Influences of residual stresses and initial distortion on springback prediction of 7B04-T651 aluminium plates in creep-age forming

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Abstract. Springback behaviour of surface-machined 7B04-T651 aluminium plates of 3 to 8 mm thicknesses, creep-age formed under a single curvature bending radius of 1200 mm, has been experimentally investigated. The surface residual stress levels of the plates have been measured and the typical residual stress distribution and values in 7000-series aluminium alloys have been reviewed and analysed. Finite element models have been developed to simulate creep-age forming (CAF) processes and predict the amount of springback in the physical CAF test. Machining-induced residual stresses and distortion have been considered in the finite element models. This paper presents a first attempt to study the effect of machining-induced residual stresses on the errors of springback prediction in CAF of 7000-series aluminium alloy panel components. It has been found that the effect of machining-induced residual stresses on the accuracy of springback prediction is related to the initial loading stress levels in CAF. In addition, when the initial distortion conforms to the loading curvature, an opposing effect on the material's deformation is observed.

Keywords: Creep-age forming, Machining-induced residual stress, Initial distortion, Aluminium plates, Springback prediction errors

Nomenclature

d	Depth below a machined plate surface where the maximum level of compressive residual stress is observed.
d_r	Displacement loading condition applied to the upper workheads of form tool.
h	Thickness of plate.
$k_{\varepsilon c}$	Creep-ageing material constant related to effective creep strain rate.
m_1	Creep-ageing material constant related to rate of age hardening.
m_2	Creep-ageing material constant related to the depletion of solute into precipitate.
m_3	Creep-ageing material constant related to rate of precipitate growth.
m_4	Creep-ageing material constant controlling the effect of dislocation density (or creep) on precipitate nucleation and growth.
m_5	Creep-ageing material constant related to rate of dislocation density.
n_d	Creep-ageing material constant related to rate of dislocation hardening.
r	Bending radius of forming tool.
$\bar{r}_p, \dot{\bar{r}}_p$	Normalised precipitate size and its rate term respectively.
$\bar{r}_p^0$	Initial value for normalised precipitate size.
r_w	Radius of revolvment of workheads of the flexible form tool.
t	Creep-ageing time.
t_1, t_2, t_3, t_4	Time of loading, heating, cooling and unloading respectively.
t_{AA}	Artificial ageing time.
t_{SHT}	Solution heat treatment time.
u_x, u_y, u_z	Translational degrees of freedom in Cartesian coordinates.
ur_x, ur_y, ur_z	Rotational degrees of freedom in Cartesian coordinates.
ν	Poisson's ratio.
x, y, z	Cartesian coordinates.
z_{ls}, z_{us}	z positions of the lower and upper splines respectively.
A_1	Creep-ageing material constant related to effective creep strain rate.
A_2	Creep-ageing material constant related to rate of dislocation hardening.
A_3	Creep-ageing material constant related to rate of dislocation density.
B_1	Creep-ageing material constant related to effective creep strain rate.
C	Constant for optimising a machining-induced residual stress profile.
C_{10}	Temperature-dependent material constant of a neo-Hookean elastomer.
C_a	Creep-ageing material constant related to the interaction between dislocations and shearable precipitates.

C_r	Creep-ageing material constant related to rate of precipitate growth.
C_s	Creep-ageing material constant related to the size, modulus and electronic mismatch of the solute.
C_p	Creep-ageing material constant related to rate of dislocation density.
D_1	Temperature-dependent material constant of a neo-Hookean elastomer.
E	Young's modulus of elasticity.
Q_r	Creep-ageing material constant representing the saturation limit for depletion of zinc solute atoms within an aluminium matrix.
R^2	R-squared in statistics.
T	Creep-ageing temperature.
T_0	Room temperature.
T_{AA}	Artificial ageing temperature.
T_{SHT}	Solution heat treatment temperature.
U	Strain energy per unit of reference volume of a neo-Hookean elastomer.
X	Operation domain of a machining-induced residual stress profile.
α	Coefficient of thermal expansion.
δ_0	Initial centre deflection of initially distorted plate.
δ_f	Final centre deflection of creep-age formed plate.
$\delta_f^{Exp}, \delta_f^{FE}$	Experimental and FE-simulated final centre deflections of formed plate respectively.
$\epsilon_e^{cr}, \dot{\epsilon}_e^{cr}$	Effective creep strain and its rate term respectively.
γ_r	Creep-ageing material constant controlling the effect of dislocation density (or creep) on precipitate nucleation and growth.
$\bar{\rho}_d, \dot{\bar{\rho}}_d$	Normalised dislocation density and its rate term respectively.
$\bar{\rho}_d^0$	Initial value for normalised dislocation density.
$\sigma_{age}, \dot{\sigma}_{age}$	Age hardening contribution to yield strength and its rate term respectively.
σ_{age}^0	Initial value for age hardening.
$\sigma_{dis}, \dot{\sigma}_{dis}$	Dislocation hardening contribution to yield strength and its rate term respectively.
σ_e	Von Mises equivalent stress.
σ_{res}	Machining-induced residual stress.
σ_{res}^{comp}	Maximum compressive stress level of a machining-induced residual stress profile.
σ_{res}^{tens}	Maximum tensile stress level of a machining-induced residual stress profile.
σ_{res}^{surf}	Surface stress level of a machining-induced residual stress profile.
$\sigma_{sol}, \dot{\sigma}_{sol}$	Solid solution hardening contribution to yield strength and its rate term respectively.
σ_{sol}^0	Initial value for solid solution hardening.
σ_x, σ_y	Transverse stress and longitudinal stresses respectively.

$\sigma_{yld}, \sigma_{yld}^0$ Current and initial yield strength respectively.

Δ Absolute difference between the experimental and FE-simulated final centre deflection of plates, $\Delta = |\delta_f^{Exp} - \delta_f^{FE}|$.

1. Introduction

Creep-age forming (CAF) refers to a process that concurrently shapes and heat-treats aluminium alloy components. During creep-ageing, internal stresses within the material relax due to creep whilst its microstructure and mechanical properties change due to ageing. CAF has many advantages in comparison with conventional forming techniques for forming large integrally stiffened panel parts (Eberl *et al.*, 2008; Zhan *et al.*, 2011a) and existing applications of CAF can be found in the production of aircraft wing skin panels and isogrid structures of space launch vehicles (Islamoff, 1965; Bilstein, 1980; Holman, 1989; Levers, 2003).

