS

In a second phase, different tests have been conducted for different bulge widths ($w=2, 3, 4$ in).

In the previous section, a preliminary verification between FEM, experimental results and sheet metal FLDs has been done with materials SS304 and LCS 1008. Furthermore, in the previous study [Aiello et al., 1999] it was found that varying the bulge width generates different strain paths. Various bulge widths have been simulated with FEM, in order to determine the dimensions that generate a significant difference in strain path. Then, a set of experiments, based on simulation results, has been setup.

For each bulge width, a large number of pressure levels should be tested in order to obtain an accurate and continuous deformation path. However, only 3 pressure levels have been applied for each bulge width, due to limitations in the number of samples available. The ultimate pressure level has been accurately selected as the bursting pressure (the pressure level that causes fracture). The other 2 levels have been selected randomly by reducing the bursting pressure.

Table I- 6: Material properties of Low Carbon Steel 1008 tubing.

Power Law: $\bar{s} = K\bar{e}^n$; Krupkowsky Law $\bar{s} = K(\bar{e}_0 + \bar{e})^n$

Parameter	Value	
Diameter	63.50 mm	
Thickness	3.00 mm	
Yield strength (σ_y)	280 MPa	
Tensile strength (σ_t)	388 MPa	
Total elongation (%)	44	
Flow stress parameter	Power law (from tensile test)	Krupkowsky law (from bulge test)
Strength Coefficient (K)	633 MPa	653 MPa
Strain-Hardening Coefficient (n)	0.178	0.253
Pre-strain (ϵ_0)	-	0.01

Table I- 7 shows the experimental matrix for the determination of the FLD for Low carbon steel 1008. Dimensions and material properties are shown in Table I- 6.

Figure I- 21 and Figure I- 22 show the plot of measured engineering strains along hoop and axial directions. It is worth noting that, in some cases, the experiments are affected by a large deviation from the average, as for the point circled with a dotted line in Figure I- 21(b).

In Figure I- 23 all the bursting strain states are plotted, for the different bulge widths. The graph shows that by decreasing the bulge width, both axial and hoop strains at fracture tend to shift upward and rightward along the trendline shown in the chart. This can be easily explained by considering that the larger the bulge width, the closer the process will be to a plane strain condition.

Table I- 7: Bulge widths and pressure levels used to conduct Forming Limit Diagram. Material: Low Carbon Steel 1008, $OD = 63.5 \text{ mm}$, $t_0 = 3.0 \text{ mm}$.

Bulge width, $2 \cdot w$, (mm)	Pressure level	Pressure (Mpa)
50.80	$P_{w2,1}$	37.0
		43.0
	$P_{w2,2}$	
76.20	$P_{w2,B}$	45.0*
	$P_{w3,1}$	37.0
101.60		
	$P_{w3,2}$	38.0
	$P_{w3,B}$	39.0*
101.60	$P_{w4,1}$	37.0
	$P_{w4,2}$	38.0
	$P_{w4,B}$	39.0*

* indicates the bursting pressure

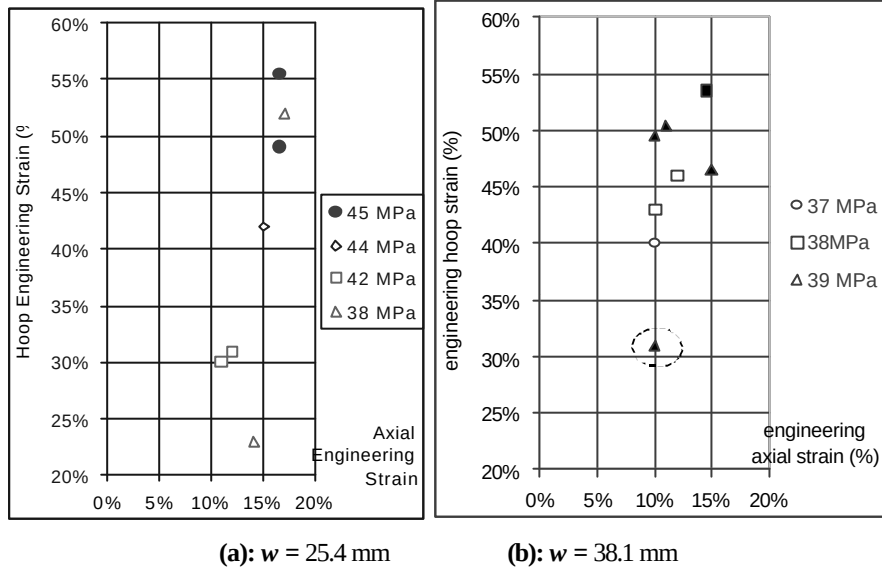


Figure I- 21: Measured strains for LCS1008; $t_0 = 3 \text{ mm}$, $OD=63.50 \text{ mm}$. Note: solid points indicate bursting.

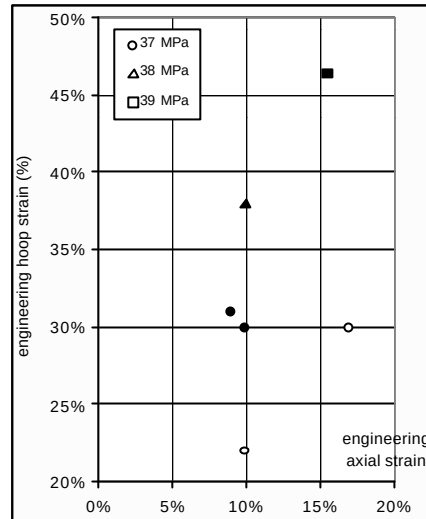


Figure I- 22: Measured strains for LCS1008 $t_0 = 0.118$ in (3mm), $OD=2.5$ in (63.50 mm) and $w = 4$ in (101.6 mm). Solid points indicate bursting.

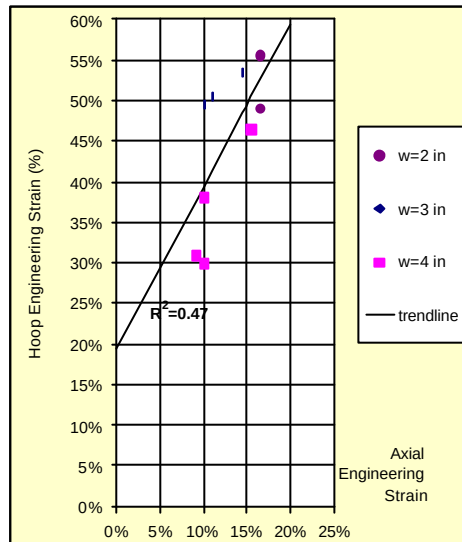


Figure I- 23: Graph shows the fracture points for all 3 different bulge widths for LCS1008. Ratio $R_0/t_0 = 11$. A linear regression fit curve is added to the chart: the value of R^2 of the regression is 0.47.

COMPARISON OF FORMING LIMIT DIAGRAMS OBTAINED FROM TUBES AND SHEETS

In [Davies et al., 2000], experiments conducted on aluminum tubes (AA 6260-T4) showed that FLDs obtained through free bulging tests of tubes are similar in shape to conventional sheet FLDs, and can be approximated by theoretical forming limit diagrams (M-K method) with the appropriate choice of imperfection level. However, none of the mentioned FLDs were able to correctly predict failure in a real THF operation (see Figure I- 24). Two conclusions can be deduced by reading this paper:

- although the shape is similar, the position of traditional FLDs (obtained by theoretical or empirical methods) is not necessarily the same as the position of FLD obtained through bulge tests.
- the accuracy of FLDs in real production is very low, regardless of the method used to determine them.

The goal of the present and the following sub-sections is to provide further evidence to the comparison between bulge test-obtained FLDs and other methods and to enlarge the knowledge on bulge test obtained FLDs.

Unfortunately, not enough experimental evidence is available on real THF bursting experiments in order to conclude anything as for the accuracy of FLDs in real production.

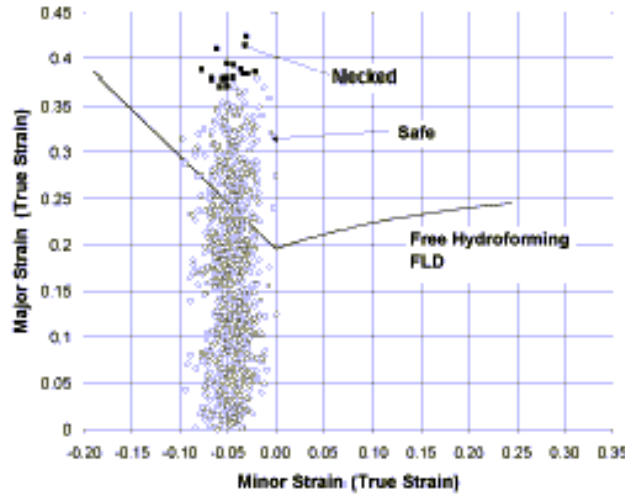


Figure I- 24: experimental FLD and circle grid analysis of a real THF operation [Davies et al., 2000]

The trendline in Figure I- 25 can be considered as the experimental FLD of the tested tubular material. It appears useful to compare this curve with the FLD of the original sheet, by using two different empirical models. The first model is suggested by the North American Deep Drawing Research Group (NADDRG) [Levy, 1986]. The second is the default empirical model implemented in LS-DYNA/DYNAFORM.

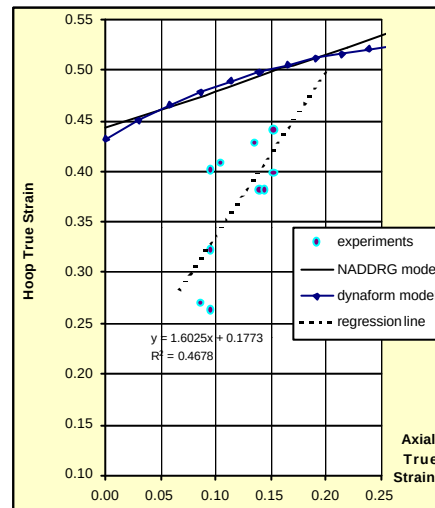


Figure I- 25: Graph shows the comparison of the empirical and the experimental FLDs.

The NADDRG model can be described as follows. Based on Equation 1, the engineering major strain at plane strain condition can be calculated.

$$FLD_0 = (23.3 + 360/25.4*t_0)*(n/0.21) \quad (16)$$

where

FLD_0 = Engineering major strain at plane strain condition

n = strain-hardening exponent

t_0 = original wall thickness of sheet metal (in)

Then, starting from this plane strain fracture point, a line is drawn on the right side with a slope of 0.364 (approximately 20 degrees in true strain). On the left side, the line is approximately drawn at -45 degrees in true strain.

When using the NADDRG model with the material data shown in Table I- 6, the model gives an intercept equal to 0.44 in true strain. The value of the intercept is extremely sensitive to a change in the n -value, but the slope, as

already stated, is always the same. It is clear in Figure I- 25 that the position and shape of the experimental linear regression line ($R^2=0.47$) is considerably different from the shape and position of the empirical models. The average error between the NADDRG and experimental FLDs is +26.4 % in true strain.

EFFECT OF TUBE THICKNESS AND DIAMETER ON THE FLD

The FLD for the sheet is on a higher position, and this is consistent with the literature [Kergen, 2000]. According to the same paper [Kergen, 2000], the distance between the tube's FLD and the sheet FLD increases as the ratio R_0/t_0 between the tube initial outer radius and the tube wall thickness decreases. Additional experiments with different values of wall thickness, tube outer radius and bulge width have been conducted to prove this phenomenon, both on carbon and stainless steels. The results are in agreement for the SS304 (as shown in Figure I- 26).

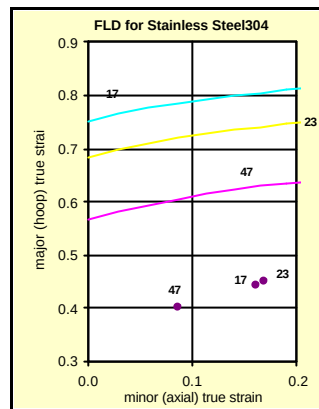


Figure I- 26: effect of the ratio R_0/t_0 on the FLD of SS304.

Straight lines are the sheet's FLDs (empirical model) and the points represent the averages of the experimental tests. The different values (17, 23, 47) of the ratio R_0/t_0 are printed on the chart.

However, both for low carbon and stainless steels, it appears that all the data, even with changing bulge widths, wall thickness and outer radius, are clustered along the same FLD. In fig. 3.13 this is even more evident, where the experimental FLD calculated as in Figure I 27 is plotted again, together with the averages of other 4 experimental conditions and with a new overall regression line. By adding 4 more points with different R_0/t_0 values to the FLD, its R^2 values increases from 0.47 to 0.54 and its position does not change much.

In order to verify if the same behavior is shown on the left side of the FLDs, experimental fracture data found in [Kergen, 2000] have been plotted in Figure I- 28. The data have been obtained by several tube bulging tests with axial feeding and different values of R_0/t_0 . However, it is possible to build an exponential regression line showing a very high value of R^2 (0.85). Therefore, also in this case, fracture data with different R_0/t_0 ratios seem to belong to the same FLD.

It can be concluded that the FLDs of steels, for the free bulging, are not very sensitive to a change in the initial tube dimensions. This can be explained as follows.

It is well known that an increase of thickness has a beneficial effect on the forming limits. On the other hand, a thicker sheet undergoes higher levels of pre-strain while form rolled into a tube, which is detrimental to formability. The two effects somehow compensate each other and the final influence of thickness on the position of the FLD is small.

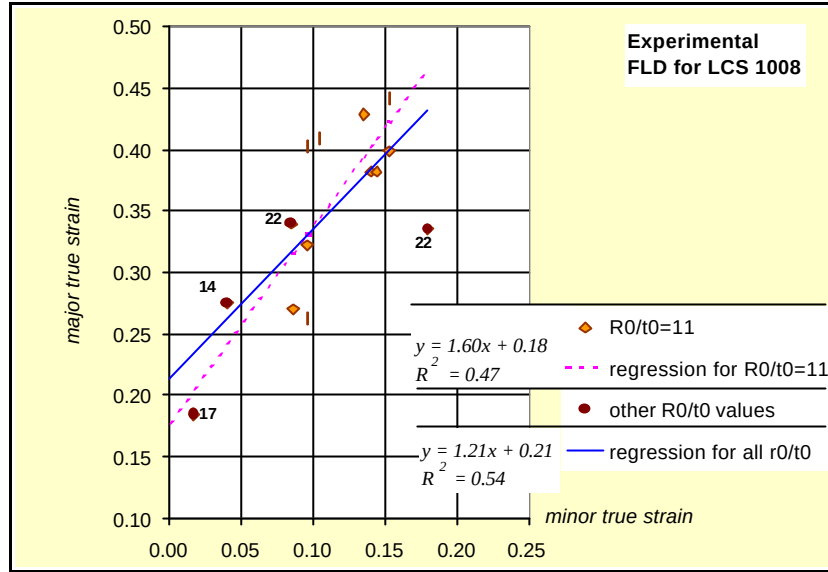


Figure I-27: effect of the ratio R_0/t_0 on the FLD of LCS1008.

The 4 additional experimental points (the actual values of R_0/t_0 are printed on the chart) are very close to the experimental FLD for $R_0/t_0=11$.

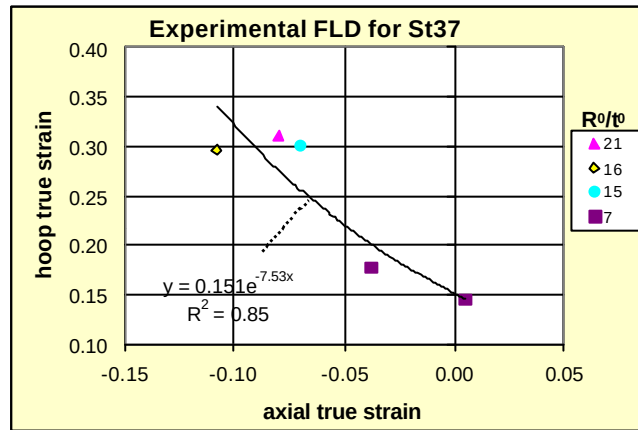


Figure I-28: experimental FLD with negative minor strain. Data extracted from [Kergen, 2000]. Material is tubular carbon steel (DIN St37 or ASTM A36), hot rolled and electric resistance welded.

2.6.2 Experiments for the determination of Damage functions and critical damage value

As already previously mentioned, criteria for ductile fracture are commonly used in bulk forming to predict the forming limit of the workpieces. A ductile fracture criterion is generally represented by a local *damage* function of the deformation history. Usually it is an integral function of stresses and strains, calculated for each element in the FEM mesh. The damage function has to be used together with a constant value (Critical Damage Value, *CDV*). In other words, simulation can proceed as long as no element in the workpiece reaches the value *CDV*:

$$f(s_{ij}, e_{ij}) < CDV \quad (I-17)$$

To be considered reliable, a fracture criterion should be able to predict where in the workpiece and when during the process the fracture occurs.

Based on these hypotheses, many criteria have been proposed also for sheet forming by several researchers, based on empirical as well as theoretical observations [Takuda et al., 2000], [Groche, 1991].

The purpose of the present study is to test the validity of some ductile fracture criteria in Tube Hydroforming. The following criteria have been considered:

1) Oyane Fracture Criterion

$$\int_0^{e_f} \left(\frac{s_h}{\bar{s}} + a \right) d\bar{e} = CDV \quad (I-18)$$

2) Cockcroft & Latham Fracture Criterion

$$\int_0^{e_f} \frac{s_{\max}}{\bar{s}} d\bar{e} = CDV \quad (I-19)$$

3) Brozzo Fracture Criterion

$$\text{Error! Objects cannot be created from editing field codes.} \quad (I-20)$$

The variable a is a material constant. See the Nomenclature for an explanation of other symbols.

OYANE FRACTURE CRITERION

Oyane's fracture criterion has been successfully implemented in various metal forming applications. Its validity has been proven for the prediction of fracture mainly in sheet metal forming processes. Oyane's fracture criterion is represented by the following equation:

$$\int_0^{e_f} \left(\frac{s_m}{\bar{s}} + a \right) d\bar{e} = CDV \quad (I-21)$$

The constants a and CDV are determined experimentally by running a set of destructive tests under at least two stress conditions.

Oyane criterion does not appear to be suitable for THF processes since it predicts the location of the maximum damaged zone near the die corner instead of the center of the bulging (see Figure I- 29).

However, Oyane damage criterion is significantly influenced by the value assigned to the material constant (a). In this study, the value to the constant a has been assigned according to the values used in literature (For low carbon steel $a = -0.043$) [Takuda et al., 1999].

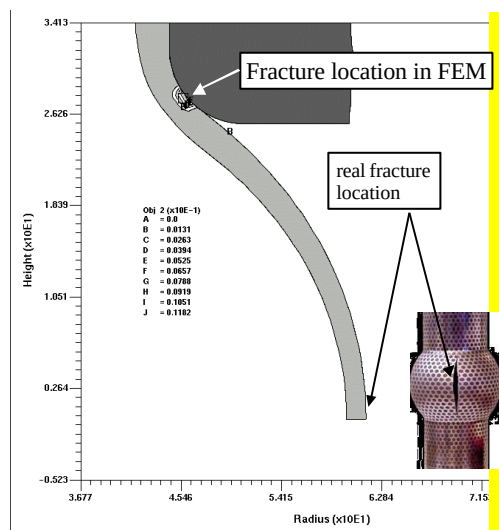


Figure I- 29: simulation result based on Oyane fracture criterion to illustrate the location of bursting area

COCKROFT & LATHAM FRACTURE CRITERION

The ductile fracture criterion proposed by Cockroft and Latham is generally expressed by the following equation:

$$\int_0^{e_f} s_{\max} de \quad (I-22)$$

However frequently the maximum normal stress is divided by the effective stress, thus obtaining the Normalized Cockroft and Latham damage function:

$$\int_0^{\bar{e}_f} \frac{s_{\max}}{\bar{s}} d\bar{e} = CDV \quad (I-23)$$

This criterion is probably the most common in bulk forming, the function is quite simple, and no material constant needs to be determined. The applications to THF process show that this criterion can correctly predict the fracture zone as shown in Figure I-30. The figure shows that Cockroft & Latham's criterion can predict correctly the zone where the fracture originates. The next step then is to verify if the moment when fracture occurs is correctly predicted by this criterion.

For this purpose the damage value has been plotted in respect of the bulge height for the 2 different bulge widths ($w=25.4$ and 50.8 mm (Figure I-31). The results for $w=38.1$ mm are not shown because they lie between the other two bulge widths and therefore they do not add meaningful information to the chart.

According to the simulation, for a given bulge diameter, the damage value increases as the bulge width decreases. However, the experiment shows the opposite, namely that the bulge diameter BD at fracture slightly increases when the bulge width w is reduced. Therefore, any choice of a constant CDV would always overestimate BD at fracture for large w and underestimate BD for small w . Furthermore, a correct value of CDV is very hard to evaluate through a 2D-axisymmetric code, since necking is a 3-D phenomenon.

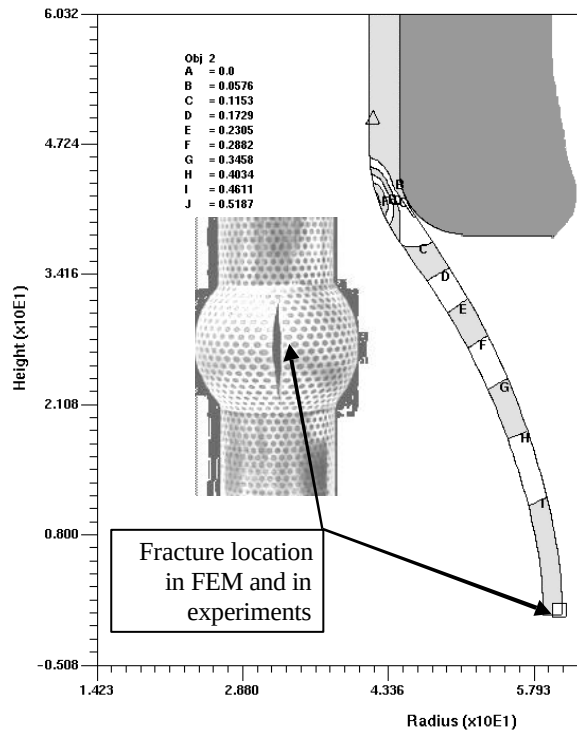


Figure I-30: simulation result based on Cockroft & Latham fracture criterion to illustrate the location of bursting area

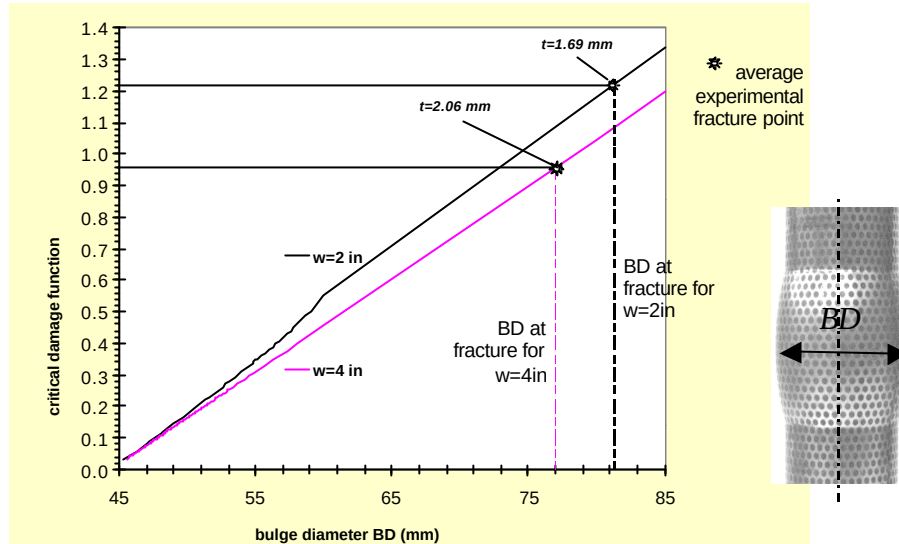


Figure I-31: Cockroft & Latham damage function vs. the bulge diameter (BD) for bulge widths $w=25.4$ mm and $w=50.8$ mm. Thickness values at fracture are shown too. $t_0=3$ mm, initial $BD=63.5$ mm.

BROZZO FRACTURE CRITERION

The simulation, as seen in Figure I-32, shows the correct location of fracture based on the Brozzo fracture criterion. As for the Cockroft & Latham criterion, the damage value is higher for smaller bulge widths, and this contradicts the experimental evidence.

Summarizing, three different fracture criteria have been tested with FEM: Oyane, Cockroft & Latham and Brozzo.

Oyane's criterion is not able to predict the correct location of fracture. Cockroft & Latham and Brozzo criteria can predict the correct location of fracture, but do not predict correctly the influence of bulge width.

Therefore, additional studies are necessary in order to determine whether these or other criteria can be used to predict fracture in the 3D FEM simulation of THF processes. The use of a 3D model is suggested for future work.

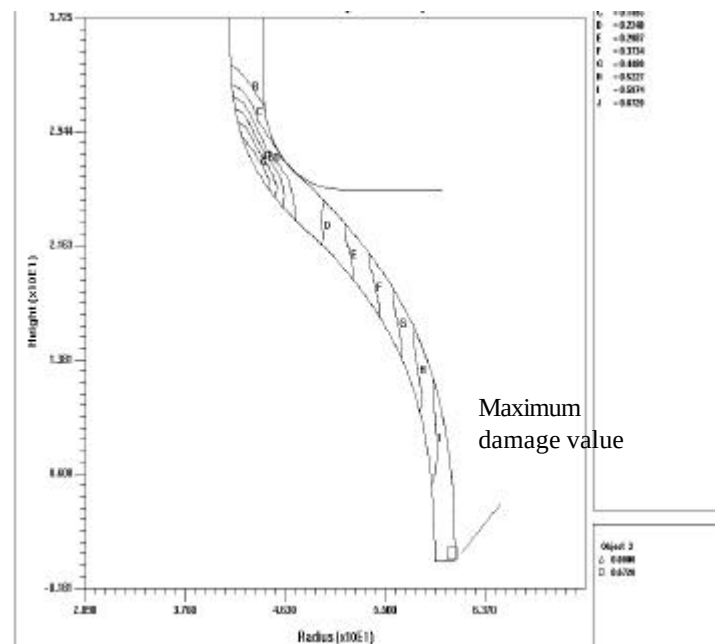


Figure I-32: simulation result based on Brozzo fracture criterion to illustrate the location of bursting area.

2.6.3 Experiments for the determination of FLSDs

Any kind of FLD (theoretical, empirical or experimental) can be transformed into a Forming Limit Stress Diagram (FLSD). In other words, for each state of strain (ϵ_θ and ϵ_z), a correspondent state of stress (σ_θ and σ_z) can be calculated thanks to simple equations (see Appendix B).

From the bulging experiments the FLSD can be calculated by using equation (B-5), (B-6), (B-7) in Appendix B. The result of this procedure is shown in Figure I- 33, where experimental values of principal stresses are clustered by bulge width. The average thickness t and bulge height $BH=BD/2-R_0$ at fracture are also shown, for each bulge width. A quadratic regression line is added to the experimental data.

FLSDs can be determined through several different methods:

- *experimentally*, i.e. converting an experimental FLD to an FLSD;
- *theoretically*, directly or converting from a theoretical FLD;
- *empirically*, directly or converting from an empirical FLD.

According to the literature review, FLSDs obtained through different methods tend to be closer to each other than the corresponding FLDs.

Indeed, the present study shows that the average absolute error between the experimental FLSD and the empirical model embedded in LS-Dyna/Dynaform is only 13.4 %. The error in the position of the corresponding FLDs is much larger: about 26 %.

The empirical models are quadratic functions of the minor stress. The experimental FLSD can also be well described by a quadratic regression equation ($R^2=0.93$), but its curvature is quite different (see Table I- 8).

The present study therefore confirms that the FLSDs are more robust and reliable than the FLDs for free tube bulging of steels. Similar results have been obtained for stainless steel.

In Figure I 34, the experimental FLSD shown in Figure I 33 is plotted again, along with the stress paths predicted by explicit FEM simulation with LS-Dyna/Dynaform. Simulations have been stopped when the BH reaches the experimental average value, which is also reported on the chart. Figure I 34 shows that the experimental FLSD is very close to the stress values at fracture given by the FEM simulation.

Table I- 8: quadratic equations describing the FLSDs.

FLSD model	Equation	curvature	R^2
Experimental	$\sigma_\theta = -4.88 \sigma_z^2 + 3.91 \sigma_z - 0.20$	0.102	0.93
Empirical (Dynaform)	$\sigma_\theta = -0.85 \sigma_z^2 + 0.81 \sigma_z + 0.47$	0.588	1

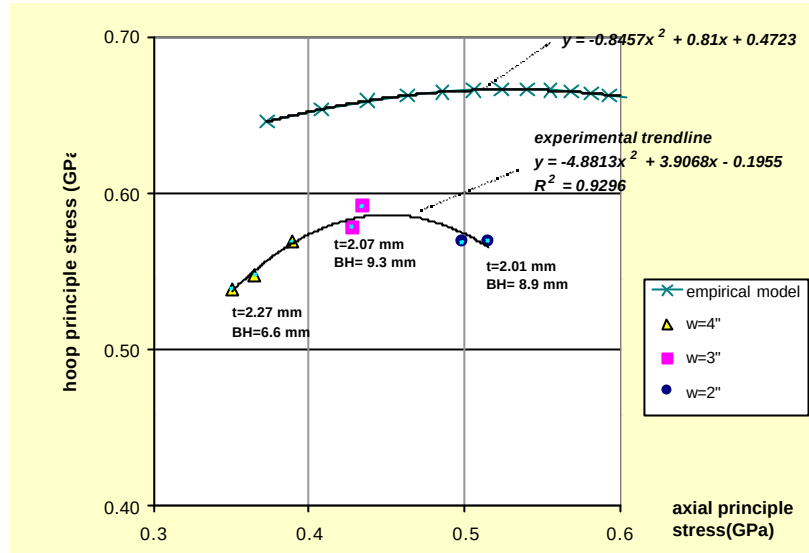


Figure I-33: comparison of experimental and empirical Forming Limit Stress Diagrams for low carbon steel 1008. Experimental points are clustered by bulge width. The initial wall thickness is 3 mm.

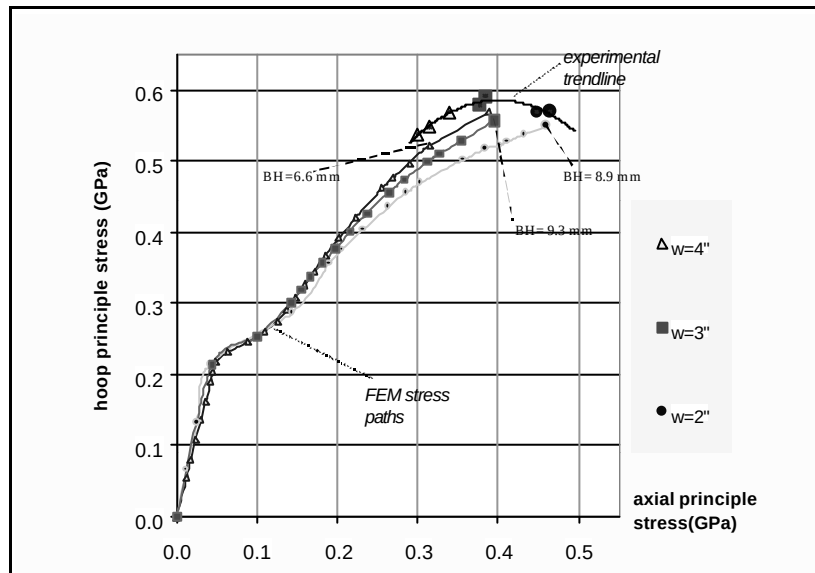


Figure I-34: experimental FLSD and stress paths predicted by explicit FEM simulation (LS-DYNA).

2.6.4 Conclusions

The bulge tooling at the ERC/NSM can be used, together with the circle grid method, to evaluate FLDs of tubes on the right side (tension-tension). Preliminary tests on LCS1008 and SS304 have been conducted. They show that:

- FEM (DEFORM 2D) can be used to predict the strain path of deformed tubes with reasonable accuracy, when the deformation state is still far from necking.
- As expected, the maximum deformation of this process occurs at the middle of bulge width (w), and both major (hoop –direction) and minor (axial –direction) engineering strains are in the positive side.
- The strain path of this process appears to be slightly non-linear.

An experimental FLD curve has been determined for low carbon steel 1008 tubes - 3 mm thickness.

- This experimental FLD is very different (both in slope and intercept) from the FLDs calculated by empirical models.
 - The experiments also show that both axial and hoop strains at fracture increase as the bulge width decreases.
- The Forming Limit Diagrams can be transformed into Forming Limit Stress Diagrams (FLSDs).
- In this case, the distance between the empirical models and the experimental models decreases.
 - Moreover, the data points on the FLSD seem to be less scattered than the corresponding points on the FLD.
- Three different fracture criteria have been tested with FEM: Oyane, Cockroft & Latham and Brozzo.
- Oyane's criterion is not able to predict the correct location of fracture.
 - Cockroft & Latham and Brozzo criteria can predict the correct location of fracture, but can not predict the effect of bulge width upon fracture.

Thus, additional work is necessary to identify a “fracture criterion” in tube forming.

Part I, Chapter 3

Preforming and bending design and production methods

The starting tube geometry for hydroforming may be straight or it may have a pre-bent/formed shape depending on the complexity of the final product to be manufactured. Preforming operations on tubes generally include bending and crushing, with or without internal fluid pressure (Figure I- 35). The effect of bending on the process is described in Section 3.1.

Hydroformed tubes usually undergo crushing operations, especially when processing tubes with diameters that are larger than the die width [Hartl, 1999]. Using large diameter tubes is generally necessary when severe deformations must occur and a larger surface is required to complete the necessary expansion without bursting. In fact, a higher surface may lead to an increased maximum bulge height or to a lower maximum thinning value. To achieve fully plastic deformation in the whole part, and thus, to reduce the springback, tube perimeter should be made slightly smaller than the die geometry in each cross-section. Tube crushing operation becomes very important in aluminum tube hydroforming. With limited formability of aluminum alloys, accumulations of material are needed in areas with large expansions. These can be achieved by careful cross-section analyses.

One of the purposes in the present Chapter is to investigate whether and how tubes of unusual configuration, not utilized presently in industry, could possibly be used as preforms in tube hydroforming, thus providing an alternative to crushing (Section 3.2).



Figure I- 35: Automotive tubular parts formed at intermediate stages

3.1. Bending of tubes

The system approach described in Chapter I can certainly be applied to tube bending operations. There are several variables necessary to successfully complete tube bending operation: tube material, lubrication, machine, tools and set-up. These determine the quality of bent tubes: ovality, thinning, wrinkling, and spring-back [Granelli, 1994]. The strains in the bent tube determine the part formability (available uniform elongation) during tube hydroforming, thus they affect the strength distribution in the final part. This fact must be considered during the design stage so that the formed part has the desired properties prior to subassembly. FEA is well suited in

designing complex automotive tubular parts due to the capability to carry to all the strain distributions induced in each stage through out all the processes.

CNC bending is nowadays a common, well established and mature technology. As a consequence, there is not so much research activity in this field, but there is an increasing industrial interest towards fast and simple FEA packages for tube bending applications. In fact, in many applications, the time spent on the setup and pre-processing preparation of the bending FEA is longer than the time actually spent on the FEA of the actual hydroforming operations.

The present Section 3.1 describes the results of a campaign of experiments, held at Kawasaki HydroMechanics Corp. in Japan and of FEM simulations. Here the focus is not on the bending process in itself, but on the great consequences of the process on the actual formability during the following hydroforming operations. The study described as follows will demonstrate how changing the geometrical parameters of the bending process and thus the geometry of the preform can more easily produce a difficult to form part. The specific goal of the campaign is to find a feasible Loading Path (LP) for the part in Figure I- 37. This example is given in order to illustrate the importance of a correct preform design.

3.1.1 Experimental setup

The die used in the campaign is given in Figure I- 36. Several materials and initial wall thickness t_0 have been used. The tube outer diameter OD is 48.6 mm. The original tube bending radius is 80 mm (Figure I- 37). All the loading paths experimentally tested with this bending geometry have given a negative result, i.e. generated wrinkled and/or burst parts (see Figure I- 38).

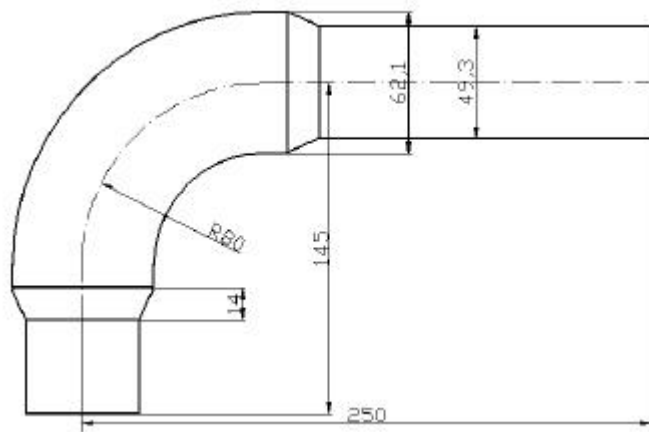


Figure I- 36: die used for experiments and simulations. Right and left feed (d_{left} and d_{right}) directions are shown. d_{left} is positive, d_{right} is negative.

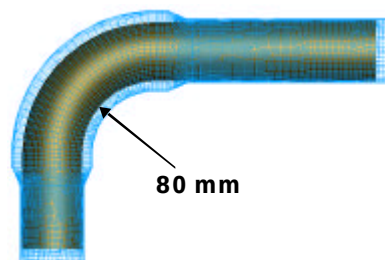


Figure I- 37: blank tube with bending radius 80 mm

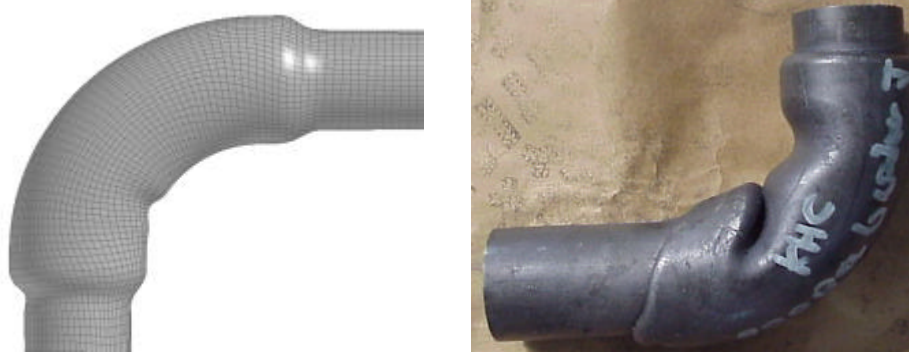


Figure I- 38: example of FEM simulation plot (left) and of actual hydroformed part (right)

3.1.2 FEM simulations with the original bending radius

FEM simulations have been run with 2 different software packages: Dynaform and PAM-STAMP. The initial tube bending operation has been simulated with both codes, but with different techniques. In PAM-STAMP 2000 a bending module is available (see Figure I- 39), that simulates the rotary bending process with mandrel balls. In Dynaform the bending process has been simulated by drawing an originally straight tube through a die without a mandrel (see Figure I- 40). In FEM simulation, the following flow stress equation has been used:

$$\bar{s} = K(e_0 + e)^n$$

3 materials have been simulated:

- Hot rolled carbon steel SGP ($K=0.462$ GPa, $\epsilon_0=0.06$, $n=0.3$), initial wall thickness $t_0=3.5$ mm
- Stainless steel AISI 409 ($K=0.765$ GPa, $\epsilon_0=0$, $n=0.23$), $t_0=2$ mm
- Stainless steel AISI 304 ($K=1.47$ GPa, $\epsilon_0=0.06$, $n=0.58$), $t_0=2$ mm

All simulations run with the original bending radius 80 mm are summarized in Table I- 9.

