Finite Analysis of the Cold Expansion of Aircraft Fastner Holes

S J Houghton

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FINITE ELEMENT ANALYSIS OF THE COLD EXPANSION OF AIRCRAFT FASTENER HOLES

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Abstract
Enhancing the fatigue performance of aging aircraft structures is of significant concern for military and civil operators worldwide. Inducing permanent compressive stresses in the region surrounding fastener holes, using hole cold expansion, is one such method. The beneficial effect derived from this process is entirely dependent on the magnitude and distribution of the residual stress surrounding the hole, therefore identification of accurate residual stress profiles is critical.

This research focused on the development of finite element simulations of the hole cold expansion process. It evaluated the residual stress fields developed from FEA models of different levels of complexity. This included two and three dimensional uniform expansion simulations and three dimensional simulations with the expansion developed by contact with a rigid mandrel, with and without the inclusion of a lubricated sleeve.

The residual compressive stress profiles were shown to vary significantly through the thickness of the workpiece and were also strongly influenced by the direction of mandrel motion. Therefore the 2D and 3D uniform expansion models were unable to accurately capture the residual stress. The inclusion of the sleeve was important when friction was introduced into the simulation as it prevented direct axial deformation from the mandrel. The use of a kinematic hardening law was required to accurately capture reverse yielding effects near the hole surface.
EXECUTIVE SUMMARY

Background

Enhancing the fatigue performance of aging aircraft structures is of significant concern for military and civil operators worldwide. Cost effective measures are required that do not lead to a significant increase in weight, the wholesale premature replacement of structural components or overly arduous inspection and maintenance intervals. Inducing permanent compressive stresses in the region surrounding fastener holes, using hole cold expansion, is one method of enhancing fatigue performance. The split-sleeve hole cold expansion process, developed by Boeing in the late 1960s, has been successfully used on aircraft structure for over thirty years. This fatigue enhancement method was included in the centre wing upgrade as part of the RNZAF C-130 Life Extension Programme.

This process involves pulling an over-sized tapered mandrel, pre-fitted with a lubricated split sleeve, through a fastener hole. The combined diameter of the mandrel and sleeve is sufficiently greater than the hole diameter so as to develop a prescribed amount of plastic deformation around the hole. Upon mandrel removal the reaction of the elastically deformed material on an annulus of plastically deformed material immediately surrounding the hole creates a compressive residual stress field. In comparison to other techniques, this method develops a large, controllable residual compressive zone with high compressive stresses.

Accurate assessment of the residual stress profile surrounding a cold expanded hole is critical because the magnitude and distribution of the residual stress is directly related to the fatigue performance of the hole. Residual compressive stresses have the effect of reducing the stress concentration after the application of tensile and bearing loads to the fastener hole and therefore reduce the effective stress intensity factors for cracks emanating from the hole.

A number of analytical models, experimental techniques and numerical simulations have been developed to identify the residual stress field induced by the cold expansion process. Analytical models and experimental measurements of the residual stress field are limited by the fact that they are unable to predict the significant through thickness variation of residual stress. Therefore, research has focused on developing numerical simulations using finite element analysis (FEA) tools. Advancements in FEA and computing technology have meant that increasingly complex simulations can be performed effectively and efficiently.

Sponsor

RNZAF Director of Aeronautical Engineering (DAE)

Aim

The current research focuses on the development of finite element simulations of the cold expansion process. It looks to compare and quantify the residual stress fields developed as the level of complexity of the finite element model increases. The following models are included in the study:

- 2D plane stress/strain simulation with uniform expansion applied to hole
- 3D simulation with uniform expansion applied to hole
- 3D simulation with expansion developed by contact with axially drawn rigid mandrel
- 3D simulation with expansion developed by contact with axially drawn rigid mandrel and including a deformable sleeve.
Other factors investigated include the effect of friction between contact surfaces and the effect of constitutive hardening law used.

**Conclusions**

1. The 3D uniform expansion (UE) model indicates that there is significant through thickness variation in the residual stress field. The 2D UE models show similar trends to the 3D UE model. They approximate the circumferential stress at the mid-thickness with reasonable accuracy but fail to capture the through thickness variation. Therefore simulating the cold expansion process using 2D FEA is inadequate.

2. The magnitude of compressive, circumferential residual stress (relative to the hole) has the most influence on the level of fatigue enhancement achieved through hole cold expansion. This stress lowers the net tensile stress at the hole, delaying crack initiation. It also directly aids crack closure, retarding fatigue crack growth.

3. At the entrance face the 3D UE model does not follow the more complex circumferential stress profiles developed in the mandrel models. This shows the influence of the mandrel contact on the resulting residual stress in this region. At the plate mid-thickness all the stress profiles correlate very closely. This indicates that the resulting mid-thickness residual stress is independent of contact and related to expansion only.

4. At the exit face, the circumferential compressive stresses reported by the mandrel models are significantly greater than the corresponding stress profiles at the entrance face. This clearly shows that the direction of mandrel motion has a strong influence on the residual stress. The simpler 3D UE model is unable to capture this effect and therefore underestimates the circumferential compressive stresses at the mandrel exit face.