In CAF, the specific temperature-time combination of the creep-ageing thermal cycle is determined not for the shaping purpose, but to yield the desired mechanical properties of the formed component. As a result, internal stresses of the loaded component cannot be fully relaxed and springback is inevitable (Holman, 1989; Ho *et al.*, 2004a). In order to produce accurately shaped components that also satisfy the given strength requirement via CAF, collaborative efforts between the development of flexible tooling and process modelling techniques for springback compensation in CAF have been initiated (Levers, 2010; Zhan *et al.*, 2011a; Lam *et al.*, 2015a).

Accurate springback modelling for CAF requires the knowledge of stress states throughout the component immediately before unloading, which is controlled by the alloy's creep-ageing response. Unlike conventional creep, the primary creep stage is most important in CAF, during which the creep rate plummets with an ongoing ageing response. In order to enable this unique material behaviour of CAF to be captured, an early attempt has been made by Ho *et al.* (2004b) who developed a set of constitutive equations for modelling the creep-ageing behaviour of 7000-series aluminium alloys. The model is able to make predictions for creep deformation, precipitate radius growth and yield strength contributions from age hardening, dislocation hardening and solid solution hardening. A more recent material model developed by Zhan *et al.* (2011b) further introduces the concept of normalised precipitate size, normalised dislocation density as well as other key advancements. Both of these models are examples of physically based, unified creep-ageing constitutive equations that can be implemented into finite element solvers for CAF process modelling and springback prediction (Ho *et al.*, 2004a; Lin and Dean, 2005; Lin *et al.*, 2006; Zhan *et al.*, 2011b).

In addition to the alloy's deformation and hardening responses during creep-ageing, springback of sheet metals under the warm forming condition is also sensitive to various process parameters (Kim and Koç, 2008). More relevant of these to CAF are friction condition, thermally-induced distortions and design of tooling (Yang *et al.*, 2013). The use of finite element method further introduces numerical sensitivity to the problem in the form of numerical scheme, mesh density, element type and the number of through-thickness integration points in shell elements, to name but a few (Narasimhan and Lovell, 1999; Wagoner and Li, 2007; Wagoner *et al.*, 2013).

As for large aerospace panel parts, extra materials are removed via machining before CAF submission (Levers, 2003; Birch, 2005). Machining operations would generate geometric distortions and a high level of surface residual stresses, which has been studied extensively in terms of machining techniques (El-Khabeery and Fattouh, 1989; Rao and Shin, 2001; Ee *et al.*, 2005; Ulutan *et al.*, 2007; Denkena *et al.*, 2008; Tang *et al.*, 2009) but has not yet been considered in springback predictions for CAF. The machined plate normally contains different thickness at different sections. For example, the thickness of an aircraft wing skin panel can vary from less than 5 mm to up to over 30 mm within the same component (Levers, 2008). The effect of part distortion and surface residual stresses due to machining on springback has not been studied for CAF of aluminium plates of different thicknesses. The work described in this paper is the first time where a thorough understanding of this topic is provided through experimentation and finite element analysis.

In this paper, a brief analysis on the cause, key features and potential influence of machining-induced residual stresses is first presented. Details of a CAF test programme are then presented. The aim of carrying out the forming tests was to enable the evaluation of springback behaviour of surface-machined aluminium plates with different thicknesses. Finite element models have been developed for CAF process simulations with and without the consideration of machining-induced residual stresses and initial part distortion. The simulation results have been validated by the CAF test results, further simulations and analyses have been carried out and conclusions are given at the end of the paper.

2. Residual stresses in processed and machined 7000-series aluminium alloys

2.1. Cause

Figure 1 shows a typical processing route for 7000-series aluminium plates of T651 temper that had been subjected to surface machining prior to receive for CAF. The material is

subjected to solution heat treatment at temperature T_{SHT} for soaking time t_{SHT} and then quenched to room temperature (T_0) to produce a supersaturated solid solution. After stretching at T_0 for stress relief and first-stage-ageing at T_{AA} to make the microstructure stable, the residual stresses within the aluminium plate is very low, normally about 15 MPa in both the rolling and transverse directions (Prime and Hill, 2002; Robinson *et al.*, 2012).

Figure 1. A typical processing route for 7000-series aluminium plates in T651 temper that had been subjected to surface machining prior to receiving for CAF.

For aerospace skin applications, the T651-tempered component is further submitted to high-speed machining where typically over 90% of the initial material volume is removed for integral features such as stringers and strip surfaces to be produced (Munroe *et al.*, 2000). High level of surface residual stresses is generated as a result of the complicated thermal-mechanical loading conditions experienced by the component during machining. These residual stress profiles can be affected by the individual influence of, or interactions amongst, machining parameters (El-Khabeery and Fattouh, 1989), cutting temperature (Ulutan *et al.*, 2007), boundary and loading conditions (Denkena *et al.*, 2008), as well as the cutting tool's material (Rao and Shin, 2001), geometry (Ee *et al.*, 2005) and even its degree of wear (Tang *et al.*, 2009).

2.2. Key features

2.2.1. Machining-induced residual stress field

For ease of description, the equations that together describe a symmetric machining-induced residual stress field, σ_{res} , are first listed. For $-h/2 \leq z \leq (-h/2 + d)$ (Young *et al.*, 2005)

$$\sigma_{res}(z) = \left(z + \frac{h}{2}\right) \left(\frac{\sigma_{res}^{surf} - \sigma_{res}^{comp}}{-d}\right) + \sigma_{res}^{surf}, \quad (1)$$

for $(-h/2 + d) \leq z \leq (h/2 - d)$

$$\sigma_{res}(z) = (\sigma_{res}^{comp} - C) \left[\frac{e^{-20(X+1)} + e^{+20(X-1)}}{2} (3X^2 - 1) \right] + C, \quad (2)$$

and for $(h/2 - d) \leq z \leq h/2$

$$\sigma_{res}(z) = \left(z - \frac{h}{2}\right) \left(\frac{\sigma_{res}^{surf} - \sigma_{res}^{comp}}{d}\right) + \sigma_{res}^{surf}, \quad (3)$$

where z is the through-thickness location, h is the plate thickness, σ_{res}^{comp} and σ_{res}^{surf} are respectively the maximum compressive stress and surface stress of σ_{res} , d is the depth where σ_{res}^{comp} is observed and C and X are contained in the following descriptions of Eqs. (1) – (3).