The main conclusion of this section is that the effect of material properties and thickness are extremely important if a successful process is desired.

Table I- 9: FEM simulations with original bending radius 80 mm.
Sound part obtained only for simulation n. 5.

Simulat. number	Material	t_0 (mm)	FEM software	THF Simulation technique	Bending simulation technique	Result
1	SGP	3.5	Dynaform	Adaptive Simulation	Drawing through a die	Wrinkling and bursting
2	SGP	3.5	Dynaform	Self Feeding technique	Drawing through a die	Wrinkling and bursting
3	AISI409	2	PAM-STAMP	Trial and error	Rotary bending	Wrinkling and bursting
4	AISI304	2	Dynaform	Self Feeding technique	Drawing through a die	No wrinkling, bursting
5	AISI304	2	PAM-STAMP	Trial and error	Rotary bending	Sound part (Max thinning 27.9 %)

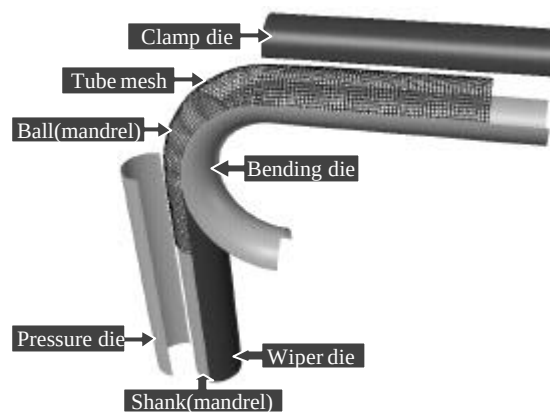


Figure I- 39: bending simulation by the PAM-STAMP 2000 bending module

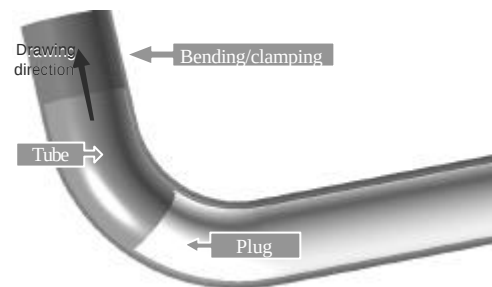


Figure I- 40: bending simulation by drawing the tube

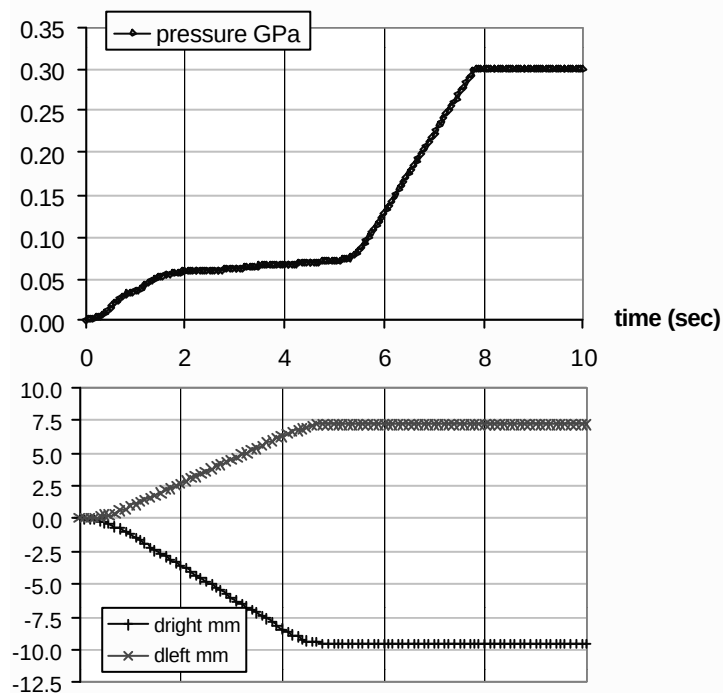


Figure I- 41: loading paths used in simulation 5

3.1.3 FEM simulations with the modified bending radius

One more trial-and-error Loading Path (material AISI 304) has been generated by conducting simulation with a different bending radius, as shown in Figure I- 42. In fact, changing the bending radius the thinning after bending is reduced from 17.7% to 14.3%. This reduction in thinning and the shape of the new pre-bent tube reduce the risk of wrinkling. This can be explained as follows.

When hydroforming starts, the tube deforms from shape (a) to shape (b) in Figure I- 43. In fact, due to axial feeding (which will generate compressive stresses) and due to an initially low internal pressure, the preform will be further bent and slightly bulged. When shape (b) is reached, the shape of the tube is very similar to the initial tube shape, only with less maximum thinning (14.4 % versus 17.7 %). Hence, when the bending radius is modified, the tube thickness distribution is more uniform and the localized wrinkle previously shown is less likely to appear.

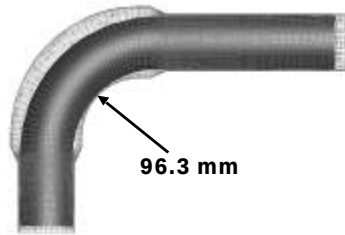


Figure I- 42: blank tube with bending radius 96.3 mm

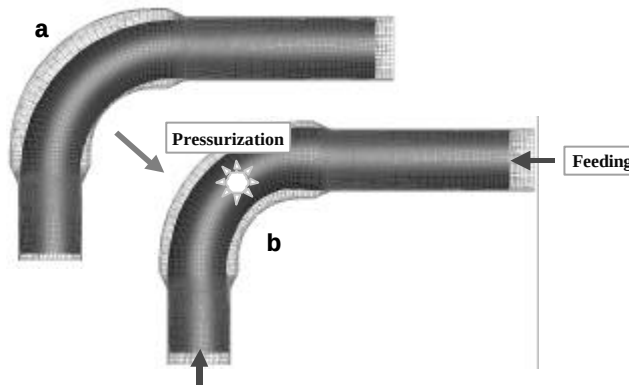


Figure I- 43: initial deformation of the tube during hydroforming, form (a) to (b)

Table I- 10: FEM simulations with new bending radius 96.3 mm.

Simulation number	Material	t_0 mm	FEM software	THF Simulation technique	Bending simulation technique	Result
6	AISI 304	2	PAM-STAMP	Trial and error	Rotary bending	Sound part Max thinning 24.9 %

The result of the simulation with modified bending radius is summarized in Table I- 10. The corresponding loading path is given in Figure I- 44.

It is clear from this section that, for the subject part, a proper design the preliminary bending operation is very important for a successful hydroforming process.

Summarizing, the results of a campaign of experiments held at Kawasaki HydroMechanics Corp. in Japan on a 90° bent part with a bulge on the bend has been presented.

- The process is very critical, since the risk of wrinkling and bursting for the subject part is very high.
- FEM simulations provided feasible loading parts only when using high formability and low wall thickness materials (AISI 304, $t_0=2$ mm).

- The process window can be extended with a different design for the preliminary bending operation, by increasing the bending radius.

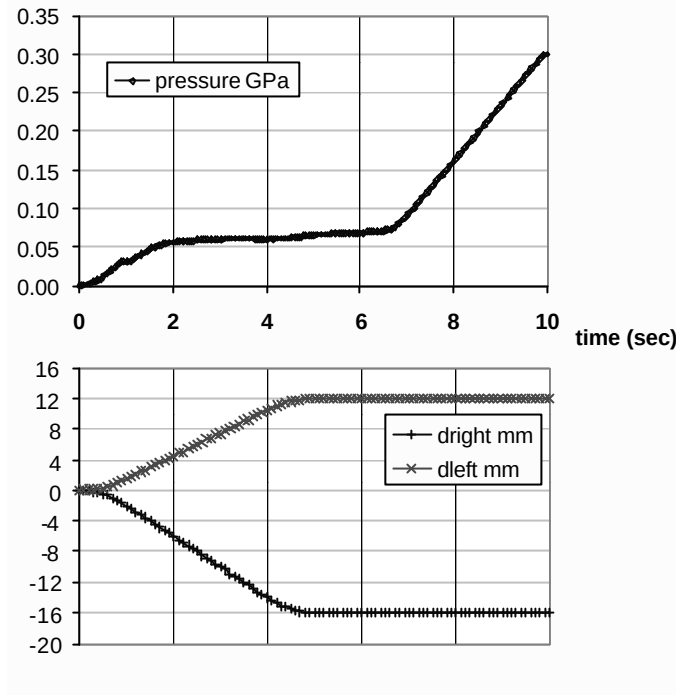


Figure I-44: trial-and-error generated loading path for the simulation n. 6.

3.2. Tube Hydroforming with Macro-Structured Preformed Tubes

In the study described in the present Section, an idea concerning the modification of the geometry of commonly used cylindrical smooth tubes is presented. Preforms with a macro-structured shape (Figure I-48) show — on the condition of equal weight — a larger surface than non-structured workpieces. This approach may improve the process, for several reasons, listed as follows.

- The forming limits of the process imposed by bursting could be postponed.
- The process planning (determination of pressure and feeding curves) could be made easier, since macro-structured tubes may reduce or eliminate the need for axial feeding.
- The need of pre-forming operations could be reduced.
- The quality of finished parts, in terms of stiffness, strength, weight or material cost could be improved.

Therefore, the objective is to explore, through theoretical analysis and FEM simulations, whether and how “macro-structured preformed” tubes could improve formability in axisymmetric (free bulging) and non-axisymmetric hydroforming operations.

The conventional process chain, from the tube making process to the final hydroforming operation can be seen in Figure I-45. The tube making process is usually roll forming for steel tubes and extrusion for aluminum tubes; occasionally, discontinuous methods such as press braking or roll bending can be used. Unless tubes are extruded, a welding operation is required.

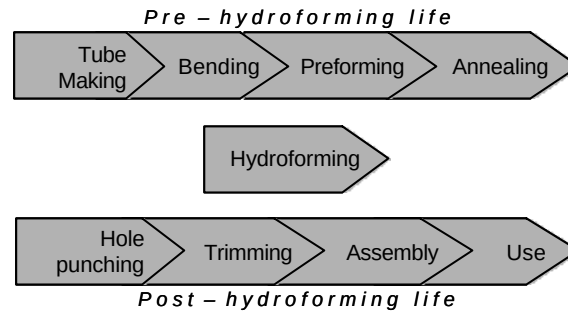


Figure I- 45: THF process chain; bending, preforming, annealing and punching are optional operations.

In the following subsection, possible macro-structured performs are classified and their potential benefits and drawbacks are further explained (Section 3.2.2).

3.2.1 Potential benefits and drawbacks of using macro-structured preforms

Two main classes of deviation from the classical cylindrical smooth geometry can be defined.

- Class A: regular variation of the cross section, uniformly along the axis (Figure I- 46a). Such tubes could be produced in continuous or semi-continuous operations as “standard” preforms, similar to conventional cylindrical tubes. They are intended as general purpose preforms, as currently smooth cylindrical tubes are.
- Class B: non-uniform variation of the cross section (Figure I- 46b). for example a tube can be expanded or reduced in diameter in one or both ends. Their design can be adapted to the specific requirements of the final product.

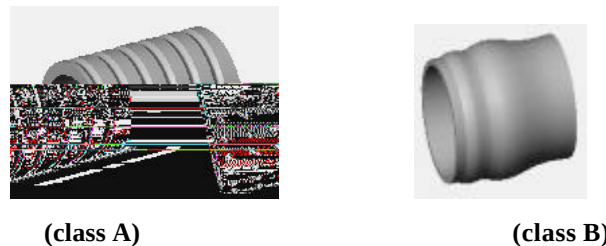


Figure I- 46: preforms of class A and B

The approach presented here may lead to an improvement of the hydroforming process, for several reasons, listed as follows.

- The forming limits of the process imposed by bursting could be postponed.
- Axial feeding is necessary when more material is required for the expansion, but the same objective can be achieved by macro-structured preforms, that may reduce or eliminate the need for axial feeding of the tube edges. The process planning phase could be made easier.
- In order to use unconventional shapes of the preforms, the whole manufacturing process of hydroformed tubes must be somehow altered: by modifying the tube making process or by adding some intermediate operations before bending. In all of these cases, the pre-forming phase of the ordinary process can be skipped (Figure I- 47). The overall process may still be cost effective.
- Geometrical features and mechanical properties of tubular preforms can be altered not uniformly along the tube length, but taking in account the deformations and the function of each branch of the processed parts. This can improve the quality of finished parts, in terms of stiffness, strength, weight or material cost.

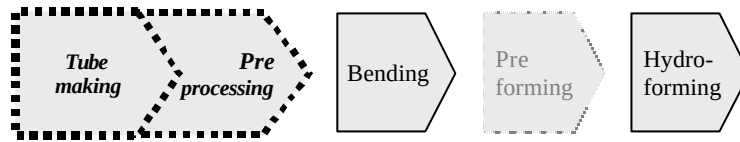


Figure I-47: modified THF process scheme

Shapes seen in class A present homogeneous geometrical features along the length of the tube and along its circumference. The main advantage of this kind of tubes is that, with fixed maximum external diameter, more material is available to be deformed and forming limits can therefore be extended. Furthermore, the part diameter can be greater than the tube diameter and pre-forming can be made in the same die or even avoided [Birkert, 1999]. It could be also investigated if introducing such kinds of features does affect the stiffness of the formed structure.

Tubes seen in Figure I-48a can be obtained by extrusion or by rolling after welding. Tubes seen in Figure I-48b can be obtained by particular kinds of extrusion or cross rolling techniques. Tubes seen in Figure I-48c can be obtained by stamping + bending + welding. Tubes seen in Figure I-50 may be rolled or obtained by stamping + bending + welding. Bending of the tubes seen in Figure I-50 and in Figure I-48a may be achieved in a hydroforming press with internal pressure.

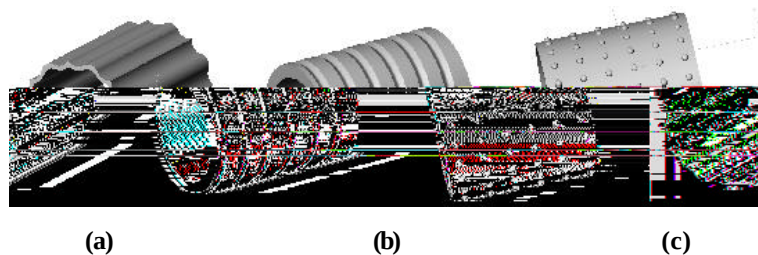


Figure I-48: macro-structured preform tubes of class A, (a) wavy profile, (b) threaded profile, (c) indented sheets

There can be also some drawbacks:

- wrinkling could happen more easily than with smooth preforms, for some kind of geometry (Figure I-48a, Figure I-50);
- the final inner and/or outer surfaces may result in irregular surfaces;
- the final weight of the part can be higher because the amount of material available is higher not only where necessary, but along the whole tube.
- the necessary internal pressure can be higher and this could lead to leaking [Prier, 1999], especially for the tube shapes seen in Figure I-48b and Figure I-50.

For tubes of class B, three examples are given in Figure I-49:

- tailored welded blanks, with sheets of different materials or thickness, joined by the tube ends;
- tailored blanks made of different materials, welded along lines parallel to the axis
 - shaped welded tubes with variable diameter and/or thickness, that could be produced by means of flowforming or spinning.

The main advantage of this class of tubes is that the properties (diameter, thickness, material) of each sector of the tube can be changed, within certain limits, depending both on the hydroforming requirements and on the parts' functions. The weight of the parts can be reduced to the minimum required by the operational functions of the product, especially for tubes like in Figure I-49c, where the thickness can be varied continuously. The main drawback of this kind of blanks is probably the additional cost of a flowforming operation. For blanks seen in Figure I-49a and Figure I-49b, care must be taken during the process design phase because, if possible, the

welding seams should not be situated in forming areas, especially for tubes seen in Figure I-49a (see ref. [Eichorn, 1999]).

In this study, the focus is on the first class of tubes and particularly in wavy profiles. The waves may be:

- in the axial direction, as in Figure I-50, or
- in the circumferential (hoop) direction, as in Figure I-48a

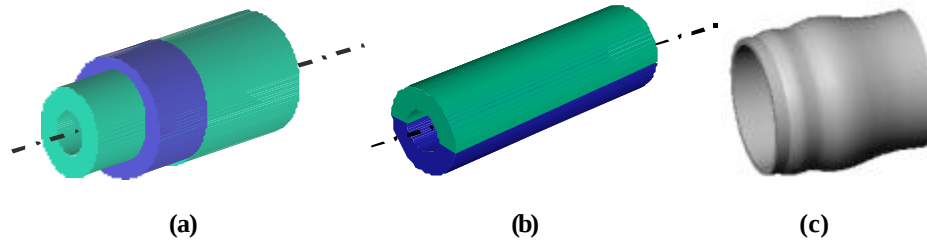


Figure I-49: class B, (a) and (b) tailored welded blanks, (c) shaped tubes

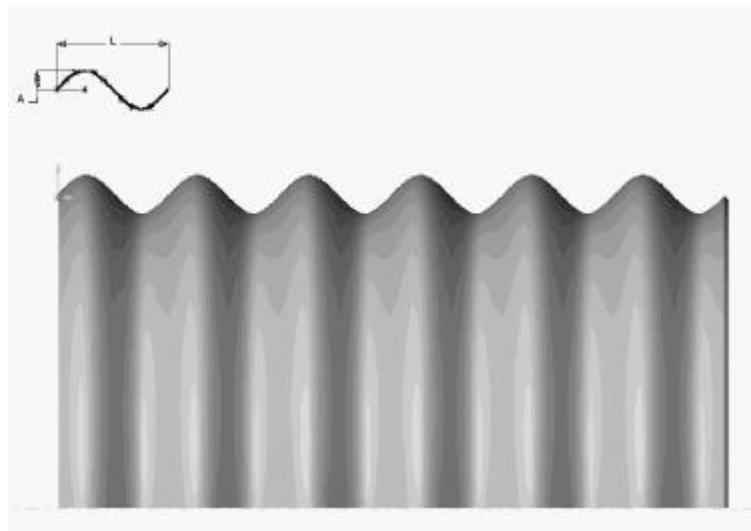


Figure I-50: wavy profile of class A; wavelength L , wave amplitude A_w - Upper half of the tube.

The feasibility of using these kinds of unconventional preform shapes has been tested and compared to the traditional cylindrical preforms used in THF operations. Two kinds of THF (Tube HydroForming) operations have been studied:

- axisymmetric free bulging, using geometrical theoretical analysis (Section 3.2.2)
- non-axisymmetric expansion with complex (round to square) die geometry, by FEM simulations (Section 3.2.3)

3.2.2 The effect of preform shapes on tube formability in free bulging of round tubes

In a pure free bulging process (with no axial feed) the material flow is not guided by the die, so that a free expansion is possible. The most critical zone of the workpiece is the symmetry line of the bulge (Figure I-51), where the maximum bulge height and the maximum amount of thinning are reached [Aue-U-Lan et al., 1999]. The thinning value should not be below a certain value, otherwise bursting may occur.

Geometrically, it is useful to consider a macro-structured (wavy) tube as deriving from the bending of a planar wavy sheet. A generic wavy structured sheet is assumed to have a sinusoidal shape with the parameters:

- amplitude A_w and
- number of wave lengths n_w (Figure I- 52).

This initial sheet can be bent around the x or z axis, so that the final tube shows a structuring in circumferential or in tube axis direction, respectively (Figure I- 53).

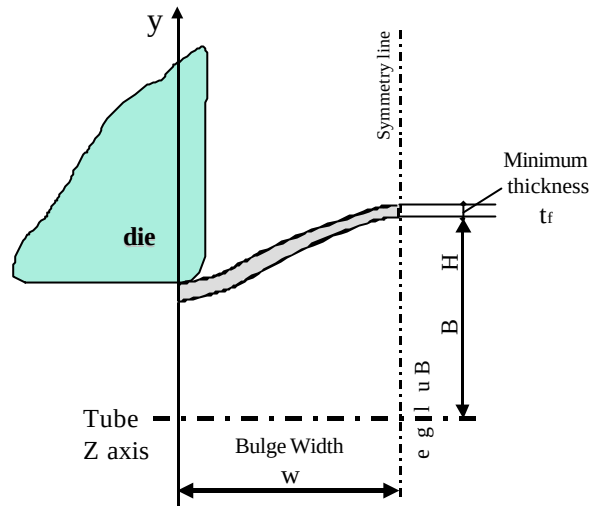


Figure I- 51: left upper half of the bulge area

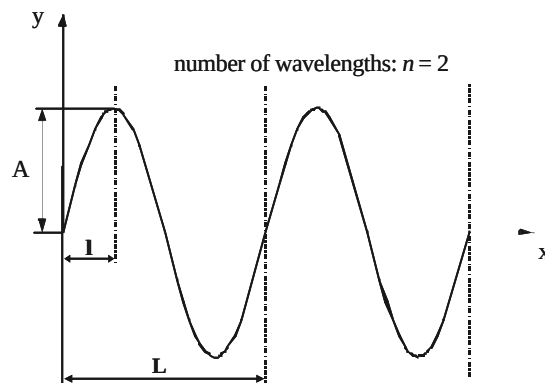


Figure I- 52: Description of the parameters wave amplitude A_w , wavelength L , wavelength $l=L/4$, and number of wave lengths n_w .

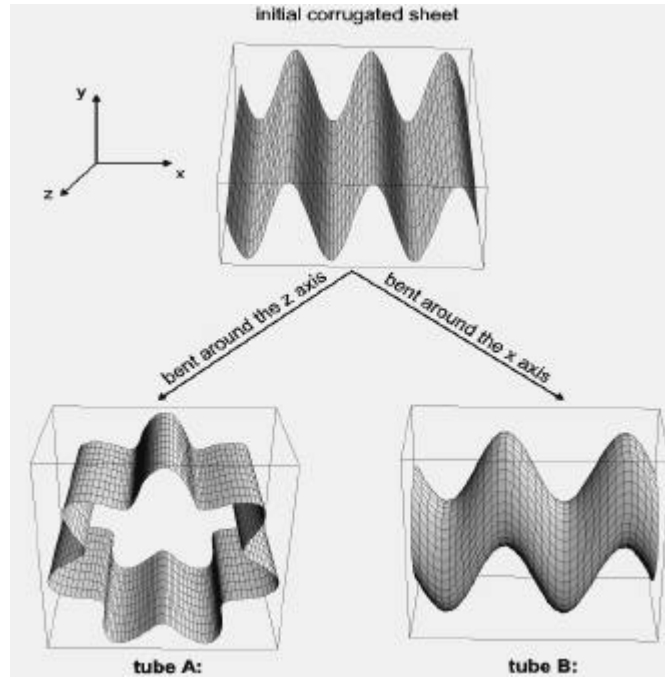


Figure I- 53: possibilities of structuring a tube; A - along the circumference, B - along the axis

PREFORMS WITH WAVES IN THE HOOP DIRECTION

The potential benefit of a structured tube is the increase of the surface area in comparison with a smooth one – on the condition that the either the mean radii R_{ve} or the maximum radii $MD_{tube}/2$ are equal. The main assumption underlying the proposed model is that, during the free bulging, the macro-structured tube can undergo an expansion without changing the wall thickness (thinning = 0) until the structured surface has become smooth (in a way similar to the expansion of an accordion). The enlargement of the surface for the circumferential structured tube can be measured as the increase of the mean radius R_{ave} (Figure I- 54).

The final expanded radius R_{smooth} can be calculated as

$$R_{smooth} = \frac{1}{2p} \int_0^{2p} \sqrt{dx^2 + dy^2} dq \quad (I-24)$$

with

$$x = [R_{ave} + A \sin(nq)] \cos q \quad (I-25)$$

$$y = [R_{ave} + A \sin(nq)] \sin q \quad (I-26)$$

The perimeter of a wavy tube, compared to the perimeter of a smooth tube with equal radius, is plotted in Figure I- 55.

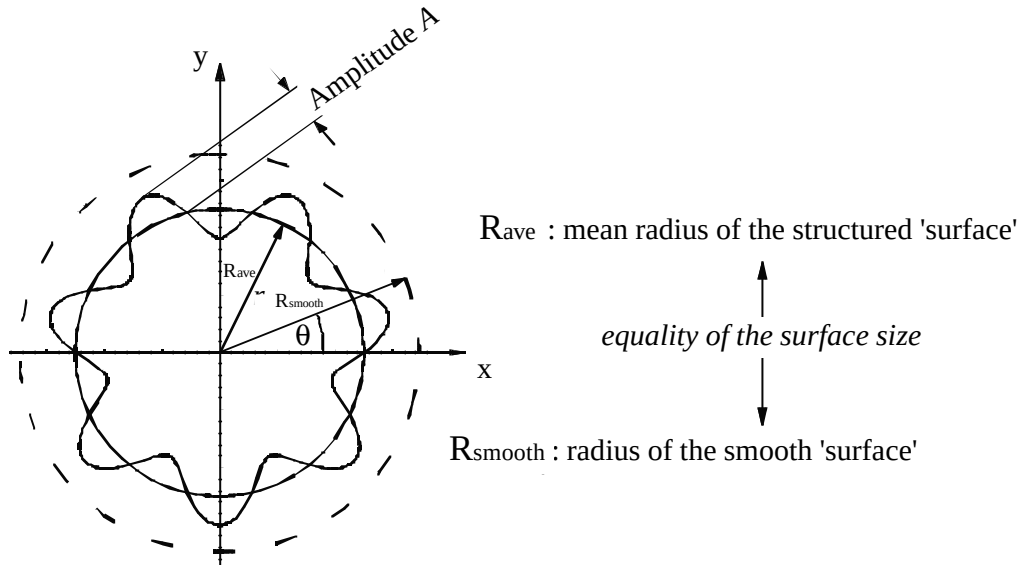


Figure I- 54: Section perpendicular to the axis (z coordinate) through a circumferential structured tube. Enlargement of the mean radius R_{ave} up to R_{smooth} by expansion (number of wave lengths $n_w = 7$)

Obviously, a high amplitude A_w combined with a high number of wave lengths n_w favors the relative enlargement of the surface.

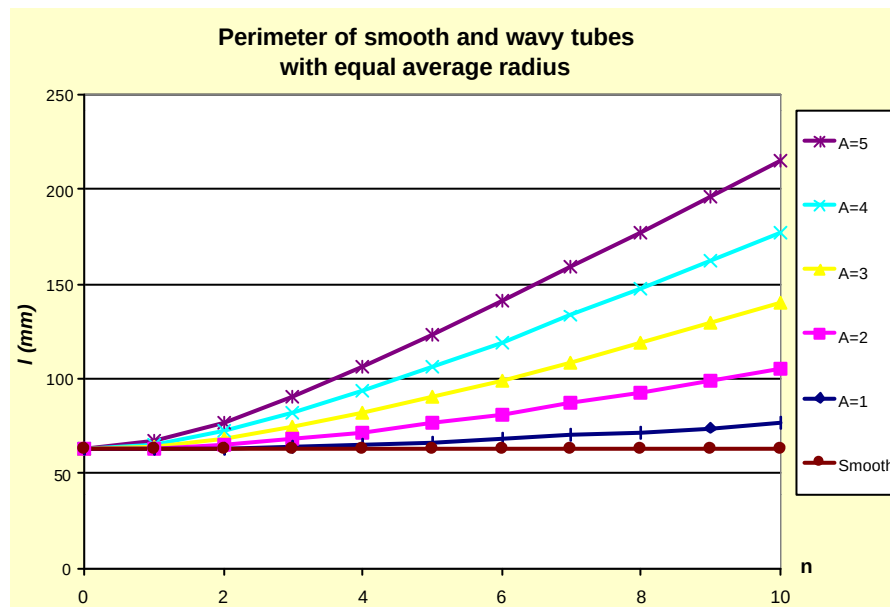


Figure I- 55: Perimeter of wavy and smooth tubes of equal average radius and with different number of wavelengths n_w and amplitude A_w . Radius of smooth tube = 10 mm - Perimeter of smooth tube = 62.8 mm².

PREFORMS WITH WAVES IN THE AXIAL DIRECTION

The enlargement of the surface of a tube, which has a structuring in the axis (z) direction can be calculated as follows. The calculation is based on the assumption that the initial sinusoidal surface expands — due to the internal tube pressure — and eventually becomes a smooth surface, with a negligible thinning until the profile lengths of the initial and deformed tubes are equal (see Figure I- 56).

The length of the initial sinusoidal profile of the tube l_c can be calculated as:

$$l_c = \int_0^{2w} \sqrt{1 + \left(\frac{dy}{dx}\right)^2} dx \quad (I-27)$$

The meaning of l_c is depicted in Figure I-57. In the same figure the profile of the bulged tube, when it has reached a smooth profile, is also shown. The profile length of the smooth bulged tube is l_s . It has been found (with the aid of FEM simulations) that the length l_s can be assumed to be a polynomial function of the z coordinate that depends on the half bulge width w and on the maximum radius of the bulged tube R_{smooth} :

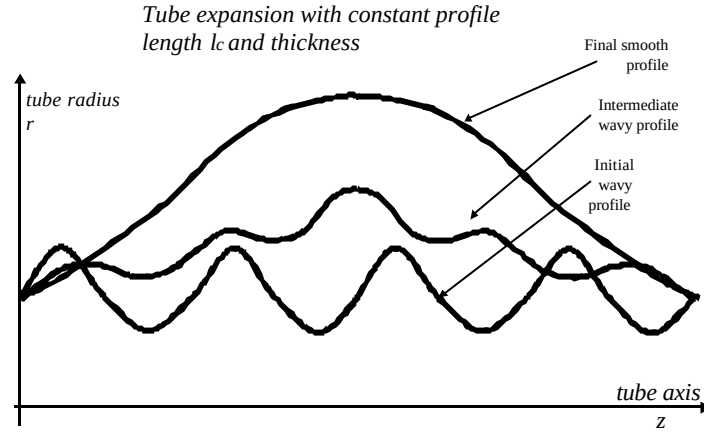


Figure I-56: bulging process of a wavy tube

$$l_s = \frac{R_{smooth}}{w} \left(\frac{3}{w} z^2 - \frac{2}{w^2} z^3 \right) \quad (I-28)$$

R_{smooth} is unknown, and it can be determined by the condition $l_c = l_s$. The ratio R_{smooth}/w can be seen as a good measure for the tube formability. Thus, the results of this section can be expressed as a relation between the ratio $R_{smooth}/2w$ and the variables amplitude Aw and number of wavelengths nw . Figure I-58 shows how, within the assumptions of the proposed formulation, the ratio $R_{smooth}/2w$ increases with increasing amplitude Aw and number of wave lengths nw increase, too.

As a conclusion, using preforms with waves either in the axial or in the circumferential directions, under the assumptions of the proposed formulation, the maximum bulge height can be increased by controlling the geometrical parameters of the wave.

Therefore, in free bulging, there is practically no technological limit to the radius expansion, as long as the right combination of the wave parameters is selected. The limitation is based on the economic manufacturability of the structured tubes. In order to study the real feasibility of wavy tubes and to find some criteria for the selection of the wavelength and amplitude, the expansion against a die must be considered. This is the focus of the next Section.

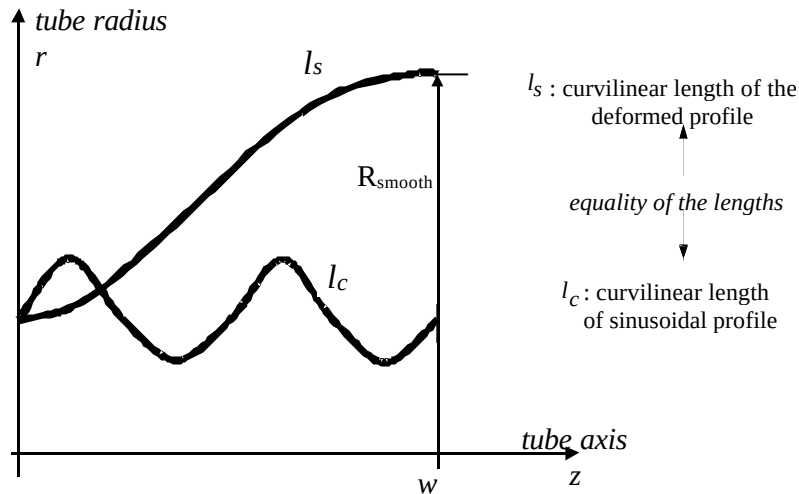


Figure I- 57: Profile of an axial structured tube on a plane passing through the axis (w is half of the bulge width). Initially, the wavy tube expands with constant length and thickness. R_{smooth} is the maximum radius reached by the deformed tube when its length l_s is still equal to the length of the initial wavy tube l_c .

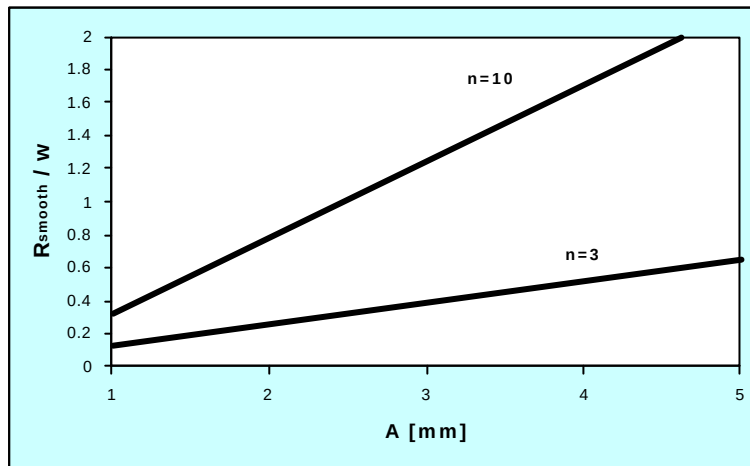


Figure I- 58: dependence of the ratio $R_{\text{smooth}} / 2w$ on the amplitude Aw and number of wavelengths nw for axial structured tubes.

3.2.3 Bulging of tubes with complex (round to square) die geometry

In order to evaluate the feasibility of using wavy tubes in hydroforming of “realistic” parts, several sets of FEM simulations have been run with a square die geometry seen in Figure I- 59 and the FEM code PAM-STAMP.

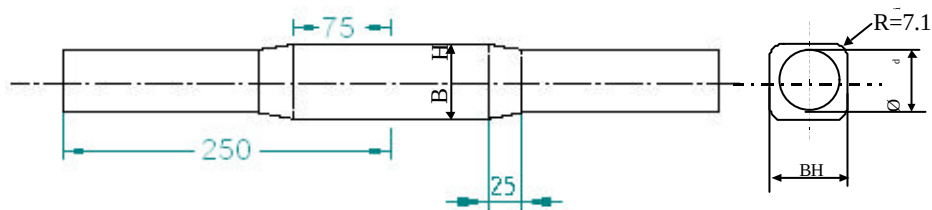


Figure I- 59: die geometry (dimensions in mm). Two sets of experiments with different values of the dimension BH (bulge height) have been used (62.1 and 75 mm) and with $MD_{\text{die}}=49.3$ mm.

Preform shapes, seen in Figure I- 60 and Figure I- 61, have been tested against a smooth traditional preform with a thickness of 2 mm. The material tested is a stainless steel (AISI 304). In all simulations, the following assumptions and data have been used.

- the thickness of the initial tube has been kept constant along the axial and circumferential (hoop) directions;
- no previous state of strain has been considered in the FEM simulations;
- the maximum diameter (MD_{tube}) of the initial tube is equal to 49.3 mm, which is equal to the minimum internal diameter of the die in Figure I- 59 (MD_{die}).
- no feeding has been used;
- the maximum internal pressure ranges from 0.27 to 0.3 GPa.
- the total simulated time ranges from 5 to 6 ms (this short process time has been selected in order to reduce the computational time required by the explicit FEM code);
- for symmetry reasons, the simulations have been conducted only with the upper right $\frac{1}{4}$ of the real tube and die cross sections (Figure I- 61).

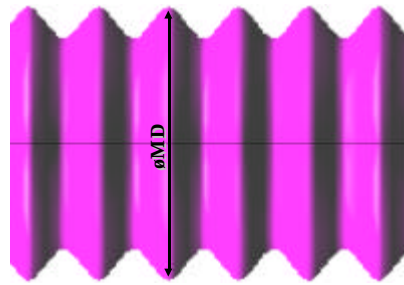


Figure I- 60: preform with waves in the axial direction

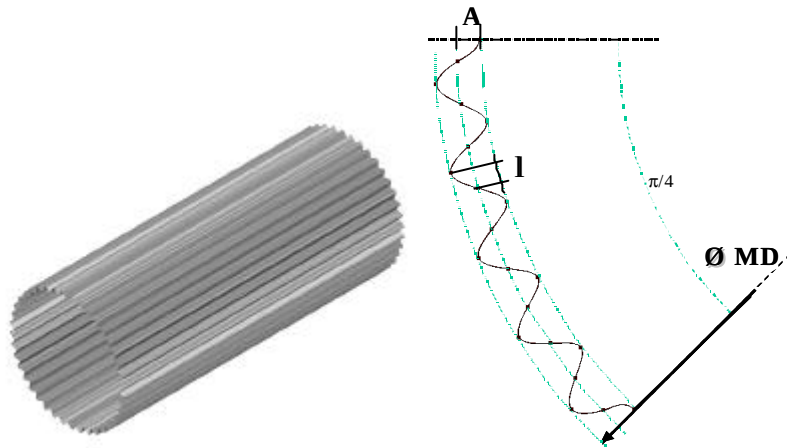


Figure I- 61: preform with waves in the circumferential (hoop) direction

PREFORMS WITH WAVES IN THE AXIAL DIRECTION (FIGURE I- 60)

The plan of the simulations is reported in Table I- 11; the results are summarized in

Table I- 12.