5. The entrance face of the hole exhibits the lowest circumferential compressive stress. Therefore this region will possess the least fatigue enhancement. This is confirmed by experimental tests on pristine test coupons which have shown that fatigue cracks in cold expanded holes frequently initiate from this location [16]. It is also possible that fatigue cracks will initiate at a flaw or corrosion pit that exists within the tensile overshoot of actual aircraft structure.

6. For the critical circumferential residual stresses, increasing the frictional coefficient increases the residual compressive stress in the region directly next to the hole and reduces this stress between 0.1 and 0.65 hole diameters away from the hole. This shows that within the range of frictional coefficients tested, an increasing level of friction has a beneficial effect for crack initiation and small crack growth and a detrimental effect for larger crack growth.

7. The use of a kinematic hardening law provides a more accurate (but still conservative) representation of the material response during hole cold expansion than isotropic hardening. This is because the kinematic law accounts for the Bauschinger effect. The loss of accuracy due to the linearisation of the stress-strain input, which is required for the kinematic model, is insignificant. Therefore a kinematic hardening model is preferable for future hole cold expansion simulations.
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1 INTRODUCTION

Enhancing the fatigue performance of aging aircraft structures is of significant concern for military and civil operators worldwide. Cost effective measures are required that do not lead to a significant increase in weight, the wholesale premature replacement of structural components or overly arduous inspection and maintenance intervals. Fatigue cracks originate from predominately tensile stress concentrations such as those present at fastener holes. The fatigue performance of the built-up structure can be improved by inducing permanent compressive stresses around these fastener holes. This has the effect of reducing the stress concentration without adding structural weight and therefore retarding crack initiation and growth. Methods of inducing residual compressive stresses around holes include shot peening, roller burnishing, mandrelizing and coining. However these techniques produce relatively shallow residual compressive regions which are subject to significant manufacturing variability [1].

Hole cold expansion (also known as hole cold working or HCW) is another well known method for inducing residual compressive stresses around fastener holes and has been successfully used on aircraft structure for over thirty years. The split-sleeve cold expansion process was developed by Boeing in the late 1960s and integrated into a commercial product by Fatigue Technology Incorporated (FTI). West Coast Industries also provides a commercial split-sleeve cold expansion system. The split-sleeve process is performed by pulling an over-sized tapered mandrel, pre-fitted with a lubricated split sleeve, through the hole (figure 1.1). This fatigue enhancement method was included in the centre wing upgrade as part of the RNZAF C-130 Life Extension Programme.

The combined diameter of the mandrel and sleeve is sufficiently greater than the hole diameter so as to develop a prescribed amount of plastic deformation around the hole. When the mandrel is removed a biaxial residual stress field is created due to the reaction of the elastically deformed material on an annulus of plastically deformed material immediately surrounding the hole. It is important to note that this is a self-equilibrating stress field, with the compressive residual stresses surrounding the hole being balanced by tensile residual stresses in the surrounding structure. In a typical application of the process, the peak compressive circumferential (hoop) stress is similar to the compressive yield strength of the material. The compressive stress region typically spans one to two radii from the edge of the hole. The peak tensile stress in the surrounding elastic material is in the order of 10-25% the yield stress, and is particularly
sensitive to edge distance [1]. In comparison to other techniques, this method develops a large, controllable residual compressive zone with high compressive stresses.

### 1.1 Cold Hole Expansion Process

The split-sleeve cold expansion process is shown in figure 1.2. Initially, the starter hole is drilled and reamed to the required size. A split sleeve is then slid over the mandrel until the flared end rests up against the nosecap. Next the mandrel and sleeve combination are slid through the starter hole until the nosecap is firmly positioned against the workpiece. The mandrel is then withdrawn through the sleeve, which is retained in the workpiece, by actuating the hydraulic puller unit. The split sleeve is then removed and discarded. Split sleeves are intended for single use only as the expansion process causes them to permanently deform. Finally the hole may be reamed up to a particular diameter for the desired fastener fit. The FTI tooling guide clearly specifies maximum reaming allowances so as to not negate the effect of the cold expansion.

![Figure 1.2 – Schematic diagram of the FTI split sleeve cold working process [2]](image)

The lubricated split sleeve allows for single sided processing, reduces the required pull force and shields the hole surface from the large axial frictional forces generated whilst the mandrel is drawn through. The downside of using the split-sleeve technique is that it creates an axial ridge of material that corresponds to the position of the split. This axial ridge can be easily removed by reaming. However, the opening of the split in the sleeve causes residual compressive stresses
to develop asymmetrically with lower hoop stresses in the vicinity of the split [3]. Therefore the split needs to be aligned with the least critical direction for fatigue crack growth in order to maximize the benefits of the cold expansion process [2].

The applied expansion of the hole by the mandrel and sleeve is nominally 4% of the hole diameter for aluminium and mild steels and 5.5% for high strength metals. The FTI process [2] can be applied to holes of up to 4 inches in diameter and 7 inches thick. This includes multiple layers of material, which is a common occurrence when retrospectively cold expanding holes in aging aircraft structure. The cold expansion process has limitations on minimum hole spacing and edge distance as reducing these has been shown to have a detrimental effect on the beneficial compressive stresses [2].

