Experimental Tools for Characterizing Fretting Contacts*

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The severe near-surface conditions associated with partial slip or fretting contact of interacting surfaces have been linked to premature and often catastrophic failure of a myriad of mechanical systems and components, including riveted aircraft structures, power generation systems and jet engines. Developing a mechanics-based characterization of these conditions through combined modeling and experimental efforts is a challenging task, confounded by factors such as an evolution of friction driven by interfacial wear and the multiaxial, non-proportional nature of the cyclic contact stress field. This paper presents results from the recent successful application of infrared thermal imaging techniques to measure near-surface temperature fields in a way that clearly discerns this change in friction coefficient. These thermal images illustrate the transition from sliding to partial slip conditions related to wear-induced increases in friction coefficient. The experimental temperature fields have also been juxtaposed with recorded histories of the requisite fretting fatigue loads and a finite element analysis of the fully coupled thermoelastic and heat conduction problems to obtain a validated model of the near-surface conditions responsible for fretting wear and fretting fatigue damage. The observed interfacial conditions have been combined with results from three-dimensional finite element modeling to assess the applicability of two-dimensional modeling approaches to contacting mechanical components of finite dimensionality. As an example, the thermography/FEM approach has been exercised to understand the influence of fretting on the fatigue failure of riveted aircraft structures.

Key Words: Fretting, Fatigue, Friction, Infrared Thermography, Finite Element Modeling, Aircraft Structures

1. Background

Fretting is the term used to describe near-surface degradation from fatigue and wear mechanisms observed in clamped mechanical assemblies subjected to oscillatory loads or vibratory excitation. In contrast to near-surface damage associated with large-scale relative motion from either rolling or sliding motions, fretting damage is a product of small-amplitude

microslip concentrated in regions near the edges of contact.

This absence of gross relative motion, often referred to as a condition of partial slip, usually results in a limited amount of interfacial wear; instead, the localized cyclic contact stresses induced by the applied oscillatory loads accelerate the nucleation of fatigue cracks in near-surface material. Szolwinski & Farris⁽¹⁾ suggested juxtaposing an accurate characterization of the near-surface contact stress field with a multiaxial fatigue life parameter to predict fretting fatigue. This approach was validated in a series of fretting fatigue experiments involving contact between cylindrical pads and flat fatigue specimens fashioned from a common aircraft aluminum alloy, as represented schematically in Fig. 1(a)⁽²⁾.

The aforementioned characterization, though,

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Fig. 1 (a) A schematic of a fretting fatigue test highlighting the contacting surfaces and applied loads. (b) An illustration of finite depth effects associated with contacting surfaces, including a photograph of a wear scar generated during a fretting fatigue test

exploited the ubiquitous assumption that for contact areas with aspect ratios ($2a/t$, Fig. 1(b)) much less than one, the contact could be modeled accurately as a plane-strain line contact of a cylinder with infinite length in contact with an elastic halfspace. For realistic finite width contacts, as presented in Fig. 1(b), this plane-strain approach would approximate most closely the conditions near the midplane ($z=0$) of the contact region, where the influence of the traction-free faces of the indenter is slight.

Johnson⁽³⁾ offers a concise, semi-empirical summary of these “roller end effects” that arise in a variety of contacting elements. As detailed in this reference, the impact of these end effects varies with the geometric details of the free faces—from an out-of-plane bulging of two coincident sharp faces generated by the Poisson effect to a broadening of the contact area in contacts where the free faces are not perfectly matched. Visual evidence of this latter effect is presented in Fig. 1(b), a photograph of a wear scar on the surface of an aluminum indenter used in a fretting fatigue test.

This document will present a blend of experimental and modeling results in a critical analysis of the mechanics of partial slip or fretting contacts. Through use of an infrared focal plane array, sequences of thermal images of the free surfaces of a fretting pad and specimen have been captured during fretting fatigue tests. By juxtaposing a priori knowledge of the evolution of friction at the interface with the monitored load histories from the tests, a two-dimensional finite element model of this contact was then developed. The model resolved the temperature effects generated by both the cyclic contact stress field via the coupled-thermoelastic effect and the frictional heating due to relative motion at the interface. Remarkable agreement between the modeling results

and the near-surface temperature fields substantiates the use of a two-dimensional formulation for approximating the contact conditions over the entire depth of the contact.

A portion of the discussion, however, will focus specifically on understanding the free-edge effects in contacts with finite depth. This effort relies on both three-dimensional finite element modeling of partial slip contacts and full-field thermal imaging of the interfacial conditions arising under partial slip conditions. Results presented hereafter will provide both qualitative and quantitative insight into the conditions at the ends of finite depth contacts and unique in-situ observation of the evolution of microslip zones in partial slip contacts subjected to cyclic tangential loading.