An effective way to directly compare the formability of wavy tubes and smooth ones is to keep constant the volume (i.e. weight) of the tubes among different simulations. The surface area S_t for wavy tubes is larger than for a smooth tube with given maximum diameter (MD_{tube}). Thus, in order to keep constant the volume, the initial

tube thickness must be changed accordingly. Initial tubes used in runs 4, 5, 6, 10, 11, 12, 13, 15 (Table I- 11) have approximately⁴ the same volume of a smooth tube with $t_0 = 2$ mm and $MD_{tube} = 49.3$ mm. In Figure I- 63, maximum thinning data obtained by these simulations are plotted. The values of thinning are always lower than 48 %, which is the value obtained with the smooth preform. However, none of the simulation showed a thinning lower than 33 % (Table I- 11 and Table I- 12 – run 14).

Table I- 11: Plan of simulations with preforms waved in the axial direction.

run	input data						
	wave parameters				surf. area S _a mm²*E-04	init. thickness t ₀ mm	tube vol. V mm³*E-04
	A mm	λ mm	v=t*A/λ mm	Rave mm			
1	4.93	7.40	1.13	19.7	1.79	1.70	3.0
2	2.75	5.50	0.75	21.9	1.90	1.49	2.8
3	4.93	7.40	0.90	19.7	1.79	1.35	2.4
4	2.75	5.50	1.00	21.9	1.90	2.00	3.8
5	4.93	7.40	1.33	19.7	1.79	2.00	3.6
6	2.75	5.50	0.95	21.9	1.90	1.90	3.6
7	1.38	2.75	0.80	23.3	1.96	1.60	3.1
8	2.75	5.50	1.25	21.9	1.90	2.50	4.7
9	1.83	2.75	0.90	22.8	2.08	1.35	2.8
10	3.67	5.50	1.33	21.0	1.96	2.00	3.9
11	1.38	2.75	0.92	23.3	1.96	1.83	3.6
12	3.67	5.50	1.23	21.0	1.96	1.85	3.6
13	1.83	2.75	1.15	22.8	2.08	1.73	3.6
14	1.83	2.75	1.33	22.8	2.08	2.00	4.2
15	2.75	2.75	1.54	21.9	2.35	1.54	3.6
smooth	-	-	-	24.7	1.85	2.00	3.7

The symbols A_w , λ_w , R_{ave} , t_0 and S_t are explained in Figure I- 62. Surface values S_t are referred to $\frac{1}{4}$ of the cross section; they are not the calculated, but measured on the actual mesh in the pre-processor PAM-GENERIS.

Other dimensions: $BH=62.1$ mm; $MD_{tube}=49.3$ mm (see Figure I- 62). In runs 4, 5, 6, 10, 11, 12, 13 and 15, tube volume is kept approximately constant.

Another set of simulations has been carried out with a higher value of the maximum bulge height ($BH=75$ mm), but the values of maximum thinning obtained were so high (> 60 %) that these simulations cannot be considered useful.

The maximum thinning is achieved always in the die corner (Figure I- 64, Figure I- 65). The distribution of the thinning (and of the other two strains) along the final shape is not uniform, but somehow reproduces the original waviness.

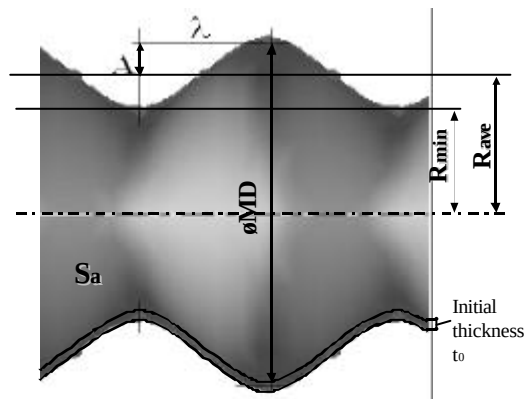


Figure I- 62: reference dimensions for Table I- 12

⁴ Even if the calculated tube volumes are exactly the same, among the real meshed parts there may be slight differences.

Table I- 12: results of simulation.

run	results		
	maximum thinning %	final minim. thickness t_f	permanent wrinkles
		mm	
1	0.49	0.87	no
2	0.49	0.76	no
3	0.48	0.70	no
4	0.47	1.06	no
5	0.46	1.08	no
6	0.43	1.08	no
7	0.41	0.94	no
8	0.41	1.48	no
9	0.38	0.84	no
10	0.38	1.24	no
11	0.38	1.13	no
12	0.36	1.18	no
13	0.36	1.11	no
14	0.33	1.34	no
15	-	-	yes
smooth	0.48	1.04	no

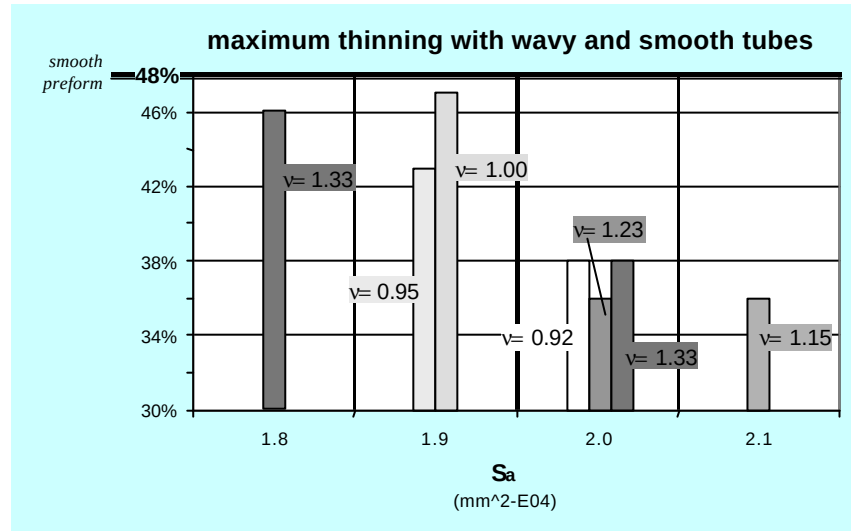


Figure I- 63: maximum thinning for tubes with waves in the axial direction, with a given value of tube volume (rows 4, 5, 6, 10, 11, 12, 13 in Tab. 1 and 2). Data are plotted vs. surface area S_a and vs. the aspect ratio $n_w = t_0 \frac{A_w}{l_w}$ [mm] (see **Figure I- 62**).

The geometrical parameter $n_w = t_0 \frac{A_w}{l_w}$ was found to be very important. Once the die geometry is selected, then:

- the higher the value of n_w , the higher the probability that permanent wrinkles grow during the process, since the only case where wrinkles were observed was the one with the higher value of n_w (1.54 mm as shown in Table I- 11 and Table I- 12– run 15);
- for a given value of n_w , the maximum thinning decreases as the initial tube surface increases (Figure I- 66 and Figure I- 67).

Summarizing, unconventional tube preforms as seen in Figure I- 60 (waves in the axial direction), according to the FE simulation results, show an average thinning value lower than the traditional smooth initial tubes, with a

given tube volume or weight (see Table I- 11). However their potential use is limited, because the use of feeding can be very difficult, since feeding can rapidly increase the aspect ratio n_w , thus causing a very high probability of permanent wrinkles at the end of the process.

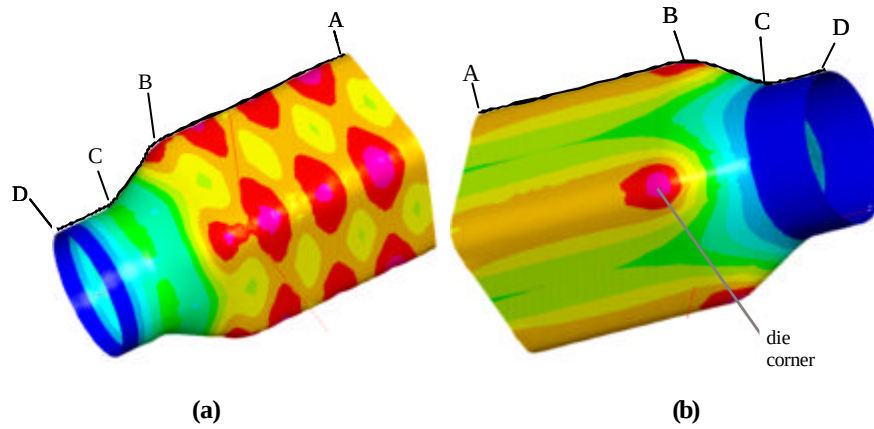


Figure I- 64: distribution of thinning for preforms with (a) waves in the axial direction and with (b) constant circular cross section (smooth tubes). In both cases the maximum thinning is obtained in the corner of the die (marked as point B) and the minimum is obtained at the point marked as D.

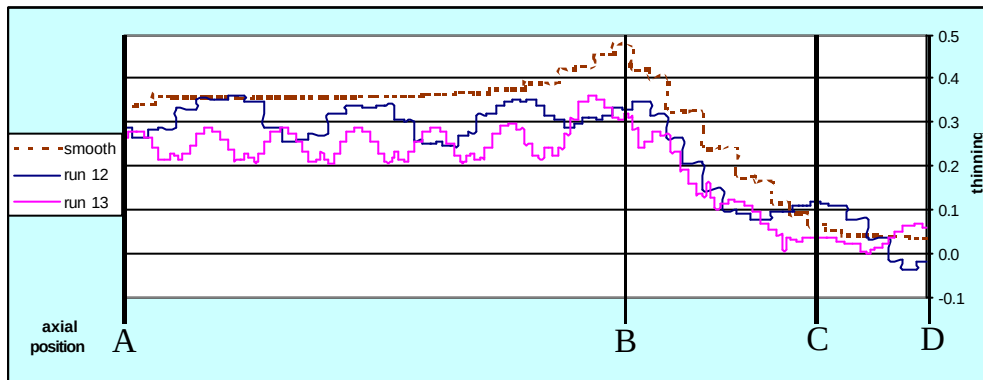


Figure I- 65: distribution of thinning for smooth preforms and preforms with waves in the axial direction. The thinning is plotted along the line ABCD shown in Figure I- 64, and the four points marked A, B, C and D are highlighted. - Preforms with waves in the axial direction. Initial tube dimensions are given in Table I- 11: row 12, 13 and “smooth”.

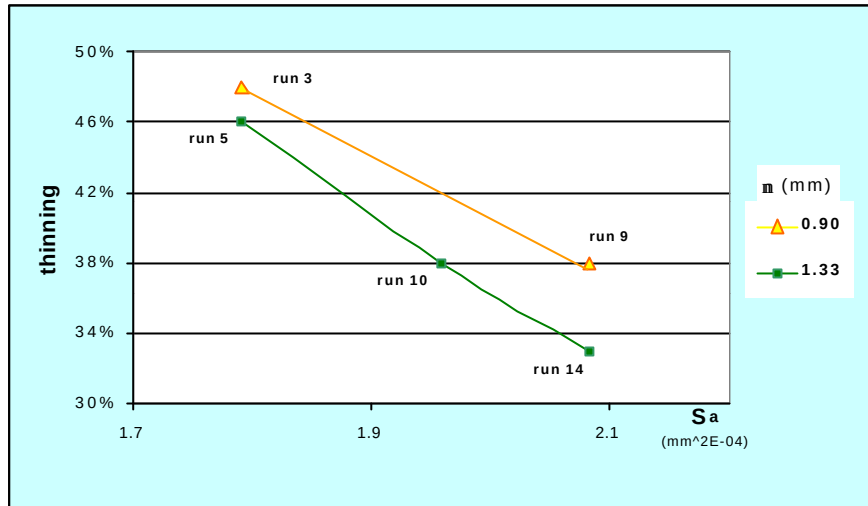


Figure I- 66: effect of the tube initial surface S_t and of the geometrical parameter $n_w = t_0 \frac{A_w}{l_w}$ on the thinning at the die corner. Preforms with waves in the axial direction. Initial tube dimensions given in rows 3, 5, 9, 10, 14 of Table I- 11.

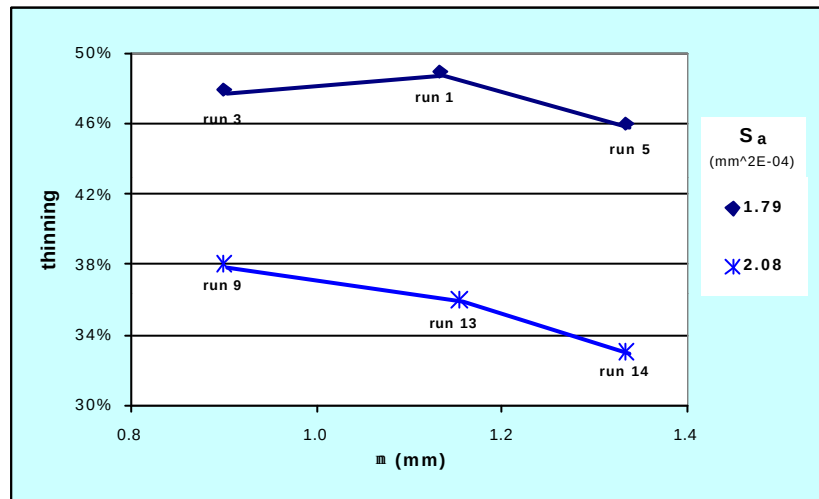


Figure I- 67: effect of the tube initial surface S_t and of the geometrical parameter v_w on the thinning at the die corner. Preforms with waves in the axial direction. Initial tube dimensions given in rows 1, 3, 5, 9, 13, 14 of Table I- 11

Using rolled preforms as seen in Figure I- 68 can solve this problem. In this preform, the waves in the axial direction are only in the bulging zone. Thus, once the aspect ratio n_w is fixed, the surface of the initial tube can be much higher since the maximum diameter (MD_{tube}) is not limited by the diameter of the die in the feeding zone (MD_{die}). The maximum thinning may be reduced to the desired target value, without feeding.

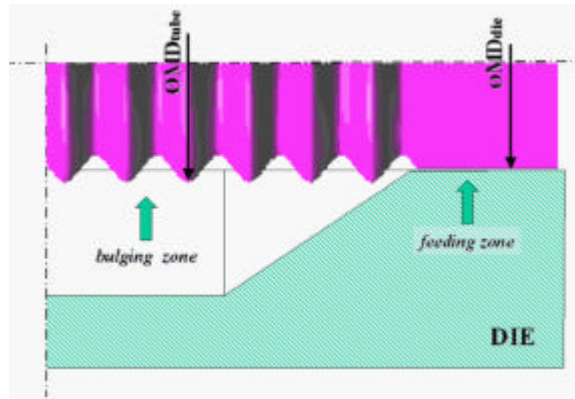


Figure I- 68: preform with smooth cross section in the feeding zone and with wavy cross section in the bulging zone

PREFORMS WITH WAVES IN THE CIRCUMFERENTIAL DIRECTION (FIGURE I- 61)

The plan of the simulations is reported in Tab. 3. The results are summarized in Table I- 13 and Figure I- 69.

Like in the previous section, it is useful to compare the maximum thinning achieved with smooth and wavy tubes, with a given tube volume (i.e. the weight). Run 1 and 2 in Table I- 14 show a volume value approximately equal to the volume of a smooth tube with $t=2$ mm and $MD_{tube}=49.3$ mm. The thinning values measured by FEM simulation are 28 and 30 % for the wavy tubes and 48 % for the smooth one.

In tested cases, the wrinkles do not disappear at the end of the process, especially in the feeding area of the die (Figure I- 71), where practically no expansion takes place. Even if the internal pressure is doubled from 0.3 to 0.6 GPa, the wrinkles do not disappear. This is due to a mainly compressive state of stress in that area which makes difficult the expansion of the tube material to the die surface. Using smaller values of MD_{tube} can solve this problem.

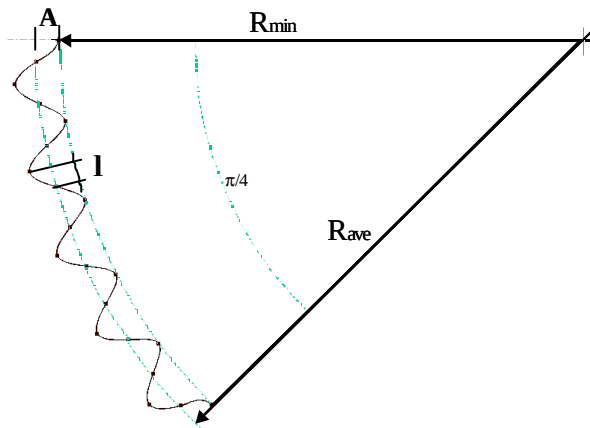


Figure I- 69: reference scheme for next Figure.

Table I- 13: plan of simulations with preforms waved in the circumferential (hoop) direction. BH=62.1 mm, $MD_{tube}=49.3$ mm

	input data						
	wave parameters			R_{ave} mm	Tube Surface S_a mm ²	initial thickness t_0 mm	tube volume V mm ³ *E-04
	A mm	λ mm	v mm				
1	1.00	0.93	1.40	23.7	2.67	1.30	3.5
2	0.80	0.94	1.28	23.9	2.44	1.50	3.7
3	0.80	0.85	1.22	23.9	2.30	1.30	3.0

Table I- 14: results of simulation planned in **Table I- 13**.
Preforms waved in the circumferential (hoop) direction

	results		
	maximum thinning %	final thickness mm	permanent wrinkles
1	0.28	0.94	yes
2	0.30	1.05	yes
3	0.37	1.00	yes

The surface area of the initial tubes is, on average, higher than in the previous case (as showed by a comparison between Table I- 11 and Table I- 13) and more material is available to the expansion in the hoop direction, where the strain is usually higher then the axial strain in THF operations. Thus, in average, lower values of maximum thinning are achieved in this case, as showed by the comparison between Table I- 12 and Table I- 14.

Also in this case, the maximum thinning seems to be mainly influenced by the aspect ratio n_w and the initial tube surface S_t (Figure I- 70).

Summarizing, preforms as in seen Figure I- 61 (waves in the circumferential direction) potentially allow a lower level of maximum thinning, since the strain in the hoop direction (which is generally the highest in THF) is reduced by the presence of the waves in the circumferential direction. Moreover, the higher is MD_{tube} the lower is the thinning.

Feeding is possible, even if a higher level of feeding force is required due to the higher stiffness of the tubes.

Wrinkles are very likely to happen, especially in the feeding area of the die (Figure I- 71), when the maximum diameter of the initial tube is in contact with the die. This can be avoided by reducing the maximum tube diameter $MD_{tube} < MD_{die}$, thus allowing the tube to expand before touching the die.

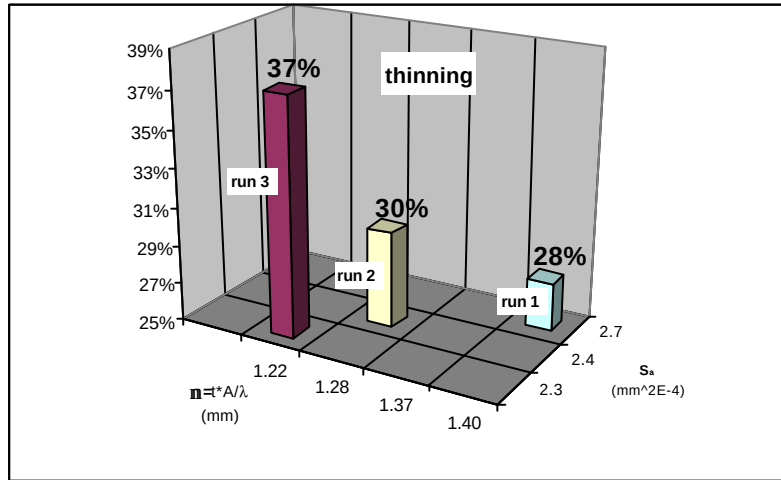


Figure I- 70:effect of the tube initial surface S_t and of the aspect ratio n_w on the thinning at the die corner. Preforms with waves in the hoop direction. Dimensions given in Table I- 13.

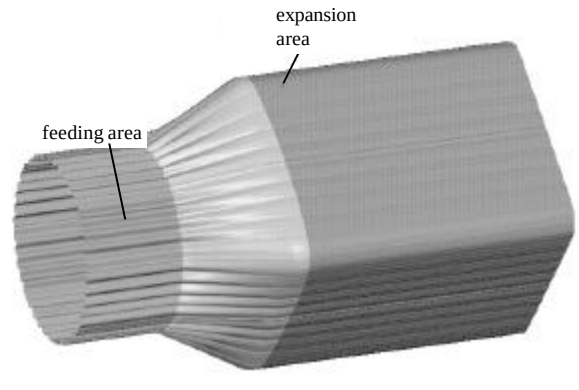


Figure I- 71: wrinkles in the hydroforming of preforms with waves in the circumferential (hoop) direction

In conclusion, in using initial tubes as seen in Figure I- 61, a compromise between the risk of bursting and wrinkling must be found in order to select the proper value of MD_{tube} and n .

3.2.4 Summary

Formability of tubes with pre-shaped waved profiles has been evaluated. Two kinds of macro-structured shapes (possibly rolled or extruded) have been studied using FEM simulations and theoretical analysis:

- preforms with waves in the axial direction,
- preforms with waves in the circumferential (hoop) direction.

These macro-structured preforms have been tested both in axisymmetric free bulging and in non-axisymmetric expansion against a die.

Both kind of preforms show an average thinning value lower than the traditional smooth tubes, but some steps must be considered in order to avoid wrinkles and to allow axial feeding.

In the present study the following results have been obtained.

- Feasible shapes for macro-structured preforms have been classified and their potential benefits and drawbacks are further explained.
- Free bulging, using preforms with waves either in the axial or in the circumferential directions, has been studied. Virtually no technological limit to the radius expansion has been found, as long as the right combination of the wave parameters is selected.
- The expansion of macro-structured preforms with waves either in the axial or in the circumferential directions against a die has been tested by FEM simulations. The results are summarized in the following Table I- 15.

If these novel concepts for structured preforms appear to have some potential application, then it may be useful to:

- conduct experimental tests, to confirm the results of the theoretical and numerical analysis;
- consider the pre-straining of the initial tube, i.e. the strains generated in the producing the wavy preforms;
- extend the study also to some different shapes for the initial tube and for the die;
- investigate whether introducing such kinds of features affect the stiffness of the formed structure

Table I- 15: synthesis of results of this Section

	Preforms with waves in the AXIAL direction	Preforms with waves in the CIRCUMFERENTIAL direction
Maximum thinning	With a given value of tube volume, maximum thinning can be up to: 25 % less than with smooth cylindrical preforms. 42 % less than with smooth cylindrical preforms. The amount of thinning is controlled by surface St and ratio n_v. The right combination of these two parameters is to be selected in order to minimize thinning.	
Wrinkles	When $MD_{tube}=MD_{die}$, and with no axial feeding, wrinkles are likely to be present in the final deformed tube only with high values of the aspect ratio n_v (i.e. $n_v > 1.5$ mm). With higher MD_{tube} the limiting value for v may be lower.	When $MD_{tube}=MD_{die}$, and with no axial feeding, wrinkles are very likely to be present, regardless of the value of v . The right value of MD_{tube} must be found in order to avoid wrinkles. The internal pressure required for a sound product may be higher.
Feeding	Feeding is possible, but the higher is the feed rate, the higher is the risk of increasing the ratio v and therefore the higher the risk of permanent wrinkles.	Feeding is possible, without increasing the risk of wrinkles, but a higher level of axial force is probably necessary due to the higher stiffness of the initial tube.
Best solution	To use preforms with smooth cross section in the feeding zone and with wavy cross section in the bulging zone (Figure I- 68), selecting the values of v and MD_{tube} that allow the required thinning level. In this case, axial feeding may not be necessary.	A compromise between the risk of bursting and wrinkling is to be found in order to select the proper value of MD_{tube} and v .

Part I, Chapter 4

Product, die and tool design

4.1. Introduction

Die and tool design is obviously strictly related to part design. More generally speaking, die, tool and part design are a smaller portion of process planning, which will be addressed thoroughly in Part II. However, in the present Chapter, a few useful notions for preliminary process design will be given.

As tube hydroforming becomes more competitive, there is an increasing need for quickness in the product design. On the other hand, a communication gap between the designer and manufacturer is known to have an important impact on production effectiveness and lead-time. Due to the fact that THF is a relatively new technology, the part design methodology is still being developed. Thus, there is a need for the development of general guidelines for part and process design, possibly in computerized format, for an easy use in the prototyping stage.

At the ERC/NSM a computer program called THF-PAL is under development, whose purpose is to assist engineers in the product and process design phases. The first version of the program has already been released. The main objective of THF-PAL is to reduce the trial and error in the development stage of a new THF part. For a given part design, THF-PAL can assist the designer in determining the initial tube geometry, in deciding whether the part can be produced as designed or requires design changes. It can also be used to approximately predict process parameters before conducting a detailed process simulation with FEM. It currently contains a part database, a set of design guidelines and a material database (see Figure I- 72):

- The *part database* includes information of characteristic parts manufactured by tube hydroforming. It includes details of the part geometry, material and process parameters used to manufacture that part.
- The set of *design guidelines* can be used to design particular features of a given part (e.g. achievable corner and fillet radii, achievable protrusion heights, etc.).
- The *material database* has information of the properties of materials used for manufacturing parts by tube hydroforming process.

The information currently contained in THF-PAL all come from experiments, production data and FEM simulations. Of course, the knowledge base (Part DB and Material DB) can be easily expanded by the end user to include company's confidential information on more parts. However, the most critical part of the software is definitively the Design Guidelines section, as a lot more fundamental research work is required in this area.

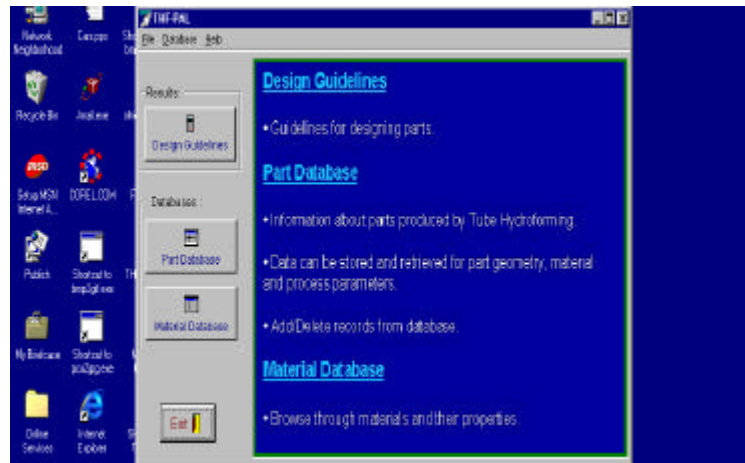


Figure I- 72: main menu of THF-PAL

4.2. Part Design Guidelines

Although the detailed planning of THF processes is carried out through FEA, a preliminary feasibility study is often recommended or even necessary. In an early design stage of any THF parts, the designer is always faced with manufacturability issues. Several analytical models and empirical rules are available in the literature for quick calculations of necessary axial feeds and pressure levels [Ahmed, 1997], [Birkert et al., 1999], [Rimkus et al., 2000], [Asnafi et al., 2000], [Hong et al., 1999], [Thiruvarduchelvan et al., 1999], [Koç, 2000], [Morphy, 1999], [Hillmann et al., 2000]. A good presentation of design guidelines for hydroforming of aluminum tubes can be found in [Hoffmann et al., 2001]. However, most of the times these models are useful for simple parts, i.e. for individual THF features such as T-shapes, and axisymmetric bulges.

Since the literature on this subject is so vast, no attempt is done in this dissertation of giving design guidelines for specific THF operations. However, a novel and general concept for design of complex parts will be proposed.

Initially, some criteria for classification of complex THF parts are provided.

Then, a methodology for a preliminary analysis of part design is proposed, to be carried out before an FEA based detailed process planning.

4.2.1 Geometrical features of complex THF parts

Several attempts of classification of tube hydroformed parts can be found in the literature. Usually, in papers and technical reports on the fundamentals of tube hydroforming, parts are classified according to their function as an end product [Koc, 2001], [Klaas, 2000]. If limiting the analysis to the automotive industry, a functional classification of THF parts would be as summarized by the three categories in Table I- 16: a) piping, b) structural parts (chassis and body, steering and suspension, safety), c) engine and drive cases. However, this kind of classification is not very helpful in the process planning phase.

THF parts were first classified by [Engel et al., 1995] as part of a dissertation on development of fuzzy control systems for hydroforming process. He grouped parts with respect to: (a) their variation along longitudinal axis, (b) variation of the feature position relative to the longitudinal axis and (c) variation of the cross-section. Categorization of parts depending on their shape complexity was also conducted. However, this classification was limited with only parts in exhaust systems excluding structural frame parts. A more useful approach can be found in [Koc, 1998], where structural parts are considered such as frame rails, axles, cradles, along with exhaust components. This classification was based on geometrical features.

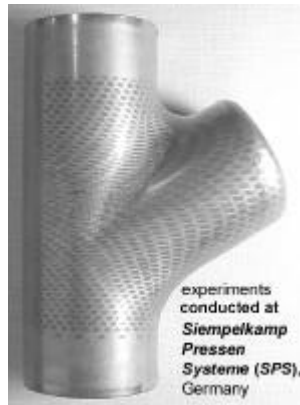
The most critical features are:

- Number and position of *protrusions* (T-shape or Y-shape, see Figure I- 73). A protrusion can be identified when the tube asymmetrically expand in a side branch.

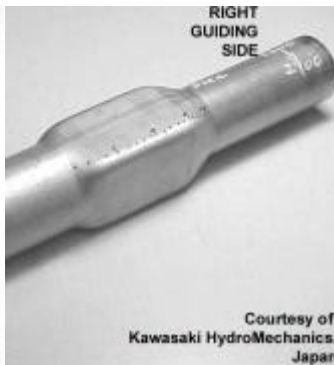
- Number and position of *bulges* (see Figure I- 73). A bulge can be identified when the tube cross sectional perimeter rapidly increases (with either a round, square or irregular shape) without any preferential radial direction.
- Number and position of *bends* (see Figure I- 73).
- The shape of the tube central line, or *spline* (see Figure I- 76). The spline is one-dimensional (1-D) if the tube axis is straight. The spline is 2-D if the bends are only in one plane. The spline is 3-D if the bends are in more than 1 plane.

Table I- 16: classification of THF parts according to their function [Koc, 2001], [Klaas, 2000], [VOLLERTSEN F. 2001], [BOEHM A. 2001]

CATEGORY	materials	examples
<i>Piping</i>	usually stainless steel (AISI 304, AISI 309), but also aluminum for airpipes	exhaust pipes, engine tubes, catalytic converters, pressure tubes, tail pipes, connectors and manifolds
<i>Structural parts</i>	low to medium carbon steels (A 570 Gr. 36, A 738, etc.) and aluminum (AA 1050, AA 5015, etc.) for structural and cost related reasons	<u>chassis and body:</u> front and rear engine cradles, ladder frames, hitch bars, side roof rails and roof bows, instrument panel beams, radiator frames , space-frame components , windshield headers, body side rails
		<u>steering and suspension:</u> control arms, trailing links, steering columns
		<u>safety:</u> roll-over bars, seat frames and shock absorber housings, bumper beams
<i>Engine and drive cases</i>	case hardening steel (SAEM 1015, SAE 1045, SAES 115)	Hollow camshafts, drive shafts and gear shafts



(a)



(b)



Aluminum rear axle part,
courtesy: BMW, 1997

(c)

Figure I- 73: Y-shape protrusion (a), bulge (b) and bend (c).

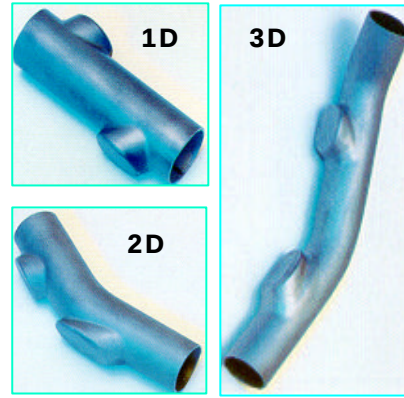


Figure I-74: 1-D, 2D and 3-d tube splines with protrusions along the spline

4.2.2 Technological classification of THF parts

The usual inputs to a hydroforming press are the loading paths (LP): left and right axial displacements $d_{\text{left}}(t)$ and $d_{\text{right}}(t)$ vs. time; internal pressure vs. time $P(t)$; counterpunch force vs. time (when required). Other important design parameters are the tube initial shape and dimensions. Finally, a critical issue is the design of the preliminary optional crushing/preforming operations. All THF parts obviously undergo expansion and calibration through the effect of the internal fluid pressure. The maximum pressure depends on the smaller internal radius. Large, little or no axial feeding may be required, depending on part geometry, size and material.

As a consequence, parts can be classified according to the amount of axial feed they require (or they can sustain). The parts may belong to one or another category depending on their main geometrical features, previously described. As an example, a very long part, with many 3-D bends and with a relatively small outer average perimeter (such as a complex engine cradle part) cannot be axially fed, because friction forces would be too high and because the material would hardly flow from the tube edges towards the center (Figure I-75a). In this case, the forming process is mainly accomplished by crushing + expansion, or only by expansion and little axial feed at both ends only to provide sealing.

The opposite case is a T-shaped part with a straight and short spline: axial feed is not only possible, but also required in order to obtain a significant useful protrusion height.

Between these two extremes lays a full range of intermediate cases, whose classification can be made according to the following considerations:

- **Length of spline.** Fixed the tube initial outer diameter (OD), the longer the spline is the higher are the friction forces. This prohibits the material flow from the tube edges towards the center. With high friction forces, axial feed mainly causes thickening at the tube edges. The tube spline length can be measured as the initial tube length L_i or the final useful tube length L_f .
- **Number and geometry of bends.** Feeding potentialities decrease drastically with bends, especially if they are sharp (bend angle $\geq 90^\circ$). If a part has more than 2 sharp bends (as in Figure I-75a), feeding becomes virtually impossible.
- **Number of bulges.** The need for axial feeding increases as the number of bulges along the spline increases. More than three bulges are difficult to form. Even with 3 bulges, the central one could not easily be fed.
- **Number of protrusions.** Whenever a part has a protrusion (T- or Y-shape), unless the protrusion has a very small height, feeding becomes generally necessary, since the only action of internal pressure would not be enough to form the part without premature fracture or excessive thinning. The higher the number of protrusions, the higher the amount of feeding required. If a part has more than 2 protrusions on different positions along the spline (as in Figure I-75b), forming the central protrusions becomes very difficult and the part might be unfeasible with a conventional 1-step hydroforming process. Analytical and empirical models are available to approximately calculate the maximum height of protrusions, in terms of axial force and internal pressure. In most cases, the use of a counterpunch is required to form a protrusion.

With the aforementioned considerations, it should be possible to understand by large approximation the total required amount of axial feed (d_{ax}). The correct amount of d_{right} and d_{left} can be determined only after FEA or

prototyping. Parts can be roughly divided in four technological groups depending on the required amount of axial feed, non-dimensionally expressed as the ratio between the feed itself and the initial tube length d_{ax} / L_i :

- Group A: No feeding (expansion only) $d_{ax} / L_i \approx 0$
- Group B: Small feeding $d_{ax} / L_i \approx 0.01 \div 0.1$
- Group C: Average feeding $d_{ax} / L_i \approx 0.1 \div 0.2$
- Group D: Large feeding $d_{ax} / L_i \approx 0.4$ or more

Table I- 17 summarizes the considerations expressed in the present section. In Part II, it will be shown that, when using FEA to estimate the process parameters, Table I- 17 can be very useful in selecting the appropriate FEA approach, as described in the following.

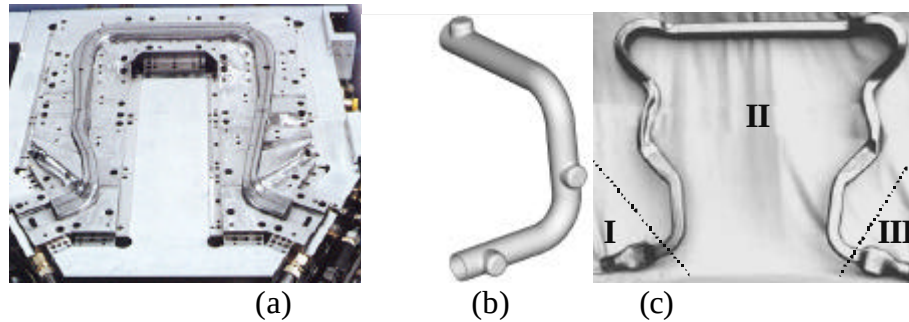


Figure I- 75: (a) part with more than 2 sharp bends (courtesy: Schuler Hydroforming);
 (b) more than 2 protrusions on different positions along the spline;
 (c) engine cradle part (courtesy: SPS, Germany)

Table I- 17: Classification of THF parts according to the amount of feeding. This classification is valid for parts to be manufactured in a THF operation with no intermediate dies.

		GROUP A	GROUP B	GROUP C	GROUP D
GEOMETRICAL FEATURES	general description	long parts with no bulges or protrusions and with sharp bends	parts with no bulges, protrusion nor sharp bends	bulged parts	parts with protrusions
	# of sharp bends	>2	<=2	0	0
	# of protrusions	0	0	0	<=2
	# of bulges	0	0	>0	<=1
	spline length (L_f/OD)	usually > 20	usually > 15	≈ 10	<5
PROCESS DESIGN VARIABLES	CRUSHING	yes	optional*	no	no
	amount of AXIAL FEED d_{ax}/L_i	no feed 0	small feed ≈ 0.05	average feed ≈ 0.2	large feed ≈ 0.4

*usually yes, not only for short steel parts

4.2.3 Modular design of THF parts

As mentioned in the previous Section, in some cases, long THF parts may have different features along the spline, so that their analysis should be decomposed in different modules, each one of these modules belonging to a different technological group. This concept is further explained in this Section.

Problems may occur when designing large and complex parts consisting of many simple part features, i.e., bends, simple bulges, and T-shapes (see as an example the schematic given in Figure I 76). This example part is symmetrical along the plane located next to the T-shape. Thus, it is reasonable assuming than no tube material

flows into/from the right side of the T-shape and that the value of axial stress s_f at the symmetry plane is almost zero. For this part, the T-shape would therefore be the most difficult-to-form feature.

When this part is fed axially at the left end by a displacement d_{left} , the movements of all points in the tube along the axial direction will decrease ($< d_{\text{left}}$) due to the effect of interface friction and of the stress distribution along the tube spline. For simplicity of notation, in the following d_{left} will be indicated as d_0 .

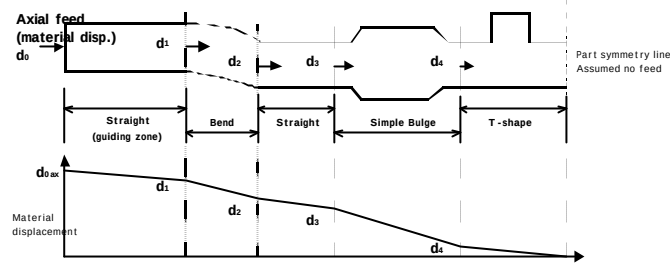


Figure I- 76: Trend curve of material displacement along axial direction at a constant pressure with axial feed = d_0 applied at the left end.