1.2 Determination of the Residual Stress Field

Identification of the accurate residual stress profile surrounding a cold expanded hole is critical because the magnitude and distribution of the residual stress is directly related to the fatigue performance of the hole. The residual compressive stress has the effect of reducing the stress concentration after the application of tensile and bearing loads to the fastener hole and therefore reduces the effective stress intensity factors for cracks emanating from the hole. A number of analytical models, experimental techniques and numerical simulations have been developed to identify the residual stress field induced by the cold expansion process of a hole.

Analytical studies have been performed to determine closed form solutions for the residual stresses induced by the cold expansion process. Hsu and Forman [4] obtained an elastic-plastic solution for the residual stresses surrounding a cold expanded hole that considered the unloading of the hole after the removal of the mandrel. This solution has subsequently been extended to include the effects of reverse compressive yielding during unloading for both plane strain and plane stress cases [5-7]. However, these models are based on two-dimensional approximations and are unable to predict the through thickness variation of residual stress.

Attempts have been made to determine the residual stress profile experimentally, but the methods adopted are limited by their inability to measure the through thickness variation of residual stress [8]. Non-destructive methods such as X-ray and neutron diffraction have been widely used. X-ray diffraction provides a reliable method but is limited by low accuracy in regions of high stress gradient and can only be used for surface measurements [9]. The destructive Sach’s boring technique [10] estimates the average of through thickness residual stress.

Considering the limitations of analytic solutions and difficulties and limitations associated with experimental measurements, research into the residual stress profiles at cold expanded holes has focused on developing numerical simulations using finite element analysis (FEA) tools. Advancements in FEA and computing technology have meant that numerical simulations can be carried out more efficiently, with increasing complexity which can account for the majority of physical effects.

2D plane strain/stress, 2D axisymmetric and 3D elastic-plastic models have been produced where the cold expansion process is simulated by applying uniform radial displacements to the surface of the hole, which simulates the mandrel interference [11,12]. The prescribed displacement constraints are subsequently released so that the material can spring back, simulating mandrel removal. The 2D plane strain/stress models of this nature are unable to model the through thickness effects. The 2D axisymmetric models can account for thickness
effects but are limited by being unable to model realistic boundary conditions. However, these uniform expansion models are limited because in reality the expansion is applied sequentially through the axial motion of the oversized mandrel, rather than uniformly.

Babu et al. [13] developed a staggered prescribed displacement process where expansion and recovery were applied independently at distinct layers through the thickness of the hole. This method attempts to mimic the sequential application of expansion and recovery created by the mandrel. The staggered expansion approach provides a better approximation, however it neither completely simulates the steady continuous expansion applied by the tapered mandrel nor the contact conditions between components.

2D axisymmetric [14] and 3D models [15,16] have been developed that model the mandrel as a non-deformable rigid component and enforce contact between the mandrel and the surface of the hole as the mandrel is drawn through the hole. These simulations have been extended to include the steel sleeve [11,17] which is modeled as an elastically deformable annulus, thus modeling the actual expansion process more accurately.

More recent work develops this approach further and includes the effect of the split in the sleeve [3,18], which can account for the resulting circumferential variation in residual stress. All the above methods, except the split sleeve model, can take advantage of quarter symmetry to reduce the required computational effort. However, the split sleeve model can adopt half symmetry with the split along the plane of symmetry but this still results in a significant increase in computational effort.

The current research focused on the development of finite element simulations of the cold expansion process. It compared and quantified the residual stress fields developed as the level of complexity of the finite element model was increased. The following FEA models were included in the study:

- 2D plane stress/strain uniform expansion
- 3D uniform expansion
- 3D mandrel contact without sleeve
- 3D mandrel contact with unsplit sleeve

Models that included the split sleeve were not considered. It must be noted that the split is generally aligned at 90 degrees to the critical fatigue crack growth plane and that Ismonov [18] has shown that there is very little difference in the residual stress profile at this location when the split sleeve is taken into account. Other factors investigated include the effect of friction between contact surfaces and the effect of the constitutive work hardening law used.
2 METHODOLOGY

Four different FEA simulations of the hole cold expansion process were developed. They were:

- 2D plane stress/strain simulation with uniform expansion applied to hole
- 3D simulation with uniform expansion applied to hole
- 3D simulation with expansion developed by contact with axially drawn rigid mandrel
- 3D simulation with expansion developed by contact with axially drawn rigid mandrel and including an elastically deformable sleeve.

The non-linear finite element code ABAQUS 6.9EF was used to perform the analyses. The basic geometry consisted of a 0.25” thick plate that was 4” long and 2” wide. At the centre of the plate there is a single hole with a starting diameter of 0.236”. The plate is completely fixed along its two shorter edges.

The cold expansion procedure adopted for the analyses is based on the FTI split sleeve process specification [2], using the 8-0-N tooling set to develop a cold expanded hole appropriate for a nominal ¼ inch fastener. The starting hole diameter of 0.2365” used in the models represents the midpoint between the minimum and maximum starting hole diameters for 8-0-N tooling. The total expansion applied was 0.246” in diameter, equivalent to a total expansion of 4% of the starting hole diameter.