Figure 2 shows a typical σ_{res} profile that was reconstructed using Eqs. (1) – (3). In Figure 2, $\bar{z}$ is the normalised thickness such that $\bar{z} = z/h$.

Figure 2. Key features of a typical machining-induced residual stress profile through the normalised plate thickness.

σ_{res}^{comp} does not exist on the machined surfaces but within the subsurface region (Brinksmeier *et al.*, 1982). Eq. (1) describes the linear subsurface σ_{res} that grows from σ_{res}^{surf} on the bottom plate surface to σ_{res}^{comp} at a depth of d above the bottom surface. Young *et al.* (2005) investigated machining-induced residual stress and distortion and a family of basis functions was proposed in order for them to determine the possible stress distributions that could have led to the distortions they observed in their experiments. One of the basis functions was adapted in Eq. (2) and forms the core of σ_{res} . The basis function operates

within the domain $-1 \leq X \leq 1$, exhibits large magnitudes near the machined surfaces and decays exponentially to small values towards the plate centre – these are the typical features of machining-induced residual stresses. The constant C is required for σ_{res} to satisfy force and moment equilibria. Finally, Eq. (3) describes the linear subsurface σ_{res} for the top surface of the plate.

2.2.2. Quantification of key features

Table 1 summarises the values that quantify the key features of σ_{res} based on literature findings. Results are tabulated for the average residual stress values in σ_{res} (with standard deviation) due to machining operations performed on 7000-series aluminium alloys.

Table 1. A summary of the average (standard deviation) surface, maximum compressive and maximum tensile residual stress levels due to the surface machining operations performed on 7000-series aluminium alloys by the referenced studies.

σ_{res}^{surf} (MPa)	σ_{res}^{comp} (MPa)	σ_{res}^{tens} (MPa)	References
25 (97)	-155 (86)	14 (18)	Denkena et al. (2008) ; Tang et al. (2009) ; Huang et al. (2013)

The σ_{res}^{comp} usually appears at $d = 22 \pm 8 \mu\text{m}$ below a machined surface with its magnitude significantly greater than σ_{res}^{surf} and σ_{res}^{tens} . The latter two are on average in a low tensile state but can also be compressive at times; this is particularly so for the σ_{res}^{surf} which exhibits a wide range of variation across stress states in the literature.

2.3. Potential influence

Initial deformation experienced by components in CAF is predominantly elastic and viscoplastic deformation is dependent on the current internal stress state of the component ([Zhan et al., 2011b](#)). Figure 3a shows the through-thickness schematic of a plate with the above-described σ_{res} profile. As a reference, the typical σ_{res}^{comp} in a surface-machined 7000-series aluminium alloy is around -155 MPa (Table 1) and it has been reported that the creep activation von Mises equivalent stress of a similar alloy can be as low as 60 MPa ([Lam and Lin, 2014](#)). Figure 3b and Figure 3c show respectively the elastic loading and loaded stress profiles of the plate during CAF. Because of the presence of a high level, asymmetric (about the vertical axis) residual stress field, the shape of the elastic loading stress profile has been

disturbed and position of the neutral surface has been shifted. It was hypothesised that such σ_{res} -induced stress disturbances could lead to a change in the amount of creep strain accumulation in CAF, which may eventually influence springback prediction; and it is the purpose of this paper to report an investigation into this.

Figure 3. Through-thickness schematics of (a) a typical machining-induced residual stress profile, (b) elastic bending stress and (c) the resultant stress profile in a fully loaded plate with residual stress.

3. Experimental details

3.1. Material

7B04-T651 aluminium plates of dimensions 600 mm $\times$ 350 mm in 3 to 8 mm thicknesses were supplied by AVIC BAMTRI (Beijing, PR China) in the longitudinal rolling orientation. Plate billets of 25 mm thickness had undergone solution heat treatment at 475 °C for 1 h followed by water quenching, 1.5% stretching for relieving residual stresses and artificial ageing at 115 °C for 24 h to get peak strength. The billets were then wire-cut through the thickness to approximate the requested thicknesses. The cut plates were eventually surface-

machined prior to delivery. Figure 4a shows the gridlines that were drawn on all the plates upon receiving them. An average thickness value (h) was obtained for each plate based on twenty measurements that were recorded at the gridline positions along the four edges of the plate using a pair of digital Vernier callipers.

Figure 4. Schematics of (a) the gridlines drawn on the plates and (b) initial bending distortion and the locations where initial deflections were measured. Units are in millimetres.

3.2. Initial distortion measurements

All the plates had a various degree of initial distortion due to the presence of residual stresses after machining. The major mode of initial distortion was single curvature bending. In order to characterise the initial bending distortion, vertical differences between the y -plate-centre and the gridline positions, as depicted in Figure 4b, were measured using a digital height gauge. Three sets of measurements were obtained from each side of the plates at $x = \pm 175$ mm, which results in twelve sets of half-length measurements. The results were then averaged to yield an average initial deflection profile for half a plate.

3.3. Surface residual stress measurements

Surface residual stress measurements were performed using a Pulstec μ -X360 compact x-ray residual stress analyser (Pulstec Industrial Co Ltd, Wales, UK). The instrument has a penetration depth of 10 μ m from the surface and analysis was based on the diffracted x-rays that were detected by an area detector. Strains were first determined from the acquired

Debye-Sherrer ring and it was followed by the calculation of residual stress using the cosine α method (Woodward, 2014).

3.4. Creep-age forming tests

A flexible CAF tool, constructed based on the design method proposed by Lam *et al.* (2015a), was used for the CAF tests in this study. Figure 5a shows the CAF tool design and forming test setup with the top and bottom die holders each holding twenty-five forming pins. Further details of the CAF tool design and CAF test setup will become apparent as the corresponding numerical model is presented in a subsequent section. A 250 ton Instron (Norwood, MA, USA) servo-hydraulic testing machine was used for loading and unloading. Heat treatment cycles were carried out in a fan-assisted Aerotherm (Staffordshire, England, UK) box oven with a built-in digital microprocessor and independent overheat controls. A digital thermometer was used to monitor the temperature of the plates during creep-ageing.