Let us consider two connecting features, e.g. the straight portion (guiding zone) and the bend in Figure I- 76, as two black boxes. The axial displacements associated with these two features can be visualized as the inputs and outputs of these black boxes, Figure I- 77. A Flow Ratio can be defined as $R_n = d_{(n)} / d_{(n-1)}$. R_n is the ratio expressing how easily the material flows (pushed by axial feed, $d_{(n-1)}$) through a part feature # n . The Flow Ratio R_n will be in the range of $0 < R_n < 1$. The closer R_n is to 1, the easier the material can flow through that part feature. With the flow ratios (R) available for each of all common THF part features, one can approximately estimate the available material being fed into any part feature of the given part.

Considering again the part in Figure I- 76, the total combined flow ratios of all the features on the left of the T-shape is assigned as R_{1-4} :

$$R_{1-4} = R_1 \times R_2 \times R_3 \times R_4 \\ = (d_1 / d_0) (d_2 / d_1) (d_3 / d_2) (d_4 / d_3) = d_4 / d_0$$

Therefore, the available material fed into the T-shape at the left side can be calculated as $d_4 = d_0 \cdot R_{1-4}$

At this time, it appears that an analysis of any complex part could be possible by using some known information (i.e. the flow ratios R_n) of each feature in the entire part. For this reason this analysis approach can be called “Modular Design (MD) Approach”. The MD approach seems promising for preliminary analyses of any complex THF parts that can be considered as an array of less complex THF part features. Implementation of the MD approach is an ongoing project at the ERC/NSM. Ultimately, this MD Approach will be incorporated into THF-PAL to enable analyses of THF parts with complex designs.

Potential applications of the Modular Design Approach are stated in the following.

1. Estimate necessary axial feeds for a given THF part and determine initial tube length. This is probably the main and most immediate application. Necessary axial feed (d_0) can be calculated from the known total flow ratio and necessary axial feed for the last feature down stream (from Figure I- 77, the T-shape would be the last feature down stream). Then, the necessary initial tube length can be approximated.
2. Decide methods of detailed FEM analyses on the part. As it will better explained later in this Chapter, parts can be classified according to the amount of feed they require. The MD approach can be useful for a quantitative and fairly accurate estimation of the total amount, therefore it can be used to select the most appropriate FEA strategy (Adaptive Simulation or Self Feeding, see Part II). For longer and more complex parts, the MD approach can be used to determine the effective feeding length of the part, i.e. the part portion (may consist of many part features) in which there is metal flow along the axial direction. The effective feeding length would end where the total flow ratio becomes very small. With this information, different FEM simulation strategies can be applied to different portions of the part: the AS or SF techniques should be used only to the part portions within the effective feeding length. Expansion 2D FEM simulations should be applied to the part portions that are out of the effective feeding length, i.e. where no feeding is possible and deformation is by expansion only.

3. Suggest design changes for the dimensions of each feature. When designing a complex THF part, at the early design stage, all the features in the part will have to be simplified into common THF features (such as simple axisymmetric bulges, bends, and T-shapes) whose flow ratios have already been determined. Then, dimensions of the part features can be adjusted/refined in order to maximize the total flow ratio (i.e. maximize the material flow in the entire part).

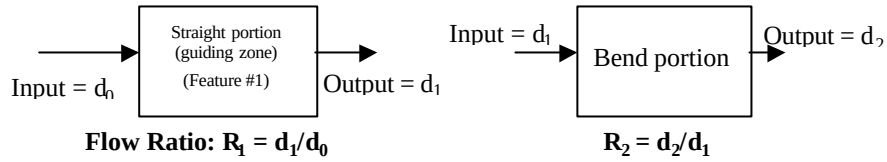


Figure I- 77: output-input representation of straight and bent portions (refer to **Figure I- 76**).

Part I, Chapter 5

The other components of the THF system

5.1. Die-Workpiece Interface: Wear, Friction and Lubrication

Tribological conditions are critical for a correct analysis of the THF system, mainly because of three important features of the process [Prier, 2001]: high surface pressures, considerable relative movements between tools and parts, partial plastic surface expansion during contact. Therefore, friction conditions in hydroforming are very critical especially in parts where substantial axial feeding and high pressure levels are required and where the cross section of the die presents large variations along the tube spline. In such cases, lubrication is used to reduce the sliding friction and prevent sticking and galling to reduce tool wear, axial forces and excessive thinning. Three different friction zones can be identified in THF: guiding zone, transition zone, expansion zone Figure I- 78. Mostly, large relative movements occur in the guiding zone. As a consequence, friction conditions in the guiding zones (see Figure I- 78a) are, very important. Lubrication also becomes a critical factor during the calibration stage when tube cross-section is stretch formed to the final dimensions (see expansion zone in Figure I- 78a).

Different experimental tests have been developed in the literature [Ngaile et al., 2001] and can be used for the evaluation of lubricants (and coatings) in the three different process zones: transition (Figure I- 79), guiding (Figure I- 80) and expansion (Figure I- 81). Main goals of the lubrication tests are:

- to identify the most suitable lubricants and die coatings for a given production application;
- to estimate the value of the coefficient of friction for use in process simulation;
- to evaluate innovative methods for reducing friction in THF (e.g. using textured outer tube surface).

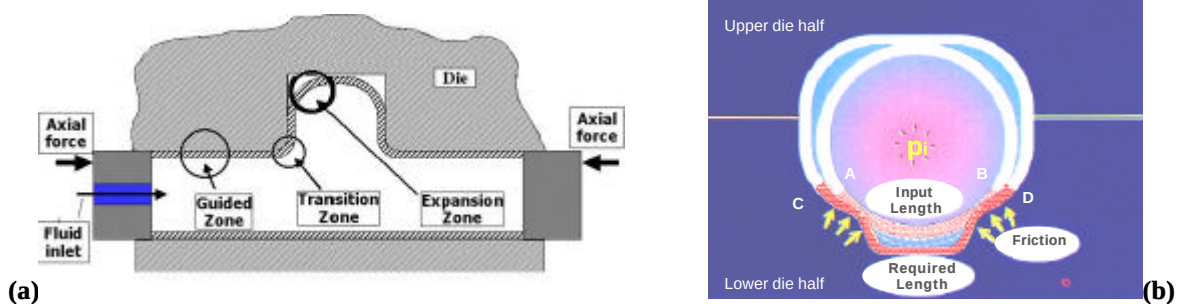


Figure I- 78: (a) Guiding, transition and expansion zone in friction. **(b)** Local wall thinning during forming of a cross section. The section AB of the initial tube is stretched to form the arc length of CD in hydroforming

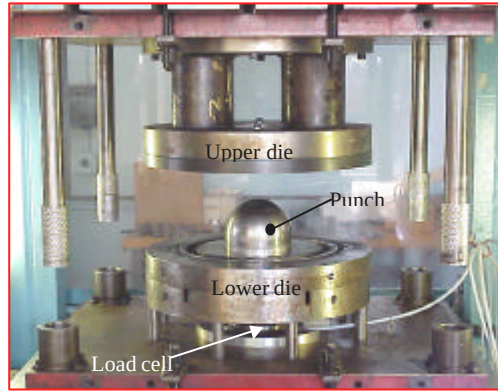


Figure I-79: Limiting Dome Height tooling for lubrication tests in the transition zone

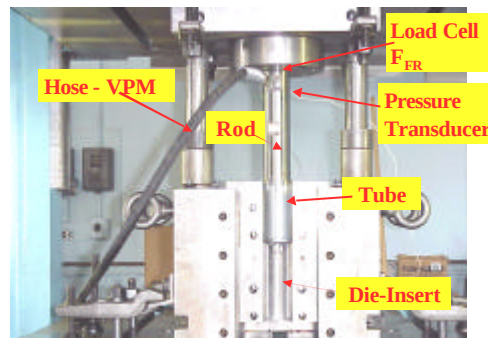


Figure I-80: Tooling for lubrication tests in the guiding zone



Figure I-81: Pear-shaped tooling for lubrication tests in the expansion zone.

5.2. Mechanics of the process

Successful tube hydroforming processes require mainly proper combinations of two driving parameters: internal pressure (P_i), axial feed (d_{ax}).

Internal pressure causes the expansion of the tube while axial feed at the tube edges provides additional material for tube expansion. The expansion in tube hydroforming is mainly due to the internal pressure. It is also possible to hydroform a tube with only internal pressure (i.e., no axial feed). In this case, however, the achievable amount of deformation is limited.

In tube hydroforming, compressive state of stress occurs in regions where the tube material is axially fed, and tensile state of stress occurs in expansion regions (i.e., increase in tube diameter or cross-sectional area). Thus, the main failure mechanisms are bursting (excessively high tensile stress), buckling, and wrinkling (excessively high compressive stress), see Figure I- 82.

An excessively high internal pressure may lead to severe thinning and bursting in the expansion area, if there is insufficient axial feed applied. Insufficient axial feed can also cause leakage problem at the tube edges and the axial punches. On the other hand, excessive axial feed with insufficient internal pressure causes other defects such as buckling and wrinkling.

In order to effectively design the process and select the process parameters, it is therefore necessary a full comprehension of the mechanics governing the THF process. It is important to have an idea of:

- the possible failure modes of a typical THF operation.
- How the internal pressure, the axial cylinders' stroke and the counterpunch force (see Figure I- 80) influence (qualitatively) the tube deformation history and how these parameters can increase or decrease the probability of each kind of failure.

This knowledge is required in order to run accurate and effective Finite Element Analyses. Even before FEA, they are useful to complete a preliminary design process, aimed at approximately calculating some of the process parameters. A few examples are given in the following list.

- Initial tube dimensions (wall thickness, length and diameter)
- Approximate preforming parameters (bending radii, crushing sequences, etc.)
- Minimum axial displacements (or forces) required to avoid leaking.
- Maximum total displacement of axial cylinders to prevent wrinkling or buckling.
- Minimum internal pressure required to bulge the tube.
- Maximum internal pressure (calibration pressure) required to form the inner radii.
- Minimum clamping force required to hold the dies.
- Maximum counterpunch force.

Design guidelines can be helpful in this pre-design phase. Besides, in recent times, more and more articles are available in the scientific and technical literature on these topics.

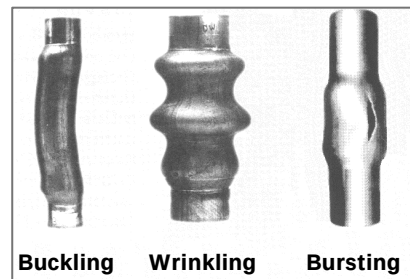


Figure I- 82: failure modes in THF

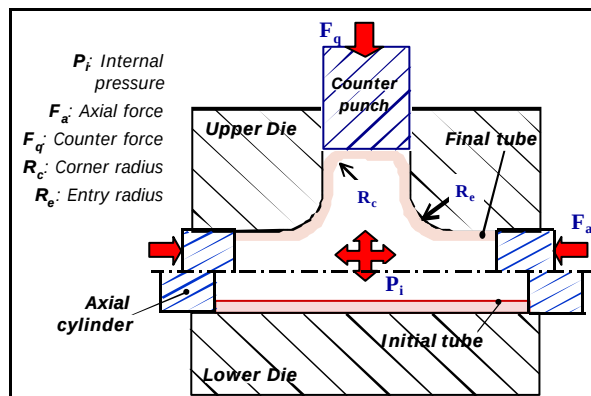


Figure I- 83: main active forces in the hydroforming of a T-shape

5.3. Equipment, press and environment related issues

The main functions of hydroforming equipment are:

- to pump high pressure fluid into the tube (thanks to a pressure intensifier);
- to open and close the dies;
- to provide clamping load during the forming process to eliminate elastic deflections and die separation;
- to transmit adequate axial force to the tube edges in order to allow axial feed (thanks to axial hydraulic cylinders).

In many hydroforming applications, the maximum required internal pressure is considerably high, especially when forming high strength steels or when calibrating small radii. Therefore, an enormous amount of clamping force is required to hold the tube in the die cavity. Employment of a press is often the solution utilized to overcome the internal force in order to cope with higher internal hydraulic pressure. However, some R&D activity (e.g., [Chang et al., 1996], [Siegert, 1998]) has been going on in the last 10-15 years for on simpler machine and equipment design, aimed at the reduction of the investment costs.

Displacing large fluid volumes to open/close the dies, moving the part in/out of the tooling, filling/pressurizing the tube are several factors increasing the cycle time. However, one of the main factors limiting the production rate is the bending/pre-forming operation. Therefore, several bending machines may be used to supply parts to the tube hydroforming process, or forming several parts at a time in hydroforming are various ways of increasing the production rate.

As far as hydroforming equipment is concerned, various press design concepts, are seen in Figure I- 84. Table I- 18, Table I- 19 and Table I- 20 give some examples of current production costs and cycle times. These tables show that machine cost represents an important share of the whole manufacturing cost. However, the machine cost could in the future be reduced. As an example, the operation of the press schematic seen in Figure I- 84(b), is shown in Figure I- 85 [Siegert, K. 1998]. According to this concept, the ram and the upper die are actuated by a low pressure/ long stroke cylinder. Once the upper die is lowered, horizontal (spacer) cylinders are moved to lock the upper die in position. Then, the lower die is lifted by means of a high pressure/short stroke cylinder to close the die and provide the clamping load. Now axial feed cylinders engage and feed the ends of the tube while the internal pressure is increased to form the part. After the part is formed, the lower vertical cylinder pressure is released, the horizontal spacers are pulled out, the ram moves upward to open the dies. The axial feed cylinders are usually built on the hydroforming tooling in order to assure precise guiding and a stiff structure. One of the advantages of this system is to reduce the volume of the hydraulic fluid used for the motion of the upper and lower vertical cylinders. This results in a shorter closure and cycle time.

Table I- 18: cycle times for various parts [Mason, 1996]

Component	Parts per Stroke	Cycle Time (approximate)
Exhaust Manifolds	2-4	15-20 sec
Side-rail for pick up truck	2	40 sec
Instrument panel beam	1	35 sec
T shapes	Up to 25	13 sec

Table I- 19: production costs of an engine cradle for various technologies [Boehm et al., 2000]

Variant	# of parts	Weight (Kg)	Tool costs (USD)	Part Cost (USD)
Classic deep drawing	34	24.56	5,359,090	51
THF of steel	30	20.5	3,712,636	42.83
THF of aluminum	30	14.41	3,891,727	73.17
THF of steel - low pressure	30	22.77	3,152,398	45.55

Table I- 20: piece cost analysis of hydroformed components [Treude et al., 2001]

Cost	% of total production cost
Machine costs	43.3
Material costs	47.1
Wages	5.3
Spare and ware parts costs	2.8
Operating material costs	1.5

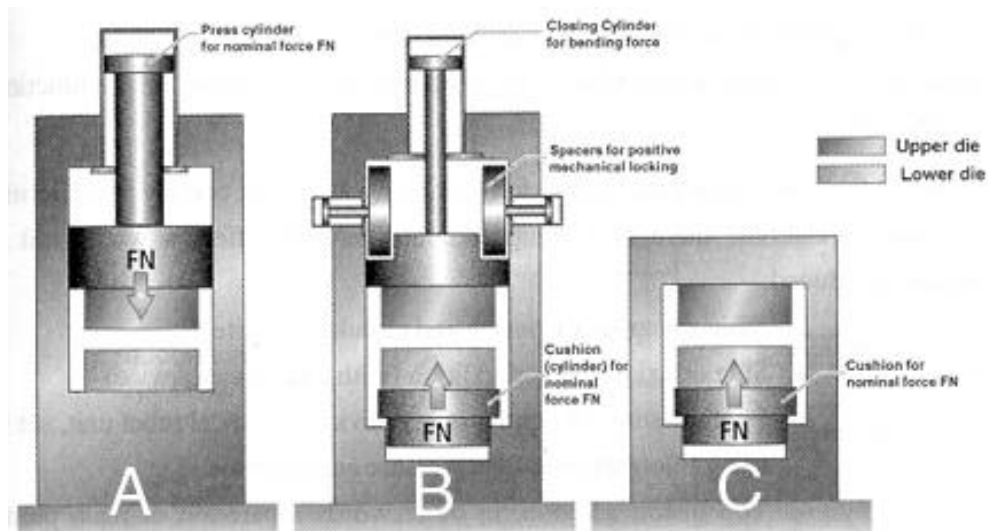


Figure I- 84: various press designs [Siegert, 1998]

In installing tube hydroforming presses it is also necessary to consider the infrastructure and human resources available in a plant. Since tube hydroforming is a relatively new technology efficient use of expensive presses is best achieved when well trained personnel is available.

Hydraulic fluid is usually recirculated to prevent any adverse effects to environment. As it is used in all hydraulic press operations, the potential environmental hazards of operating THF presses are considered when installing new production facilities. The hydraulic pressure medium, used in the presses, is recycled and not inadvertently released to the environment. Therefore, the environmental cost of running THF operations can be considered low.

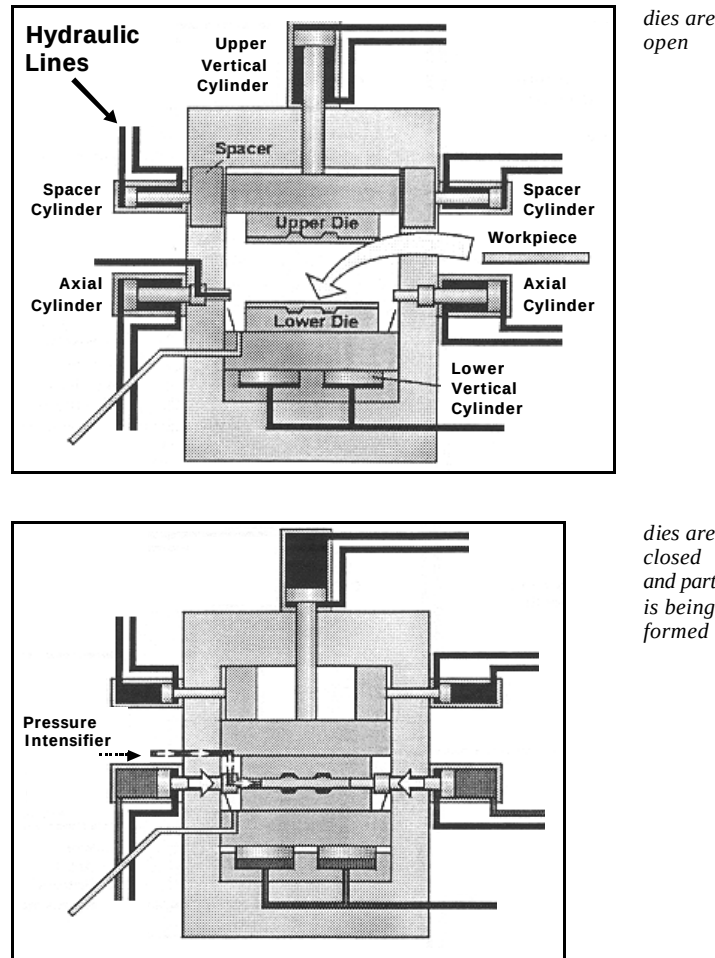


Figure I- 85: concept of press shown in Figure I- 84.

5.4. Specifications and requirement of the hydroformed part

The macro- and microgeometry of the product, i.e., its dimensions and surface finish, are obviously influenced by the process variables. The processing conditions (temperature, strain and strain rate) determine the microstructural variations taking place during deformation and often influence the final product properties. Moreover, THF parts often show marks caused by several reasons (the tool parting lines, the use of inserts in the hydroforming tool, ejectors/positioners, gripper/clamping jaw by bending, axial forming/sealing punches. Consequently, a realistic systems approach must include consideration of the relationships between properties and microstructure of the formed material and the quantitative influences of process conditions upon strain hardening and wall thinning throughout the hydroformed part.

The dimensional accuracy of hydroformed parts is generally superior to comparable stamped parts due to extremely high pressures/forces used in hydroforming processes. The same pressures and forces, on the other hand, cause elastic deflection of dies and press, and die wear, which are the major causes of part inaccuracy. Generally, achievable tolerance in diameter (~ 50 mm) of exhaust components is in a range of 0.35 mm. For engine cradles [Boehm et al., 2000], the error due to springback is of about 1 mm on the nominal position (initial tube: 1800 x 65 $\varnothing$ x 2.5 mm, steel). For other hydroformed components, position tolerance of 0.15-0.35 mm and form tolerance of 0.15-0.20 mm were reported [Vollertsen, 2000].

Due to the fact many processes are involved in manufacture hydroformed tubular components (i.e., tube rolling, bending, crushing, hydroforming, and assembly), the total dimensional accuracy results from accumulation of dimensional errors of the forming stages. Thus, in order to achieve high dimensional accuracy:

- Types of weld have to be properly applied for different tubular materials to avoid premature failures during the process chains; LCS tubes are normally high frequency welded, while laser weld is used for SS tubes due to its high toughness.
- Spring back of preformed and final parts have to be taken into account through out the processes.
- Hydroforming press and die inserts have to be able to withstand necessary loads required in any given hydroforming process.
- Proper lubricant is critical not only for increasing formability but to achieve reduced die wears.

5.4.1 Crashworthiness

Crashworthiness becomes more and more important for hydroformed automotive structural components in recent years due to 1) more stringent safety requirement by governments regulations; 2) lighter structure as the result of weight reduction that adversely affects the energy absorption during crash. Crash absorbing components in current automotive industry are more and more produced through hydroforming operations and R&D activities in this field are increasing. There are different ways of addressing this issue in THF.

In the first place, the steel industry has been under the pressure to develop new tube steel grades for a different type of conflicting needs: better formability for improved manufacturability and higher strength to achieve better or equivalent performance with lighter gages (see following subsection) [Chen et al., 2001].

On the other hand, in shock-absorbent joints used throughout the structure, if possible, each of the components is usually designed to contain dimpled/corrugated features. This obviously introduces more complexity into tube hydroforming.

Third, crashworthiness FEM simulations are conducted for the entire structural body with mapped strain distributions induced from forming processes. Available strains left (i.e., available stretching/shrinking to fracture) in hydroformed components is one of the main factors defining component shock absorbability. The more available strains left in the part, the better shock adsorbent it will perform. Therefore, part programs to bend, crush, and hydroform any structural components should be selected with the goal of minimizing strain.

In the following subsection, an overview is given on steels used in THF for anti-crash applications. Although this section should be considered as part of Chapter 2, it is probably more useful to see the use of crashworthy materials as a direct consequence of a precise requirement of many THF parts.

NEW FORMABLE AND CRASHWORTHY MATERIALS

There are many factors that should be considered in the development of a new steel product. The performance during manufacturing process includes formability in the first place, but also weldability, low elastic recovery, etc. The performance under service conditions includes yield strength, dent resistance, durability, crashworthiness, corrosion resistance, etc.

The microstructure of conventional steels often makes it impossible to obtain concurrently good ductility and high strength. However, some applications, especially in the transportation industries, require economical high strength steels with good formability.

In the last two decades, the steel industry has developed a series of steels to meet the constantly changing market requirement. Interstitial free (IF) steels, bake hardenable (BH) steels, high strength low alloy (HSLA) steels and ultra high strength (UHS) steels are some of them. These steel grades provide a wide range of formability and strength combinations and have been used for automotive body panels and structure parts. Interstitial Free (IF) steel was developed to meet the formability requirement as required by the complicated styling and improved stamping yield. In order to improve dent resistance while maintaining good formability, bake hardenable (BH) steels were developed.

Recently, dual phase (DP) steels [Bayram et al., 1999], transformation induced plasticity (TRIP) steels [Girault et al., 1998] and other complex phase (CP) steels have attracted a lot of attention due to their advantages in formability and crash energy absorption. The capability of energy absorption by a steel can be described by the area below the stress/strain curve and is related to the tensile strength and total elongation. Although high strength steels have higher strength, their lower total elongation often limits their ability of energy absorption. In

contrary, dual phase steels and TRIP steels exhibit higher tensile strength as well as total elongation and thus higher energy absorption.

Dual phase steel is a ferrite + martensite steel with C less than 0.25%. Current Dual Phase steel products have yield strength in the range of 400 – 800 MPa with total elongation 15 – 30%. The steel is produced by quenching the steel from the intercritical annealing temperature in a continuous annealing or hot dip galvanizing line. The austenite transforms into martensite. In the hot dip galvanizing line, some bainite may also present. The properties depend on the dispersion and volume fraction of martensite. The volume expansion due to martensitic transformation results in high mobile dislocation density in the neighboring ferrite adjacent to the grain boundary. This high density dislocation structure helps plastic deformation under applied external stress and results in a low yield strength. Dual phase steels typically exhibit lower yield/tensile ratio, higher n-value and total elongation, and thus higher energy absorption. The yield/tensile ratio is normally < 0.6 . The n-value can be as high as 0.2, which is similar to drawing quality steels.

Transformation Induced Plasticity (TRIP) steel is a multiphase steel which has a microstructure of ferrite, bainite and retained austenite. During plastic deformation, the retained austenite transforms to martensite that leads to higher strength and total elongation. The more the retained austenite, the higher the uniform elongation. The TRIP steel thus provides the best combination of strength and formability. As expected, the high work hardening and total elongation offer the steel with very high energy absorption. It is clear that the key to the TRIP steel is maintaining a sufficient amount of retained austenite. The steel is heated to the intercritical annealing temperature to form the austenite and ferrite. The steel is then subjected to the carefully designed cooling in order to enrich the austenite with carbon by precipitating more ferrite and bainite. Thus the M_s temperature is driven to below room temperature and more retained austenite is possible. TRIP steels with strength ranging from 600MPa to 980MPa are commercially available and efforts are underway to use them for crash critical parts.

Summary

In this Part I, a system approach for the analysis of the Tube Hydroforming technology has been proposed. The following key issues and components have been addressed.

PROPERTIES AND QUALITY OF INCOMING TUBES

The tube bulge test has been introduced for the determination of the stress-strain curve, with its drawbacks and advantages (Chapter 2). A simple tooling for the bulge test has been described.

Then, an energy method has been given and validated, for the determination of the flow stress law parameters. The proposed inverse energy method is suitable to provide approximated flow stress data. The method is relatively simple and computationally inexpensive.

The literature on the determination of forming limits has been reviewed. Forming Limit Diagrams (FLD), Damage models, Forming Limit Stress Diagrams (FLSD) and numerical perturbation methods have been described.

Then, the use of the bulge test for the determination of FLDs has been explained and experimental findings have been reported, with a comparison of Forming Limit Diagrams obtained from tubes and sheets. Furthermore, the effect of tube thickness and diameter on the FLD has been investigated. Experiments have been run for the determination of damage functions and critical damage value. The practical use of forming limit stress diagrams (FLSDs) has been explored.

PREFORMING AND BENDING DESIGN AND PRODUCTION METHODS

A set of experiments and FEM simulations has been described in order to demonstrate the effect on bending parameters, and particularly of the bending radius, on formability (Chapter 3).

The idea of using macro-structured preformed tubes has been explored via FEM simulations. Potential benefits and drawbacks of using macro-structured preforms have been shown, along with the effect of preform shapes on tube formability in free bulging of round tubes and in bulging of tubes with complex (round to square) die geometry. Preforms with waves in the hoop direction and in the axial direction have been studied.

PRODUCT, DIE AND TOOL DESIGN

The most typical geometrical features of complex THF parts have been indicated in order to put the basis for the development of a set of computerized part design guidelines. A technological classification of THF parts has been proposed and the idea of modular design of THF parts/processes has been introduced (Chapter 4).

THE OTHER COMPONENTS OF THE THF SYSTEM

Finally, issues related to the other components of the THF system have been briefly addressed (Chapter 5):

- Die-Workpiece Interface: Wear, Friction and Lubrication; some tests for evaluation of friction in different zones of the process have been given as examples.
- Mechanics of the process; the main process parameters and possible defects have been listed.
- Equipment, press and environment related issues; some press concepts for reduction of investment costs have been shown.

- Specifications and requirement of the hydroformed part.
- Crashworthiness.

PART II

Process Analysis and Design

Part II, Chapter 1

Introduction

In order to successfully design and develop a new Tube HydroForming (THF) process or operation, attention must be paid to several aspects and issues of the technology and a system approach to the resolution of problems is recommended. In other words, when designing a new process, problems and improvements in each area of the THF technology and their interaction should be considered. The main components and key issues of a complete THF system have been described in Part I:

- A.** Quality and material properties of incoming tubes;
- B.** Preforming and bending design and production methods;
- C.** Die and tool design guidelines;
- D.** Die-workpiece interface issues: wear, friction and lubrication;
- E.** Mechanics of the different deformation zones;
- F.** Equipment, press and environment related issues;
- G.** Specifications and requirement of the hydroformed part.

The information concerning each of these points should be used by the process engineer, when developing a new THF operation (see Figure 1 in the Foreword), along with appropriate and effective design tools, such as:

- design guidelines,
- Finite Element Analysis (FEA),
- prototyping.

Design guidelines have already been addressed in Part I, Chapter 4. Physical prototyping issues are not addressed in the present dissertation. The most recent Finite Element Analysis strategies for process design will be discussed in this Part II.

In the product and process design phases, in order to reduce the cost of physical prototyping, Virtual Prototyping, i.e. computer simulation using Finite Element Analysis (FEA) plays a major and key role [Hora et al., 2001]. Both the

- input data and
- the strategies

used in Virtual Product and Process Development must be carefully selected, if an accurate and relatively fast response is required. Although the present dissertation focuses almost entirely on the central hydroforming operation, it is important to underline that virtual prototyping should include many other processes. Indeed, for accurate response, the whole cycle life (except disassembly and recycling) of the part should be considered (see Section 3.1.2), especially when intermediate annealing treatments are not performed on the tube. Even the effect of the welding seam can sometimes be taken into account [Hielscher, 2001]. One effective example of the importance of simulating the whole manufacturing process, carrying the strain history from the tube making process to the final hydroforming operation can be found in [Carleer, 2001]. The cited paper omits to mention that the strain/stress distribution on the tube can also be used for structural elastic or dynamic elastic-plastic FEA of the part's behavior, whose most significant example is the use of process simulation results for crash test simulations.

In Part I, the technological critical factors for obtaining reliable input FEA data (quality and material properties of incoming tubes, preforming and bending simulation methods, die-workpiece interface issues), have been reviewed. In this Part II some original FEA strategies and techniques developed (and currently used at the ERC/NSM) for effective process design are described.

The control of the process is the most critical issue in the hydroforming technology, since it affects the occurrence of defects, especially wrinkling and necking. Therefore, the process parameters must be very carefully selected, if a successful THF operation is desired. The success of a THF operation is largely dependent on the selection of the loading paths (hereinafter referred to also as LP), i.e. of the:

- internal fluid pressure vs. time curve $P(t)$,
- (left and right) axial feed vs. time curves $d_{\text{left}}(t)$ and $d_{\text{right}}(t)$, in case material is fed by two axial punches,
- counterpunch force vs. time curve., in case the part has protrusions, such as a T-shape or Y-shape

The process is often very sensitive to a change in the position or shape of these curves. See as an example the possible defects that can occur on the test part pictured in Figure II- 1.

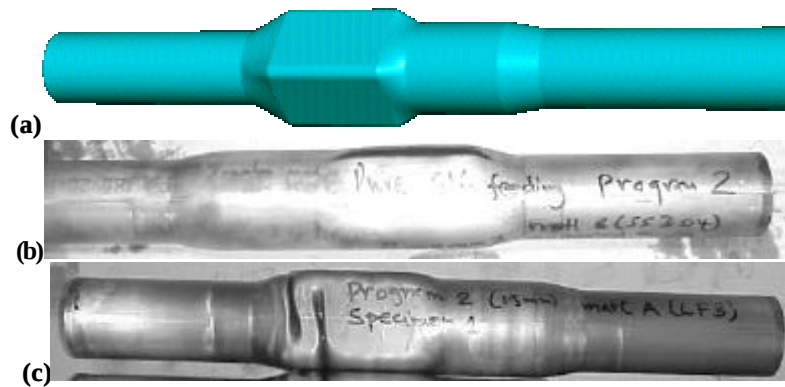


Figure II- 1: (a) test part for the Self Feeding and Adaptive Simulation studies and possible defects (experiments conducted at Kawasaki HydroMechanics, Japan); (b) fractured part; (c) heavily wrinkled part

During process planning, Finite Element Analysis (FEA) is extensively used to predict the occurrence of defects and to approximately select appropriate pressure/time and axial feed/time curves. FEA is usually conducted on a 'trial and error' basis, i.e. loading paths are selected to conduct a simulation and, if the results are not satisfactory, the input paths are reselected by 'intuition' and the simulation is run again. This trial and error method can be very time consuming. However, if the process designers are well experienced on the particular problem, a very good solution, in terms of parameter optimization, can be rapidly found.

Different approaches, aimed at the reduction of the simulation times are under development in several research institutes and companies. Two main different strategies can be pursued:

- sequential optimization of loading paths (LP), obtained through iteration of several simulation runs,
- adaptive selection of loading paths, obtained by adjusting the LPs as the simulation proceeds, according to the instantaneous results of the simulation itself.

The latter approach will be hereinafter referred to also as Adaptive Simulation¹ (AS). It is important to underline that the two different strategies are not alternative, i.e. sequential optimization can be based on iterations of single AS runs.

Both methods require a post simulation phase, during which the output loading paths are refined. In fact, the proposed approaches sometimes require certain assumptions or neglect some effects. For these reasons, in Chapter 4 all kinds of possible adjustments will be explained.

¹ In the scientific literature on FE methods, the term adaptive simulation is often referred to the automatic re-meshing (h-adaptivity) or to the adaptivity of the elemental shape functions (r-adaptivity). In the present context, the meaning of adaptive simulation is the automatic adjustment of the process parameters during the FEM run.

In the following Chapters, these two techniques will be presented and explained. In many occasions, the conclusions of each Chapter and Section will be based on a series of experimental tests that are described in detailed in Appendix C; each experiment is also numbered for reference with the label LP, followed by a digit.

1.1. Sequential optimization strategies

The Finite Element Method is today a well-established technique for numerical simulation of sheet forming processes. The industrial success of the method is mainly due to the rapidity and reliability of formulations that use shell elements and explicit time integration schemes. In fact, numerous software houses (especially French and North American) offer commercial packages, suited for sheet forming applications. Several software houses are also developing and distributing optimization packages, to be used in combination with the FEM code, able to help and guide the user towards the best process design. Optimization techniques are usually sequential, i.e. based on the results obtained by several simulation runs². The sequence is usually driven by an AI (Artificial Intelligence) software, or by an analytical optimization methodology, or by a combination of both. These methods are usually able to interpolate among (or to learn by) the past simulations and to suggest the loading paths for the next trial. The main advantage is that they reduce the computational time, and make possible some degree of optimization of the process parameters. Usually, sequential optimization techniques are best suited for the geometric design of tools and dies. However, they can also be applied to the selection of the loading paths, i.e. for the optimization of time-dependent process control variables (velocities, forces, pressures, etc.).

As a general rule, sequential optimization methods show some limitations when used for the determination of loading paths in forming processes where control is a critical issue and where the process window is not very large. In fact, the main problems of sequential optimization techniques when dealing with control parameters are:

- The loading curves must be parameterized.
- They all require an initial guess.
- High number of simulations.

A very simple, but effective method for sequential optimization, called Self Feeding (SF) approach is proposed in Chapter 2.

In the literature, one of the most recent and convincing examples is given in [Yang et al., 2001]. This mentioned paper deals with an optimization technique used to minimize the tube thickness variation by determining the optimal LP in a simple tube expansion operation and in a tube sub-frame forming operation. The optimization is performed by means of a gradient-based method including sensitivity analysis. A constraint function is used in order to minimize the distance between the tube and the die, i.e. to ensure complete die filling. The sensitivity of the objective function is calculated by direct differentiation of the equation of motion.

1.2. Adaptive simulation

The adaptive simulation approach is based on the ability to detect/identify the onset and growth of defects during the process and promptly react to them (see simplified scheme in Figure II- 3). Loading paths can therefore be adjusted, within the same simulation run, to correct those defects. The ultimate goal is the selection of a feasible part program with a minimum number of simulations, or even within a single run. If compared to the previous class of methods, the required computational time is greatly reduced, but the process optimization is harder to accomplish and it can be done only locally. In the literature, for a full comprehension of the state of the art in this field, it may be useful to consider not only the publication on hydroforming, but also the bibliography on adaptive control of blank holder force (BHF) in stamping. A good literature review on this topic can be found in [Obermeyer; 1998]. Therefore, in the present section, only specific studies on THF will be considered.

Work is being conducted at the ERC/NSM [Strano et al.; 2000], at the IFUM, Hanover [Doege et al.; 1998], at the Chalmers University of Technology [Mattiason et al., 1996] and at the RIKEN (Institute of Physical and

² Here is a partial list of references on sequential FEA optimization: [Chung et al. 1998], [Trowsdale et al., 1998], [Larkiola et al. 1998], [Young-Hae et al., 1996], [Fujikawa et al.; 1993], [Hsu et al.; 1997], [Kini et al.; 1998], [Zong-hua et al.; 1993], [Oh et al., 2001].

Chemical Research, Hirosawa, Japan) [Xing et al., 2001], in order to develop techniques for the full-automated adaptive simulation of tube hydroforming. The first two techniques (ERC and IFUM) are based on criteria for the detection of wrinkling, buckling and bursting and act with constant time control interval.

The technique proposed in [Mattiason et al., 1996], [Lundqvist, 1998] has the ultimate goal of developing a procedure for the solution of force-driven, quasi-static problems by means of an explicit, dynamic FE-method, which yields a solution time in the same range as for a corresponding displacement-driven problem. The proposed solution is an adaptive loading procedure in which the loading rate is automatically adjusted to meet the target function in the form of a prescribed velocity norm as a function of time. The main goal of this technique derives therefore from strictly numerical considerations, but it certainly has effects also on the technological aspects of the process. Indeed, test parts used in this work are geometrically simple and do not have a critical point in the selection of process parameters.

The solution proposed by RIKEN is even more deeply involved with mathematical formulation of the FEM solver. In fact, the first three techniques are based on the use of commercial FEA codes, whereas in [Xing et al., 2001] ITAS3D, an in-house finite element code is used. In this paper, first, in order to get an optimum deformation path for THF, the hydroforming limit of isotropic and anisotropic tubes subjected to internal hydraulic pressure, independent axial load or torque is proposed based on the Hill's general theory for the uniqueness to the boundary value problem and compared with those of the conventional sheet forming. The above theory is used as a criterion to control the materials flow and to prevent failures. Finally, the tubular hydroforming of a geometrically simple part (an automobile differential gear box) is taken as a test case (see Figure II- 2).