The power of the elegant alliance between the thermal imaging technique and finite element method was then harnessed to understand the role of fretting in the fatigue failure of riveted lap joints that comprise the bulk of primary aircraft structure. While thermography has been used traditionally in the aerospace community as a tool for non-destructive evaluation, thermal images of lap joints test specimens have been used to prescribe accurate boundary conditions on a finite element model of the joint. This model has then been exercised to assess the impact of manufacturing, material and operational parameters on the load-transfer mechanics and related fatigue lifetimes of the joints. Predicted fatigue lifetimes from these analyses correlate well with observed failures of laboratory test articles.

2. Experimental Characterization of Fretting Contacts

2.1 Thermal imaging of fretting contacts

To characterize the near-surface conditions that drive both fretting wear and fretting fatigue damage, a thermal imaging system capable of capturing sequences of full-field temperature maps was employed in conjunction with a fretting fatigue test setup similar to that presented in Fig. 1(a). Complete details of this setup may be found in the literature⁽⁴⁾.

As shown in the inlay of Figs. 2(a) and (b), sequences of thermal images were acquired from an area on the exposed surfaces of an aluminum alloy (2024-T351) pad and specimen near the contact interface during a fretting fatigue test. Each sequence presented contains ten thermal images captured over one fully reversed, 2 Hz load cycle. Details of the calibration technique and imaging parameters employed can also be found in the aforementioned reference⁽⁴⁾. While the range of temperatures in each sequence is nearly identical, the qualitative differences

Fig. 2 (a) Sequence of thermal images from a cycle of sliding (cycle 16); (b) Sequence of thermal images from a cycle of partial slip (cycle 152)

Fig. 3 A comparison between near-surface temperature fields during sliding from both finite element modeling and thermal imaging experiments

in the near-surface fields can be attributed to the kinematic and intimately related frictional conditions at the interface.

As shown in Fig. 2(a), a patch of localized heating that spans the entire contact width can be identified in the thermal images corresponding to tangential loads near Q_{max} and Q_{min} (upper row, second and third from left; lower row, third from left, respectively). Simultaneous load and displacement measurements from this early test cycle (cycle 16) indicated that the pad was sliding over the surface of

Fig. 4 A comparison between near-surface temperature fields during partial slip conditions from both finite element modeling and thermal imaging experiments

Fig. 5 A schematic of the setup used to acquire the thermal images of the aluminum/sapphire interface. The inset provides dimensional details of the pad holder with silver mirror surface used to reflect the infrared radiation from the interface to the focal plane array

Fig. 6 Images of the sapphire/aluminum interfacial conditions captured by the focal plane array at both the maximum and minimum tangential loads during a partial slip fretting fatigue test. For the images shown, $P=7.5$ kN, $Q_{max} = -Q_{min}$ 3.4 kN, $\Delta L/2=24.4$ kN, $L_{min}/L_{max} = -0.85$

the specimen during each of these frames—and thus frictional heat was generated over the entire contact patch. Furthermore, the coefficient of friction at the interface, as determined from the ratio of the applied tangential load and normal load during the period of sliding, was found to be $\mu=0.22$.

In contrast, the images in Fig. 2(b) from a cycle later in the test (cycle 152), do not reveal any indication of frictional heating over the entire contact width. The monitored load and displacement data supported this observation by indicating that the contact was in a regime of partial slip, where $Q/P=0.34 < \mu$. As the applied loads were kept at a constant amplitude throughout the test, these two sequences captured the transition from gross sliding to partial slip initiated by an increase in friction coefficient due to interfacial wear. Furthermore, in spite of the sliding relative motion present during sequence (a) or small-amplitude microslip in (b), only small changes in temperature ($<1^\circ\text{C}$) are recorded in both sequences.

2.2 Thermal imaging and FEM

In the absence of frictional heating during the partial slip regime, these measured changes in the temperature field must be attributed to effect of the change in volumetric strain on temperature, known as the coupled-thermoelastic effect. It can be shown that for a linear-elastic, isotropic material under adiabatic conditions⁽⁵⁾, the change in hydrostatic stress, $\Delta\sigma_{kk}$, can be related to a small change in temperature, ΔT :

$$\Delta\sigma_{kk} = C\Delta T \quad (1)$$

The constant C is a function of isothermal values for the coefficient of thermal expansion, mass density and specific heat at a constant pressure.

This equation is the foundation for a popular approach to experimental stress analysis by thermal imaging. However, application of this relationship requires adiabatic conditions, which are often assumed when the cyclic stresses are applied at frequencies in excess of 50 Hz—twenty-five times higher than those used in the fretting experiments presented in Fig. 2. In addition, the presence of friction introduces irreversibility not accounted for by Eq. (1). To overcome these obstacles that impeded an analysis of the thermal images in Fig. 2, a two-dimensional finite element model of the fretting contact configuration in Fig. 1(a) was formulated. This model was able to resolve the temperature changes arising from not only the coupled-thermoelastic effect associated with the cyclic contact stresses but also frictional heating due to relative motion at the interface. Furthermore, the incorporation of heat conduction into the analysis eliminated the adiabatic assumptions implicit to Eq. (1).

