PROCESS ANALYSIS AND TOOL COMPENSATION FOR CURVED COMPOSITE L-ANGLES

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ABSTRACT

As larger and more integrated composites structures are made in the aerospace industry, it gets more difficult to manufacture these within dimensional tolerances. The cured part shape often differs from that of the room temperature tooling, and process tools may need geometric compensation to account for this. Geometric compensation of the tool requires knowledge of how the part is going to deform – before it is made. Standard practice is to rely on experience and rules-of-thumb to estimate how much the tool needs to be compensated. This empirical approach becomes unreliable and risky as structures get larger and more complex.

This paper presents a case study where process analysis was used to predict the process-induced deformations of a curved L-angle, and thereby aid in geometric compensation of the process tools. A systematic approach to the problem is presented where the main drivers for process-induced deformation are examined one-by-one. By combining approximate closed-form analysis with higher fidelity finite element analysis, confidence in model prediction is gained and insight into the root causes of deformation developed. Measurements on pre-production articles showed excellent agreement with model predictions and a tool compensation scheme based on the model results was developed.

INTRODUCTION

The challenge was to manufacture a large number of different sized composite products that are approximately a curved L-angle with cut-outs, shaped as a circle sector (Figure 1). The parts are made of 180°C cure carbon-epoxy prepreg and are processed on a female metal tool in an autoclave. The dimensional control of the product is very stringent for fit-up reasons. The "standard" tool material for the application is Invar because of good thermal stability, matching that of the graphite composite and high hardness. However, Invar tooling is expensive and difficult to manufacture so there was interest in using steel tooling instead. The drawback with steel tooling is a larger thermal expansion which may have a negative effect on dimensional control of the product. Experience has shown that to maintain dimensional control a geometric compensation of the process tool may be necessary, especially if steel tooling is used. Geometric compensation is typically done based on experience from similar structures and processes as well as on experimental prototype data. Because of the large number of products with varying geometries that were to be manufactured, it was time and cost prohibitive to experimentally determine the appropriate tool compensation for the whole product family. Instead it was decided that process modelling should be used, in conjunction with experience and limited prototype testing, to develop tool compensation recommendations for the products. The Composites Innovation Centre in Winnipeg were instrumental in getting the project off the ground as well as providing partial funding and project management during the project.

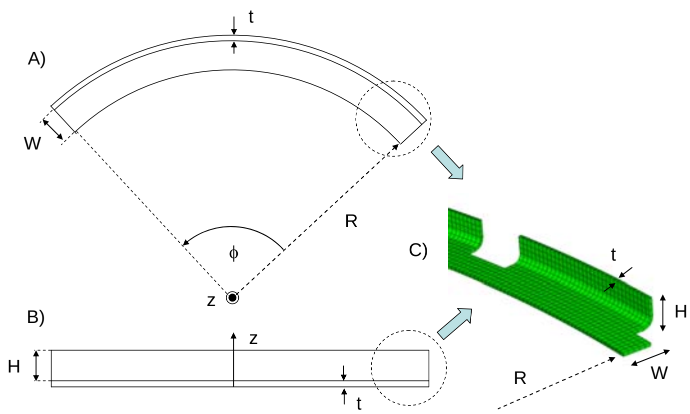

Figure 1. Schematic of a typical product. A) Outline in R-ϕ plane; B) Outline in ϕ-z plane; C) Detail showing cut-outs in the flange.

APPROACH AND ANALYSIS

In order to predict the process-induced deformation of the products, the key question to answer is: what causes the product to deform? Based on extensive academic research and industrial experience, the main mechanisms for process-induced deformation are summarized in Table 1. The list may be different for other structures and/or process conditions.

Table 1. Mechanisms that potentially contribute to process-induced deformations.

Mechanism	**Drivers**	**Description**
1) Tool growth	• Tool/Part CTE • Cure temperature	The tool expands when heated up, and the part "sets" in shape when the tool is in its expanded state. When cooled down to room temperature, tool and part may shrink differentially.
2) Spring-in	• Anisotropy in thermal expansion and cure shrinkage of the laminate • Curvature of laminate	A larger shrinkage through-thickness than in-plane cause closed angles to spring-in. In compound curvature parts, spring-in may also cause other deformations due to 3D geometric interaction effects.
3) Tool-part interaction	• Tool/Part CTE • Cure temperature • Tool-part interface • Shear behaviour of laminate • Length and thickness of laminate	When the tool expands on heat up, shear stress at the tool-part interface may cause through-thickness stress gradients in the part which causes the cured part to warp away from the tool. The effect is strongly dependent on the length and thickness of the laminate.
4) Cure gradients	• Fast heat-up rates • Poor heat transfer • Thick tools/parts	May cause stress gradients and warpage of the part due to differential shrinkage and stress build-up through-thickness.