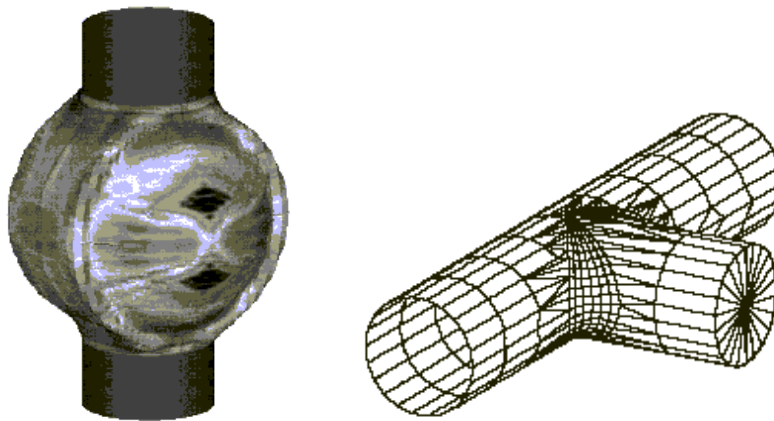


Figure II- 2: differential gear box [Xing et al., 2001] and T-shape [Lundqvist, 1998]

In Doege et al., '98, a fuzzy control algorithm for the detection of defects and the determination of pressure and axial force increments is presented. This algorithm is applied at each simulation time step Δt . However, the above method has some shortcomings. To apply the detection algorithm at each simulation time step can considerably increase the total simulation time and the resulting control paths can be too often re-adjusted. To select a constant time control interval ($\Delta \tau$), longer than the average simulation time step (Δt), can cause the risk of not promptly reacting to a growing defect.

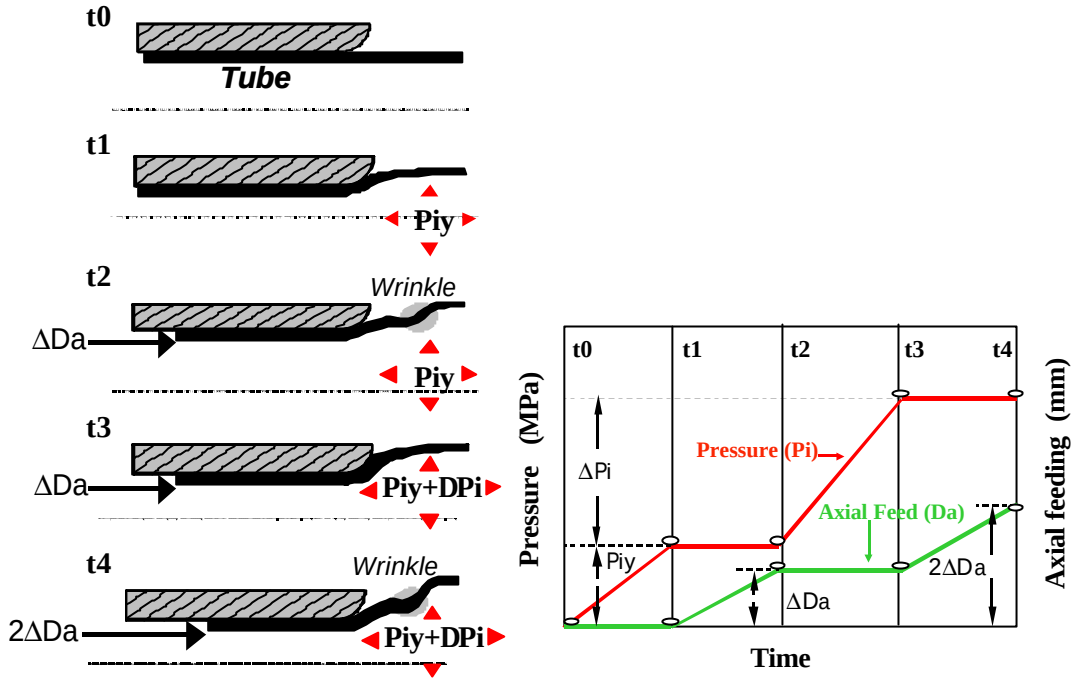


Figure II- 3: Schematic of the adaptive simulation procedure.

P_{iy} : internal pressure; DP_i : internal pressure increment; DDa : axial feed increment.

The adaptive control algorithm proposed in [Altan et al., 1999] was implemented and tried out with commercial FEM codes, i.e., DEFORM-2D and PAM-STAMP. The algorithm automatically adjusts the internal pressure by a predefined pressure increment (ΔP_i) and the axial feed by a predefined axial feed increment (Δd_{ax}). In Strano et al., 2000, the adaptive control algorithm has been modified by adding an optimization technique for the control time interval $\Delta \tau$, able to reduce the total computational time, without increasing the risk of growing an irreversible defect during the control interval. The proposed adaptive control algorithm is semi-automated, i.e. the detection and the evaluation of defect is left to the FEM user. The pressure (ΔP_i) and the axial feed rate (Δf) increments are determined at the end of each simulation interval $\Delta \tau$.

All the presented procedures are based on a wrinkle indicator **I**, able to detect and possibly evaluate the severity of the wrinkle. The wrinkle indicator may be:

- global i.e. based on a variable integrated all over the tube;
- local i.e. based on a variable integrated for a single node or element or a local cluster of elements/nodes.

Part II, Chapter 2

The Self Feeding (SF) Approach

The *self feeding* approach is a simple optimization method designed to restrict the search for the loading paths to a proper family of curves and to select the optimum within this family. A number of consecutive FEA runs are conducted with the same LPs, but with different increasing amount of axial feed, until the final hydroformed part shows permanent wrinkles. The main goal of the self feeding approach is to obtain a partially optimized solution. It usually takes a longer time than the adaptive simulation, since more runs are required.

The starting point of the SF approach is to run an initial FEM simulation without boundary conditions on the tube edges and with zero friction. As a consequence, the tube will be pulled towards the bulging area only by the effect of internal pressure. This initial simulation provides a minimum required value of axial feed and it is also useful to understand the needed proportion between left and right feed. Then, the amount of axial feed is progressively increased, with repeating FEM simulations, until satisfactory results are achieved. The approach is not appropriate in parts where the natural feeding is prohibited or restricted by the geometry of the die, i.e. where the bulge area is strongly non axisymmetric, such as in T-shapes and Y-shapes. However, the approach provides good results for structural and frame parts and it has been used in several studies at the ERC/NSM.

2.1. Calculation of process parameters in SF

The self feeding approach can be summarized as in the following two 2.1.1 (calculation of pressure vs. time) and 2.1.2 (calculation of axial feed vs. time).

2.1.1 Determination of pressure vs. time

An initial simple pressure curve vs. $P_i^{self}(t)$ curve is assumed. This curve is then used as an input to the FEM simulations required to select the axial feed d_{right} and d_{left} . Finally, the desired or “best” loading curve $P_i(t)$ is determined through a post-processing (evaluation) phase (see Chapter 4). $P_i^{self}(t)$ is calculated as follows.

Step 1. A maximum value (upper bound) for the calibration pressure P_{max} is initially roughly estimated as:

$$P_{max} = \bar{s}_{MAX} \frac{2}{\sqrt{3}} \frac{t_0}{r_c}, \quad (II-1)$$

where r_c is the minimum internal die radius. $\bar{s}_{MAX}$ can be chosen as a high value on the flow stress curve, or as the Ultimate Tensile Strength (UTS), i.e.:

$$\bar{s}_{MAX} = K \left(\epsilon_0 + \frac{2}{\sqrt{3}} \epsilon_{min} \right)^n \quad (II-2)$$

where ϵ_{min} is the minimum admissible thickness strain and ϵ_0 is the pre-strain.

It must be underlined that this value is only a rough estimate, the real maximum pressure P_{max} will be selected after the conclusion of the simulation. In case where P_{max} , as calculated by Eq. 2, exceeds the capability of the intensifier, the maximum pressure available on the press is used. Moreover, the real final $P_i(t)$ curve will hardly be linear, since the LPs are refined after the simulation, as explained in Chapter 4.

Step 2. A total simulated time (T_{FEM}) is then estimated depending on the number of elements in the model and on the required accuracy. The FEA is explicit; therefore the simulated time will not be equal to the real process time.

Step 3. Finally, $P_i^{self}(t)$ is calculated as linearly increasing from the point ($t=0, P_i=0$) to the point (T_{FEM}, P_{max}).

Therefore, the self feeding approach has the limitation that it can only produce pressure vs. time curves $P_i(t)$ that are non decreasing.

2.1.2 Determination of axial feed vs. time

The approach can be summarized as follows:

Step 1. Obtain loading paths (called *self feeding* LP) that generate an unwrinkled part, but with very high thinning or fracture. The outputs of this step are: $d_{left}^{self}(P_i)$, $d_{right}^{self}(P_i)$. The self feeding simulation is run with the following boundary conditions:

- zero friction at the interface die/tube;
- no boundary conditions on the tube edges;
- P_i equal to $P_i^{self}(t)$, as described in the previously described.

Step 2. Run several FEA simulations using the following curves:

- pressure vs. time curve always equal to $P_i^{self}(t)$;
- axial feed vs. time obtained by transforming the axial feed curves $d_{left}^{self}(t)$ and $d_{right}^{self}(t)$ as follows:

$$\begin{aligned} d_{left} &= a_{sf} \cdot d_{left}^{self}(t) \\ d_{right} &= a_{sf} \cdot d_{right}^{self}(t) \end{aligned} \quad (II-3)$$

with, $a_{sf} > 1$.

Step 3. To repeat Step 2, increasing at each iteration the feeding factor a_{sf} by a certain amount, until the maximum thinning of the hydroformed part is within the desired limits; the simulated process time T_F must be equal for all simulations.

The factor a_{sf} carries the function of increasing the axial feed in order to avoid excessive thinning. In other words, a_{sf} is the parameter that drives the optimization method. The amount of increase of the factor a_{sf} can be empirically decided by the end user. Alternatively, it can be determined through optimization algorithms (e.g. gradient based), where the controlled variables are the amount of thinning (or thickness strain) and wrinkling.

2.2. The risk of leaking in feed controlled THF

In THF with control of axial feed, particular care must be taken that the risk of leaking is prevented, especially when the amount of axial feed is small. In FE simulation, feed control means assigning velocity or displacement boundary condition to the tube edges. In this case, when the feed is small, there is no guarantee that leaking is prevented. Therefore some corrective action must be taken, depending on the specific sealing configuration of the axial cylinders and on the control system of the hydroforming press.

For an example sealing configuration, given in Figure II- 4 the normal pressure σ_N at the tube/cylinder interface is:

$$\sigma_N = s_f \sin(\alpha) + s_r \cos(\alpha) \quad (II-4)$$

where σ_ϕ and σ_r are the axial and radial stresses.

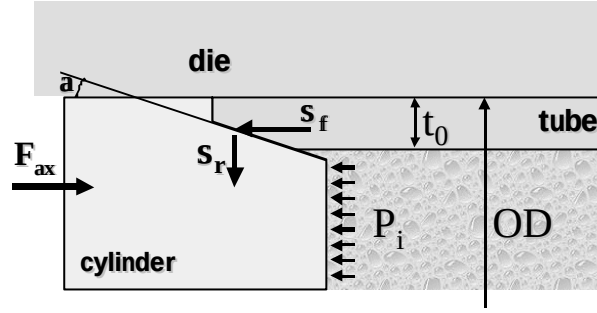


Figure II- 4: Forces, pressure and stresses at the tube edge with conical punch.

The nodes on the tube edge have a velocity (or displacement) boundary condition applied. It is therefore possible to track the value of σ_N as an output of the simulation. A “safe” condition for preventing leaking would simply be:

$$s_N < -P_i \quad (II-5)$$

During FEM simulation with shell elements, it is difficult to simulate the effect of a conical punch, as in Figure II- 4. The FEM simulated model, obtained by applying velocity (or displacement) boundary conditions on the tube edges, would be as depicted in Figure II- 5. In this case $\sigma_N = \sigma_\phi$ and the stress condition for preventing leaking would be:

$$s_f < -P_i \quad (II-6)$$

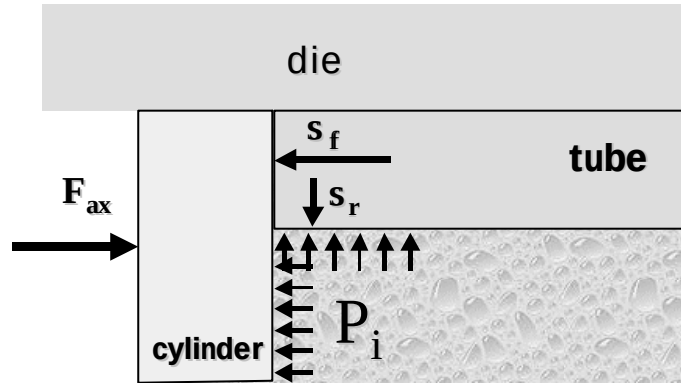


Figure II- 5: Force balance with (ideal) cylindrical punch

However, it must be clear that the actual limiting value for σ_ϕ given in eq. 6 depends on the specific design of the sealing mechanism.

During the initial self feeding run, since no boundary condition is applied at the tube edges, obviously the axial stress is identically $\sigma_\phi = 0$ and leaking would most likely occur in the real process (see Section 2.4).

Since no friction is assumed during the initial run, $d_{self}(t)$ should be regarded as the maximum possible value of “natural” draw-in, i.e. the maximum possible amount of feed caused only by the effect of the internal pressure. During the real process simulation, in order to respect the condition given by eq. 6, a minimum amount of axial feed is required, $d_{sealing}(t)$, that ensures sealing. During any following simulation, if at any time $d_{sealing}(t) > a_{sf} d_{self}(t)$, then leaking might be occurring. Therefore, equations 3 should be changed as:

$$\begin{aligned} d_{left} &= \max \left\{ d_{left}^{sealing}, a_{sf} \cdot d_{left}^{self}(t) \right\} \\ d_{right} &= \min \left\{ -d_{right}^{sealing}, a_{sf} \cdot d_{right}^{self}(t) \right\} \end{aligned} \quad (II-7)$$

2.3. Calculation of $d_{sealing}$

In this section, we will explain how to calculate the minimum feeding value that guarantees sealing ($d_{sealing}$), with reference to Figure II- 6. The following assumption can be reasonably made close to the tube edges, for small feeding values. The hoop (circumferential) stress and the thickness strain can be neglected ($\sigma_\theta=0$, $\epsilon_t=0$). An upper bound for the effective strain at the tube edges can be calculated under the assumption that all the “extra” feed (additional to d^{self}) upsets the tube in the guiding zone, i.e. no material is actually fed into the bulge area:

$$\bar{e} = -\frac{2}{\sqrt{3}} \ln \left(\frac{L_g - d^{sealing}}{L_g - d^{self}} \right) \quad (II-8)$$

On the other end, a lower bound can be obtained assuming that all the extra feed pushes the material into the bulging zone, without actually deforming the tube in the guiding zone. In this second case the effective strain would be zero. The real value of effective strain can be known only after solving the complete plastic problem, i.e. after running the real FEM simulation. However, a rough estimate of the effective strain at the tube edges when feeding values are small can be assumed as the half of equation 8:

$$\bar{e} = \frac{1}{\sqrt{3}} \ln \left(\frac{L_g - d^{self}}{L_g - d^{sealing}} \right) \quad (II-9)$$

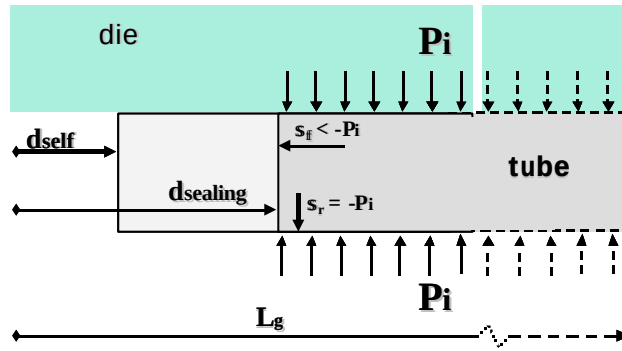
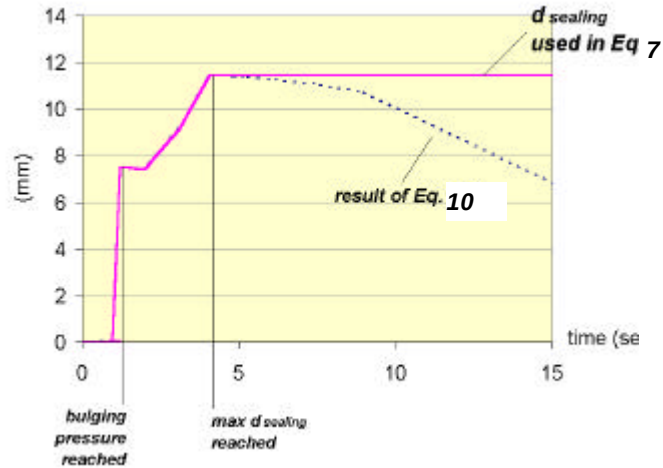


Figure II- 6: schematic for calculation of $d_{sealing}$

The radial stress can be estimated as $\sigma_r = -P_i$. The condition on the axial stress must be $\sigma_z < -P_i$. The effective stress is therefore $\bar{\sigma} = P_i$. If combining this value with the flow stress law $\bar{\sigma} = K(e_0 + \bar{e})^n$ and with equation II-9, the minimum feeding value that prevents leaking can be calculated as:

$$d_{sealing}(t) = L_g + e^{\sqrt{3} \left(\frac{P_i(t)}{K} \right)^{1/n} - e_0} (d^{self}(t) - L_g) \quad (II-10)$$

Equation 10 is valid only for P_i greater than the minimum bulging pressure, i.e. only when the tube is already in contact with the die in the guiding zone. When the bulging pressure has not been reached yet $d_{sealing} = d^{self}$. A typical plot for $d_{sealing}$ is shown in Figure II- 7. Since $d_{sealing}$ increases with increasing d^{self} and with decreasing pressure and since both d^{self} and P_i increase with time, $d_{sealing}$ vs. time will have a maximum value. After this maximum is reached, the value of $d_{sealing}$ used in Eq. 7 can be held constant.

Figure II- 7: d_{sealing} vs. time

2.4. Example of Application

In this Section, it will be shown that the self feeding approach can provide a feasible (sub-optimal) solution within a relatively small number of FEM simulations. As an example, consider the experiments LP 2.0, 2.1 and 3 [mat. B, $t_0=2.1$ mm], as described in App. C. The left feed values are shown again in Figure II- 8.

- LP 2.0 is a pure self feeding LP, obtained as described in Section 2.1 (Step 1).
- LP 2.1 has been obtained multiplying the feed in LP 2.0 by a factor $a_{sf}=2$, as described in Section 2.1 (Step 2). The limitation on d_{sealing} explained in Section 2.3 has not been taken into account. The LP is below the leaking limit for $t < 9$ sec (in fact, leaking occurs). The LP path is very close to d_{sealing} for $t > 9$ sec.
- LP3 has been obtained multiplying the feed in LP 2.0 by a factor $a_{sf}=4.3$. The limitation on d_{sealing} explained in Section 2.3 has been taken into account and leaking does not occur.

The results of these three experiments, and of additional FEM simulations (LP 5, LP 6, LP 7) are given in Table II- 1. In FEM simulations, tubes are considered fractured if $th_{\text{max}} > 30$ %. As the feeding parameter a_{sf} increases, the part evolves from fractured (LP 2.0, 2.1) to unwrinkled (LP 3, 7) and finally wrinkled (LP 6, 5).

Table II- 1: Results of experiments on self feeding.
Material SS304 type B, $t_0=2.1$ mm.

Feeding factor a_{sf}	LP #	Observations	Result of FEM simulation
1	2.0	leaking, part not completely formed	Fractured
2	2.1	bursting at the welding line without excessive thinning (13 %)	Fractured
4.3	3	good part, maximum thinning $th_{\%}=35$ %	sound part, maximum thinning $th_{\%}=30$ %
4.7	7	No experiment	sound part, maximum thinning $th_{\%}=26$ %
5.2	6	No experiment	Wrinkled part, (see Fig. 5-6)
6	5	No experiment	Wrinkled part

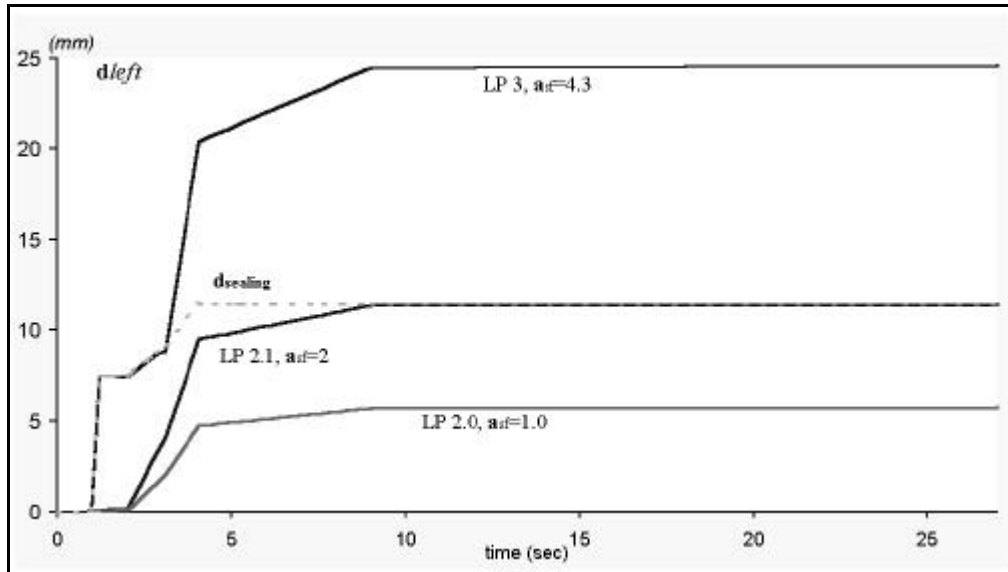


Figure II- 8: Calculated left feed d_{left} vs. time curves for [mat. B, $t_0=2.1$ mm]. Trends for d_{right} curves are similar. Actual experimental LPs are given in Fig. 2-4.

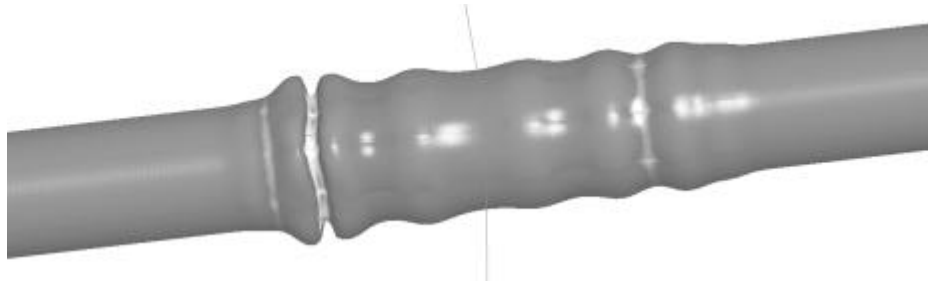


Figure II- 9: FEM shaded plot of a part expanding in a wrinkled mode.
LP 6 [mat. B, $t_0=2.1$ mm].
Different tones of gray indicate different curvatures: brighter areas indicate small radii.

Part II, Chapter 3

The Adaptive Simulation (AS) Approach

3.1. Introduction

The *adaptive simulation* approach is based on the ability to detect/identify the onset and growth of defects during the process and to promptly react (see simplified scheme in Figure II- 3). Loading paths can therefore be adjusted, within the same simulation run, to correct those defects. The ultimate goal is the selection of a feasible part program with a minimum number of simulations, or even within a single run, whereas “feasible” means an LP that produces a part free of defects (wrinkling, leaking, bursting).

The general strategy of the proposed method is to maximize axial feed and minimize pressure, while preventing the insurgence of irreversible wrinkles³. This approach generates very low or negative values of the stress ratio $\beta = \sigma_z / \sigma_\theta$ (σ_θ hoop stress, σ_z axial stress), which has been proved to be beneficial for both closed and open die hydroforming, [Manabe et al., 1984] [Asnafi, 2000]. The stress ratio β should therefore be kept as small as possible, but a lower limit is unfortunately given by the onset of wrinkling. It seems therefore that an optimal loading path should push the process a little above the edge of wrinkling, let a limited number of recoverable wrinkles grow, and finally flatten them out during the calibration phase. One crucial aspect of the whole procedure is therefore the availability of a reliable wrinkle indicator.

The proposed methodology is based upon the interaction between the simulation and three modules, described as follows (Figure II- 10):

MOD.1. After each simulation interval, the results of the simulation are checked; wrinkles are detected and measured, according to some wrinkle indicator (W), and the risk of fracture is evaluated. The module is described in Section 3.3

MOD.2. Internal pressure (P_i) and axial feed (f) for the next control interval are selected according to a set of rules and equations. The determination of P_i and f is strongly influenced by the value of the wrinkle indicator used in MOD.1. Most of the rules and equations change as the hydroforming process evolves. In fact, the proposed method divides the THF process in three time phases: the ‘free bulging’ phase, the following ‘contact expansion’ phase and the final ‘calibration’ phase. The module is described in Section 3.4.

MOD.3. The time length of the next interval is determined. The module is described in Section 3.5.

The wrinkle indicator is one of the most critical issues of the whole procedure. For this reason, an entire Section (3.2) of the present dissertation is devoted to wrinkle indications. Wrinkle indicators have to be robust with respect of any part geometry, able to detect small and large wrinkles, and computationally inexpensive.

³ FEM software can predict with reasonable accuracy the onset and growth of wrinkles in THF (see Figure II- 20).

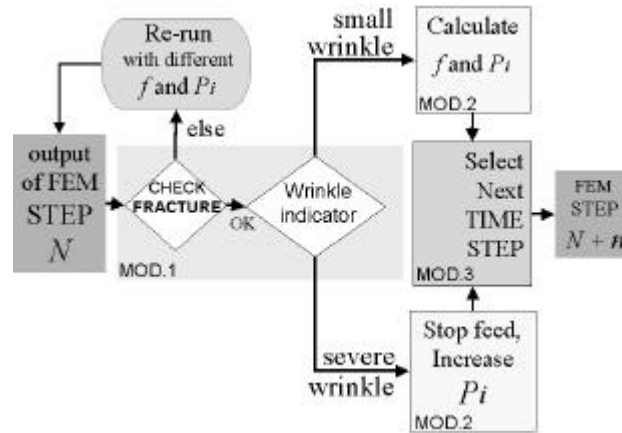


Figure II- 10: flow chart of the adaptive simulation approach

3.2. Wrinkle detection and indication in AS

The literature on the mechanics of wrinkling is very extensive. In order to describe better each possible wrinkle indicator, it is helpful to give an overview on the main scientific publications in this field.

3.2.1 Energy/Stress Approaches

The analysis of onset and growth of wrinkles in sheet metals, in almost any of the papers reviewed, is accomplished with the aid of some energy approach. In this section, a brief description of the main energy-based methods and of the potential wrinkle indicators deriving from each method is given.

PLASTIC BIFURCATION THEORY

The most used (and probably the most accurate) theory for the prediction of the onset of wrinkles in sheet metals is the plastic bifurcation theory, based on the work of [Hill; 1958] and [Hutchinson; 1974]. The underlying idea is that, for unperturbed (perfect) shell structures, wrinkling may start when the solution to the energy equation describing the mechanical problem is not unique. After this bifurcation point, wrinkles may appear or the unwrinkled state may hold until another bifurcation point. Several papers deal with this approach [Triantafyllidis et al.; 1980], [Tomita et al.; 1988], [Neale et al.; 1990], [Durban et al.; 1999], [Kim et al.; 2000] which in most cases brings to the resolution of an eigenvalue problem in order to find the wrinkle length that determines the bifurcation point. Some of the studies in literature use this theory in combination with FEM software.

Solving the bifurcation problem may be useful in giving, for a each specific THF process, the state of stress that can initiate wrinkling. If the locus of instability is plotted in terms of principal stresses, as in Figure II- 11, a wrinkle indicator may be built on the distance between the actual state of stress and the critical stress state, calculated for each element. Therefore, this wrinkle indicator (W_h) would be a local indicator. The problem of this *wrinkling limit diagram* (WLD) is that, unlike FLD, it cannot be used universally, regardless of the specific forming process, but it must be traced *ad hoc*, as proved in [Szacinski et al.; 1991].

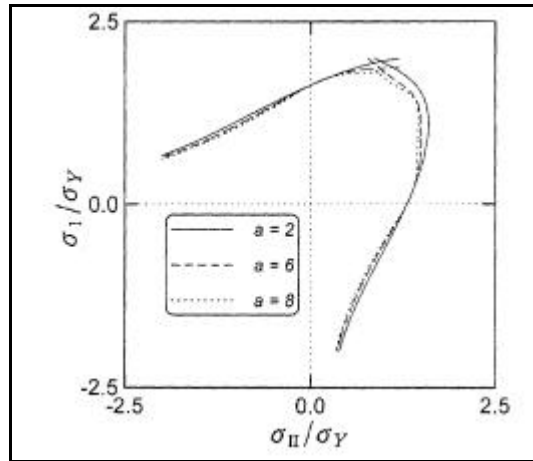


Figure II- 11: Critical stress states [Kim et al.; 2000], a is the exponent of the Hosford's anisotropic yield criterion

Another drawback of the plastic bifurcation theory is that it only deals with initially unperturbed structures, when wrinkling may potentially appear with any wrinkle frequency (or length). Indeed, during hydroforming operations, wrinkling may appear at any stage of the process, when the combined effect of the internal pressure and of the die geometry have already altered the tube geometry and stress/strain distribution, thus allowing for favorable modes of wrinkling.

A solution to this problem can be found in [Nordlund et al.; 1997], where a wrinkle indicator (W_n) is built, for each finite element, based on the plastic bifurcation theory. A wrinkle is detected when W_n becomes negative, see Figure II- 12. The main advantages of this approach are:

- no assumption is made a priori on the shape and frequency of wrinkles;
- it is independent on the material behavior model;
- it is not limited to detection of wrinkling onset for unperturbed shells;
- it is not necessary to solve the eigenvalue problem associated with the bifurcation theory.

The approach has been widely tested in both explicit and implicit FEM codes [Nordlund; 1998] and also applied to hydroforming of non-tubular metal sheets. It seems to be very effective in the early detection of wrinkles and the only drawback is that it fails when large rigid-body rotations occur or when dealing with low frequency (large-scale) wrinkles.

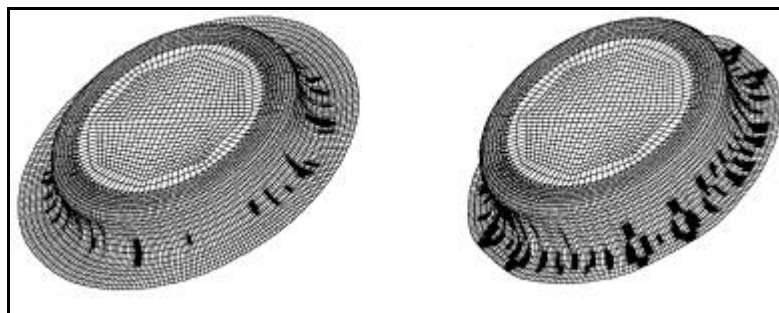


Figure II- 12: The regions where the wrinkle indicator I_n is negative in two different cup hydroforming processes [Nordlund et al.; 1997].

ENERGY BALANCE METHOD

Another way to avoid the eigenvalue problem associated with the plastic bifurcation theory is a method based on an energy balance. The underlying idea is: the energy required for the unwrinkled deformation mode is greater than the corresponding energy associated with the buckled deformation mode.

For instance in [Yossifon et al.; 1984], the loading paths of the hydroforming of a cup are determined in order to respect the following inequality:

$$\Delta T \leq u^b + u^p,$$

where DT is the work done by the compressive in-plane membrane stresses, Du^b is the bending energy of the buckled plate and Du^p is the work against the fluid pressure.

The wrinkled and unwrinkled states for different wrinkle lengths are calculated and a locus of plastic instability may be plotted in terms of the loading paths (pressure and punch travel), as seen in Figure II- 13. In this case, the wrinkle indicator (W_y) may be the distance between the critical path and the actual path during the hydroforming simulation.

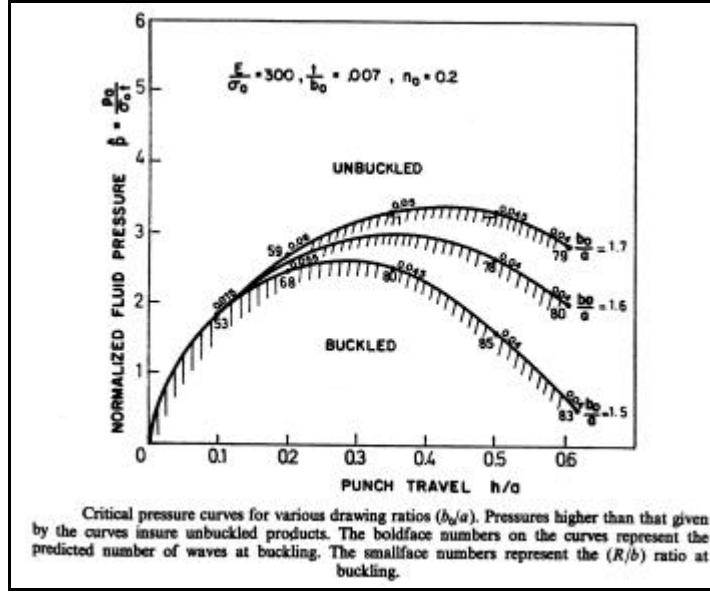


Figure II- 13: Critical loading paths [Yossifon et al.; 1984]

A similar approach has been used in combination with an FEM method [Cao et al.; '97], but it seems that several simulations are required in order to determine the locus of wrinkling instability. Thus, a wrinkle indicator (W_y) based on this method may not be very useful if the ultimate goal is to simulate a THF unwrinkled operation in only one simulation run.

A possible solution to this last problem, instead of comparing the wrinkled and unwrinkled states, would be to directly control some energy value associated with the process. In fact, from energy balance and experimental considerations⁴, -during simple axial pushing of tubes with constant axial cylinder speed, the work made by the axial cylinders is oscillatory due to the wrinkle formation phenomenon. Therefore, a potential wrinkle indicator (W_a) would be the variation of the work rate made by the axial pistons. The problem with applying such a wrinkle indicator (W_a) in THF is that the work done by the cylinders is affected also by the friction forces, the feeding speed and by the pressure increase and it would be difficult to separate these effects from the wrinkling effect. Table II- 2 summarizes the four possible indicators discussed in this section.

⁴ See, for instance, the extensive literature on axial crushing of tubes [Kim et al.; '99], [Gupta et al.; 1995]

Table II- 2: Potential wrinkle indicators deriving from the energy approaches

Energy Indicator		Mathematic theory	Location	Related literature	Main drawbacks
W_h	distance between the actual state of stress and the critical stress state	Plastic bifurcation	Local	[Kim et al.; 2000] [Szacinski et al.; 1991] [Tomita et al.; 1988] [Triantafyllidis; 1980] [Neale et al.; 1990] [Durban et al.; 1999]	The locus of plastic instability must be traced for each specific process
W_n	Rate of Internal Power indicator	Plastic bifurcation	Local	[Nordlund et al.; 1997] [Nordlund; 1998]	It fails when large rigid-body rotations occur or when dealing with low frequency (large-scale) wrinkles.
W_y	distance between the critical loading path and the actual loading path	Energy method	Global	[Yossifon S. et al.; 1984] [Cao et al.; 1997]	The locus of plastic instability must be traced for each specific process
W_a	work rate made by the axial pistons	Energy method	Global	[Kim et al.; 1999] [Gupta et al.; 1995]	In THF, the work done by the cylinders may oscillated not only in consequence of wrinkling but also for variations in friction forces, feeding speed and pressure level

3.2.2 The Geometry/Strain Approaches

In tube hydroforming, unlike in sheet forming, small wrinkles can be eliminated by an appropriate increase in the internal pressure and/or a decrease in the axial feed rate during the forming process. Therefore, it is possible to let wrinkles grow up to relatively small dimensions and then to detect them thanks to geometrical considerations, rather than based on energy/stresses. There are several advantages of the geometry-based approach over the energy/stress-based approaches

- It is simpler than an energy-based criterion since no particular assumptions are required in order to detect the instability point.
- In most cases and the mathematical formulation required to build a geometry-based indicator is much simpler.
- A small amount of wrinkling in the axial direction may be even helpful in preventing excessive thinning in the bulging area, since it is a way to accumulate material in the bulging area.

In the literature, two criteria have already been formulated and tested with FEM codes. All of them are local, i.e. they are calculated for single elements/nodes or for local clusters of elements/nodes.

1. The difference in the strains for the upper and lower skins of the shells (W_e), [Doege et al.; 1998]. In order to discriminate among real wrinkles and curvatures due to the contact with the die, the algorithm checks also the normal nodal velocity of potentially wrinkled elements (see Figure II- 14). If the velocity is low, the tube contour is probably following the die and the situation is considered acceptable. If the indicator W_e and the normal velocity are both high, then a wrinkle is detected.
2. The wrinkle's aspect ratio (W_{ar}), [Strano et al.; 2000]. The indicator ($W_{ar} = l/n$) is shown in Figure II- 15. As in [Doege et al.; 1998], in order to assess the severity of the wrinkle, it is necessary to check if the workpiece is in contact with the die, and this is done through the calculation of the distance tool-workpiece.

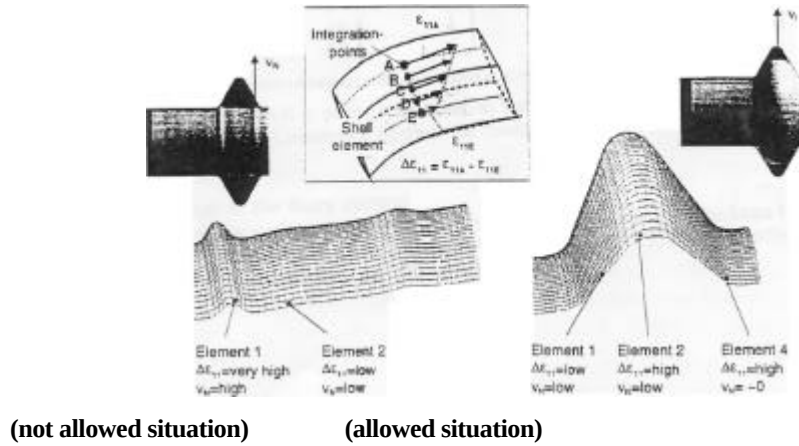


Figure II- 14: W_e , difference in the strains for the upper and lower skins of the shells [Doege et al.; 1998]

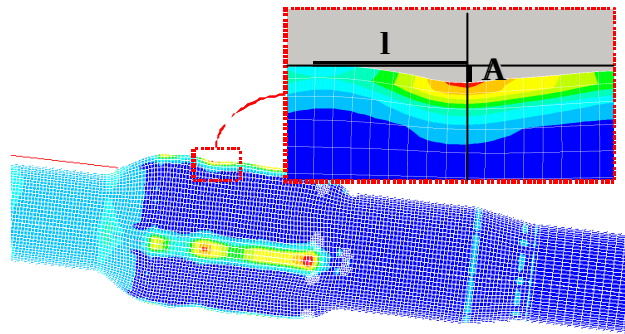


Figure II- 15: Wrinkle aspect ratio $W_{ar} = \lambda/\nu$ [Strano et al.; 2000]

In the following section a new simple geometry-based wrinkle indicator is presented, which appears to be computationally inexpensive, suitable for many part geometries, sensitive to small and large frequency wrinkles and independent on the mathematical formulation of the FEM code.