The only variation from the FTI process specification occurred in the FEA model that included the sleeve. A 0.006” thick sleeve was used in the FEA rather than the 0.008” thick sleeve specified. The 0.006” thick sleeve was used for 2/16 inch and 3/16 inch tooling and an error was made in adjusting the model to ¼ inch tooling. The total expansion however, remained the same. Kang [12] showed that the effect of reaming on the cold expansion residual stress is insignificant and therefore this part of the process was not modeled.

2.1 Material Properties

The aluminium alloy used in this work was 7075-T6512. The non-linear stress strain curve was developed from experimental tests of 0.25” rolled plate [19]. The curve was an average of four tensile tests; two with the specimen aligned in the longitudinal direction and two aligned in the transverse direction. This resulted in a Young’s modulus of 10370ksi and the true stress – true plastic strain curve shown in figure 2.1. The material properties for this aluminium alloy are not significantly different between the longitudinal and transverse plate directions. However, if these properties did vary considerably with plate direction, it would be possible to use an orthotropic material model. The Poisson’s ratio of 0.33 was obtained from MMPDS [20].

An isotropic hardening law was used initially to define the plastic deformation of the aluminium plate. This method allows an accurate piece-wise linear curve of stress against plastic strain to be input into ABAQUS. However, it may not accurately capture the reversed yielding effects as accurately as other constitutive laws, such as kinematic hardening. The ABAQUS implementation of kinematic hardening however only permits a linear hardening curve [21]. The kinematic hardening law is investigated as an alternative in Section 3.5.

1 Note that the RNZAF application of hole cold expansion is in relation to aircraft designed in the USA, therefore Imperial units will be used throughout this report.
2 Chosen because it is the primary alloy for tension (lower) components of the C-130 wing
2.2 2D Uniform Expansion FEA Model

The geometry and boundary conditions of the 2D uniform expansion (UE) models are shown in figure 2.2. This shows the fixed ends of the plate and the uniform radial displacement (0.00475") applied to the hole to simulate mandrel interference. The prescribed displacement constraints were subsequently released so that the material can spring back, simulating mandrel removal.

The mesh (figure 2.3), consisting of 4100 elements and 12468 nodes, had significant refinement in the region surrounding the hole. The plane stress model used the CPS8R element, which is an 8-node biquadratic plane stress quadrilateral with reduced integration. The plane strain model used the equivalent plane strain element, CPE8R. The results show considerable mesh independence.
Uniform radial expansion applied to surface of hole, which is subsequently released to simulate recovery.

Fully fixed surfaces - displacements fixed in:
- x & y directions for 2D
- x, y & z directions for 3D

Figure 2.2 – Geometry and boundary conditions for the 2D and 3D uniform expansion FEA models

Figure 2.3 – Mesh used for the 2D FEA including close-up of refined region surrounding hole
2.3 3D Uniform Expansion FEA Model

The geometry and boundary conditions of the 3D uniform expansion (UE) model (figure 2.2) are similar to that of the 2D uniform expansion model. Uniform radial displacements (0.00475") are applied to the hole surface to simulate mandrel interference. The prescribed displacement constraints are subsequently released so that the material can spring back, simulating mandrel removal. For the 3D model however, due to symmetry only one quarter of the plate was modeled. This allows much greater mesh refinement through the plate thickness within existing computational constraints. The mesh, shown in figure 2.4, consists of 18620 elements and 82142 nodes. The 3D model used C3D20R elements, which are 20-node quadratic bricks with reduced integration.

![Figure 2.4 - Mesh used for the 3D UE FEA including close-up of refined region surrounding hole](image)

2.4 3D FEA Model with Mandrel

Two 3D models were produced that develop the hole expansion through contact with an axially drawn oversize mandrel. This more closely replicated the physical process than previous FEA methods. The first of these contact models had direct frictionless contact between the mandrel and the hole surface. The second model included the sleeve. Contact was provided for between the mandrel and the sleeve as well as between the sleeve and the plate. The sleeve was modeled as an elastic body with material properties consistent with steel (Young’s modulus of 30,000 ksi and a Poisson’s ratio of 0.3). In reality the sleeve is lubricated to reduce friction, however, a small friction between contact surfaces will still be present. Therefore the effect of friction on the resulting residual stress was investigated in the sleeve model only. Frictionless contact was compared against the effect of frictional coefficients of 0.04, 0.08 and 0.12. The same frictional coefficient was used for the mandrel-sleeve and sleeve-plate contact.