Given the large variation in size and geometry of the different products it was decided that two representative geometries should be modeled, one large and one small. For dimensions see Table 2.

Table 2. Approximate geometries for the large and small products modeled (Figure 1).

Product	Radius R (mm)	Sector angle ϕ (°)	Sector length $R\cdot\phi$ (mm)	W (mm)	H (mm)	t (mm)
Large	2770	50.5	2440	50	20	4
Small	1500	50.5	1320	50	20	4

Before developing detailed models, the effect of the four mechanisms listed in Table 1 on dimensional control can be estimated using simplified analysis.

Tool growth

When a tool is heated up an amount ΔT, all dimensions will increase proportionally. For this product, tool growth in the ϕ and z directions are not important as they can easily be trimmed to size in those dimensions, after the part is cured. However, the radial growth of the tool cannot be addressed in this way. When the tool is in its expanded state, the part cures and sets in shape. During cool-down to room temperature, the part will shrink due to thermal contraction. If the coefficient of thermal expansion (CTE) of the tool and part (in the direction of interest) are different, they will not have the same dimensions at room temperature. To estimate the radius of the cured part at room temperature, R_P, compares to the radius of the room temperature tooling, R_T, consider the following sequence of events: initial tool radius, R_T, tool growth due to thermal expansion, ΔR_T, part sets in place, radial contraction of the part due to thermal contraction, ΔR_P, giving final radius R_P:

$$R_P = R_T + \Delta R_T - \Delta R_P \tag{1}$$

$$\Delta R_T = R_T CTE_T \Delta T \tag{2}$$

$$\Delta R_P = R_T \left(1 + CTE_T \Delta T\right) CTE_P \Delta T \tag{3}$$

$$R_P = R_T \left(1 + CTE_T \Delta T\right)\left(1 - CTE_P \Delta T\right) \tag{4}$$

CTE_T, and CTE_P are the tool and part coefficients of thermal expansion in the radial direction, and $\Delta T = T_{cure} - T_{room}$. From eq. (4) we see that if the tool CTE is larger than the part CTE in the radial direction (as in the case with this product as processed on steel tooling), $R_P > R_T$, and the room temperature radius is greater than that of the room temperature process tool. Thus, to get a desired part radius at room temperature R_P, the room temperature tool radius R_T should be:

$$R_T = \frac{R_P}{\left(1 + CTE_T \Delta T\right)\left(1 - CTE_P \Delta T\right)} \tag{5}$$

In the current case, we have that $CTE_T \approx 12.6*10^{-6}$ (/°C), $CTE_P \approx 4.10*10^{-6}$ (/°C), and $\Delta T = 160$ °C, which gives that

$$R_T \approx 0.99863 \cdot R_P \tag{6}$$

Thus the tool radius should be approximately 0.137% less than the design radius. The tool CTE was taken from a tool design manual and the part CTE was calculated using laminated plate theory, based on ply CTE properties and the lay-up orientation.

Note that the difference in tool and part radius is not the same as the gap δ between the two if the part is placed back on the tool after cure, see
Figure 2. This gap δ is also the gap seen when fitting up to the mating structure during assembly.

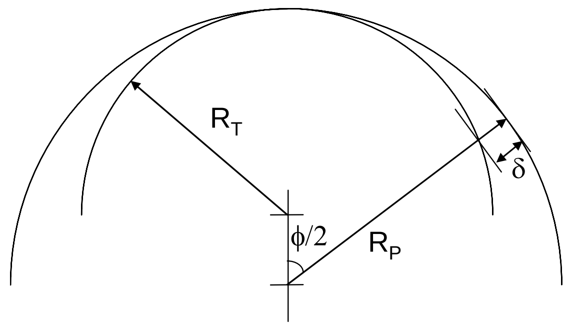

Figure 2. Gap δ between tool and part.

Geometry gives that the gap δ is related to R_P, R_T, and the angle of the circle sector ϕ as follows:

$$\delta \approx (R_P - R_T)\cdot\left(1-\cos\frac{\phi}{2}\right) \tag{7}$$

With a sector angle of 50.5°, the gap $\delta \approx (R_P - R_T)\cdot 0.1$, thus the gap is only 10% of the difference in radii in the current case.