Figure 5. (a) The creep-age forming tool and test setup. (b) Definitions of the coordinate system, bending radius (r), initial centre deflection (δ_0) and final centre deflection (δ_f). (c) Final curvatures of three of the formed plates in (#1) 3.2, (#4) 5.3 and (#7) 8.2 mm thicknesses.

CAF tests were performed on all the plates using the same tooling setup which was designed to produce a single curvature bending radius (r) of 1200 mm. As illustrated in Figure 5b, the plates were positioned in such a way that the initial bending curvature always conforms to the loading curvature r . During loading, the punch velocity was 4 mm/min. During creep-ageing, the recorded plate temperature was maintained at $115 \pm 2^\circ\text{C}$. After unloading, final centre deflections (δ_f^{Exp}) of the unloaded plates were measured for springback to be characterised (Figure 5b). Only the profile shape between $y = \pm 240$ mm was considered as the outer portions of the plates were outside the forming region of the CAF tool.

4. Experimental results

An average surface residual stress value of $\sigma_{res}^{surf} = -43 \pm 19$ MPa was measured which is closer to the compressive end of the literature range (Table 1). Table 2 summarises the measured values of h , initial deflections and δ_f^{Exp} of the plates. The initial deflections measured at $y = 240$ mm are equivalent to δ_0 in Figure 5b. The thinner plates tend to have larger initial deflections. No apparent trend is observed by making comparisons between h and δ_f^{Exp} but the latter seems to have a certain degree of correlation with the initial deflections of the plates. Figure 5c shows the final curvatures of three of the formed plates (#1, #4 and #7) in different thicknesses.

Table 2. Average thickness, initial deflections and final deflection of the plates.

#	h (mm)	Initial deflections (mm) at				δ_f^{Exp} (mm)
		$y = 0$ mm	$y = 120$ mm	$y = 240$ mm	$y = 300$ mm	
1	3.20	0	0.67	1.75	2.42	3.27
2	3.26	0	0.75	1.95	2.70	3.76
3	4.88	0	0.50	2.50	3.50	4.99
4	5.30	0	0.65	2.38	4.97	4.91
5	5.31	0	0.06	0.10	0.12	2.53
6	8.09	0	0.05	0.10	0.13	3.57
7	8.20	0	0.35	0.70	0.88	4.51

5. Finite element modelling

Finite element (FE) method was chosen for its past successful applications demonstrated in the modelling of creep-ageing behaviour (Zhan *et al.*, 2011b), residual stresses (Koç *et al.*, 2006) and springback of sheet metal parts (Ho *et al.*, 2004a). Figure 6 illustrates a flowchart of the FE modelling process and detailed descriptions of the individual procedure are contained in the following subsections.

Figure 6. Flowchart of the finite element modelling process.

5.1. Geometry and mesh

Figure 7 shows an FE model of the flexible CAF tool constructed in Abaqus (v6.11, Dassault Systèmes Simulia, Providence, RI, USA). Only workheads of the forming pins were modelled which are assumed to be perfectly spherical with a radius of $r_w = 14.65$ mm. The workheads were modelled using four-node discrete rigid elements R3D4.

In order to study both the individual and combined effects of residual stresses and initial distortion on springback, three cases were simulated under the conditions presented in Table 3. As further detailed research will be needed to provide scientific insights into the relationship between initial distortions, residual stress fields as well as the corresponding processes and processing conditions that caused them, the possible relationship between them is not considered in this work. This is achieved by modelling the initial distortion and residual stress field in the form of geometric model and initial stress field respectively. Whilst details about the latter is provided in a subsequent section, the flat plates were modelled using flat shells built in the conventional way whilst initial distortion (ID) and initial distortion with the consideration of residual stresses (ID-RS) of the plates were taken into account by creating a unique geometric model for each plate. For each ID or ID-RS plate model, four points were created, each point was positioned to the yz coordinates that correspond to the measured

initial deflections (Table 2), the four points were connected using a spline function and the connected spline was extruded to a depth of 175 mm in the x direction to represent the width of a quarter plate.

Figure 7. (a) Isometric view of the finite element model. (b) Finite element mesh of a workhead with an arbitrary revolving axis (dashed line) and the radius of revolvment, r_w . (c) xz plane view of the finite element model with the different parts illustrated.

Table 3. Simulation conditions of the three cases.

Case	Initial distortion	Residual stress
Flat	No	No
ID	Yes	No
ID-RS	Yes	Yes

A summary of the element and geometric properties assigned to the different parts are presented in Table 4. Mesh convergence studies were carried out on one-mesh-at-a-time on the deformable meshes of the splines, elastomeric sheet and panel component. The final conditions of the convergent FE meshes are presented in Table 5. Convergences were achieved for the shape of the splines and both the shape and stress state of the panel component. For the workheads which were modelled as rigid bodies, the mesh of each workhead was globally seeded with an approximate global size of $0.05 r_w$ to ensure smooth contact surfaces.

Table 4. Element and geometric properties assigned to the different parts.

Part name	Element type	$x \times y \times z$ dimensions (mm)	Number of through-thickness Simpson's integration points
Upper workheads	R3D4	-	-
Upper splines	S4R	$35^* \times 300 \times 3$	31
Panel component	S4R	$175 \times 300 \times h$	99
Elastomeric sheet	C3D8RH	$175 \times 300 \times 12$	-
Lower splines	S4R	$35^* \times 300 \times 3$	31
Lower workheads	R3D4	-	-

* The widths of the upper and lower splines in the middle along $x = 0$ are 17.5 mm due to x -symmetry.

Table 5. Number of elements and equivalent approximate global size of the convergent meshes.

Mesh	Number of elements	Approximate global size (mm)
Full splines	420	5
Elastomeric sheet	2900	6
Panel component	2100	5

5.2. Positioning and boundary conditions

A graphical illustration of the model positions and boundary conditions applied is shown in Figure 8. Nine workheads were positioned in the xy plane to match a quarter of the physical tool design; a centre workhead was first positioned at $x = y = 0$ and the other workheads were positioned in a way so that they are spaced equidistantly by $x = 60$ mm and $y = 120$ mm in the final configuration.