3.3. *Mod.1 in the AS algorithm: the ratio (tube surface)/(fluid volume)*

The ratio between the surface area of the tube (S_t) and the volume of fluid contained inside the tube (V_{fc}) could be used as a wrinkle indicator (Figure II- 16).

The evolution of S_t as the fluid cell volume changes is not known a priori. However, for a given THF process and for a given value of V_{fc} the value of S_t is larger for a wrinkled tube than for an unwrinkled tube. Moreover, if comparing different unwrinkled deformation histories (i.e. different loading paths), similar values of S_t/V_{fc} will be found.

The following will explain which factors, including wrinkling, influence the ratio S_t/V_{fc} and what assumptions are required for the validity of the model.

The main assumption underlying the model is that the tube surface is a monotonously increasing function of the fluid volume $S_t = S_t(V_{fc})$ and that the volume is a monotonously increasing function of the maximum bulge height $V_{fc} = V_{fc}(BH)$. These two assumptions can be considered correct only if S_t and V_{fc} are calculated in a fixed bounding box, centered on the bulge area, as in Figure II- 16. In fact, for many THF parts (as for T-shapes and Y-shapes) the total value of V_{fc} , if calculated including the guiding zones, can even decrease as the bulge grows.

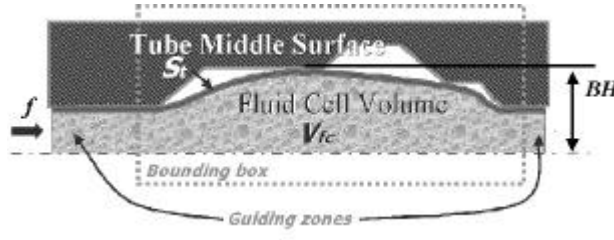


Figure II- 16: example of bounding box used for the calculation of V_{fc} and of S_t

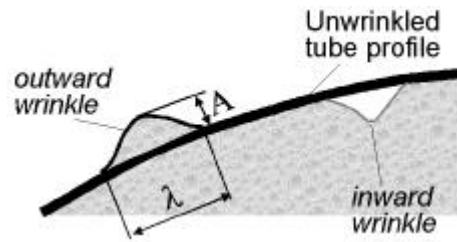


Figure II- 17: tube profile with inward and outward wrinkles

THE EFFECT OF WRINKLES ON S_t/V_{fc}

When comparing a wrinkled tube expansion mode with an unwrinkled mode, the S_t/V_{fc} ratio tends to be higher in the wrinkled case. Wrinkles can be inward, outward or a combination of both (see Figure II- 17).

Inward wrinkles reduce the fluid volume and increase the surface, thus they increase the ratio S_t/V_{fc} . Inward wrinkles are the most critical in THF, since they are more difficult to suppress by increasing pressure P_i or decreasing the feed rate. Moreover, as the tube approaches the calibration phase, i.e. expands against the die, all unsuppressed wrinkles necessarily become inward. The contribution of outward wrinkles to the overall S_t/V_{fc} depends on their aspect ratio (λ/A). If λ/A is small, i.e. the outward wrinkle is sharp and critical, S_t/V_{fc} will increase. If λ/A is large, the wrinkle is easy to suppress and technologically insignificant. In this case S_t/V_{fc} will remain approximately constant or slightly decrease.

THE EFFECT OF DEFORMATION HISTORY ON S_t/V_{fc}

When considering free tube bulging with axial feed, as the bulge height BH grows, the shape of the tube obviously changes too. For a given BH , the actual shape depends on the ratio between the hoop and the axial stresses $\beta = \sigma_\phi / \sigma_\theta$, which is dependent on the loading paths: pressure P_i and axial feed f vs. time. The effect of the stress ratio on the shape of the tube has been proven both experimentally and theoretically [Manabe et al., 1984], [Tirosh et al. 1996. IJMS].

THE EFFECT OF THICKNESS DURING CALIBRATION

A more important role in determining the value S_t/V_{fc} is played by the tensile thickness strain ϵ_t , not during free bulging, but during the calibration phase, i.e. when large portions of the tube are in contact with the die.

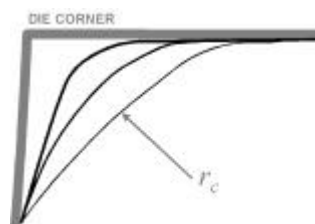


Figure II- 18: schematic of calibration

It is well known that, for a given tube material and internal pressure P_i , the minimum achievable radius during calibration r_c is proportional to the tube thickness. In other words, when the thickness is smaller, i.e. when ε_r is larger, the tube is more easily coined to the die shape and the internal radii decrease. As the internal radii decrease, the ratio S/V_{fc} increases, as Figure II- 18 qualitatively shows. The thickness strain can be globally expressed as $\varepsilon_r = (\varepsilon_{rmax} + \varepsilon_{rmin})/2$, where ε_{rmax} and ε_{rmin} are the minimum and maximum tensile thickness strains in the bounding box, respectively (they are usually both positive).

Summarizing,

- when wrinkling occurs, the ratio S_t/V_{fc} increases;
- when thinning occurs, the ratio S_t/V_{fc} increases too.

FEM simulations show that the effect of the thinning distribution is a lot less important than the effect of wrinkling, and it can be observed only when large portions of the tube are in contact with the die. However, if S_t/V_{fc} has to be used as a wrinkle indicator, the two effects should be somehow separated. More precisely, the effect of thinning should be dampened. One way of doing this is to multiply the actual value of S_t by an empirical correction factor, inversely proportional to the thickness strain. As an example, the following equation can be used:

$$S = S_t [1 - \varepsilon]^\gamma \quad (II-11)$$

The exponent γ is used to control the amount of the reduction of the surface. In case where $\gamma = 0$, no correction is applied and FEM results show that the indicator is still able to detect inward and severe outward wrinkles. When $\gamma > 0$, the calculated tube surface S_t is reduced when thinning occurs. In the next Section 3.3.1, more considerations will be given on the effect of the exponent γ on the wrinkle indicator.

3.3.1 Formulation of the wrinkle indicator

The evolution of S as the fluid cell volume increases is not known a priori. In order to use it for the detection and indication of wrinkles, it is necessary to track and compare its evolution with a known unwrinkled deformation history.

In most cases, a practical way to obtain an unwrinkled deformation history is to run one ideal FEM analysis under self feeding conditions. This preliminary simulation is run with the following boundary conditions:

- zero friction at the interface die/tube;
- no essential or natural boundary conditions on the tube edges;
- P_i is linearly increasing.

The self feeding simulation is not supposed to be realistic or accurate, but it is used only as a reference for further calculations. During the self feeding simulation, the value of the fluid cell volume can be recorded as a function of the time t ($V_{fc}^{self}[t]$), while the surface can be traced as a function of the volume: $S^{self}[V_{fc}]$.

These reference values are then used, during the real simulation, to transform S and V_{fc} into non-dimensional control variables, using equations (12) and (13) where T_0 and T_f are the initial and final simulation times.

$$V[t] = \frac{V_{fc}[t] - V_{fc}^{self}[T_0]}{V_{fc}^{self}[T_f] - V_{fc}^{self}[T_0]} \quad (II-12)$$

$$W[V] = \left(\frac{S[V]}{S^{self}[V]} - 1 \right) \cdot 100 \quad (II-13)$$

The variable W is the final wrinkle indicator. W can be plotted as a function of the non-dimensional fluid cell volume V . The greater the value of W , the higher the probability that wrinkling is occurring during the real FEM run.

The accuracy of the calculation of $W[V]$ is dependent on some strictly numerical parameters. For this reason, the final simulation should be carried out with numerical parameters similar (or equal) to those used in the self feeding run. The most important precautions to be taken are listed as follows.

- The initial mesh and the mesh adaptivity factors should be equal, since the mesh size affects the calculation of S and V.
- Since the simulation of THF processes is usually carried out with explicit codes, the range of axial feed rates used should be similar and the simulated process time should be similar or equal.

3.3.2 Application of the wrinkle indicator

The indicator W can be embedded in an adaptive simulation routine. However, in order to evaluate its efficacy in the detection of wrinkles, the indicator W has been tested in several ordinary simulation runs, designed to generate different wrinkling modes in the workpiece.

Indeed, the indicator has been tested with three different die shapes and two different commercial FEM codes (LS-DYNA/DYNAFORM and PAM-STAMP). Both codes use an explicit time integration scheme.

As an example, some of the experiments described in App. C have initially been used as a test for the wrinkle indicator.

Three different THF operations will be described, referred to as cases A, B and C. Case A is an unwrinkled hydroforming process (LP1.2, material A). The loading paths used in case B produced a slightly wrinkled final shape (LP7, material A). The part used in Case C is severely wrinkled (LP2, material A). The three loading paths are given in App. C and again in Figure II- 19, along with the pure SF loading path, obtained as described in Chapter 2.

In Figure II- 21, the value of W, equation (13), is plotted as a function of V, equation (12), for the three mentioned THF operations; the value of the correction exponent γ in equation (11) is 0.05. When $\gamma=0$, W is not able to discriminate between case A and case B during calibration, i.e. is not able to distinguish between the effect of small wrinkles and the effect of calibration. As γ increases, the indicator W becomes increasingly more sensitive to wrinkling. $\gamma=0.05$ has been empirically selected and used throughout the whole study.

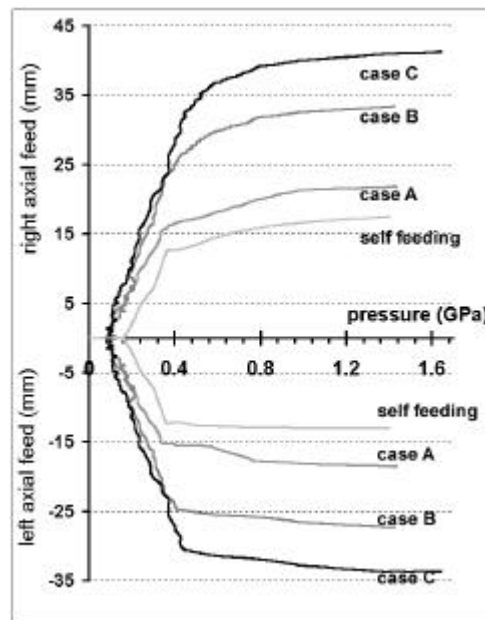


Figure II- 19: loading paths used for both experiments (cases A, B and C) and simulations (cases A, B, C and self feeding)

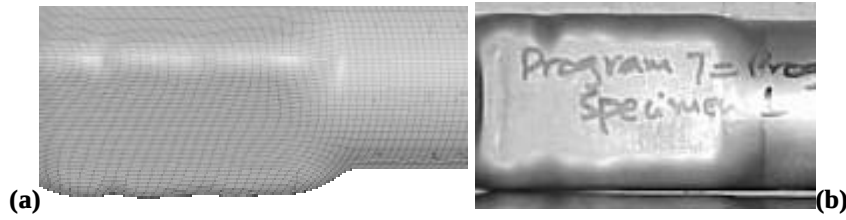


Figure II- 20: (a) detail of an FEM simulation plot reproducing the small wrinkling seen in (b). LP7 (see App. C)

- The part in Case A is completely free of wrinkles throughout the whole simulation. If the indicator W was dependent only on wrinkling and completely independent on the loading paths, its value in this case would be constant and equal to 0. Indeed, the measured value fluctuates between -0.03 and $+0.17$.
- In cases B and C, wrinkles develop as soon as the non-dimensional volume V reaches the value of about 0.35. Then, they increase in size and finally decrease to the final shapes, during the calibration phase. When the first small wrinkles become visually recognizable in the post processor, the value of W is in both cases above 0.2.

As a consequence of these results, it appears that a critical value W_{cr} can be identified, able to discriminate between a wrinkled and an unwrinkled region (Figure II- 21). For the set of experiments here presented, $W_{cr}=0.2$.

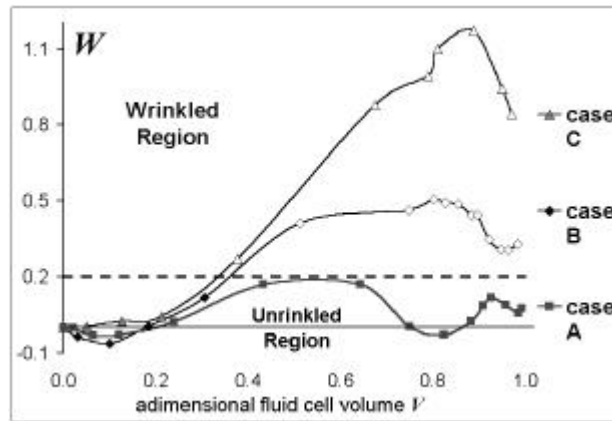


Figure II- 21: wrinkle indicator W as a function of V for cases A, B, C. Solid points indicate absence of wrinkles;

In order to evaluate the generality of the proposed indicator and the range of variation of the indicator W and of the critical value W_{cr} , additional experiments and simulations have been used, listed as follows.

- Hydroforming of a stainless steel Y-shape (see Figure II- 22), using the software PAM-STAMP.
- Hydroforming of a 90° bent low carbon steel tube (see Chapter I-3), using the software LS-DYNA/DYNAFORM.

In all the examples tested, the value of W increases as wrinkles grow bigger, and can therefore be used as a wrinkle indicator in adaptive simulation. For each experimental set, a proper value of W_{cr} can be identified, when using values of $\gamma \geq 0.04$.

These tests show that W_{cr} depends mainly on the die geometry. However it also increases as the tube initial thickness and the thickness correction exponent γ increase and as the dimensions of the bounding box decrease. In other words, most factors mentioned previously influence the determination of W_{cr} . Throughout all the examples evaluated, the value of W_{cr} always fell between 0.1 and 0.4.

However, even if a correct value for W_{cr} cannot be identified a priori, the indicator W can still be used in an adaptive algorithm based on wrinkle control, since it is undoubtedly strictly related to the wrinkling phenomenon.

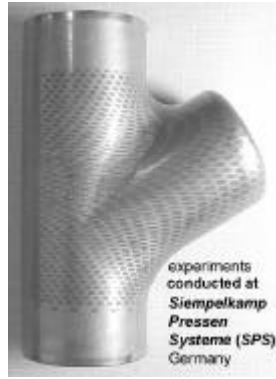


Figure II- 22: Y-shape

3.4. Mod.2 in the AS algorithm: adaptive control of pressure and feed

In the present study an approach will be used, based on control theory. The process is divided in two phases: bulging, calibration. One unique pressure increment is calculated for the calibration phase (and also 1 small feed increment). In order to calculate feeding increments Δd and pressure ΔP increments before calibration, a simple 1-step ahead self-tuning control system is proposed, based on a LQ (linear dynamics, quadratic costs) optimization model. The idea of the algorithm is to manipulate feed increments so as to:

- keep the wrinkle indicator W at a constant target value W_T (e.g. $W_T=0.1$), which is known to prevent the insurgence of wrinkles; if this value is not available $W_T=0$ can be chosen;
- maximize feed increments Δd , i.e. select Δd so to be as close as possible to a maximum predetermined increment value $\Delta d_{\max}$;
- minimize pressure increments ΔP .

The rationale of the proposed method is that by maximizing feed and minimizing pressure increments, the amount of thinning per increment should be accordingly minimized.

Initially, boundary values for both pressure and feed must be calculated.

BOUNDARY VALUES FOR PRESSURE

First, the minimum bulging pressure $P_{\min}$ and the maximum calibration pressure $P_{\max}$ are obtained through a preliminary self feeding simulation run. If #c.i. is the target number of control intervals, the maximum pressure increment per interval can be calculated as:

$$\Delta P_{\max} = \left(\frac{P_{\max} - P_{\min}}{\text{\#c.i.}} \right) \quad (\text{II-14})$$

BOUNDARY VALUES FOR FEED

Given the initial and final tube spline length L_i and L_f , the maximum total amount of axial feed must be slightly less than $(L_i - L_f)$, i.e. $0.95 \cdot (L_i - L_f)$. Therefore, the maximum feed per interval in the pre-calibration phase is:

$$\Delta d_{\max} = 0.95 \left(\frac{L_i - L_f}{\text{\#c.i.}} \right) \quad (\text{II-15})$$

One unique pressure increment is calculated for the calibration phase (and also 1 small feed increment). It is worth noting that in MOD.1, the algorithm had already decided if the next interval belongs to the bulging or calibration phase.

CALCULATION OF PRESSURE AND FEED INCREMENTS BEFORE CALIBRATION

In order to calculate feeding and pressure increments before calibration, a 1-step ahead self-tuning control system is proposed [Mosca, 1995], based on a LQ (linear dynamics, quadratic costs) optimization model [Whittle, 1982]. The general problem can be stated as follows. The AS method is designed to select the values of a vector of control variables $\underline{u}(t)$, by detecting and controlling the values of a vector of state variables $\underline{x}(t)$. A linear dynamic model (Eq. 16) is necessary to estimate the relation between process parameters $\underline{u}(t)$ and state variables $\underline{x}(t)$. The coefficients of the transformation matrix $\underline{\Gamma}(t)$ are adaptively estimated at each control step as the simulation proceeds (1-step ahead) thanks to a time series analysis. The problem is solved by optimizing the quadratic cost function given in Eq. 17, where the weights of the matrix $\underline{y}(t)$ are increasing with the time t , in order to give higher importance to the state variables as the simulation gets closer to the end of the process. In Eq. 16 and 17, t is the time at the beginning of the control step and Δt is the duration of the control step.

$$\underline{x}(t + \Delta t) = \underline{x}(t) + \underline{\Gamma}(t) \cdot \underline{u}(t) \quad (II-16)$$

$$C(t) = \underline{u}(t)^T \cdot \underline{u}(t) + \underline{x}(t + \Delta t)^T \cdot \underline{y}(t) \cdot \underline{x}(t + \Delta t) \quad (II-17)$$

when adapting this general model to the problem of controlling the simulation of a THF operation with internal pressure and axial feed, the model can be particularized as to:

- choose the wrinkle indicator W as the state variable;
- keep the wrinkle indicator W at a constant target value W_T (e.g. $W_T=0.1$), which is known to prevent the insurgence of wrinkles; if this value is not available $W_T=0$ can be chosen;
- choose pressure and feed increments as control variables;
- minimize pressure increments ΔP .
- maximize feed increments Δd , i.e. select Δd so to be as close as possible to a maximum predetermined increment value $\Delta d_{\max}$;

By maximizing feed and minimizing pressure increments, the amount of thinning per increment should be accordingly minimized. These two goal can be achieved by modeling the simulation as a control system with the following features:

control variables (feed and pressure increments)

$$u_1(t) = \frac{\Delta P}{\Delta P_{\max}} \quad (II-18)$$

$$u_2(t) = \frac{\Delta d}{\Delta d_{\max}} \quad (II-19)$$

state variable (wrinkling)

$$x(t) = W(t) - W_T \quad (II-20)$$

linear dynamic model

$$x(t + \Delta t) = x(t) + a(t) \cdot u_1(t) + b(t) \cdot u_2(t) \quad (II-21)$$

quadratic cost function

$$Cost(t) = u_1^2 + (u_2(t) - 1)^2 + c \cdot V(t) \cdot x^2(t + \Delta t) \quad (II-22)$$

where:

- t is the time at the beginning of the control interval Δt ;
- $\Delta P_{\max}$ [GPa] and $\Delta d_{\max}$ [mm] are the maximum possible increments in pressure and feed per interval;
- $a(t)$, $b(t)$ are coefficients of the model;
- c is a constant weight for the cost function $Cost(t)$;

- $V(t)$ is the nondimensional fluid cell volume; $V=0$ at the beginning of the process and $V=1$ at the end.

At each time step, $u(t)$ is chosen so to optimize the control problem described in equations 18 to 22. This optimization is accomplished only 1-step forward, no attempt is done to accomplish a global optimization. The reason is that the parameters $a(t)$ and $b(t)$ are not known a-priori but they are estimated as the process proceeds.

The estimate $\hat{b}(t)$ for the non constant coefficient $b(t)$ is calculated simply as:

$$\hat{b}(t) = b(t-1) \quad (II-23)$$

During the preliminary self feeding run, by definition $W(t)=0$ and therefore,

$$x(t + \Delta t) - x(t) = a(t) \cdot u_1(t) + b(t) \cdot u_2(t) \equiv 0 \quad (II-24)$$

The ratio between the coefficients $a(t)$ and $b(t)$, during the self feeding run, can be therefore tracked and recorded as a function of the nondimensional fluid cell volume: $R_{ab}(V(t))$:

$$R_{ab}(t) = \frac{a(t)}{b(t)} = -\frac{u_2(t)}{u_1(t)} \quad (II-25)$$

Once $b(t)$ has been estimated, $a(t)$ can be estimated as:

$$\hat{a}(t) = R_{ab}(V(t)) \cdot \hat{b}(t) \quad (II-26)$$

The solution to the problem given by equations 18 to 22 is:

$$\begin{aligned} u_1(t) &= \max \left\{ 0, -a(t) \cdot c \cdot V(t) \frac{b(t) + x(t)}{1 + c \cdot V(t) \cdot [a^2(t) + b^2(t)]} \right\} \\ u_2(t) &= \max \left\{ 0, \frac{1 + c \cdot V(t) \cdot [a^2(t) - b(t)x(t)]}{1 + c \cdot V(t) \cdot [a^2(t) + b^2(t)]} \right\} \end{aligned} \quad (II-27)$$

As already mentioned, c is a cost coefficient, to be fixed by the user according to the risk of wrinkling involved with the process. When $c=0$, the algorithm does not perform any control on wrinkling. Therefore the two values W_T and c indicate the tolerance to wrinkles required from the algorithm.

Finally, the value of Δd determined by the control algorithm is the sum of left and right feed increments:

$$\Delta d = [d_{left}(t+\Delta t) - d_{right}(t+\Delta t)] - [d_{left}(t) - d_{right}(t)] \quad (II-28)$$

After Δd has been selected, $d_{left}(\tau + \Delta \tau)$ and $d_{right}(\tau + \Delta \tau)$ can be calculated using the same proportion as in the initial self feeding simulation run.

For the sake of simplicity, all the formulas given are valid under the assumption of uniform constant values of control interval time Δt . When this is not the case, all formulas can easily be modified accordingly. In fact, the total simulation time required by the AS model to determine a solution can be further reduced by using a variable optimized time control interval Δt (see [Strano, Jirathearant, Altan, 2000]).

Alternatively a constant Δt can be used, but both solution variables $\{u_1(t), u_2(t)\}$ can be multiplied by an equal factor, designed to keep the total simulation time under control. This second approach has been followed in the present report.

CALCULATION OF PRESSURE AND FEED INCREMENTS DURING CALIBRATION

During the calibration phase, the algorithm does not perform check any more check on the results, since it is hardly possible to modify the final outcome of the process, once the calibration phase has been reached. pressure is simply linearly increased to its predetermined maximum value P_{max} and an amount of additional axial feed is calculated basing on the preliminary self feeding simulation, only to prevent leaking.

3.5. Mod.3 in the AS algorithm: determination of time control interval

The purpose of this module is to determine the time length of the next control interval ($\Delta\tau_{next}$).

$\Delta\tau_{next}$ is selected through the minimization of the expected total time (ET_{next}) required for the FE simulation of the next interval, including the pre-processing and the simulation times. In fact, the expected time ET_{next} depends on $\Delta\tau_{next}$, as illustrated in Fig. 6:

- the longer is $\Delta\tau_{next}$, the higher is the risk that an irreversible wrinkle will grow during the next interval, i.e. the probability of re-running the simulation interval;
- on the other hand, the shorter is $\Delta\tau_{next}$, the higher is total number of restarts.

Thus a compromise between these two opposite effects is to be found. More details on this time minimization model are given in Appendix B.

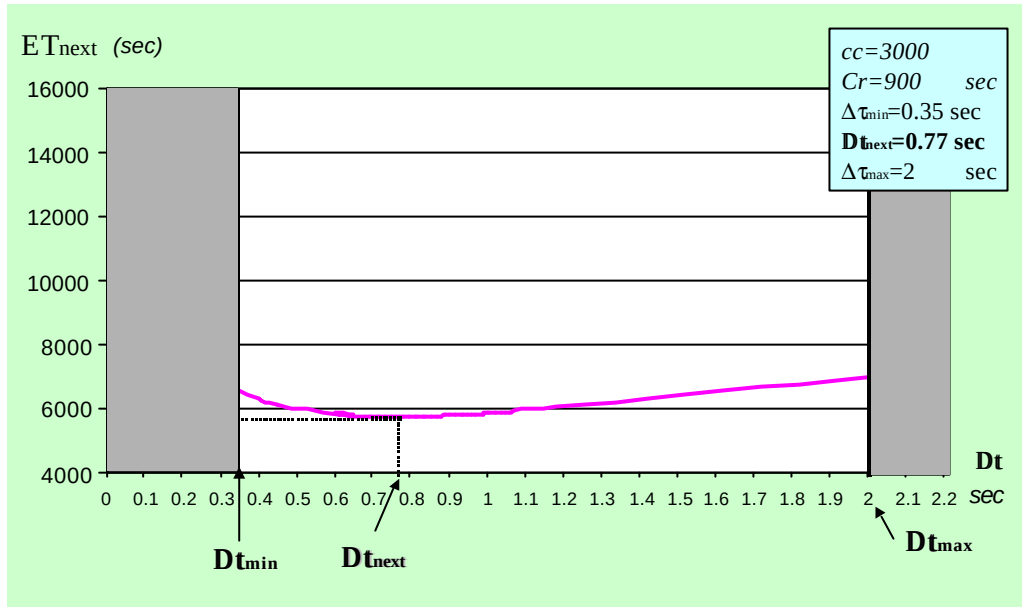


Figure 1: example plot of the function ET_{next} .

- cc_{next} is the computational cost per simulation time unit for the next control interval, which is estimated basing on cpu_{prev} : $cc_{next} = \frac{cpu_{prev}}{\Delta\tau_{prev}}$;
- Dt_{min} is the minimum time required by the system to raise the pressure level by one pressure unit, which is a constant data (Tab. 1);
- C_r the time required by the user at each restart for preparing the next simulation run;
- $\Delta\tau_{max}$ is the estimated time after which an irreversible defect grows.

The output of Mod.3 is the simulated time for the next control interval $\Delta\tau_{next}$. If the control time interval is too long, it can happen that an irreversible defect develops, and the simulation must be restarted from the previous control time with different process parameters. Therefore the real computational time required for each simulation step could be higher than the simulation step itself, depending on the probability that an irreversible defect grows during the time interval. One simple formulations for the local minimization of the total simulation time (solver CPU time + pre/post processing time) is:

$$\min[ET_{next}(\ddot{A}\hat{o})] = \frac{\ddot{A}\hat{o} \cdot cc + C_r}{\ddot{A}\hat{o}} \cdot [1 + P_d(\ddot{A}\hat{o})], (Dt, Dt_{min}) < Dt < Dt_{max}; \text{Equation 1}$$

where:

- C_r is the computational cost (or time) required for each restart: it is of course much higher for the semi-automated adaptive procedure than for the full-automated one and it does not depend on $\Delta\tau$;
- P_d is the probability that an irreversible defect grows during $\Delta\tau$.
- Δt is the simulation time step used by the FE code.
- $\Delta\tau_{\max}$ is the estimated time after which an irreversible defect grows, i.e. $P_d(\Delta\tau_{\max})=1$. $\Delta\tau_{\max}$ is higher for lower values of f_{next} and is calculated in different ways, when switching from the free bulging to the contact expansion phases.

The main problem of this formulation is to find out a proper and reliable probability function $P_d(\Delta\tau)$. One effective form for $P_d(\Delta\tau)$ is given in eq. 13.

$$P_d(\ddot{A}\hat{o}) = \frac{\ddot{A}\hat{o}}{\ddot{A}\hat{o}_{\max}} * 100, \text{ Equation 2}$$

The probability is a non-decreasing function of the control interval $\Delta\tau$, and Eq. 1 then becomes a non-linear function of $\Delta\tau$ with a minimum (fig. 6), which is the selected value of $\Delta\tau_{\text{next}}$.

Equation 1 is only one of several possible minimization functions, and it can be changed or improved in order to take in account other technological or economical issues. For instance, a correction factor can be included in the minimization function in order to increase the control frequency when the adaptive control algorithm generates low values of feed rate.

3.6. Example of Application of the AS algorithm

The algorithm, as described in sections 3.3 and 3.4 has been used on part, whose experimental plan is described in App. C. The resulting LP (before calibration) is shown in Figure II- 23, [mat. A - $t_0=1.5$ mm]. The maximum calibration pressure is 0.14 GPa. The maximum thinning (22 %) of the final part is below the fracture limit (23 %). The part is free of wrinkles at the end of the process, although wrinkles develop during the simulation. Figure II- 24 shows the value of the wrinkle indicator W vs. time.

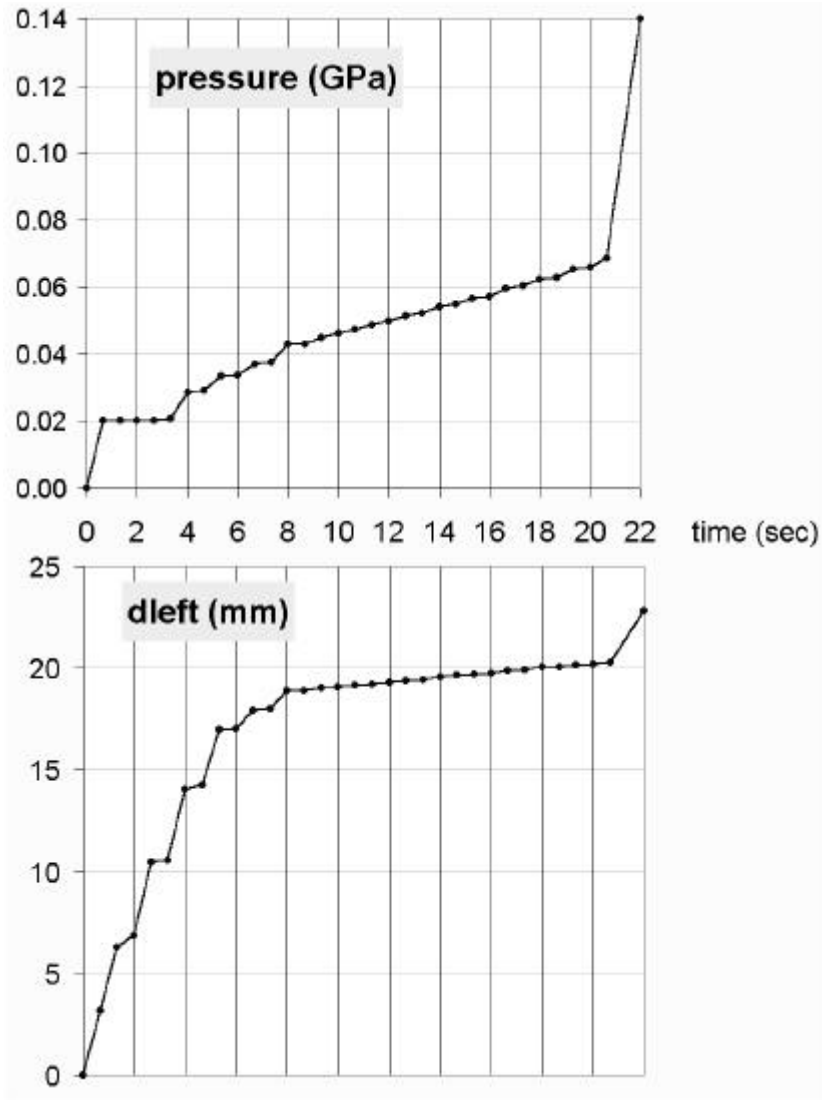


Figure II- 23: LP generated by the AS algorithm [mat. A, $t_0=1.5$ mm]. The last segment of the two curves is the final calibration phase.
Only d_{left} is given, since d_{right} shows a similar trend.

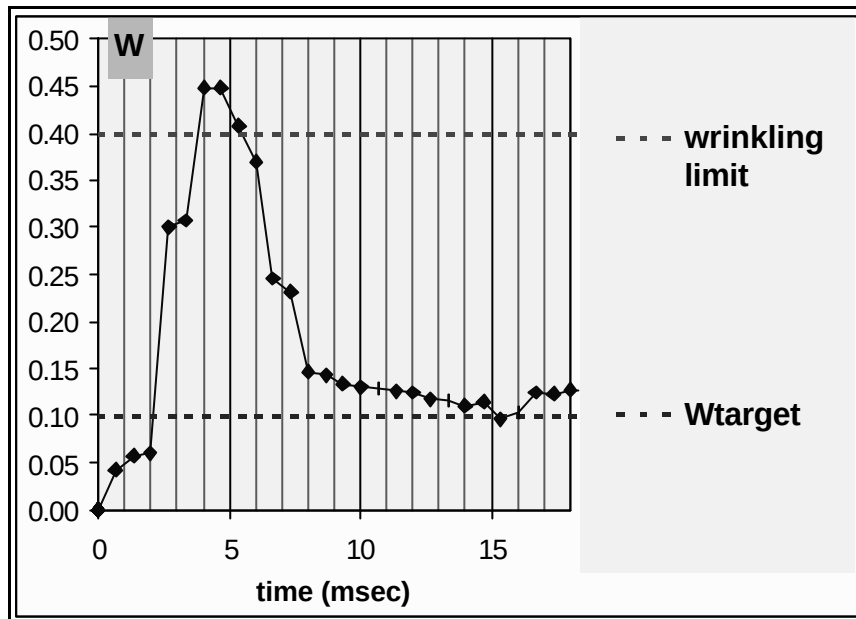


Figure II- 24: Wrinkle indicator W vs. time as controlled by the AS routine, before calibration.



Figure II- 25: geometry obtained by adaptive FEM simulation of the LP given in Figure II- 23.
Different tones of gray indicate different thickness values.

Part II, Chapter 4

Post processing the Loading Paths

There are several reasons why the loading paths (LP) need to be refined, after they have been generated through any FEA strategy, such as Adaptive Simulation (AS) or Self Feeding (SF). In the following Sections, the main reasons will be listed and appropriate corrective actions will be suggested.

4.1. Smoothing of loading paths

The hydraulic control of hydroforming presses cannot adjust to sudden drastic changes in the feed rate or in the pressurization rates. In other words, in actual THF systems, for control reasons, there is always a difference between the input and the actual measured loading paths (see, as an example Figure II- 26). When this difference is large, the effect of the actual LP on the process may be unpredictable and too much dependent on the control behavior of the hydroforming press. In order to reduce this discrepancy, the input LP vs. time ($P_i(t)$ or $d_{ax}(t)$) should be designed with minimum or no discontinuities in their first derivatives $\dot{P}_i(t)$, $\dot{d}_{ax}(t)$.

Unfortunately, due to the nature of stepwise path correction in the Adaptive Simulation, output loading paths (especially the feed rate $\dot{d}_{ax}(t)$) may have discontinuous slope, as shown in Figure II- 27, where a generic LP (it could be either pressure or feed) is plotted vs. time. The difference in the values of $\dot{P}_i(t)$ or $\dot{d}_{ax}(t)$ among adjacent time intervals can sometimes be very large. For these reasons, especially when the control time step used in the adaptive algorithm is small, the output LP should be smoothed out, e.g. by B-spline interpolation. In cases where the distance between the FEM curve and the smoothed curve is small, the hydroforming press can directly use the modified LP.

When the difference between the smoothed LP and the FEM LP is considered to be too large, another correction measure should be taken, as described in the next section.

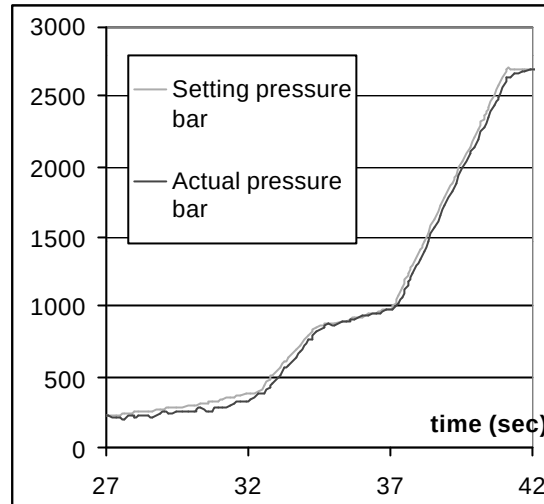


Figure II- 26: Example of setting and actual pressure curves for the Kawasaki Hydromechanics hydroforming press. Max available pressure is 3000 bar.

4.2. Time rescaling of loading paths

TIME RESCALING DUE TO SMOOTHING

In some cases, after the FEM LPs have been smoothed out, locally the distance between the original and the modified LPs can be very large. As an example, in Figure II- 27, the original and the smoothed loading paths somewhat differ for $\{5 \text{ sec} < t < 7 \text{ sec}\}$, due to a sudden change in slope at time 5.5 sec.

In most practical THF applications, the material is strain rate independent and inertia effects can be neglected. Therefore, the real process time can be extended or shortened, without significantly affecting the predictions of the FEM simulation. It is therefore possible to “stretch” the time span where local stability problems are identifiable in the LPs. Obviously, if the time axis is stretched or deformed due to one LP (e.g., pressure vs. time), the same modification must be applied to the time axis of the other LPs (e.g., left and right feed vs. time).

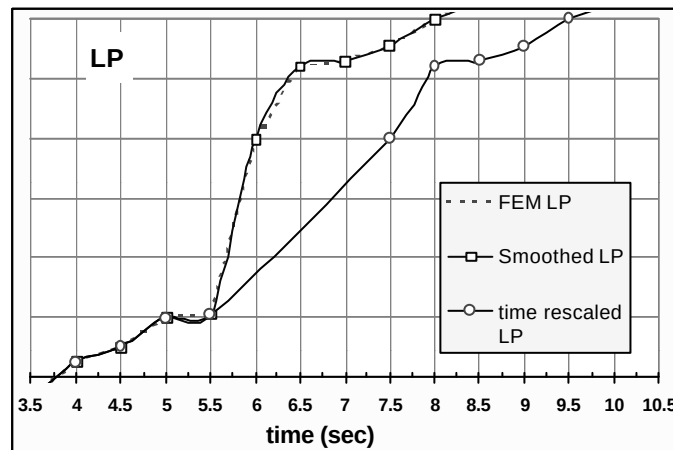


Figure II- 27: Hypothetical LP generated by an adaptive simulation routine with control interval =0.5 s. The original FEM and the smoothed LPs differ for $\{5 \text{ sec} < t < 7 \text{ sec}\}$. The time rescaled LPs is shown, too.