Spring-in and out-of-plane deformations

Due to a larger shrinkage through-thickness than in-plane, curved laminates will tend to "spring-in" which leads to a reduction in enclosed angles. For laminates with a single and constant curvature this change in angle can be estimated from the following equation [1]

$$\Delta\theta = \theta_0 \frac{(\varepsilon_{IP} - \varepsilon_T)}{(1+\varepsilon_T)} \tag{8}$$

, where ε_T and ε_{IP} are the strains in the through-thickness and in-plane directions, respectively.

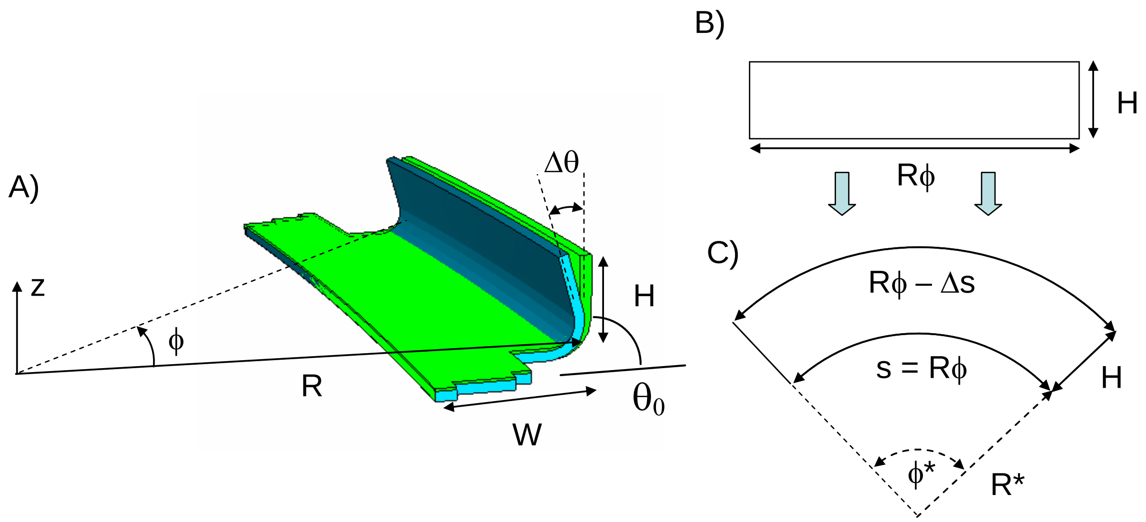

Figure 3. Spring-in deformation illustrating the coupling between flange spring-in Δθ and out-of-plane deformation, causing a radius of curvature R*.

Because of the curvature R, flange spring-in cannot occur freely. If the flanges spring-in an amount Δθ (Figure 3) we have from geometry that

$$\Delta R = -\Delta\theta \cdot z \tag{9}$$

$$\Delta s = -\Delta R \cdot \phi = -\Delta\theta \cdot z \cdot \phi \tag{10}$$

, where ΔR is the change in radius and Δs the change is circumferential distance s = Rϕ. Equation (10) suggests that there will be increasing in-plane compression of the flange as we go from the corner towards the tip of the flange (z = H). The energy required to compress the flanges according to eq. (10) is high, but warpage of the web out of the R-ϕ plane provides a means of accommodating the flange spring-in without compressing the flange (Figure 3). Geometry arguments give

$$R \cdot \phi \approx R^{*} \cdot \phi^{*} \tag{11}$$

$$H \cdot \phi^{*} \approx \Delta\theta \cdot H \cdot \phi \tag{12}$$

$$R^{*} \approx \frac{R}{\Delta\theta} \tag{13}$$

Equation (13) shows the approximate relationship between flange spring-in Δθ and out-of-plane radius R* because of the product curvature R. The resulting out-of-plane deflection δ can be calculated from

$$\delta \approx R^* \cdot \left(1 - \cos\frac{\phi^*}{2}\right) \tag{14}$$

Tool-part interaction

An analytical model, based on an experimental study on similar tooling and prepreg materials [2], gives that for flat laminates, the maximum warpage δ induced by tool-part interaction is given by

$$\delta_{flat} \approx 1.5 * 10^{-8} \frac{L^3}{t^2} \tag{15}$$

where δ, L, and t are all in meters. δ is the maximum gap between the tool and the cured warped part of length L and thickness t, when the part is placed back on the process tool. We can use this expression to estimate whether warpage due to tool-part interaction is a major driver. Considering Figure 1, we see that the longest continuous dimension in contact with the tool is the sector length R·ϕ. For the large products, we have that the sector length L ≈ 2.4 m and the thickness t ≈ 0.004 m (Table 2)

$$\delta_{flat} \approx 1.5 * 10^{-8} \frac{L^3}{t^2} \approx 1.5 * 10^{-8} \frac{2.4^3}{0.004^2} \approx 0.013(m) \tag{16}$$

This is the expected warpage for a flat part. However, this does not account for the stiffening effect of the flange on out-of-plane deformation. A rough estimate of this effect gives

$$\delta_{actual} \approx \delta_{flat} \cdot \left(\frac{4}{20}\right)^3 \approx 0.0001(m) \tag{17}$$

, which is insignificant, especially as the product can fairly easily be pushed back into shape in the R-ϕ plane.