As illustrated in Figure 8a and Figure 8b, symmetric boundary conditions were applied along the lines of axes $x = 0$ and $y = 0$. An additional boundary condition, $u_x = 0$, was assigned to the centre lines of the full splines to avoid slippage to occur in the x direction. Boundary conditions of the rigid workheads were assigned through their reference points, which are at the tip of each workhead (Figure 8c). The lower workheads were all assigned with an encastré boundary condition and z -positioned according to the pin height requirement for $r = 1200$ mm. Similarly, the upper workheads were fixed in all directions except in z , as each upper workhead were z -positioned and assigned with a unique displacement loading condition, d_r , specific to $r = 1200$ mm.

Figure 8. Positions and boundary conditions of (a) the panel component, (b) upper and lower splines and (c) an arbitrary pair of workheads (positions not specified).

5.3. Material properties

Table 6 summarises the material properties assigned to the different parts of the FE model where E is the Young's modulus, ν is the Poisson's ratio and α is the coefficient of thermal expansion. The splines were modelled as spring-tempered steel and are not expected to experience any plastic deformation. The elastomeric sheet has a Shore A hardness of 60 and was modelled as an isotropic hyperelastic material using the neo-Hookean strain energy potential, U , which requires the input of two material parameters, C_{10} and D_1 (Abaqus, 2011).

Experimental and FE characterisation performed on the rubber and polyurethane under tension and compression at a low strain rate of 0.05 s^{-1} suggest that the value of C_{10} for elastomers of the same hardness grade ranges between 1.3 and 1.6 MPa (Lam *et al.*, 2011). A value of $C_{10} = 1.45 \text{ MPa}$ was assigned to the elastomeric sheet and $D_1 = 0 \text{ MPa}$ which implies full incompressibility.

Table 6. Material properties assigned to the different parts.

Part name	Material	E (GPa)	ν	α (K^{-1}) (Bhushan, 2000)
Upper workheads	-	-	-	-
Upper splines	Spring steel	210	0.3	1.3E-5
Panel component	7B04-T651	67	0.33	2.3E-5
Elastomeric sheet	Silicone rubber	-	-	2.5E-5
Lower splines	Spring steel	210	0.3	1.3E-5
Lower workheads	-	-	-	-

The panel component was modelled as aluminium alloy 7B04-T651 with its creep-ageing behaviour described by Eqs. (4) – (10) and implemented via the user-defined subroutine CREEP.

$$\dot{\epsilon}_e^{cr} = A_1 \sinh\{B_1[\sigma_e(1 - \bar{\rho}_d) - k_{ec}\sigma_{yld}]\} \quad (4)$$

$$\dot{\sigma}_{age} = C_a \dot{\bar{r}}_p^{m_1} (1 - \bar{r}_p) \quad (5)$$

$$\dot{\sigma}_{sol} = -C_s \dot{\bar{r}}_p^{m_2} |\bar{r}_p - 1| \quad (6)$$

$$\dot{\sigma}_{dis} = A_2 \cdot n_d \cdot \bar{\rho}_d^{n_d-1} \dot{\bar{\rho}}_d \quad (7)$$

$$\sigma_{yld} = \sigma_{sol} + \sqrt{\sigma_{age}^2 + \sigma_{dis}^2} \quad (8)$$

$$\dot{\bar{r}}_p = C_r (Q_r - \bar{r}_p)^{m_3} (1 + \gamma_r \bar{\rho}_d^{m_4}) \quad (9)$$

$$\dot{\bar{\rho}}_d = A_3 (1 - \bar{\rho}_d) \dot{\epsilon}_e^{cr} - C_\rho \bar{\rho}_d^{m_5} \quad (10)$$

In Eqs. (4) – (10), $A_1, B_1, k_{ec}, C_a, m_1, C_s, m_2, A_2, n_d, C_r, Q_r, m_3, \gamma_r, m_4, A_3, C_\rho, m_5$ are material constants. This equation set was proposed by Zhan *et al.* (2011b) for describing concurrently the viscoplastic deformation, microstructural evolution and hardening mechanisms of 7000-series aluminium alloys in CAF. Whilst detailed descriptions of the

equation set should be referred to [Zhan *et al.* \(2011b\)](#), the initial conditions and material constants used are presented in Table 7. The tabulated values had been determined by [Lam *et al.* \(2015b\)](#) using test-pieces produced from the same batch of materials as those of the plates in this study.

Table 7. Initial conditions and material constants for CAF of 7B04-T651 at 115°C for up to 22 h ([Lam *et al.*, 2015b](#)).

Initial conditions								
σ_{sol}^0 (MPa)			σ_{age}^0 (MPa)	σ_{yld}^0 (MPa)	$\bar{r}_p^0$	$\bar{\rho}_d^0$		
190			347	537	1	1E-5		
CAF material constants								
A_1 (h ⁻¹)	B_1 (MPa ⁻¹)	$k_{\varepsilon c}$	C_a (MPa)	m_1	C_s (MPa)	m_2	A_2 (MPa)	n_d
1.95E-6	0.042	0.16	4	0.9	0.2	0.45	260	0.7
C_r (h ⁻¹)	Q_r	m_3	γ_r	m_4	A_3	C_ρ (h ⁻¹)	m_5	
0.098	1.8	2.2	3	2	150	0.145	1.5	

5.4. Contact scheme

Tangential behaviour was modelled using isotropic penalty formulation with friction coefficients of 0.2 and 0.25 assigned between the elastomer-metal ([Dirikolu and Akdemir, 2004](#)) and aluminium-steel ([Foster *et al.*, 2009](#)) interactions respectively. The accuracy of these friction coefficients is not known but they are used for this work due to the absence of any other data available.

5.5. Residual stress fields

The residual stress field used in this study was reconstructed and quantified based on the measured surface residual stress value (Section 4) and the related findings reported by researchers in the field (Section 2.2). This study further assumes that the stretching operation has eliminated most of the residual stresses in the plates prior to the surface machining operation. Therefore, the residual stress fields considered in this study is due only to the final finishing machining operation.