In the example of Figure II- 27, the time interval $\{5.5 \text{ sec} < t < 6 \text{ sec}\}$ could be “stretched” by 1.5 seconds, thus transforming the LP (plotted with square dots) into the smoothed and time stretched LP (plotted with round

dots). The relative position along abscissa and ordinate of all other points would remain unchanged, therefore the total process time would be increased by 1.5 sec. However, this is not a general solution, since the actions needed to rescale the time axis depend on the specific LP.

In some cases, even if the adaptive and smoothed LP are similar, it might be necessary or convenient to rescale the time axis, due to other reasons, explained as follows.

TIME RESCALING DUE TO THE MAXIMUM AVAILABLE POWER AT THE INTENSIFIER

The pressure intensifier provides a maximum performance in terms of available pressure (P_i), water flow rate (Γ) and available power (Γ times P_i). If, during any given time control interval Δt (or a number of consecutive control intervals), the maximum value of power is exceeded, time rescaling should be applied. The length of the interval Δt (or the range of intervals) is increased as previously explained. However, during the AS, a limit for the pressure increase $\Delta P / \Delta t$ is fixed in advance and this reduces the possibility of exceeding the intensifier capability.

TIME RESCALING DUE TO THE MAXIMUM AVAILABLE POWER AT THE AXIAL CYLINDERS

The power required at the cylinders is equal to $F_{ax} \dot{d}_{ax}(t)$ (axial force times feed rate). This value can 'theoretically' exceed the maximum available power. In practice this hardly happens, for two reasons:

1. in the AS and SF approaches, maximum values for $\dot{d}_{ax}(t)$ are determined in advance;
2. when $F_{ax} \dot{d}_{ax}(t)$ grows, usually buckling or wrinkling occur before the maximum value is reached.

However, if during any given time control interval Δt_i (or a number of consecutive control intervals), the product $F_{ax} \dot{d}_{ax}(t)$ exceeds the maximum available power, Δt_i should be "stretched". This would reduce the $\dot{d}_{ax}(t)$ value and therefore the power requirement on a given axial feeding cylinder.

4.3. Checking for leaking

Another reason for refining the adaptive LP is due to the effect of the axial force on the cylinders. In fact, both Adaptive Simulation (AS) and Self Feeding (SF) routines are based on determination of feeding position. Some precautions are taken in both approaches to sealing problems. In fact, the adaptive control algorithm described in Chapter 6, is based on the concurrent maximization of feed rate and minimization of pressure. It can therefore be expected that the axial feed values provided by the adaptive simulation are already very high, and do not need adjustments due to leaking. When using the SF approach, the risk of leaking can be reduced by a proper selection of the curve $d_{sealing}$.

However, in both AS and SF no direct checks are done on the axial force values during the simulation. If the axial force F_{ax} is too small, the risk of leaking increases. Therefore, the value of σ_z on the tube edges should be tracked, after the simulation (either adaptive or self feeding based). If σ_z on the tube edges is positive (tensile) and does not respect the condition given in Chapter 2, during any given time control interval Δt_i (or a number of consecutive control intervals), the axial feed for that interval $\dot{d}_{ax}(\Delta t_i)$ (or that range of intervals) should be increased. In other words, from that interval on, the feed vs. time curve should be shifted upwards.

If the leaking condition requires too many or very large adjustments on the loading paths, the simulation should be re-run starting from the first control interval where leaking was observed.

4.4. Example of Application

As an example, the LP generated through the AS algorithm (Chapter 3), would change as shown in the following Figure II- 28, after the adjustments suggested in the present Chapter. However, both LPs have the same value of feed at the same value of pressure. In other words, the plot of axial feed vs. internal pressure is the same for both original adaptive and refined sets of curves (as in Figure II- 29).

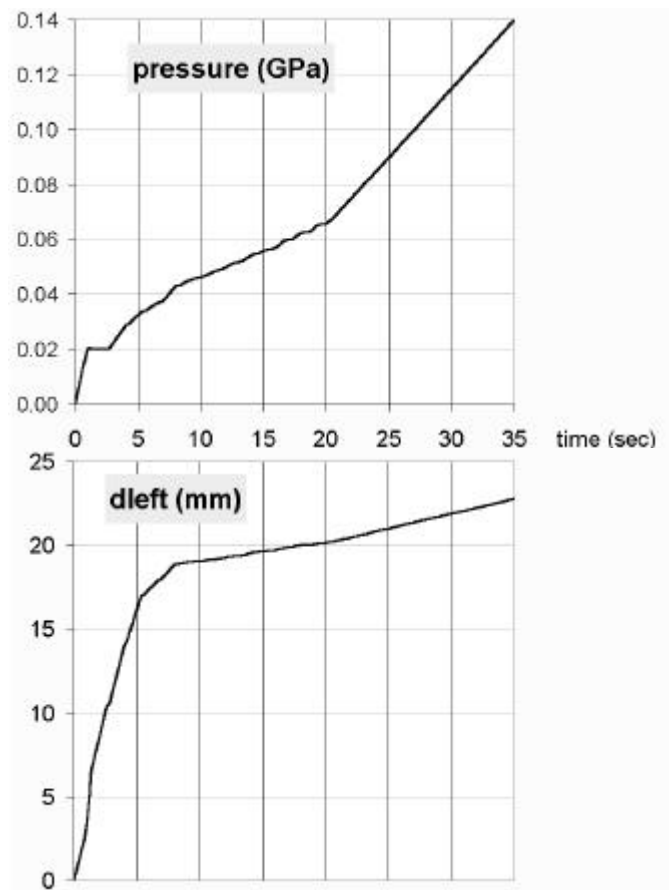


Figure II- 28: Smoothed and rescaled adaptive simulation LP.
No risk of leaking was detected during the simulation.

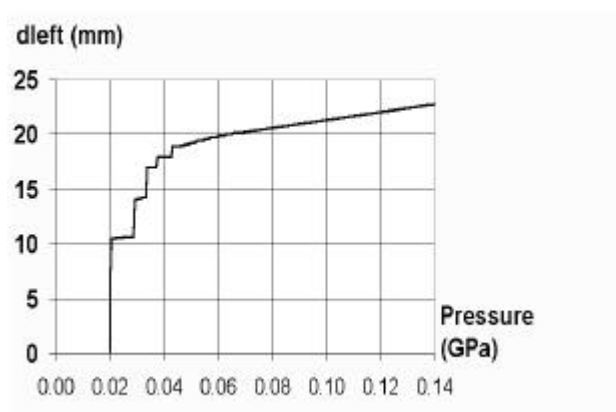


Figure II- 29: Axial feed vs. pressure plot for Figure II- 29

Part II, Chapter 5

Selection of FEA strategies

In Chapter I-4, a criterion is suggested for the classification of THF parts into four technological groups, according to the amount of axial feed they require.

In the present Chapter, an attempt is made to establish a link with a process oriented classification.

After considering the geometrical features described in Chapter I-4 and the material properties of the tube, it should be possible for the process engineer to understand approximately the total amount of axial feeding needed (d_{ax}). The correct amount of d_{right} and d_{left} can be determined only after FEA or prototyping. As already mentioned in Chapter I-4, parts can be roughly divided in 4 classes depending on the required amount of axial feed, non-dimensionally expressed as the ratio between the axial feed itself and the initial tube length d_{ax} / L_i :

- CLASS A No feeding (calibration only) $d_{ax} / L_i \approx 0$
- CLASS B Small feeding $d_{ax} / L_i \approx 0.01 \div 0.1$
- CLASS C Average feeding $d_{ax} / L_i \approx 0.1 \div 0.2$
- CLASS D Large feeding $d_{ax} / L_i \approx 0.4$ or more

When using FEA to estimate the process parameters, the proposed classification can be useful in selecting the appropriate FEA approach, as discussed below.

- No feeding, class A. The Adaptive Simulation (AS) and Self Feeding (SF) approaches are inappropriate, since only the pressure curve is to be selected. An *optimization technique* could be used to determine the best value of the initial tube diameter or the correct choice for the shape of the extrusion. Crushing is usually necessary.
- Small feeding, class B. There are no significant bulges in this class of parts, therefore the feasibility is ensured by a proper design of the initial outer diameter OD and by the optional use of crushing. The selection of the process variables is not extremely critical. Both AS and SF approaches can be used. However, since the required amount of axial feed is usually small for this kind of parts (especially if crushing is used), the self feeding approach seems to be more appropriate.
- Average feeding, class C. Both AS and SF simulation techniques can be used, but Adaptive Simulation seems to be more appropriate, since both fracture and wrinkling can easily occur and since the process window is not very large. In this case, the AS routine should try to feed as much as possible, keeping pressure as low as possible. The main concern would be in this case to prevent irreversible wrinkling. The SF approach could be used, but it could take many iterations before getting to an acceptable solution.
- Large feeding, class D. Self feeding cannot be used, because natural drawing of material towards the protrusion is prohibited by the non-symmetric geometry of T- and Y-shapes. The occurrence of wrinkling is not very likely, therefore the AS routine should be focused on postponing fracture (or thinning), rather than on wrinkling. In other words, an approach similar to the one described in Chapter 3 can be used, by proper changes in the state variables and the cost function; both should be based on thinning, rather than wrinkling. Moreover, one more control parameter (the counterpunch force) should be added to the internal pressure and the axial feed.

A vast majority of typical THF parts belongs to one of the mentioned groups. However, in some cases THF parts cannot be directly classified, for the following reasons.

1. Some parts could not be feasible by ordinary THF operations, i.e. they require particular operations, such as an intermediate annealing or the use of multiple dies.

2. In other cases long THF parts may have different features along the spline, so that they could be divided in different modules, each one of these modules belonging to a different technological group. As an example, the part in can be divided in three modules: I, II and III. Module II clearly belongs to group A, modules I and II to group C.

In some cases, THF parts cannot be directly classified into any of the four mentioned categories. As an example, the part shown in Figure II- 30 is a very long part with more than 2 sharp bends. Therefore, it should belong to the class A (no feed). On the other hand, this part shows two bulges very close to the tube edges, and these bulges may require the use of axial feed. In cases like this, if possible, the part should be divided into different portions, when running FEA simulations. This would simplify the analysis and reduce the total computational time. In the presented example, the central area should be treated as for class A. The tube end should be simulated separately as for Class C.



courtesy: SPS, Germany

Figure II- 30: A part that represents an exception to the technological classification

Summary

In this Part II, several issues related to process analysis and design have been presented, especially concerning Virtual prototyping and FEA of THF operations.

In the introduction, available strategies for FEA have been described and two main alternative approaches have been identified:

- Sequential optimization
- Adaptive simulation

An example of sequential optimization technique has been proposed in Chapter 2: the Self Feeding (SF) approach, and it has been shown how this method can be used to calculate pressure and feed vs time curves for THF. Additional issues concerned with feed controlled THF operations have been addressed, such as the risk of leaking.

In Chapter 3 a method for Adaptive Simulation (AS) has been given in detail. Particular attention has been given to detection of wrinkles and a gemotricla wrinkle indicator has been proposed. An example of application of the AS algorithm is also presented.

In Chapter 4, it is explained why and how FEA determined LPs need to be refined and pot-processed.

Finally, in Chapter 5, a brief set of guidelines for selecting the best strategy in conducting FEA analysis is proposed.

References

About 130 entries are given in references below; they are only a partial list of the whole bibliography examined during the present study, which includes about 400 publications. The scientific and technical papers, books and reports here listed are all explicitly mentioned in the dissertation's text.

- Ahmed M., Hashmi, M. S. J., 1997. Estimation of Machine Parameters for Hydraulic Bulge Forming of Tubular Components. *Journal of Materials Processing Technology*, Vol. 64, pp. 9-23.
- Aiello G., Aue-u-lan Y., Altan T., 1999. Determination of forming limit diagrams of tubular materials - 1st progress report THF/ERC/NSM-99-R-44.
- Al-Qureshi H.A., 1970. Comparison between the bulging of thin-walled tubes using rubber forming technique and hydraulic forming process, *Sheet Metal Ind.* (July) 607-612.
- Altan T., Koc M., Aue-u-lan Y., Tibari K., 1999. Formability and design issues in tube hydroforming, *Hydroforming of Tubes Extrusions and Sheet Metals*, vol. 1. Ed. K. Siegert.
- Arnold B., Aue-u-lan Y., Muammer K., Mustafa A., Altan T., 1999. Prediction of material properties for Low Carbon Steel Tubes. THF/ERC/NSM-99-R-8.
- Arrieux R., 1995. Determination and use of the forming limit stress surface of orthotropic sheets. *J. Mater. Process. Tech.* 53, pp. 47-56.
- Arrieux R., 1997. Determination and use of the forming limit stress diagrams in sheet metal forming. *J. Mater. Process. Tech.* 64, pp. 25-32.
- Asnafi N., 1999. Analytical modeling of Tube Hydroforming, *Thin-Walled Structures*, vol. 34.
- Asnafi N., Skogsgardh A., 2000. Theoretical and Experimental Analysis of Stroke-controlled Tube Hydroforming. *Materials Science And Engineering-A*, Vol. 279, N. 1-2 , pp. 95 – 110.
- Aue-u-lan Y., Muammer K., Altan T., 1999. Flow Stress of different tubular materials. THF/ERC/ NSM-99-R-33 (1999)
- Aue-u-lan Y., Strano M., Altan T., 2000. Determination of Flow Stress and Formability of Tubes for Hydroforming, ERC/NSM internal report No. THF/ERC/NSM-00-R-11.
- Bayram A., Uguz A., Ula M., 1999. Effects of Microstructure and Notches on the Mechanical Properties of Dual-Phase Steels. *Materials Characterization* 43:259–269.
- Birkert A., Neubert J., 1999. Tool and Part Design for Tube Hydroforming, *Hydroforming of Tubes, Extrusions and Sheet Metals*, vol.1, ed. By K. Siegert, p. 47-60. Technische Universität, Stuttgart.
- Bleck W., Deng Z., Papamanellos K., Gusek C.O., 1998. A comparative study of the forming limit diagram models for sheet steels, *J. Mater. Process. Tech.* 83, pp. 223-230.
- Boehm A., Hartl C., 2000. Process and Tool Technology for Hydroforming: Case Studies & Technical & Economical Considerations. *Proceedings of “Innovations in Tube Hydroforming Technology*, June 1999, Troy, MI.
- Bohm, 1993. Numerical Simulations for the Process of Internal High Pressure Forming, Dissertation, University of Paderborn, Germany,
- Boudeau N., Gelin J.C., 1996. Necking in sheet metal forming. Influence of macroscopic and microscopic properties of materials. *J. Mater. Process. Tech.* 60, pp. 325-330.
- Boudeau N., Gelin J.C., 2000. Post-processing of finite element results and prediction of the localized necking in sheet metal forming. *International Journal of Mechanical Sciences*, Vol. 42, pp. 2209-2232.
- Brunet M., Mguil S., Morestin F., 1998. Analytical and experimental studies of necking in sheet metal forming processes, *J. Mater. Process. Tech.*, 80-81, pp. 40-46.
- Brunet M., Sabourin F., Mguil-Touchal S., 1996. The Prediction of Necking and Failure in 3 D. Sheet Forming Analysis Using Damage Variable. *Journal De Physique 4*, Vol. 6, N. 6, pp. C6-473-C6-482.

- Cao J., Boyce M. C., 1997. Wrinkling behavior of rectangular plates under lateral constraint. *International Journal of Solids and Structures*, Vol. 34, No. 2, pp. 153-176.
- Carleer B.D., 2001. FE Process simulation for Tube Hydroforming, Starting with the Tube Forming Process. *Hydroforming of Tubes, Extrusions and Sheet Metals*, vol.2, ed. By K. Siegert, p. 421-437. Technische Universität, Stuttgart.
- Chan K.C., Lee W. B., 1989. Numerical computing of limit curves in the stretch forming of anisotropic sheet metals. *Numiform '89*, ed. by Thompson et al, pp. 413-418.
- Chang, D. I. Nees, R. Morit, M., 1996. Application of Hydroforming Technology for Exhaust Manifolds. *SAE Transactions* Vol. 105; N. 5, pp.: 713-718.
- Chen X.M., M.F. Shi, P.M. McKune, 2001. Applications for High-Strength Steels in Hydroforming Comparison Dual Phase vs. HSLA. *SAE paper n. 2001-01-1133*.
- Chow C. L., Yu L. G., Tai W. H., Demeri, M. Y, 2001. Prediction of forming limit diagrams for AL6111-T4 under non-proportional loading. *International Journal of Mechanical Sciences*, Vol. 43; N. 2, pp. 471-486.
- Chung J.S., Hwang S.M., 1998. Application of a genetic algorithm to process optimal design in non-isothermal forging. *Journal of Material Processing Technology*, vol. 80-81, pp. 136-143.
- Davies R., Grant G. Herling D., Smith M., Evert B. Et al., 2000. Formability Investigation of Aluminum Extrusions under Hydroforming conditions. *SAE technical paper 2000-01-2675*.
- Doege E., Kisters R., Ropers C, 1998. Determination of Optimised control Parameters for Internal High Pressure Forming Processes with the FEM. *Proc. of the Int. Conf. on Sheet Metal '98*, ed. by Kals, Geiger, et al.
- Dohmann F., Bieling P., 1991. Theoretical basis and applications of high pressure forming, *Bleche Rohre Profile* 38 (5) 379-385.
- Durban D., Ore E., 1999. Plastic buckling of Circular Shells Under Nonuniform Axial Loads. *ASME-J. of Appl. Mech.*, Vol. 66, pp. 374-379.
- Eichorn A., 1999. Innovative Developments concerning hydroforming of tubes, *Hydroforming of Tubes, Extrusions and Sheet Metals*, vol.1, ed. By K. Siegert, p. 391-405. Technische Universität, Stuttgart.
- Engel B., Dick. P., 1995. New forming spectrums with internal high pressure forming process, unknown source, 1995 (in German).
- Fuchizawa, S., 1984. Influence of Strain-Hardening Exponent on the Deformation of Thin-Walled Tube of Finite Length Subjected to Hydrostatic Internal Pressure, *Advanced Technology of Plasticity v. I* pp. 297-302.
- Fuchizawa, S., 1990. Deformation of Metal Tubes under Hydrostatic Bulge Forming with Closed Die, *Advanced Technology of Plasticity v. 3* pp. 1543-1548.
- Fuchizawa, S., Narazaki, M., 1993. Bulge Test for Determining Stress-Strain Characteristics of Thin Tubes, *Advanced Technology of Plasticity* pp. 488-493.
- Fujikawa S., Ishii K., Altan T., 1993. A diagnostic Expert System for Defects in Forged Parts. 26th ICGF Plenary Meeting, September '93, Osaka.
- Gavrus, A.; Massoni, E.; Chenot, J.L., 1996. An inverse analysis using a finite element model for identification of rheological parameters. *Journal of Materials Processing Technology* Volume: 60, Issue: 1-4, pp. 447-454
- Girault E., Jacques P., Harlet Ph., Mols K., Van Humbeeck J., Aernoudt E. Delannay F., 1998. Metallographic Methods for Revealing the Multiphase Microstructure of TRIP-Assisted Steels. *Materials Characterization* 40:111-118.
- Gouveia, B.P.P.A., Rodrigues, J.M.C., Martins, P.A.F., 2000. Ductile fracture in metalworking: experimental and theoretical research, *J. Mater. Process. Tech.*(2000), 101, pp. 52-63.
- Granelli, T., 1999. Fundamentals of bending for hydroforming. *Automotive Conference*, TPA, Dearborn, MI
- Grey J.E., Devereaux A.P., Parker W.N., Apparatus for making wrought metal T's, *US Patent 2,203,868* June (1939).
- Groche P., 1991. Fracture criteria for sheet forming. *Ph.D dissertation*, Institut fur umformtechnik und umformmaschinen, Hannover Universitat.
- Gupta N.K., Velmurugan R., 1995. An Analysis of Axi-symmetric Axial Collapse of Round Tubes. *Thin-Walled Structures*, vol. 22, pp. 261-274
- Hartl Ch., 1999. Theoretical Fundamentals of Hydroforming. *Hydroforming of Tubes, Extrusions and Sheet Metals*, vol.1, ed. By K. Siegert, p.23-35. Technische Universität, Stuttgart.
- Hashmi M.S.J., Crampton R., 1985. Hydraulic bulge forming of axisymmetric and asymmetric components: comparison of experimental results and theoretical predictions, in: *Proceedings of the International MTDR Conference*, pp. 541-549.

- Hielscher C., 2001. Tube Testing for the Production of complex Hydroforming Parts. Hydroforming of Tubes, Extrusions and Sheet Metals, Vol. 2. Ed. by K. Siegert, pp. 63-84.
- Hill R., 1958. A general theory of uniqueness and stability in elastic/plastic solids. Journal Mech. Phys. Solids Vol. 6, pp.236-249.
- Hillmann M., Kubli W., 2000. Finding the Best Way to Form Automotive Sheet Metal Parts - Using Parametric Die Face Design and Optimization. SAE Technical paper 2000-01-2704.
- Hoffmann A., Birkert A., 2001. Design Guidelines for Hydroformed Structural Components of Aluminium. Hydroforming of Tubes, Extrusions and Sheet Metals, Vol. 2. Ed. by K. Siegert.
- Hong S. Z., Wang G. X., Zeng Z.P., 1999. Technological analysis and engineering calculation on hydraulic extrusion-bulge forging of multi-way tube joint. Advanced Technology of Plasticity, Vol. II, Proceed. of the 6th ICTP.
- Hora P., Skrikerud M., 2001. Virtual Planning and simulation of fluid Medium Based Forming Processes. Hydroforming of Tubes, Extrusions and Sheet Metals, Vol. 2. Ed. by K. Siegert, pp. 385-404..
- Hora, P., Feurer U., Wahlen A., Reissner J., 2000. Numerical Methods for Control and Prediction of Process Sensitivity. Innovations In Tube Hydroforming Technology, International Conference, Troy (Detroit), June 2000.
- Hsu, Q.-C., Lee R.-S., 1997. Cold forging process design based on the induction of analytical knowledge. Journal Of Materials Processing Technology, Vol. 69; Number 1/3, pp. 264-272.
- Hutchinson J. W., 1974. Plastic buckling. Adv. Applied Mech., vol. 14, pp. 67-144.
- Hutchinson J., Neale K.W., 1978. Mechanics of sheet metal forming. Sheet necking, New York, pp. 127-153.
- Keeler S. P., 1965. Determination of forming limits in automotive stamping, SAE N. 650535, pp. 591-599.
- Kergen R., Lescart J.C., Duroux P., 2000. Analysis of the Tube Formability in Perspective of Tube Hydroforming. Proc. of Innovations In Tube Hydroforming Technology, Troy, June 2000.
- Kim D.-K., Lee S., 1999. Impact energy absorption of 6061 aluminum extruded tubes with different cross-sectional shapes. Materials and Design, Vol: 20, Issue: 1, pp. 41-4.
- Kim Y, Son Y, 2000. Study of wrinkling limit diagram of anisotropic sheet metals. Journal of Material Processing Technology, vol. 97 pp. 88-94
- Kini, S., Shivpuri, R., 1998. A Roll Pass Design Optimization Applying Fuzzy Reasoning Techniques. Technical Papers-SME-All Series, Issue 229.
- Klaas F., 2000. Experience in Development and Production of Hydroform-Components and Assemblies. Proc. of Innovations In Tube Hydroforming Technology , Troy. June 2000.
- Koç M., 2000. Use of Guidelines, Analytical Methods and FEA During Development of a Hydroform Part - Concept to Production. Proceedings of Innovations In Tube Hydroforming Technology , Troy. June 2000.
- Koç M.; Allen T.; Jiratheranat S. Altan T., 2000. The use of FEA and design of experiments to establish design guidelines for simple hydroformed parts. International Journal of Machine Tools and Manufacture, Vol: 40, Issue: 15, pp. 2249-2266.
- Koç, M., Altan, T., 1998. Development of Design Guidelines for Part, Process and Tooling in THF- Classification of Parts & Prediction of Process Parameters. ERC/NSM internal report No. THF/ERC/NSM-98-R-34 .
- Krzanowski W.J., Marriott F.H.C., 1994. Multivariate analysis. London: E. Arnold; New York: Halsted Press.
- Kusiak, J., Kawalla, R., Pietrzyk, M., Pircher, H., 1996. Inverse analysis applied to the evaluation of material parameters in the history dependent flow stress equation in hot forming of metals. Journal of Materials Processing Technology, Vol. 60, N. 1-4, pp. 455-461.
- Larkiola J., Myllykoski P., Korhonen A.S., Cser L., 1998. The role of neural network in the optimisation of rolling processes. Journal of Material Processing Technology, vol. 80-81, pp. 16-23.
- Lee W.B., Tai W.H., And Tang, C.Y. 1997. Damage evolution and forming limit predictions of an Al024-T3 aluminum alloy, , J. Mater. Process. Tech.(1997), 63, 100-104.
- Levy S.B., 1996. A comparison of empirical forming limit curves for low carbon steel with theoretical forming limit curves of Ramaekers and Bongaerts, IDDRG WG3, Ungarn.
- Lundqvist J., 1998. Numerical Simulation Of Tubular Hydroforming With An Adaptive Loading Procedure In Dyna 3D. 11th Nordic Seminar on Computational Mechanics, October 16-17, 1998
- Manabe K., Nishimura H., 1983. Influence of material properties in forming of tubes, Bander Bleche Rohre 9.
- Manabe K., Suzuki K., Mori S., Nishimura H., 1984. Bulge Forming of Thin Walled Tubes by Micro-Computer Controlled Hydraulic Press. Proceed. of the 1st Int. Conf. on Tech. of Plast.

- Manabe K., Yang M., Yoshibara S., 1998. Artificial intelligence identification of process parameters and adaptive control system for deep-drawing process. *Journal of Material Processing Technology*, vol. 80-81, pp. 421-426.
- Marciniak Z., Kuczynski K., 1967. Limit strains in the process of stretch-forming sheet metal, *International Journal of Mechanical Sciences* 9 p. 609.
- Mason, M. 1996. Exploring the Tube Hydroforming Methods. *The Fabricator*, Oct 1996, p.37.
- Mattiason K., Bernspang L., Samuelsson A., 1996. Solution of quasi-static, force-driven problems by means of a dynamic-explicit approach and an adaptive loading procedure. *Engineering Computations*, Vol. 13, No. 2/3/4, 1996, pp. 172-89.
- Mellor P.B., 1960. The ultimate tensile strength of thin-walled shells and circular diaphragms subjected to hydrostatic pressure, *Int. J. Mech. Sci.* 1 () 216-228.
- Mingyao L., Chandra, Chinghua, 1999. Influence of strain-rate sensibility on necking and instability in metal forming, *J. Mater. Process. Tech.*(1999), 96, pp. 133-138.
- Montgomery D.C., 1997. Design and analysis of experiments. New York : Wiley.
- Morphy G., 1999. Hydroform product design. Automotive tube fabricating conference. April '99.
- Mosca E., 1995. Optimal, Predictive and Adaptive Control. Prentice Hall Information and System Sciences Series.
- Neale K.W., Tugcu P., 1990. A numerical Analysis of wrinkle formation tendencies in sheet metals. *IJNME VOL. 30*, pp. 1595-1608
- Ngaile G., Altan T. 2001, "Lubrication in Tube Hydroforming"- Lubrication Mechanisms and Evaluation of Lubricant Performance, NAMRC 2001.
- Nordlund P., 1998. Adaptivity and wrinkle indication in sheet metal forming. *Computer Methods in Applied Mechanics and Engineering*, 161: 127-143.
- Nordlund P., Haggblad B., 1997. Prediction of wrinkle tendencies in explicit sheet metal-forming simulations. *International journal for numerical methods in engineering*, vol. 40 pp. 127-143
- Obermeyer E.J., Majlessi S.A., 1998. A review of recent advances in the application of blank-holder force toward improving the forming limits of sheet metal parts. *Journal of Material Processing Technology*, vol. 75, pp. 222-234.
- Ogura T., Ueda T., 1968. Liquid bulge forming, *Metalworking Prod.* (April) 73±81.
- Pawelski O., Rasp W., Schroller F., 1998. The structuration of strips by cold rolling for light-weight constructions. *Proc. NUMIFORM 98*, Enschede, Netherlands, June 22-25, 1998, p. 727-730.
- Prier M., 2001. Tribological requirements of Hydroforming and its realization in Practice. *Hydroforming of Tubes, Extrusions and Sheet Metals*, vol.2, ed. By K. Siegert, p. 421-437. Technische Universität, Stuttgart.
- Prier M., 1999. Tribology of Internal-High-Pressure-Forming, *Hydroforming of Tubes, Extrusions and Sheet Metals*, vol.1, ed. By K. Siegert, p. 379-390 Technische Universität, Stuttgart.
- Rees, D.W.A., 1995. Instability limits to the forming of sheet metals, *J. Mater. Process. Tech.* (1995), 55, 146-153.
- Rimkus, W.; Bauer, H.; Mihsein, M.J.A., 2000. Design of load-curves for hydroforming applications. *Journal of materials processing technology* . Volume: 108, Issue: 1, pp. 97-105.
- Schroller F.; Pawelski O.; Rasp W., 1999. Analysis of an improved width-flow concept for the production of ribbed strips by cold rolling. *Steel research* 70 (1999) 3, p. 110-114.
- Schuler Hydroforming, 1996. Technical Guidelines for ASE Components.
- Siegert K., 1998. Recent Developments in Hydroforming Technology, *Proceedings of the Conference on Sheet Metal Forming Technology*, Columbus, Ohio, October 5-7, 1998
- Sing W.M., Rao K.P., 1997. Roles of strain hardening laws in the prediction of forming limit curves, *J. Mater. Process. Tech.* (1997), 63, pp. 105-110.
- Sing W.M., Rao K.P., 1997. Study of sheet metal failure mechanisms based on stress state conditions, *J. Mater. Process. Tech.*(1997), 67, pp. 201-206.
- Sing W.M., Rao K.P., K.P., 1995. Influence of material properties on sheet metal formability limits, *J. Mater. Process. Tech.*(1995), 48, pp. 35-41.
- Sokolowski, T. Gerke, K. Ahmetoglu, M. Altan, T., 2000. Evaluation of tube formability and material characteristics: hydraulic bulge testing of tubes. *Journal Of Materials Processing Technology*, Vol 98, N. 1, pp. 34-40.
- Storen S., Rice J.R., 1975. Localised necking in thin sheets, *Journ. Of Mechanics of Physics and Solids*, 23, p. 421.

- Hielscher C., 2001. Tube Testing for the Production of complex Hydroforming Parts. Hydroforming of Tubes, Extrusions and Sheet Metals, Vol. 2. Ed. by K. Siegert, pp. 63-84.
- Hill R., 1958. A general theory of uniqueness and stability in elastic/plastic solids. Journal Mech. Phys. Solids Vol. 6, pp.236-249.
- Hillmann M., Kubli W., 2000. Finding the Best Way to Form Automotive Sheet Metal Parts - Using Parametric Die Face Design and Optimization. SAE Technical paper 2000-01-2704.
- Hoffmann A., Birkert A., 2001. Design Guidelines for Hydroformed Structural Components of Aluminium. Hydroforming of Tubes, Extrusions and Sheet Metals, Vol. 2. Ed. by K. Siegert.
- Hong S. Z., Wang G. X., Zeng Z.P., 1999. Technological analysis and engineering calculation on hydraulic extrusion-bulge forging of multi-way tube joint. Advanced Technology of Plasticity, Vol. II, Proceed. of the 6th ICTP.
- Hora P., Skrikerud M., 2001. Virtual Planning and simulation of fluid Medium Based Forming Processes. Hydroforming of Tubes, Extrusions and Sheet Metals, Vol. 2. Ed. by K. Siegert, pp. 385-404..
- Hora, P., Feurer U., Wahlen A., Reissner J., 2000. Numerical Methods for Control and Prediction of Process Sensitivity. Innovations In Tube Hydroforming Technology, International Conference, Troy (Detroit), June 2000.
- Hsu, Q.-C., Lee R.-S., 1997. Cold forging process design based on the induction of analytical knowledge. Journal Of Materials Processing Technology, Vol. 69; Number 1/3, pp. 264-272.
- Hutchinson J. W., 1974. Plastic buckling. Adv. Applied Mech., vol. 14, pp. 67-144.
- Hutchinson J., Neale K.W., 1978. Mechanics of sheet metal forming. Sheet necking, New York, pp. 127-153.
- Keeler S. P., 1965. Determination of forming limits in automotive stamping, SAE N. 650535, pp. 591-599.
- Kergen R., Lescart J.C., Duroux P., 2000. Analysis of the Tube Formability in Perspective of Tube Hydroforming. Proc. of Innovations In Tube Hydroforming Technology, Troy, June 2000.
- Kim D.-K., Lee S., 1999. Impact energy absorption of 6061 aluminum extruded tubes with different cross-sectional shapes. Materials and Design, Vol: 20, Issue: 1, pp. 41-4.
- Kim Y, Son Y, 2000. Study of wrinkling limit diagram of anisotropic sheet metals. Journal of Material Processing Technology, vol. 97 pp. 88-94
- Kini, S., Shivpuri, R., 1998. A Roll Pass Design Optimization Applying Fuzzy Reasoning Techniques. Technical Papers-SME-All Series, Issue 229.
- Klaas F., 2000. Experience in Development and Production of Hydroform-Components and Assemblies. Proc. of Innovations In Tube Hydroforming Technology , Troy. June 2000.
- Koç M., 2000. Use of Guidelines, Analytical Methods and FEA During Development of a Hydroform Part - Concept to Production. Proceedings of Innovations In Tube Hydroforming Technology , Troy. June 2000.
- Koç M.; Allen T.; Jiratheranat S. Altan T., 2000. The use of FEA and design of experiments to establish design guidelines for simple hydroformed parts. International Journal of Machine Tools and Manufacture, Vol: 40, Issue: 15, pp. 2249-2266.
- Koç, M., Altan, T., 1998. Development of Design Guidelines for Part, Process and Tooling in THF- Classification of Parts & Prediction of Process Parameters. ERC/NSM internal report No. THF/ERC/NSM-98-R-34 .
- Krzanowski W.J., Marriott F.H.C., 1994. Multivariate analysis. London: E. Arnold; New York: Halsted Press.
- Kusiak, J., Kawalla, R., Pietrzyk, M., Pircher, H., 1996. Inverse analysis applied to the evaluation of material parameters in the history dependent flow stress equation in hot forming of metals. Journal of Materials Processing Technology, Vol. 60, N. 1-4, pp. 455-461.
- Larkiola J., Myllykoski P., Korhonen A.S., Cser L., 1998. The role of neural network in the optimisation of rolling processes. Journal of Material Processing Technology, vol. 80-81, pp. 16-23.
- Lee W.B., Tai W.H., And Tang, C.Y. 1997. Damage evolution and forming limit predictions of an Al024-T3 aluminum alloy, , J. Mater. Process. Tech.(1997), 63, 100-104.
- Levy S.B., 1996. A comparison of empirical forming limit curves for low carbon steel with theoretical forming limit curves of Ramaekers and Bongaerts, IDDRG WG3, Ungarn.
- Lundqvist J., 1998. Numerical Simulation Of Tubular Hydroforming With An Adaptive Loading Procedure In Dyna 3D. 11th Nordic Seminar on Computational Mechanics, October 16-17, 1998
- Manabe K., Nishimura H., 1983. Influence of material properties in forming of tubes, Bander Bleche Rohre 9.
- Manabe K., Suzuki K., Mori S., Nishimura H., 1984. Bulge Forming of Thin Walled Tubes by Micro-Computer Controlled Hydraulic Press. Proceed. of the 1st Int. Conf. on Tech. of Plast.

- Manabe K., Yang M., Yoshibara S., 1998. Artificial intelligence identification of process parameters and adaptive control system for deep-drawing process. *Journal of Material Processing Technology*, vol. 80-81, pp. 421-426.
- Marciniak Z., Kuczynski K., 1967. Limit strains in the process of stretch-forming sheet metal, *International Journal of Mechanical Sciences* 9 p. 609.
- Mason, M. 1996. Exploring the Tube Hydroforming Methods. *The Fabricator*, Oct 1996, p.37.
- Mattiason K., Bernspang L., Samuelsson A., 1996. Solution of quasi-static, force-driven problems by means of a dynamic-explicit approach and an adaptive loading procedure. *Engineering Computations*, Vol. 13, No. 2/3/4, 1996, pp. 172-89.
- Mellor P.B., 1960. The ultimate tensile strength of thin-walled shells and circular diaphragms subjected to hydrostatic pressure, *Int. J. Mech. Sci.* 1 () 216-228.
- Mingyao L., Chandra, Chinghua, 1999. Influence of strain-rate sensibility on necking and instability in metal forming, *J. Mater. Process. Tech.*(1999), 96, pp. 133-138.
- Montgomery D.C., 1997. Design and analysis of experiments. New York : Wiley.
- Morphy G., 1999. Hydroform product design. Automotive tube fabricating conference. April '99.
- Mosca E., 1995. Optimal, Predictive and Adaptive Control. Prentice Hall Information and System Sciences Series.
- Neale K.W., Tugcu P., 1990. A numerical Analysis of wrinkle formation tendencies in sheet metals. *IJNME VOL. 30*, pp. 1595-1608
- Ngaile G., Altan T. 2001, "Lubrication in Tube Hydroforming"- Lubrication Mechanisms and Evaluation of Lubricant Performance, NAMRC 2001.
- Nordlund P., 1998. Adaptivity and wrinkle indication in sheet metal forming. *Computer Methods in Applied Mechanics and Engineering*, 161: 127-143.
- Nordlund P., Haggblad B., 1997. Prediction of wrinkle tendencies in explicit sheet metal-forming simulations. *International journal for numerical methods in engineering*, vol. 40 pp. 127-143
- Obermeyer E.J., Majlessi S.A., 1998. A review of recent advances in the application of blank-holder force toward improving the forming limits of sheet metal parts. *Journal of Material Processing Technology*, vol. 75, pp. 222-234.
- Ogura T., Ueda T., 1968. Liquid bulge forming, *Metalworking Prod.* (April) 73±81.
- Pawelski O., Rasp W., Schroller F., 1998. The structuration of strips by cold rolling for light-weight constructions. *Proc. NUMIFORM 98*, Enschede, Netherlands, June 22-25, 1998, p. 727-730.
- Prier M., 2001. Tribological requirements of Hydroforming and its realization in Practice. *Hydroforming of Tubes, Extrusions and Sheet Metals*, vol.2, ed. By K. Siegert, p. 421-437. Technische Universität, Stuttgart.
- Prier M., 1999. Tribology of Internal-High-Pressure-Forming, *Hydroforming of Tubes, Extrusions and Sheet Metals*, vol.1, ed. By K. Siegert, p. 379-390 Technische Universität, Stuttgart.
- Rees, D.W.A., 1995. Instability limits to the forming of sheet metals, *J. Mater. Process. Tech.* (1995), 55, 146-153.
- Rimkus, W.; Bauer, H.; Mihsein, M.J.A., 2000. Design of load-curves for hydroforming applications. *Journal of materials processing technology* . Volume: 108, Issue: 1, pp. 97-105.
- Schroller F.; Pawelski O.; Rasp W., 1999. Analysis of an improved width-flow concept for the production of ribbed strips by cold rolling. *Steel research* 70 (1999) 3, p. 110-114.
- Schuler Hydroforming, 1996. Technical Guidelines for ASE Components.
- Siegert K., 1998. Recent Developments in Hydroforming Technology, *Proceedings of the Conference on Sheet Metal Forming Technology*, Columbus, Ohio, October 5-7, 1998
- Sing W.M., Rao K.P., 1997. Roles of strain hardening laws in the prediction of forming limit curves, *J. Mater. Process. Tech.* (1997), 63, pp. 105-110.
- Sing W.M., Rao K.P., 1997. Study of sheet metal failure mechanisms based on stress state conditions, *J. Mater. Process. Tech.*(1997), 67, pp. 201-206.
- Sing W.M., Rao K.P., K.P., 1995. Influence of material properties on sheet metal formability limits, *J. Mater. Process. Tech.*(1995), 48, pp. 35-41.
- Sokolowski, T. Gerke, K. Ahmetoglu, M. Altan, T., 2000. Evaluation of tube formability and material characteristics: hydraulic bulge testing of tubes. *Journal Of Materials Processing Technology*, Vol 98, N. 1, pp. 34-40.
- Storen S., Rice J.R., 1975. Localised necking in thin sheets, *Journ. Of Mechanics of Physics and Solids*, 23, p. 421.