Cure and thermal gradients

Large cure gradients through-the-thickness can create gradients in residual stress and thereby warpage of cured laminates. Large cure gradients require large temperature gradients through the laminate that are sustained for an extended period of time. A simplified 1D analysis of the thermal gradients within part and tool during a cure cycle has been presented in the literature [3]. Using this analysis to current tool materials, tool and part thicknesses, and autoclave heat transfer characteristics, it is seen that temperature gradients are relatively small in the current case and they are not sustained for any significant length of time. Thermocouple data from a prototype part supported this analysis, and cure gradients were ruled out as a significant contributor for dimensional control.

Summary of mechanisms

The simplified analysis showed that the main mechanisms for process-induced deformation were: tool growth – resulting in an increase in the part radius compared to the room temperature tooling, and flange spring-in/out-of-plane deformation.

RESULTS & DISCUSSION

Based on the simplified analysis several 3D thermo-elastic finite element models were created to better capture the discussed mechanisms and effects, and to better capture the actual detailed geometry of the products. Figure 4 shows predicted spring-down and flange spring-in from 3D thermo-elastic models.

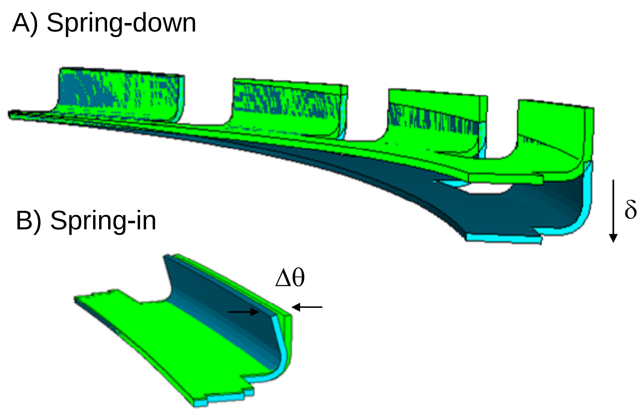

Figure 4. Predicted deformations from FE analysis A) Spring-down; B) Flange spring-in. Deformations are exaggerated for illustrative purposes.

Table 3 shows a summary of predicted flange spring-in and spring-down for the large and small products from closed-form and finite element (FE) analysis. The agreement between FE and closed-form analysis for flange spring-in is very good, whereas the closed-form analysis of spring-down is approximately 15% lower than FE results. The discrepancy in spring-down is not surprising as the closed-form analysis is rather approximate.

Table 3. Results from Finite Element and Closed form analysis.

Deformation	Large product	Small product
Flange spring-in (FE analysis)	0.78 °	0.76 °
Flange spring-in (Closed form analysis)	0.83 °	0.83 °
Spring-down (FE analysis)	4.3 (mm)	2.4 (mm)
Spring-down (Closed form analysis)	3.8 (mm)	2.0 (mm)
Radial growth (Closed form analysis)[a]	3.8 (mm)	2.1 (mm)

[a)] *Radial growth was observed in the finite element models but the magnitude was not quantified.*

One concern before the analysis work started was that the product may behave differently along the circumference as the lay-up orientation will vary in the ϕ-direction (Figure 1). Analysis of the lay-ups, however, showed that the in-plane and out-of-plane laminate CTE's are independent of the ϕ-coordinate, despite the varying lay-up orientation in the ϕ-direction. Because it is the laminate CTE in-plane and out-of-plane that drives the deformation, we don't expect any variation in flange spring-in, radial growth, or spring-down along the circumference of the product.

Based on the closed-form and finite element analysis results, it was recommended that the flanges should be compensated for a spring-in of 0.8°, and the radii according to eq. (6). Despite the large number of products with different geometries, thicknesses, and lay-ups, the developed understanding of the root causes of deformation resulted in a simple and straight-forward tool compensation recommendation.