Residual stress fields of the plates were assigned via the through-thickness integration points (IPs) of the shell elements. In order to ensure as accurate as possible a representation of the residual stress fields, the Simpson's rule was employed with ninety-nine IPs which is the largest number that Abaqus supports. The restriction on the number of IPs in Abaqus has

two implications. (i) An exact value of d cannot be assigned but varies with h ; this was $d = h/99$. The potential effect of this difference in d was assumed to be negligible due to the small range of h variation in this study (3.20 to 8.20 mm, which gives at most a 51 μm difference in d). (ii) The residual stress field, σ_{res} , cannot be represented by a single equation – the surfaces and core values of σ_{res} were assigned separately as a result. For the first and the ninety-ninth IPs, a value of $\sigma_{res}^{surf} = -43$ MPa (the measured average) was directly assigned. For the second to the ninety-eighth IPs, which together represent the core of σ_{res} , the following procedure was followed: (i) Implement Eq. (2) into MATLAB (MathWorks, Natick, MA, USA) and assign it with $\sigma_{res}^{comp} = -155$ MPa (the literature average in Table 1). (ii) Split the core of σ_{res} evenly into ninety-seven data points.

The same σ_{res} profile was assigned to the transverse and longitudinal directions of the plate as the initial σ_x and σ_y via the user-defined subroutine SIGINI. A pilot simulation was run for each case in a geostatic step to establish force and moment equilibria for the initial σ_x and σ_y . During the geostatic step, redistributions of σ_x and σ_y occur until equilibria are satisfied. At the same time, further distortions may occur depending on the difference between the initial and final converged stresses. Since initial distortions of the plates were already taken into account by the geometric models, the distortions that arise during the geostatic step are effectively unwanted displacements. The constant C in Eq. (2) was therefore optimised so that as equilibria were satisfied, the change in maximum compressive stress due to stress redistribution and the maximum change in vertical displacement of the plate had to be smaller than 1 MPa and 1E-3 mm respectively. The final σ_{res} profile was reached for each plate upon satisfying both the equilibria and the criteria imposed on the change in maximum stress and vertical displacement at the same time.

5.6. Numerical procedure

A six-step modelling procedure was employed (Figure 6). Loading, heating, cooling and unloading were run in a general static step and creep-ageing was run in a visco step. As described earlier, the value d_r in loading and unloading is a unique loading condition applied to each upper workhead and equals to the distance between the reference points of any pair of upper and lower workheads minus the sum of thicknesses of the upper splines, panel component, elastomeric sheet and lower splines. Table 8 presents the variables assigned which describe the temperature and loading profiles of the simulations (Figure 9).

Figure 9. Schematics of the loading and temperature profiles.

Table 8. Variables that describe the temperature and loading profiles of the forming simulations.

Variable	(Unit)	Assigned value
t_1	(h)	0.1
t_2	(h)	4
t	(h)	22
t_3	(h)	4
t_4	(h)	0.1
T_0	(K)	295.15
T	(K)	388.15

6. Finite element modelling results

Figure 10 illustrates comparisons of the δ_f^{Exp} with those simulated using FE (δ_f^{FE}) under the three cases: Flat, ID and ID-RS. The plates were further categorised into two groups by their amount of initial deflections, δ_0 . Those in group A are the plates that have greater initial distortion ($\delta_0 > 1$ mm), these are the 3.20, 3.26, 4.88 and 5.30-mm-thick plates. The plates in group B are those with $\delta_0 \leq 1$ mm, which include the 5.31, 8.09 and 8.20-mm-thick plates.

Figure 10. (a) Initial and final centre deflections of the plates. (b) Absolute differences between the experimental and simulated final centre deflections.

Table 9 summarises averages of the absolute differences between the experimental and FE-simulated deflections ($\Delta = |\delta_f^{Exp} - \delta_f^{FE}|$) of the plates. δ_f^{FE} of the flat plates generally show greater deviations from δ_f^{Exp} (larger Δ) than the other cases (ID and ID-RS). This deviation is more apparent for the plates with larger δ_0 (group A). The plates in group A have an average δ_0 of 2.15 mm and an average Δ of 3.75 mm. On the other hand, for the plates in group B, which have an average δ_0 of 0.30 mm, an average Δ of 0.78 mm is recorded.

For case ID, which has initial distortions of the plates taken into account by the geometric models, the average Δ of group A has reduced by 51.5% (from that of case Flat) to 1.82 mm, that of group B has reduced less by 18.0% to 0.64 mm and that for all the plates has reduced by 46.8% to 1.32 mm.

Table 9. Group criterion, average initial deflection at $y = 240$ mm and average absolute differences between experimental and FE-simulated final centre deflections of the plates in group A and group B.

Group	Criterion	Average δ_0 (mm)	Average Δ (mm)		
			Flat	ID	ID-RS
A	$\delta_0 > 1$ mm	2.15	3.75	1.82	1.16
B	$\delta_0 \leq 1$ mm	0.30	0.78	0.64	0.50
All the plates	-	1.35	2.48	1.32	0.87

The Δ values are smallest when simulations were performed with both the initial distortions and residual stress fields taken into account (case ID-RS). The average Δ values have dropped further (from that of case ID) by 36.3% to 1.16 mm for group A, by 21.9% to 0.50 mm for group B and by 34.1% to 0.87 mm when all the plates are considered altogether.

7. Discussions

7.1. Influence of initial distortion on springback prediction

Taking into account initial distortions of the plates has improved the δ_f prediction for the plates in group A significantly by an average of 51.5%; whilst those for the plates in group B has also improved by an average of 18.0%. Although the plates in group B are overall thicker, it does not imply that springback prediction for thicker plates are less affected by δ_0 . Figure 11 shows the strong positive correlation ($R^2 = 0.98$) between the measured initial deflections of the plates (δ_0) and the predicted change in final centre deflections as a result of modelling initial distortions ($|\delta_f^{ID} - \delta_f^{Flat}|$). It can be seen that for the 8.20-mm-thick plate which has $\delta_0 = 0.70$ mm, $|\delta_f^{ID} - \delta_f^{Flat}|$ has a value of 0.38 mm and this combination correlates well with the rest of the data points in the plot. This suggests that, at least applicable to within the limit of this study ($r \geq 1200$ mm and $h \leq 8.2$ mm), the influence of initial distortion on springback prediction is not affected by the bend ratio, r/h . Rather, the overall greater improvement observed on the δ_f prediction for the thinner plates (group A: 51.5%) than those for the thicker plates (group B: 18.0%) is due to the fact that thinner plates in this study generally have greater δ_0 .

In the comparisons made between distortion measurements of a machined part and the corresponding distortion predictions, [Young et al. \(2005\)](#) found that machining-induced distortion takes on a very different shape from that due to bulk stress. More specifically, machining-induced distortion is dominated by asymmetric through-thickness shear stresses

that cause a net twisting effect. On the other hand, the major mode of distortion due to the material's bulk stress was bending, which resembles that of the as-received plates in this study. Their prediction results show in general larger machining-induced distortions for thinner parts, which could be one of the contributing factors behind the overall larger δ_0 measured from the thinner plates in group A.