- Stoughton T.B., 2000. A general forming limit criterion for sheet metal forming, *Int. J. Mech. Sci.* (2000), vol. 42, n. 1, pp. 1-27.
- Strano M., Aue-u-lan Y., Aiello G., Altan T., 2000. Determination Of The Forming Limits For Tubular Materials. ERC/NSM report No. THF/ERC/NSM-00-R-29.
- Strano M., Aue-u-lan Y., Schroller F., Altan T., 2000. An Upper Bound Approach for the Determination of Flow Stress of Tubular Materials, ERC/NSM internal report No. THF/ERC/NSM-00-R-17.
- Strano M., Jirathearanat S., 2000. Adaptive Simulation Concept for Tube Hydroforming. *Proc. of Innovations In Tube Hydroforming Technology*, Troy, June 2000.
- Strano M., Jirathearant S., Altan, T., 2000. Selection of the Loading Paths in the Hydroforming of Variable Cross Section Tubes. ERC/NSM Report No. TH/ERC/NSM-00-R-05.
- Strano M., S. Jirathearanat, T. Altan, 2001. Adaptive FEM Simulation for Tube Hydroforming: a Geometry-Based Approach for Wrinkle Detection. 51st Annals of CIRP.
- Szacinski A.M., Thomson P.F., 1991. Investigation on the Existence of a Wrinkling-Limit Curve in Plastically Deforming Metal Sheet. *Journal of Material Processing Technology*, vol. 25 pp. 125-137
- Takuda H.; Mori K., Hatta N., 1999. The application of some criteria for ductile fracture to the prediction of the forming limit of sheet metals, *J. Mater. Process. Tech.*, 95, pp. 116-121.
- Takuda H.; Mori K.; Takakura N., Yamaguchi K., 2000. Finite element analysis of limit strain in biaxial stretching of sheet metals allowing for ductile fracture, *Int. J. Mech. Sc.*, 42, pp. 785-798.
- Tang C.Y., Chow C.L., Shen W., Tai W.H. 1999. Development of a damage based criterion for the ductile fracture prediction in sheet metal forming, *J. Mater. Process. Tech.*, 91, pp. 270-277.
- Thiruvurudchelvyan, S. Lewis, W., 1999. A note on hydroforming with constant fluid pressure. *Journal Of Materials Processing Technology* 1999 Vol 88; Number 1-3, pp 51 – 56.
- Thomas W. J., 1999. Product, Tool and Process Design Methodology for Deep Drawing and Stamping of Sheet Metal Parts. Ph.D. Dissertation, ERC/NSM.
- Tibari K., Jirathearanat S., Strano M., Altan, T., 2000. Adaptive Simulation in Tube Hydroforming. ERC/NSM report no. THF/ERC/NSM-00-R-10.
- Tirosh J., Neuberger A., Shirizly A., 1996. On tube expansion by internal fluid pressure with additional compressive stress. *International Journal of Mechanical Sciences*, Vol: 38, Issue: 8-9, pp. 839-851.
- Tirosh, J., Neuberger, A. Shirizly, A., 1996. Tube Expansion with 'Unlimited' Strain, *Advanced Technology of Plasticity*, 5th Annual ICTP, p. 527-530.
- Tomita Y., Shindo A., 1988. Onset and growth of Wrinkles in Thin Square Plates Subjected to Diagonal Tension. *Int. J. Mech. Sci.*, vol. 30 no. 12, pp. 921-931
- Triantafyllidis N., Needleman A., 1980. An Analysis of Wrinkling in the Swift Cup Test. *J. of Eng. Mater. and Tech.*, vol. 102, pp. 241-248
- Treude M, Engel B., 2001. Piece Cost Redution of Hydroformed Parts in Series Production. *Hydroforming of Tubes, Extrusions and Sheet Metals*, Vol. 2. Ed. by K. Siegert.
- Trowsdale A.J., Usherwood T.W. et alii, 1998. Neural networks providing 'on-line' access to discretised modeling techniques. *Journal of Material Processing Technology*, vol. 80-81 pp.475-480.
- Vollertsen, F., 2000. Accuracy in process chains using hydroforming. *Journal of Materials Processing Technology*. Vol. 103, pp. 424-433.
- Wagoner R.H., Chan K.S., Keeler, S.P., 1989. *Forming Limit Diagrams: Concepts, methods, and Applications*, R.H. Wagoner, TMS 1989.
- Whittle P., 1982. *Optimization over Time, Dynamic Programming and Stochastic Control*, Vol. 1. Wiley Series in Probability and Mathematical Statistics.
- Xing, H.L., A. Makinouchi, 2001. Numerical analysis and design for tubular hydroforming. *International Journal of Mechanical Sciences* 43 (2001), pp. 1009-1026.
- Xu S., Weinmann K.J., 2000. Effect of deformation dependent material parameters on forming limits of thin sheets, *Int. J. Mech. Sc.* 42, pp. 677-692
- Yang, J.B., Jeon B.H., Oh S.I., 2001. Design Sensitivity Analysis and Optimization of the Hydroforming Process, *Journal of Materials Processing Technology*, 113 (2001), pp. 666-672.
- Yoshida T., Katayama T., Usuda M., 1995. Forming -limit analysis of hemispherical-punch stretching using the three-dimensional finite-element method, *Journal of Materials Processing Techn.*, 50, pp. 226-237.

- Yossifon S., Tirosh J., Kochavi E., 1984. On Suppression of Plastic Buckling in hydroforming Processes. *Int. J. Mech. Sci.*, vol. 26 no. 6-8, pp. 389-402
- Young-Hae. L., Hyun-Moon S., Byung-Hee Y., 1996. An approach for multiple criteria simulation optimization with application to turning operation. *Computers in Industrial Engineering*, Vol, 30, No. 3, pp. 375-386.
- Zhao L., Sowerby R., Shlad, M.P., 1996. A theoretical and experimental investigation of limit strains in sheet metal forming, *Int. J. Mech. Sc.*, 38, No.12, pp. 1307-1317
- Zong-hua s., Jian-fang L., Guang-xin L., 1993. Neural networks for defects diagnosis in flow forming processes. *Advanced Technology of Plasticity 1993* ed. by Z.R. Wang, He Yuxin.

Appendix A:

Analysis of Variance for Flow Stress Determination

In this Appendix, the results described in Chapter I-2 about the difference in hardening behavior of incoming AISI 304 by different suppliers is reviewed. Statistical tools such as regression analysis, ANOVA (Analysis of Variance) and MANOVA (Multivariate Analysis of Variance) [Krzanowski and Marriott, 1994], [Montgomery, 1997] have been used. The data have been analyzed with the help of the commercial software Minitab. The bulging experiments and results are represented in Table A- 1.

Table A- 1: Bulge forming experiments for the determination of flow stress

OD (mm)	48.6			57.2				50.8	
material	A		B	C			E	D	
t ₀ (mm)	1.5	2.0	2.1	1.7	1.3	0.61	1.7	2.0	1.5
K (MPa)	1207	1401	1555	1361	1654	1363	1648	1362	1357
n	0.351	0.406	0.615	0.559	0.589	0.503	0.627	0.504	0.470
ε ₀	0.021	0.028	0.030	0.034	0.026	0.007	0.034	0.027	0.020
# of replicates	2	1	2	1	1	1	1	2	2

Regression analysis between K and n

A regression analysis proved that K and n values for the tested tubes are strongly correlated. More precisely, K can be estimated by a quadratic function of n. The regression plot is given in the following Figure A- 1.

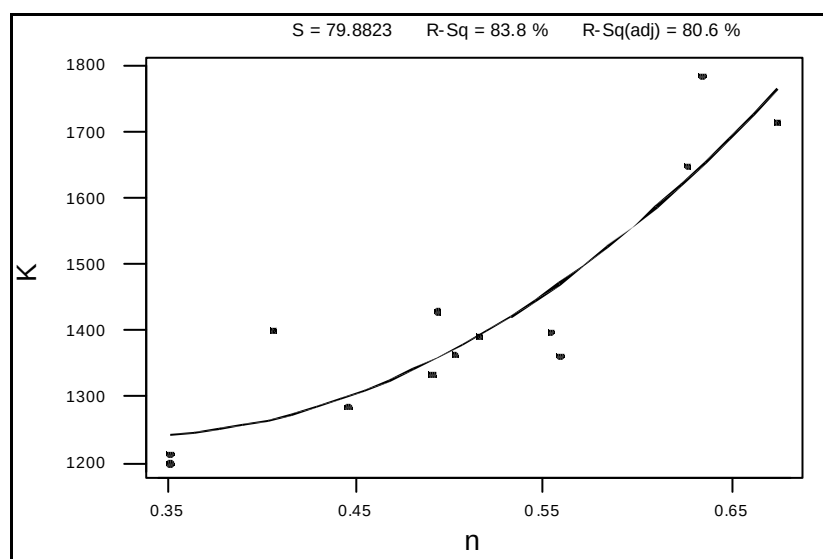


Figure A- 1: regression plot for K and n. $K=1721-2904n+4400n^2$.

ANOVA and MANOVA analysis.

There are 3 potential factors influencing the flow stress parameters:

- Outer diameter OD
- Material supplier *supplier*
- Initial thickness t_0

Both ANOVA and MANOVA proved that:

- Factor *supplier* is significant on n -value (and therefore on K), but only for material A, while flow stress curves of materials B, C, D and E do not significantly differ;
- t_0 and OD do not seem to induce any statistically significant effect on K and n .

Appendix B:

Equations for the calculation of FLSD

Conversion from the NADDRG FLD to the FLSD

For each point on the FLD, ϵ_{hoop} and ϵ_{axial} at fracture are known. Therefore, it is possible to calculate the effective strain (eq. 1) and the effective stress (eq. 2). Equation 3 estimates the stress ratio at fracture, thus leading to the calculation of σ_θ and σ_z , thanks to equations 4

$$\text{Effective strain: } \bar{e} = \sqrt{\frac{4}{3}(e_q^2 + e_q \cdot e_z + e_z^2)} \quad (A-1)$$

$$\text{Flow stress law: } \bar{s} = k\bar{e}^n \quad (A-2)$$

$$\text{Stress ratio: } b = \frac{1 + 2 \frac{e_q}{e_z}}{2 + \frac{e}{e_z}} \quad (A-3)$$

$$\text{State of stress: } s_q = \frac{\bar{s}}{\sqrt{1 - b + b^2}}; \quad s_z = b \cdot s_q \quad (A-4)$$

Conversion from the experimental FLD to the FLSD

The axial stress can be calculated as a function of pressure P_i , bulge height BH and thickness at fracture:

$$s_z = \frac{P_i \cdot 2 \cdot BH}{4th_f} \quad (A-5)$$

Then, the effective stress can be calculated as:

$$\bar{s} = k(e_0 + \bar{e})^n \quad (A-6)$$

Finally, σ_{hoop} can be calculated as:

$$s_q = \frac{1}{2}(s_z + \sqrt{4 * \bar{s}^2 - 3 * s_z^2}) \quad (A-7)$$

Appendix C:

description of the experiments

Experimental setup

The present study is based on a series of experiments conducted with the die set shown in Figure A- 2 and Figure A- 3 and the press shown in Figure A- 4. FEM simulations have been run with the commercial code PAM-STAMP. The study has been experimentally supported by using the hydroforming testing equipment made available by Kawasaki HydroMechanics.

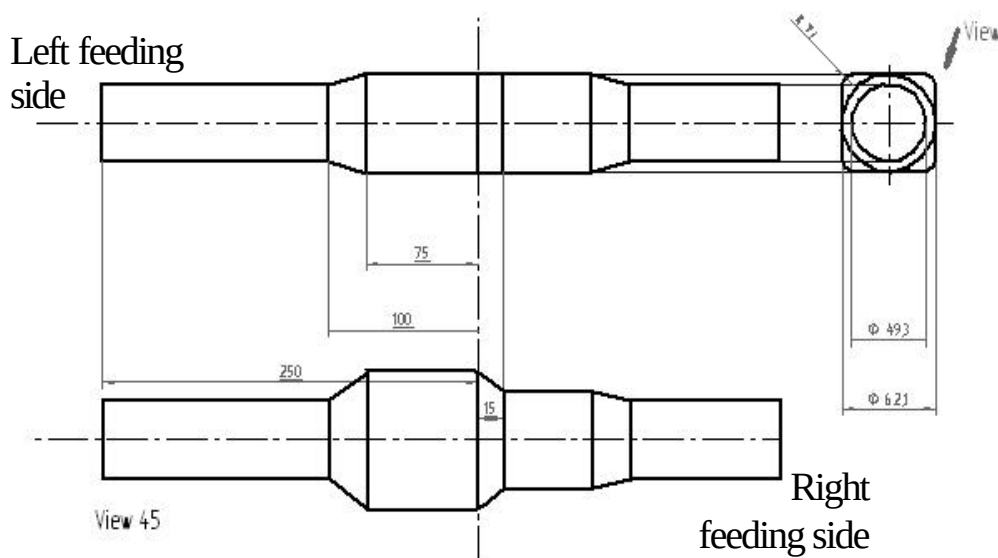


Figure A- 2: drawing of the die



Figure A- 3: Photo of die set and specimen (provided by Kawasaki HydroMechanics).



Figure A- 4: The Hydroforming press located at the Kawasaki HydroMechanics plant in Japan.

Results of Experiments and FEM Simulations

In this section the results of the experiments are summarized.

- The plan of the experiments and the results will be given in a summary tabular form.
- The loading paths (internal pressure P , left axial feed d_{left} and right axial feed d_{right}) will be given in graphical form; left and right are referred to the left and right hand sides of the part, as shown in Figure A- 2.
- Pictures of some hydroformed tubes will be shown as examples.

In the two following subsections, results are presented for experiments using tubes with nominal initial wall thickness $t_0=2$ mm and with initial wall thickness $t_0=1.5$ mm. In the following, a Loading Path (LP) number will be assigned to each experiment.

Experiments – tube wall thickness $t_0=2$ mm

The tubes, from stainless steel AISI 304, had an OD of 48,6 mm and were provided by two different suppliers, identified in the following as materials A and B. The nominal wall thickness is in both cases 2 mm. However, repeated measurements on the original tubes showed that the real values of t_0 are as follows:

- Material A, actual measured wall thickness $t = 1.95 \pm 0.05$ mm. In calculations and FEM simulations, the value 2.0 mm will be used.
- Material B, actual measured wall thickness $t = 2.13 \pm 0.08$ mm. In calculations and FEM simulations, the value 2.1 mm will be used.

The plan of experiments (Table A- 1), the Loading Paths (LP) (Figure A- 5 and Figure A- 6) and some pictures (Figure A- 7, Figure A- 8) are given below.

Table A- 2: Plan and summary of results of experiments, nominal $t_0=2\text{mm}$.

LP number	material SS304 A, $t_0=1.95\text{ mm}$		material SS304 B, $t_0=2.1\text{ mm}$	
	observations	# of samples	observations	# of samples
1	large wrinkles	2	good part at 270 MPa	2
2.0	no experiment	*	leaking, part not completely formed	1
2.1	no experiment	*	leaking + bursting at 82 MPa	2
3	bursting at 160 MPa and 120 MPa for the 2 samples	2	good part, very high thinning	2
4.0	wrinkling and bursting	1	small wrinkles	2
4.1	no experiment	*	leaking at 230 MPa good part	1
4.2	wrinkling and bursting	1	good part at 250 MPa	1
4.2	wrinkling and bursting	1	good part at 250 MPa	1

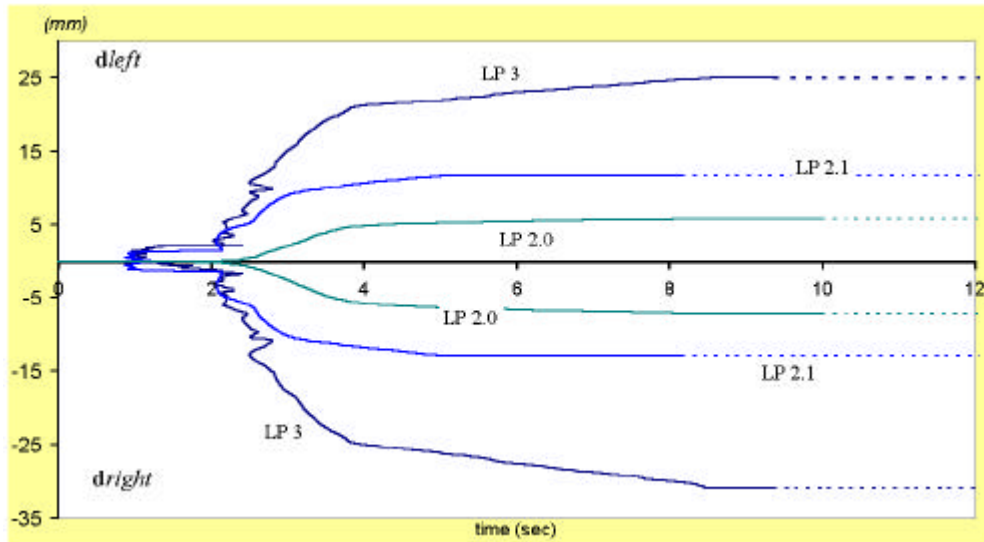


Figure A- 5: part programs 2.0, 2.1, 3 and 7, $t_0=2\text{mm}$. Left axial feed d_{left} is positive, right axial feed d_{right} is negative. Pressure is linearly increasing for all LP with maximum value 2700 bar (0.27 GPa). Final time is 27 seconds for all LPs.

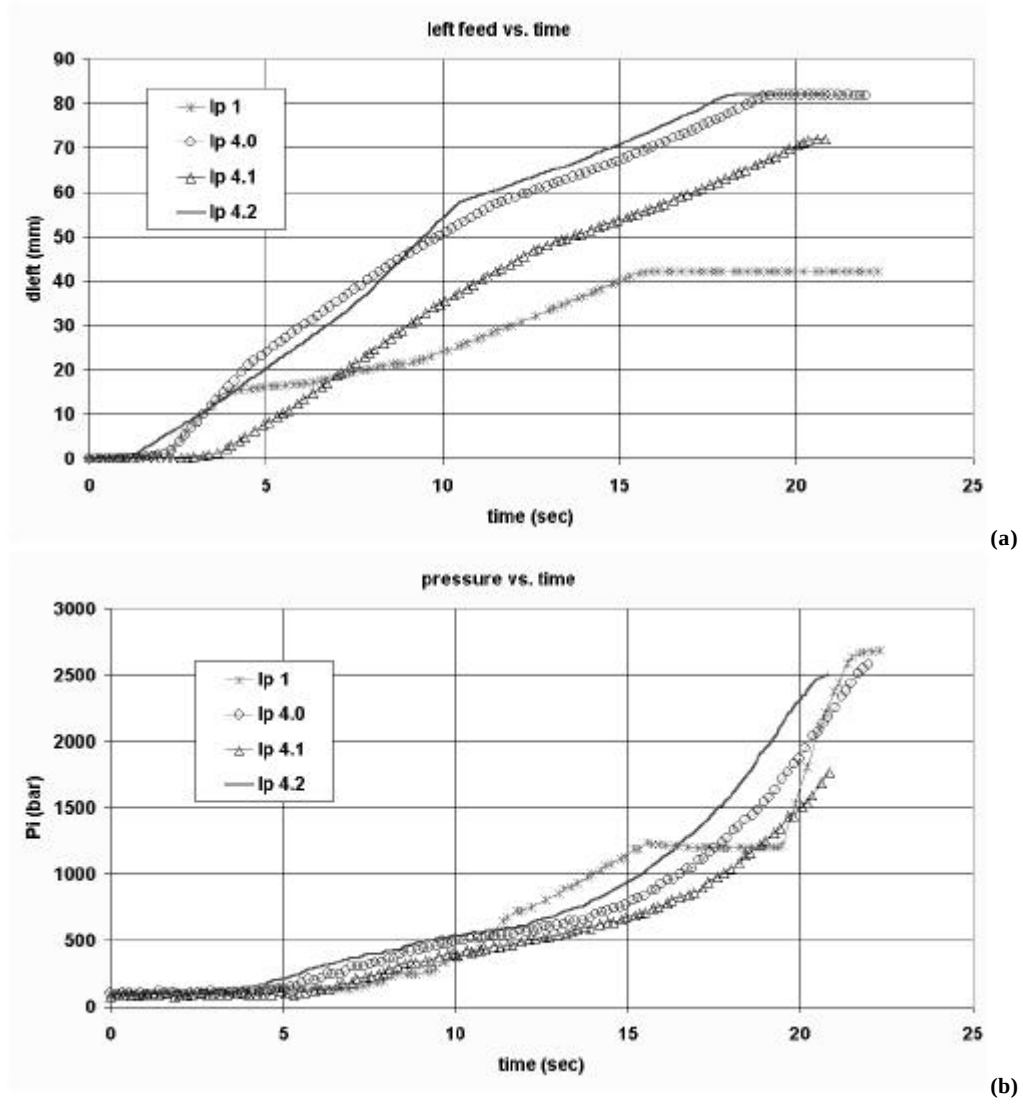


Figure A- 6: loading paths for experiments 1, 4.0, 4.1, 4.2; $t_0 = 2$ mm
(a) feed vs. time, left feed d_{left} only is plotted. For LP 1, $d_{right} = -d_{left}$, for LP 4.0, 4.1 and 4.2, $d_{right} \approx 0$.
(b) pressure vs. time



Figure A- 7: Hydroformed sample with LP 2.1, material B



Figure A- 8: Hydroformed samples with LP 3, material B

Experiments - tube wall thickness $t_0 = 1.5$ mm

All experiments and simulations for $t_0 = 1.5$ mm have been conducted using Material A. The plan of experiment (Table A- 3), the LPs (Figure A- 9) and some pictures of hydroformed tubes (Figure A- 10 to Figure A- 13) are given below.

Table A- 3: Summary of results of experiments for $t_0 = 1.5$ mm. LPs are listed in increasing amount of total axial feed $d_{ax} = d_{left} - d_{right}$.

LP number	material SS304 A	
	observations	# of samples
1	leaking - not completely formed	1
1.2	good part	2
6	good part	2
7	small wrinkles	2
2	large wrinkles	1

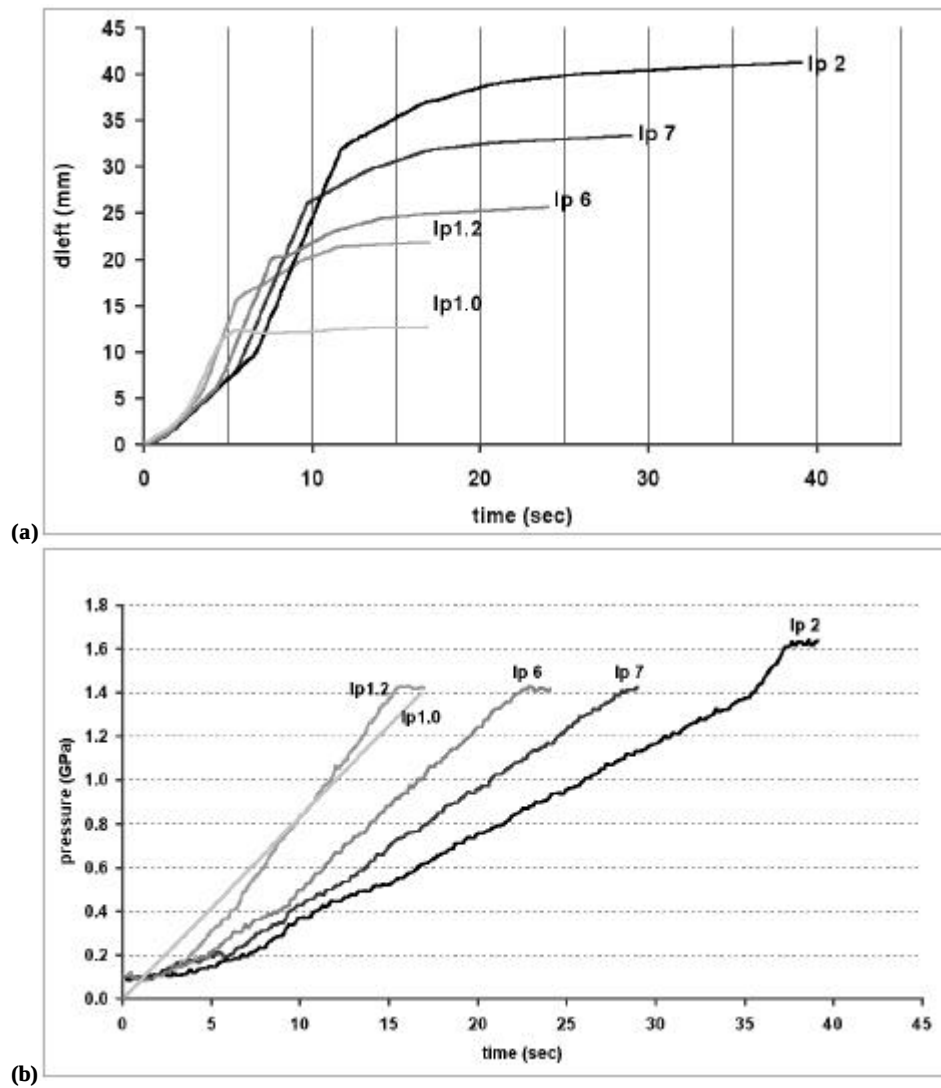


Figure A- 9: Experimental loading paths for thickness $t_0 = 1.5$ mm, mat. A.

(a) left feed vs. time ($d_{right} - d_{left}$),
(b) pressure vs. time



Figure A- 10: Hydroformed samples with LP 1.2, $t_0 = 1.5$ mm, mat. A.



Figure A- 11: Hydroformed samples with LP 2, $t_0 = 1.5$ mm, mat. A.



Figure A- 12: Hydroformed samples with LP 6, $t_0 = 1.5$ mm, mat. A.



Figure A- 13: Hydroformed samples with LP 7, $t_0 = 1.5$ mm, mat. A.

FEM simulations

For all experiments listed in the previous two subsections, FEM simulations have been run with the software PAM-STAMP. The accuracy of the simulation results has been compared using two indicators:

- a) Presence/absence of wrinkles in the final part
- b) Thickness distribution

Thickness distribution has been measured along the two different profiles A and B given in the following Figure A-14a. Thickness is usually predicted with good accuracy, unless wrinkling occurs. The examples given in Figure A-14b and Figure A- 15, show that the prediction is very accurate in terms of qualitative distribution. Quantitatively, thickness distribution along the profile B is better estimated than along profile A in this case. However, this is not a generally observed behavior. In general, the percentage difference in thickness between FEM and experiments lays between ± 8 %.

Wrinkling phenomena are usually predicted correctly by FEM in location and amplitude, although in a few cases there is some difference in shape between the actual and the simulated wrinkles (see Figure A-16).

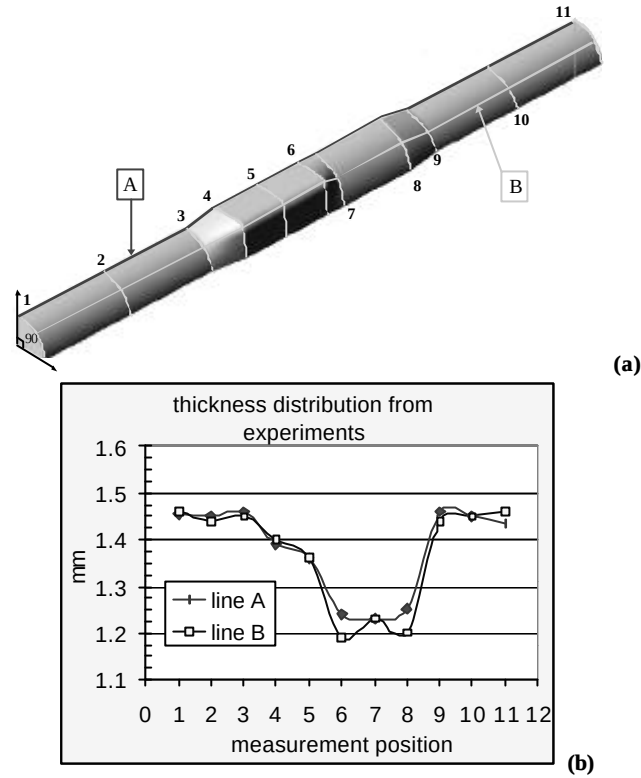


Figure A- 14: Example of measurements of thickness from the experiments: locations (a) and values (b), (LP1.2, $t_0 = 1.5$ mm, mat. A)

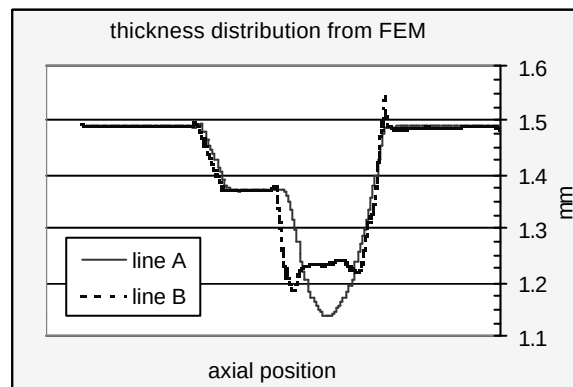


Figure A- 15: Example of thickness distribution from the FEM along lines A and B shown in Fig. A-14a (LP1.2, $t_0 = 1.5$ mm, mat. A).



Figure A- 16: Sample hydroformed tube with LP2, $t_0 = 1.5$ mm, mat. A; (a) actual wrinkle shape and (b) FEM result;

Additional FEM simulations have been run, not corresponding to any experimental LP, for various purposes. These additional simulations will be mentioned and described when necessary in the following chapters.

Determination of material properties

As already mentioned previously, three kinds of tubes have been used for the present study: [$t_0 = 2$ mm - material A], [$t_0 = 2.1$ mm - mat. B], [$t_0 = 1.5$ mm - mat. A].

In order to run accurate FEM simulations, the flow stress vs. effective strain curves are needed for each material. The flow stress laws have been determined as explained in App. A.

Similarly, the fracture limits of the material are required as input for the FEA analysis.

The results of the investigations on forming limits are reported in the following subsection “Prediction of

Prediction of fracture

In a previous study [Strano, Aue-u-lan, Aiello et al., 2000], it has been shown that, at least within the experimentally tested range, wall thickness and tube initial OD have little effect on the position of the FLD on its right hand side (tension-tension). In the cited study, the use of the Forming Limit Stress Diagram (FLSD) was suggested as a more robust and reliable approach than the traditional FLD.

However, for the part under investigation, most of the deformation occurs on the left side (compression-tension), since axial feeding is present. Moreover, some of the failed tubes are fractured at the welding line, especially for material B. For these reasons, the conclusions developed for the forming limits of tubes in the mentioned report hardly apply in this case. The variability of the occurrence of fracture at the welding line is very high. Therefore, any experimental forming limit based on strains (FLD), stresses (FLSD) or simply on thinning would not apply in this case.

Even when fracture does not occur at the welding line, the variability of occurrence of fracture is very high, especially for material A, $t_0 = 2$ mm. As an example, consider the two samples of the experiment [LP3]:

- 1st sample is fractured at 160 MPa, max thinning 20 % in the experiments (12 % in the FEM at $P_i = 160$ MPa);
- 2nd sample is fractured at 120 MPa max thinning 16 % in the experiments (11 % in the FEM at $P_i = 120$ MPa).

Due to this variability, the number of fractured experiments is not sufficient to provide a reliable fracture criterion for this material and wall thickness. Therefore, data for material A, $t_0 = 2$ mm are not further analyzed.

For the remaining two materials [mat. A, $t_0 = 1.5$ mm] and [mat. B, $t_0 = 2.1$ mm], the location of fracture is shown in Figure A-17a. Referring to Figure A-17b, the edges of the fracture run along profile B, between points 4 and 6. For these materials, an arbitrary empirical fracture criterion based on maximum thinning $th\%_{max}$ will be used in the remainder of the report. In the experiments, maximum thinning always occurs in the proximity of profile B, between points 5 and 6. In most cases, maximum thinning occurs very close to point 6. As an example, Figure A-18 shows the thinning distribution for different experiments between points 4 and 7 along profile B.

The maximum allowable percentage thinning value $th\%_{max}$ (i.e. the “critical value of $th\%_{max}$ to be used in following FEM analysis) is determined comparing experimental results and FEM simulations for two limit cases, where the tube is not fractured, but where thinning is high. Maximum thinning in the hydroformed tube is defined as:

$$th\%_{max} = 100 * (t_0 - t_{min}) / t_0; \quad (t_0 = \text{initial thickness}; t_{min} = \text{minimum thickness}).$$

The 2 limit cases and the corresponding critical $th\%_{max}$ values at point 6 along profile B (referring to Fig. A-17) are given in Table A- 4. The values $th\%_{max}$ in the rightmost column of are considered in all following simulations as a “fracture” limit.

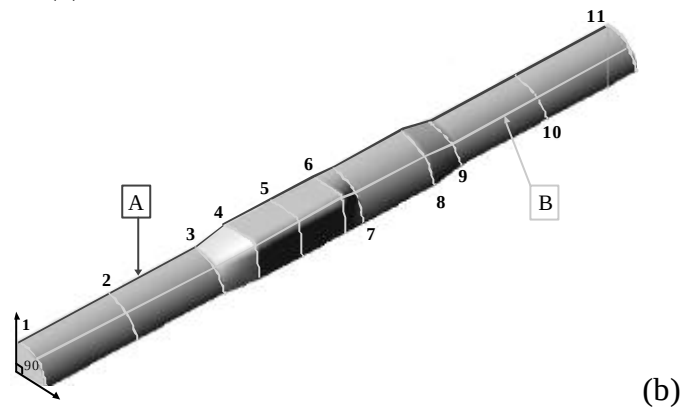
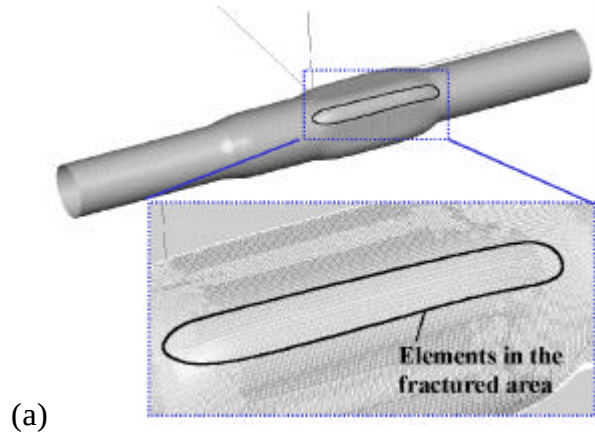


Figure A- 17: Location of fracture is along “profile B”, between points 4 and 6.

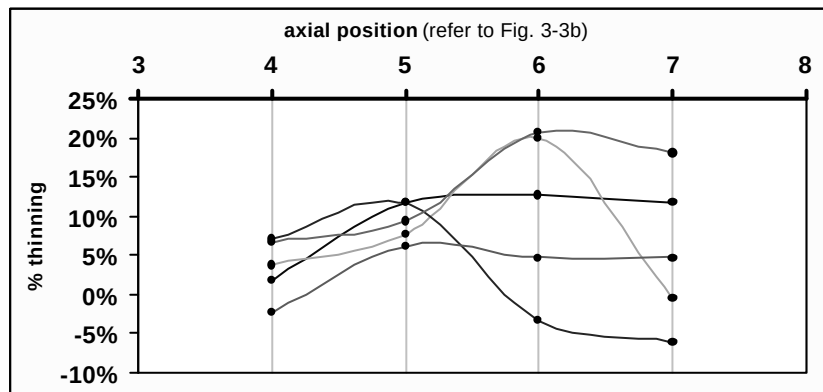


Figure A- 18: Thinning distribution for different experiments between points 4 and 7 along profile B

In summary, the conclusions of the present Chapter are:

- Flow stress parameters are similar for the two materials A (t_0 1.5 mm and 2 mm), but different for material B.
- Material A- t_0 = 2 mm has very poor forming limits, therefore no more analysis will be carried out on this material in the rest of the report.
- Material B has a very good formability ($t_{n\%max}$ =30%, n =0.615), but it sometimes fractures close to the welding line.

Table A- 4: $th\%_{max}$ values used as fracture criterion.

material	LP	EXPERIMENT			FEM	
		Minimum thickness (point 6 on profile B, Fig. A-17b)	Maximum thickness strain ϵ_{min}	Maximum thinning $th\%_{max}$	Thickness at point 6 on profile B, Fig. A-17b)	Limit thinning $th\%_{max}$
<u>material A</u> t ₀ 1.5 mm	[LP1.2] not fractured	1.17	-0.23	22 %	1.16	23 %
<u>material B</u> t ₀ 2.1 mm	[LP3] not fractured	1.39	-0.43	35 %	1.49	30 %