The geometry and boundary conditions of the 3D FEA mandrel contact model with sleeve is shown in figures 2.5 and 2.6. The model without the sleeve was effectively identical to this, minus the presence of the sleeve. Therefore the respective radii of the mandrel were increased to account for the sleeve thickness. The mandrel was modeled as a revolved analytical rigid surface.
Fully fixed surface - displacements fixed in x, y & z directions

Symmetry constraint - nodal displacements fixed in y direction on both plate and sleeve

Figure 2.5 – Plan view of geometry and boundary conditions of 3D FEA with mandrel and sleeve

Prescribed mandrel displacement in z direction

Sleeve displacement fixed in z direction

Figure 2.6 – Side view of geometry and boundary conditions of 3D FEA with mandrel and sleeve
Within these particular FEA models, the axial force required to pull the mandrel through the hole is reacted primarily through the sleeve. However, a portion of this load is reacted through the surrounding structure and induces plate bending, which influenced the resulting residual stress. In reality the sleeve is flared at one end so that it fits neatly on to the nosecap of the hydraulic puller unit, as shown in figure 1.1. This flared sleeve is placed firmly against the workpiece before the mandrel is drawn through, allowing the entire mandrel force to be reacted through the nosecap. In order to capture the reacted axial load more accurately, and therefore identify the residual stress fields with greater accuracy, the modeling of the flared sleeve and nosecap would be required. This however, is not considered for this initial cold expansion analysis due to modeling complexity and will be the focus of future work.

The mesh of the aluminium plate was identical to that used in the 3D uniform expansion analysis. The sleeve mesh consisted of 864 C3D20R elements and 4723 nodes.

Figure 2.7 – Side view of geometry and boundary conditions of 3D FEA with mandrel and sleeve
3 RESULTS & DISCUSSION

3.1 Comparison of 2D & 3D Uniform Expansion Models

The residual stress fields of the 2D and 3D uniform expansion (UE) models are presented in figures 3.1 and 3.2. The path of these residual stress profiles extends from the edge of the hole to the free edge of the plate, along the x axis shown in figure 2.2. This is the critical plane for fatigue crack growth assuming the plate is axially loaded in the longitudinal direction (y axis). As the 3D model is capable of accounting for through-thickness effects, the stresses at the surface and mid-thickness of the plate are presented. This model had no axial component to the loading therefore both the top and bottom surfaces will have identical residual stresses.

The radial residual stress curves in figure 3.1 show that all uniform expansion models follow a similar trend with the radial stress starting at zero at the hole edge, decreasing to a minimum at between 0.25 to 0.5 hole diameters from the hole edge and then steadily returning to zero towards the edge of the plate. The magnitudes of the stress minimum however are considerably different. The 3D simulation shows considerable variation of the radial stress through the thickness, as the compressive stress at the surface is double that of the stress at the mid-thickness. The plane stress and plane strain 2D models are not able to capture the extent of the minimum stress shown in the 3D model.

The circumferential residual stress is the most important factor in considering the effectiveness of fatigue enhancement created by cold expansion. This stress lowers the net tensile stress at the hole, delaying crack initiation. It also directly aids crack closure, retarding fatigue crack growth. Therefore as the compressive residual circumferential stress increases, so does the degree of fatigue enhancement. The circumferential residual stress curves for the 2D models (figure 3.2) are similar to that of the 3D model at the mid-thickness of the plate. The magnitude of the maximum compressive stress is between 90 and 115 ksi, which is greater than the yield stress of 79.2 ksi. At the surface of the 3D model this compressive stress is significantly less than at the mid-thickness and represents approximately 75% of the yield stress. This reduced compressive circumferential stresses decreases the level of fatigue enhancement at the surface, which is where fatigue cracks tend to initiate [16].

All the stress profiles show a region of tensile residual stress which begins at between 0.4 to 0.55 hole diameters from the hole edge. This region is often referred to as the tensile overshoot. The mid-thickness profile of the 3D model has the greatest peak overshoot stress of 17 ksi and the surface profile has the lowest peak with 9 ksi. This represents 11 to 21% of the yield stress respectively. These values correlate well with FTI advice [1] which indicates that the tensile overshoot begins between 0.5 to 1 hole diameters form the hole edge with tensile peaks of typically 10 to 25% of the yield stress.

The 3D uniform expansion model shows significant through thickness variation in residual radial and circumferential stresses that the 2D models are simply unable to capture. Therefore simulating the cold expansion process using 2D FEA is inadequate.
Figure 3.1 – Comparison of radial residual stress results from 2D plane stress/strain uniform expansion and 3D uniform expansion models.

Figure 3.2 – Comparison of circumferential residual stress results from 2D plane stress/strain uniform expansion and 3D uniform expansion models.
3.2 Mesh Independence of 3D Mandrel Contact Models

Early developments of a 3D FEA model that included the mandrel and sleeve showed that significant refinement of the plate mesh was required. This was particularly necessary at the surface of the hole corresponding to the mandrel entry and exit faces. The size of this model was constrained by computer memory restrictions.

The resulting fine mesh was then adopted as the primary mesh for these analyses. The mesh independence of this fine mesh was examined by producing progressively coarser meshes of the aluminium plate. This was performed by primarily reducing the number of elements in the radial direction. The number of elements located circumferentially around the quarter circle of the hole remained fixed at twelve. The resulting fine, medium and coarse meshes contained 18620, 8364 and 3344 elements and 82142, 37666 and 15857 nodes respectively. A frictional coefficient of 0.08 was used for contact in these models.