Figure 11. The positive correlation between initial deflection and absolute difference between final centre deflections of the plates simulated under cases Flat and ID.

In order to examine the possible effects of initial distortions that have on the stress states of components in CAF, comparisons between the through-y stress distributions and creep strain accumulation of the 5.30 and 5.31-mm-thick plates are made. These comparisons enable better identification of the role of initial distortions alone as they have almost the same h but different δ_0 . Figure 12 shows, for the bottommost tensile surfaces of the plates, the von Mises equivalent stress (σ_e) after heating and the total accumulated effective creep strain (ϵ_e^{cr}) after 22 h of creep-ageing along y at $x = 30$ mm where there is no spline contact (Figure 8). In this study, the major mode of initial distortion of the plates is single curvature bending and the plates are loaded in a way such that the initial curvature always conforms to the bending curvature of the plates. As a result, the plate that has a greater δ_0 was effectively bent less to achieve the same r , leading to an overall lower σ_e state than the plate with less initial distortion.

Table 10 summarises the through-y average σ_e and ϵ_e^{cr} of the 5.30 and 5.31-mm-thick plates. The difference is calculated using $|A - B| / [(A + B)/2] \times 100$. The 10% higher overall σ_e state of the plate with a lower δ_0 translated to a significantly larger percentage difference in ϵ_e^{cr} (51.9%). Despite the less ϵ_e^{cr} accumulation in the plate with $\delta_0 = 2.38$ mm,

δ_f^{Exp} of the plate is still 2.38 mm greater than that of the plate with $\delta_0 = 0.10$ mm (Figure 10). Initial distortion therefore has opposing effects on the deformation in CAF; with initial distortions that conform to the bending curvature r , δ_f increases with increasing δ_0 (Figure 11), but at the same time reduces the amount of additional deformation that would otherwise be induced by creep (Figure 12).

Figure 12. Through-y von Mises equivalent stress after heating and total accumulated effective creep strain of the 5.30 and 5.31-mm-thick plates with 2.38 mm (solid lines) and 0.10 (dashed lines) initial deflections at $y = 240$ mm respectively. Results were obtained at $x = 30$ mm on the bottommost tensile surface of the plates simulated under case ID.

Table 10. Through-y average von Mises equivalent stress and effective creep strain of the two 5.3-mm-thick plates with different initial deflections.

δ_0 (mm)	Through-y average value of	
	σ_e (MPa)	ε_e^{cr} (mε)
0.10	119.3	0.17
2.38	107.9	0.10
Difference (%)	10.0	51.9

7.2. Influence of residual stress on springback prediction

When both the measured initial distortions and typical residual stress fields are modelled together (case ID-RS), springback of all the plates was predicted to a remarkably good accuracy of $\Delta = 0.87$ mm, which was 64.9 and 34.1% better than the predictions made under the cases Flat and ID respectively (Table 9).

However, Figure 10a shows that, except for the two thicker plates, the predicted δ_f^{ID-RS} are generally lower than δ_f^{Exp} . This overestimation of springback may be attributed to the following two reasons: (i) In this study, initial distortions are modelled via the initial

geometry of the FE model rather than the internal stresses that caused them. Therefore, although initial distortion has been taken into account, the internal stress states of the components prior to CAF submission remained underestimated. As viscoplastic deformation in CAF depends on the component's stress state (Eqs. (4) – (10)), the underestimation of stress state during CAF will translate to less deformation and springback overestimation. (ii) Due to the lack in the number of experimental investigations that report the magnitude of shear residual stresses in machined 7000-series aluminium alloys in the literature, only the initial σ_x and σ_y are taken into account in this study; this can also lead to an underestimation of component's stress state prior to CAF.

Figure 13 shows a good negative correlation ($R^2 = 0.85$) between the measured plate thickness (h) and the change in final centre deflections due to the modelling of residual stresses ($|\delta_f^{ID-RS} - \delta_f^{ID}|$). Since a constant bending radius of $r = 1200$ mm was used in this study, this correlation seems to suggest that the modelling of σ_{res} for accurate springback prediction becomes less important when a component is creep-age formed under a lower r/h ratio (or higher bending stress level). To ease the following descriptions, two additional terms are introduced: (i) the upper compressive load region (CLR) and (ii) the lower tensile load region (TLR). In all cases, the upper CLR and the lower TLR each take a portion of $h/2$ and are always divided by the plate centroid at $z = 0$ mm.

To elucidate the possible mechanisms behind the above observation of Figure 13, comparisons are made in Figure 14 between the through-thickness σ_e distributions of the thinnest 3.20-mm-thick (Figure 14a) and the thickest 8.20-mm-thick (Figure 14b) plates simulated under cases ID and ID-RS. σ_e distributions are shown for the three key stages of CAF: (i) fully loaded and heated, (ii) after 22 h of creep-ageing and cooling and (iii) released from loading. Additionally, Table 11 presents comparisons of the through-thickness average σ_e relaxation in the lower TLR and upper CLR of those plates.

Referring back to Figure 14a, which shows evolutions of the σ_e distributions computed for the 3.20-mm-thick plate, when σ_{res} prior to CAF was not included in the FE model (case ID), a linear through-thickness σ_e distribution is observed in the heated plate. In general, constrained heating introduces a through-thickness compressive stress state to the plate; σ_e in the upper CLR therefore has increased slightly when the plate is heated, whilst σ_e in the lower TLR has slightly decreased. Since the 3.20-mm-thick plate was elastically loaded and only a little σ_e relaxation took place during creep-ageing (Table 11), the final through-

thickness σ_e after the release of loading is almost negligible. This has resulted in the almost full springback of the plate upon release from loading for case ID (Figure 10a).

Figure 13. The negative correlation between plate thickness and absolute difference between final centre deflections of the plates simulated under cases ID and ID-RS.