To validate model predictions a prototype was manufactured and measured with a coordinate measuring machine (CMM). The CMM data was compared to the tool geometry and process-induced deformations calculated. All data analysis was performed by an external engineering firm (SAMMER Technologies). The prototype had fifteen "tabs" on each flange and the spring-in angle was calculated at three locations on each tab from the CMM data. Figure 5 shows a comparison of the measured and predicted spring-in of the fifteen tabs. The CMM data agrees well with model predictions, and validates the analytical conclusion that spring-in is independent of the ϕ-direction (tab #).

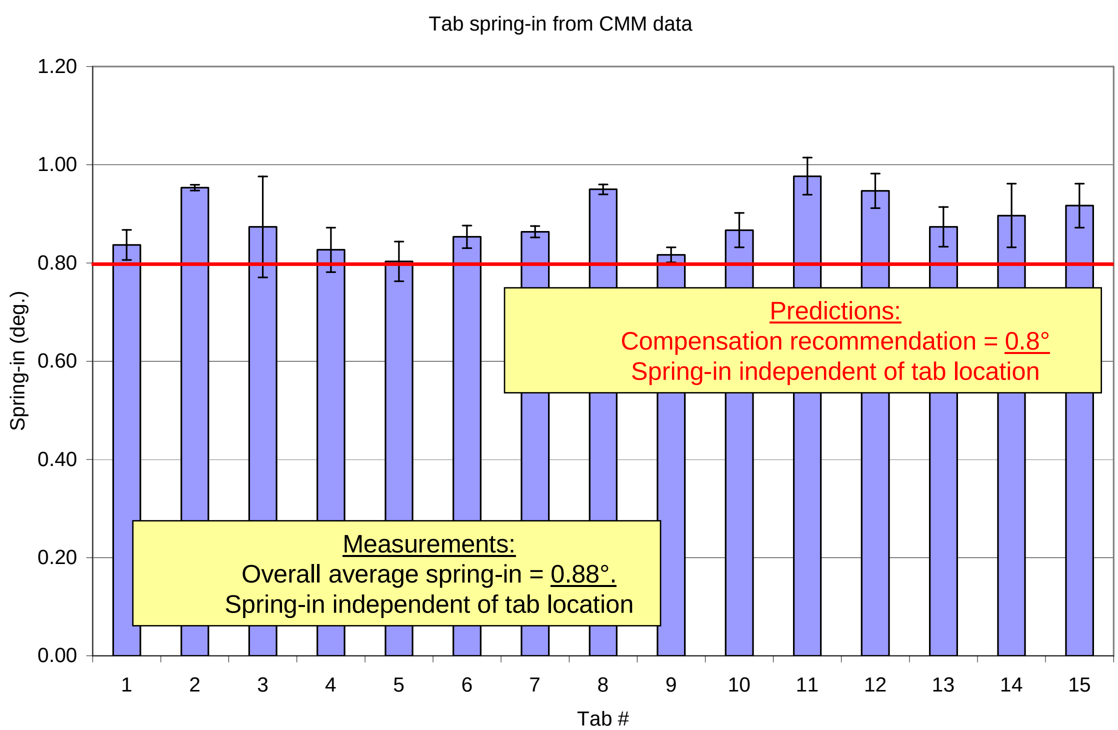

Figure 5. Comparison of predicted and measure flange spring-in. Three spring-in measurements were taken on each tab. The bars show the average value and the standard deviation.

The CMM data was not analyzed for spring-down as this mode of deformation was considered unimportant. The CMM data was analyzed for radial growth but the precision in the measurements was low. The CMM data showed that the radial growth was somewhere in the range of 0.48 to 0.71 mm, which compares well with the predicted growth of 0.66 mm. Radial growth was considered of minor importance, as the resulting gap is only about 10% of the radial growth, and the tools were only compensated for flange spring-in, with a constant compensation.

Note that flange or tab spring-in and the observed spring-down is only dependent on the properties and geometry of the product and that use of Invar tooling would not reduce this deformation. Radial growth would be less if they were processed on Invar tooling, but for the geometries of interest the effect was deemed not significant even on steel tooling.

CONCLUSIONS

This paper demonstrated how a systematic analysis approach to process-induced deformation can be used to identify root causes of deformation and accurately determine how they scale with part geometry and process parameters. The agreement between model predictions and test article data was very good. By combining the predictions of several models with test article data and experience, a simple but effective tool compensation scheme was developed.

ACKNOWLEDGEMENTS

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REFERENCES

There is an extensive body of literature on process-induced deformation of composite structures. Because this is an industrial case-study, only work that was directly used in the presented analysis is listed here.

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