The circumferential residual stress profiles at either surface of the hole were found to be most sensitive to changes in the mesh. These profiles are given in figure 3.3. Within 0.05” of the hole edge there exist small variations in stress between meshes, however good convergence is shown with the medium and fine meshes matching more closely than the coarse mesh. These results, combined with insensitivity of other stress profiles (not shown here), indicate that the fine mesh exhibits satisfactory mesh independence.

![Figure 3.3](image-url)
3.3 Comparison of 3D Models

The residual stress fields of the 3D models are presented in figure 3.4. The stress profiles follow the same path as described in section 3.1. They compare the residual stress at the mandrel entry face, plate mid-thickness and mandrel exit face. For the 3D uniform expansion model the residual stress profile is identical at the entry and exit faces, however it is reported in both instances for direct comparison against the mandrel contact models. Maximov et al. [14] used a frictional coefficient of 0.08 for the tangential contact between the lubricated sleeve and the other components. Therefore this value was adopted for the 3D mandrel contact with sleeve analysis. Contour plots of the residual stresses for the mandrel with sleeve model are shown in figures 3.5 and 3.6.

The overall trends from the 3D models are in agreement. The peak radial compressive stresses occur at both plate surfaces and the minimum stress at the mid-thickness. Conversely, the peak circumferential compressive stress occurs at the mid-thickness and the minimum stresses occur at the plate surfaces.

At the entrance face the 3D UE model does not follow the more complex circumferential stress profiles developed in the mandrel models. This shows the influence of the mandrel contact on the resulting residual stress in this region. At the plate mid-thickness all the stress profiles correlate very closely. This shows that mandrel and sleeve contact have little influence at this point and the resulting residual stress is related to expansion only.

At the exit face, the circumferential compressive stresses reported by the mandrel models are significantly greater than the corresponding stress profiles at the entrance face. This clearly shows that the direction of mandrel motion has a strong influence on the resulting residual stress profile. The simpler 3D UE model is unable to capture this effect and therefore underestimates the circumferential compressive stresses at the mandrel exit face.

The entrance face exhibits the lowest circumferential compressive stress. Therefore this region will possess the least fatigue enhancement. This is confirmed by experimental tests which have shown that fatigue cracks in cold expanded holes usually initiate from this location [16]. Fatigue tests of comparable reamed holes show that fatigue cracks show no preference for initiating at either surface or within the bore of the hole. These fatigue tests were performed with elevated load levels on pristine test samples. It is also possible that fatigue cracks will initiate at a flaw or corrosion pit that exists within the tensile overshoot of actual aircraft structure within the service environment.

There is little difference in the radial and circumferential stresses between the mandrel only model and the mandrel with sleeve model at the plate mid-thickness and exit face. At the entrance face however, there are clear variations in the stress profiles. This cause of this effect is investigated in section 3.4.
Figure 3.4 – Comparison of results from 3D uniform expansion, 3D mandrel contact without sleeve and 3D mandrel contact with sleeve models. (a) Radial ($a_1$) entrance face; ($a_2$) mid-thickness; ($a_3$) exit face; and (b) circumferential ($b_1$) entrance face; ($b_2$) mid-thickness; ($b_3$) exit face residual stresses.
3.4 Effects of Frictional Coefficient in 3D Mandrel with Sleeve Model

The split sleeve used in the hole cold expansion process is lubricated to lower the frictional resistance of the mandrel pulling process. While this reduces the effective frictional coefficient, the contact between the mandrel and sleeve is not frictionless. This frictional coefficient is not well defined and previous researchers have either assumed frictionless contact or made an estimate of the coefficient. This study also compared the effect of different frictional coefficients on the resulting residual stress.

The results show that the variation of the frictional coefficient affected the residual stress profiles at the mandrel entrance face only. However, as discussed in section 3.3, this represents the critical location for defining the level of fatigue performance achieved by cold expansion.
Therefore, only the residual stress profiles at the entrance face are shown for this study (figures 3.7 and 3.8).

The results of the mandrel only model and the frictionless mandrel with sleeve model are very similar. This shows that for frictionless models there is little benefit in including the elastic sleeve. However, including friction in the mandrel only models would cause frictional related axial deformation and damage at the bore of the hole. Applying friction to the sleeve models is acceptable because the axial forces between the sleeve and mandrel are reacted as shear in the sleeve with minimal frictional shear force transferred between the sleeve and plate.

For the critical circumferential residual stresses, an increasing frictional coefficient increases the residual compressive stress in the region directly next to the hole and reduces the stress between 0.1 and 0.65 hole diameters away from the hole. After this point the residual stress profiles are effectively identical. The radial residual stresses also demonstrate a very similar effect. This shows that within the range of frictional coefficients tested that an increasing frictional coefficient has a beneficial effect for crack initiation and small crack growth and a detrimental effect for the growth of larger cracks. However, at present actual frictional coefficients are unknown, preventing full analyses of fatigue crack development near cold expanded fastener holes.