When σ_{res} exists in the 3.20-mm-thick plate (case ID-RS), the overall σ_e relaxation was higher in the upper CLR and less in the lower TLR. In the upper CLR, more σ_e relaxation is observed in the region where it overlaps with the compressive portion of the input σ_{res} . There is negligible creep activation in the other upper CLR where it overlaps with the tensile core of the σ_{res} . The position of the minimum σ_e has shifted upwards by about 0.1 mm due to the tensile core feature of the σ_{res} . In the lower TLR, the positive contribution of the σ_{res} tensile core to the tensile bending stress was still insufficient to cause any observable creep-induced σ_e relaxation. The compressive portion of the σ_{res} reduced the overall σ_e state in the surface to subsurface region of the lower TLR, leading to the 31% less σ_e relaxation (Table 11). As a result, the σ_e in the lower TLR after tooling release takes almost the same shape as that of the σ_{res} . On the other hand, σ_e relaxation in the upper CLR has relieved the peak σ_e level by about 68 MPa whilst the general σ_e distribution still maintains the same basic shape as that of the σ_{res} . These phenomena have together resulted in a final σ_e distribution that is asymmetric about the plate centroid of the 3.20-mm-thick plate (Figure 14a).

Figure 14. Through-thickness von Mises equivalent stress distributions at the key stages of creep-age forming for (a) the 3.20 and (b) 8.20-mm-thick plates under cases ID (dashed lines) and ID-RS (solid lines). Results were obtained from the element at $x = 30$ mm and $y = 100$ mm.

Table 11. Through-thickness average von Mises equivalent stress relaxation in the lower tensile and upper compressive load regions (TLR and CLR) for the 3.20-mm and 8.20-mm-thick plates simulated under cases ID and ID-RS.

h (mm)	Case	Average σ_e relaxation in the lower TLR (MPa)	Change (%)	Average σ_e relaxation in the upper CLR (MPa)	Change (%)
3.20	ID	4.2		3.1	
	ID-RS	2.9	-31.0	4.1	+32.3
8.20	ID	15.4		17.7	
	ID-RS	12.6	-18.2	22.4	+26.6

The bending stress level of the 8.20-mm-thick plate is relatively high and has about 250 MPa of maximum σ_e on the surface even when σ_{res} was not included in the model (Figure 14b). In the plate where there is no σ_{res} (case ID), there exists a clear pure elastic

region where no creep has occurred and an outer creep region where creep has been activated. This is a typical feature of thick plates in stress-induced creep forming condition, under which the elastic core is almost impossible to be completely eliminated (Ho *et al.*, 2004a). The compressive portion of the σ_{res} has increased the amount of σ_e relaxation in the upper CLR by a maximum of 97.2 MPa. The tensile portion of the σ_{res} has shifted the position of minimum σ_e by almost the same amount of absolute distance as it did in the thinner plate. However, the shift is now relatively small due to the increased h . Of all the plates studied, the 8.20-mm-thick plate has the least differences in the σ_e distributions between the two cases when σ_{res} was (ID-RS) and was not modelled (ID). This is due to the higher bending stress level in the thicker plate relative to the constant σ_{res} field; this suggests that the influence of residual stress on springback reduces with reducing r/h ratio, which supports the observation in Figure 13.

7.3. Asymmetric through-thickness stress relaxation

The behaviour of asymmetric stress relaxation across the thickness of plates is apparent from the results tabulated in Table 11. To date, although the phenomenon of asymmetric stress relaxation had been observed in industrial CAF, the underlying mechanism remains unclear. Bakavos *et al.* (2006) studied the through-thickness microstructural gradients in 7475 and 2022 post-CAF coupons. In their investigation, 20-mm-thick plates were creep-age formed at a 3180 mm single radius curvature tool using an autoclave and vacuum bagging system. The study revealed a 15% greater mean particle radius on the compression side than on the tension side and the authors suggested two possible causes of the observed phenomenon: (i) material anisotropy as a result of the residue Bauschinger effect left behind from the stretching operation (Figure 1) and (ii) asymmetric thermal-friction interactions due to the use of the vacuum bagging technique. The suggested reasons would both enable a faster stress relaxation in compression than in tension.

The modelling results in this article suggest that the presence of a compressive residual stress field in the surface to subsurface regions of plates prior to CAF can lead to faster compressive stress relaxation in CAF. This may be applied more generally to the presence of any symmetric through-thickness residual stress field in plates prior to CAF. The microstructural changes during CAF, such as those mentioned above, may further contribute to the asymmetric through-thickness mechanical behaviour but discussion on this topic is beyond the scope of this paper. Although a much less effect in comparison to that due to

residual stresses, through-thickness asymmetry of stress due to thermal expansion of the plates under constrained heating has also been observed (Figure 14).

8. Conclusions

The influences of a typical machining-induced residual stress field and workpiece initial distortion on the accuracy of springback prediction in creep-age forming were experimentally and numerically investigated. This is the first time where their effects are studied in terms of springback prediction for the creep-age forming of panel components of different thicknesses. The following conclusions can be drawn:

- Initial distortions of plates, when conforming to the loading curvature, have opposing effects on deformation in creep-age forming. Conforming initial distortions contribute positively to the final formed shape but at the same time lead to less additional deformation introduced to the component being formed due to less creep strain accumulation during creep-ageing. In this study, a 2.3 mm difference in the initial centre deflection of two 600-mm-long, 5.3-mm-thick plates has led to over 50% difference in the average effective creep strain accumulated through the longitudinal direction of the plates. Taking into account the initial distortion of the plates in the geometric models has improved the prediction of final plate deflections by an average of 46.8%, reducing the average vertical difference between experimental and simulated plate deflections from 2.48 to 1.32 mm.
- Considering the machining-induced residual stresses in creep-age forming models leads to a higher overall stress state in the component being formed. This can cause greater accumulation of stress-induced creep strain that is sufficient to influence springback prediction. In this study, including the residual stress alone has reduced the difference between the experimental and simulated vertical plate deflections by 34.1%, improving the average difference from 1.32 to 0.87 mm.
- Influence of residual stresses on springback prediction in creep-age forming depends on the amount of deformation at the initial loading of the workpiece and its thickness. Given the presence of a constant residual stress field in a physical component, it becomes less important to consider the residual stress for springback prediction when the component is creep-age formed under a higher loading stress (for thicker plates for example).

- A symmetric through-thickness residual stress field and thermal expansion under constrained heating can both contribute to the occurrence of asymmetric through-thickness stress relaxation during creep-age forming.

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Conflict of interest

The authors declare that there is no conflict of interest.

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