The axial force required to pull the mandrel through the hole is strongly related to the frictional resistance between mandrel and sleeve. Therefore measurements of this force could be correlated against the required pull force in FEA models with known frictional coefficients, providing a better indication of actual friction effects than those assumed in this analysis and in work carried out elsewhere.

Figure 3.7 – Comparison of radial residual stresses for models with different frictional coefficients
3.5 Comparison of Plastic Hardening Models

The effect of the plastic strain hardening law used in the FEA model on the resulting residual stress profiles was also investigated. For the initial models, a non-linear isotropic hardening law was adopted, using a stress-strain curve developed from tensile testing. The advantage of using an isotropic law in ABAQUS is that it allows the stress-strain curve to be defined by a series of piece-wise linear segments, which gives a very accurate representation of the tangent modulus at any point during plastic deformation.

Strain hardening is modeled in FEA by relating the size, shape and location of the material yield surface (which represents the yield condition in stress space) to plastic strain in some appropriate way [22]. An isotropic hardening law assumes that the yield surface expands uniformly during increasing plastic strain, with no translation or change in shape. This constitutive law does not account for the Bauschinger effect, which occurs in ductile metals [22].

Alternatively, a kinematic hardening law accounts for the Bauschinger effect by allowing the yield surface to translate, without changing its size or shape. Therefore as the material deforms plastically in tension, the yield surface is moved in the direction of increasing stress. By shifting the yield surface in the tensile direction, the compressive yield stress is progressively reduced. Therefore this constitutive law better represents cyclic plastic deformation.

For the hole cold expansion application, the annulus of material surrounding the hole undergoes tensile work hardening as the hole is expanded. Upon removal of the mandrel the compressive residual stress field is created by the reaction of the elastically deformed material on the annulus of plastic material immediately surrounding the hole. The previous results have shown that this causes the material to compressively yield near to the hole surface. As mentioned above, the
compressive yield point of this material depends on the Bauschinger effect which is only captured using the kinematic hardening law. The disadvantage of adopting the kinematic hardening law is that ABAQUS only allows a linear hardening profile to be defined. This does not capture the steady reduction in tangent modulus typical for aluminium alloys.

A linear approximation was made of the piece-wise linear stress-strain curve used to define isotropic hardening (figure 3.9). This was achieved by fixing the yield point as the 0.2% offset yield value and applying a least squares linear curve fit to the original curve over the range of 0-6% plastic strain. This is the range that has been observed to contain the majority of plastic strain within the FEA models. The results of the mandrel with sleeve model were then compared using the non-linear isotropic model and the linear kinematic model. A third, linear isotropic model was used to directly analyse the effect of the reduction in stress-strain curve resolution.

Figure 3.9 - Stress – Plastic Strain curves used as material inputs to ABAQUS

The results of the hardening law comparison are given in figure 3.10. By comparing the linear and non-linear isotropic models it can be seen that there is little change caused by the linearisation of the stress-strain input. The only significant difference occurs at the region of peak tensile overshoot, where the linear models overestimate the magnitude of this peak. This is because the linear models over-estimate the initiation of yielding and the smooth transition between elastic and plastic deformation compared with the more accurate stress-strain response used by the non-linear model (figure 3.9).

Reverse compressive yielding is indicated in the circumferential residual stress profiles by the sudden change of slope (negative to positive sign) in close proximity to the hole. The kinematic model shows that the onset of reverse yielding occurs at much lower compressive stresses at the mid-thickness and exit face of the plate. At the entrance surface, the material does not reach the reverse yield stress in the isotropic models but clearly does in the kinematic model. This results in the isotropic models significantly overestimating the residual circumferential stresses. There are only minor variations shown in the radial residual stress profiles.

Therefore, the kinematic hardening law provides a more accurate and conservative representation of the material response during hole cold expansion by accounting for the Bauschinger effect. The loss of accuracy due to the linearisation of the stress-strain curve is insignificant.
Figure 3.10 – Comparison of results using non-linear isotropic, linear kinematic, and linear isotropic hardening laws for the mandrel with sleeve model. (a) Radial (a₁) entrance face; (a₂) mid-thickness; (a₃) exit face; and (b) circumferential (b₁) entrance face; (b₂) mid-thickness; (b₃) exit face residual stresses.
4 CONCLUSIONS

1. The 3D uniform expansion (UE) model indicates that there is significant through thickness variation in the residual stress field. The 2D UE models show similar trends to the 3D UE model. They approximate the circumferential stress at the mid-thickness with reasonable accuracy but fail to capture the through thickness variation. Therefore simulating the cold expansion process using 2D FEA is inadequate.

2. The magnitude of compressive, circumferential residual stress (relative to the hole) has the most influence on the level of fatigue enhancement achieved through hole cold expansion. This stress lowers the net tensile stress at the hole, delaying crack initiation. It also directly aids crack closure, retarding fatigue crack growth.

3. At the entrance face the 3D UE model does not follow the more complex circumferential stress profiles developed in the mandrel models. This shows the influence of the mandrel contact on the resulting residual stress in this region. At the plate mid-thickness all the stress profiles correlate very closely. This indicates that the resulting mid-thickness residual stress is independent of contact and related to expansion only.

4. At the exit face, the circumferential compressive stresses reported by the mandrel models are significantly greater than the corresponding stress profiles at the entrance face. This clearly shows that the direction of mandrel motion has a strong influence on the residual stress. The simpler 3D UE model is unable to capture this effect and therefore underestimates the circumferential compressive stresses at the mandrel exit face.

5. The entrance face of the hole exhibits the lowest circumferential compressive stress. Therefore this region will possess the least fatigue enhancement. This is confirmed by experimental tests on pristine test coupons which have shown that fatigue cracks in cold expanded holes frequently initiate from this location [16]. It is also possible that fatigue cracks will initiate at a flaw or corrosion pit that exists within the tensile overshoot of actual aircraft structure.

6. For the critical circumferential residual stresses, increasing the frictional coefficient increases the residual compressive stress in the region directly next to the hole and reduces this stress between 0.1 and 0.65 hole diameters away from the hole. This shows that within the range of frictional coefficients tested, an increasing level of friction has a beneficial effect for crack initiation and small crack growth and a detrimental effect for larger crack growth.

7. The use of a kinematic hardening law provides a more accurate (but still conservative) representation of the material response during hole cold expansion than isotropic hardening. This is because the kinematic law accounts for the Bauschinger effect. The loss of accuracy due to the linearisation of the stress-strain input, which is required for the kinematic model, is insignificant. Therefore a kinematic hardening model is preferable for future hole cold expansion simulations.
5 ADDITIONAL FACTORS FOR CONSIDERATION

1. This research simulated an idealized cold expansion process where pulling force applied to the mandrel was assumed to be reacted through both the sleeve and surrounding structure. The portion of load reacted through the structure induces plate bending during the expansion process and therefore the resulting residual stress will be influenced by the flexibility of the plate. In the actual cold expansion process the end of the sleeve is flared and fits neatly over the nosecap that is attached to the hydraulic puller unit. This should react all of the mandrel pull force rather than loading the surrounding structure.

2. The effect on the residual stress profiles by variables such as plate thickness, reaming, material (particularly other aluminium alloys such as 2024-T3), hole edge distance, proximity to other cold expanded holes and the expansion of multi-layered built-up structure are not well understood.

3. The effect of the level of expansion has been relatively standardized by commercial providers of hole cold expansion systems, however the process specification allows a tolerance on the size of acceptable starting holes. Therefore the residual stress profiles should be compared for the upper and lower limits of the starting hole tolerance.

4. A more accurate estimate of frictional coefficient between components could be achieved by correlating experimental measurements of mandrel pulling force with FEA models.

5. It has been shown that the split in the lubricated sleeve induces significant circumferential variation to the resulting residual stress [3,18]. It must be noted that the split is generally aligned at 90 degrees to the critical fatigue crack growth plane and there is insignificant difference in the residual stress profile at this location, compared to models without the split [18].

6. An attempt to quantify the effect that hole cold expansion has on crack initiation and growth is the ultimate goal of research in this field. Existing methods of accounting for the beneficial effects of hole cold expansion in damage tolerant design are inconsistent, somewhat arbitrary and possibly overly conservative [1]. Therefore development of more realistic analytical methods would be invaluable. For estimating crack growth this would possibly involve incorporating fracture mechanics methodologies with accurate residual stress profiles to develop stress intensity factor (SIF) solutions for a crack emanating from or near cold expanded holes.

6 FUTURE WORK

1. The development of a FEA simulation that includes an accurate representation of the actual load paths encountered during the hole cold expansion process is necessary. Specifically this involves modeling the load reacted through the flared sleeve and nosecap of the hydraulic puller unit. This updated simulation could also include a more accurate estimate of frictional coefficient between components.

2. The next step of the research is to develop a cold expansion simulation with embedded corner cracks of various sizes located at the entrance face. The J-integral, which is a geometric measure of fracture toughness, can be calculated for these cracks and this could be used to define a SIF solution for use in fracture mechanics analysis.
7 REFERENCES


# Finite Element Analysis of the Cold Expansion of Aircraft Fastener Holes

**Enhancing the fatigue performance of aging aircraft structures is of significant concern for military and civil operators worldwide. Inducing permanent compressive stresses in the region surrounding fastener holes, using hole cold expansion, is one such method. The beneficial effect derived from this process is entirely dependent to the magnitude and distribution of the residual stress surrounding the hole, therefore identification of accurate residual stress profiles is critical.**

This research focused on the development of finite element simulations of the hole cold expansion process. It evaluated the residual stress fields developed from FEA models of different levels of complexity. This included two and three dimensional uniform expansion simulations and three dimensional simulations with the expansion developed by contact with a rigid mandrel, with and without the inclusion of a lubricated sleeve.

The residual compressive stress profiles were shown to vary significantly through the thickness of the workpiece and were also strongly influenced by the direction of mandrel motion. Therefore the 2D and 3D uniform expansion models were unable to accurately capture the residual stress. The inclusion of the sleeve was important when friction was introduced into the simulation as it prevented direct axial deformation from the mandrel. The use of a kinematic hardening law was required to accurately capture reverse yielding effects near the hole surface.
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