Details of the numerical implementation of the model in a commercial finite element package, ABAQUS/Standard, are available in the literature⁽⁶⁾. Relying on an ancillary experimental characterization of evolution of friction with cycle number⁽⁷⁾, handbook values of the requisite material properties and the monitored load histories, the finite element model was exercised to simulate the experiments in Fig. 2. Figures 3 and 4 present comparisons between the temperature fields from both the thermal imaging technique and finite element solution for both conditions of gross sliding and partial slip. The excellent agreement between these results validates the finite element formulation for simulating a wide range of fretting contact conditions.

2.3 Interfacial observations

To both confirm conclusions drawn from the previous experimental results and set the stage for validation of a three-dimensional finite element model of fretting contacts, an experimental setup (Fig. 5) was developed that allowed for direct observation of partial slip interfaces. Instead of focusing on the free faces of a fretting pad and specimen near the region of contact, this experimental effort acquired thermal images directly from the interface.

As shown in Fig. 5, the majority of the experimental setup employed for these interfacial measurements, including the fretting fatigue fixture and infrared imaging system, was the same as presented earlier. Two key elements—cylindrical sapphire pads transparent in the infrared spectrum and modified pad holders—were incorporated to facilitate direct observation of the interfacial conditions. The cylindrical contact surfaces of the pads were generated on high-quality, polished sapphire blanks by a high-speed grinding operation. No subsequent polishing of this surface was performed. These pads were then bonded with a commercial structural adhesive to a steel pad holder. As illustrated in Fig. 5, this pad holder served as not only a carrier for the sapphire pad in the surrounding fretting fatigue test fixture, but also a mount for a small optical-quality silver mirror. This mirror, oriented at 45° from the contact surface normal, reflected the thermal radiation from the contact surface to the IR focal plane array positioned in front of the fretting fatigue fixture.

Figure 6 presents interfacial images from a fretting fatigue test at the maximum, $Q=Q_{\max}=3.4\text{ kN}$, and minimum, $Q=-Q_{\max}$, tangential loads. The experiment presented employed a sapphire pad with a 178-mm radius and flat 2024-T351 aluminum alloy specimen. The contact patch between the sapphire pad and specimen surface is immediately identified in each of the images. The non-uniformity of the con-

tact width $2a$ in the depth (z) direction is related to a small misalignment in the test fixture. Note also that the complete 12.7-mm depth of the contact could not be captured in the field of view of the lens used in the experiment. In spite of this truncated field of view, the pair of images provides direct and unique evidence of the evolution of microslip driven by the cyclic tangential load. Sequences of images acquired during the course of each test revealed a variation in the size of these zones that was in-phase with the applied tangential load.

Furthermore, these images unequivocally confirm the negligible temperature rises reported in the aforementioned series of thermal imaging experiments, in spite of the challenge associated with calibrating the intensity of thermal radiation detected by the focal plane array and interfacial temperature values. The primary source of this difficulty was the accumulation of wear debris on the contact surface of the sapphire pad that changed the emissivity of the pad surface.

In accounting for this buildup, point-by-point calibrations over selected regions of the pad were obtained immediately after each test. To obtain this calibration, the normal load was first removed from the pad, leaving the pad and specimen surfaces only nominally in contact. A fully reversed axial load was then applied to the specimen, while concurrent intensity measurements were collected through the wear scar on sapphire pad by the focal plane array. These intensities were then correlated to the theoretical temperatures in the specimen as predicted by thermoelastic theory for the simple uniaxial state of stress in the specimen. Although refinement of this calibration technique remains an area for future work, it can be concluded conclusively that any temperature rises associated with the microslip-induced frictional heating are on the order of the temperature variations induced by the thermoelastic effect ($<2^\circ\text{C}$).

3. Modeling Finite-Depth Effects in Fretting Contacts

While the results from the previous experimental and modeling efforts substantiate the use of two-dimensional models for the contact configurations considered, an effort is underway to quantify the effects of finite contact dimensions on conditions associated with fretting wear and fretting fatigue. The finite element model formulated for this task presented hereafter relies on an explicit contact algorithm under continuing development at Sandia National Laboratories⁽⁸⁾. The algorithm is used to solve a 230 000 node, three-dimensional finite element model corresponding to the classical two-dimensional boundary value problem associated with elastic par-

Fig. 7 An annotated version of the mesh cross-section used to simulate the three-dimensional partial slip problem. Rigid body displacements and rotations of the pad and specimen were prevented with the requisite boundary conditions

tial slip contacts solved by Mindlin⁽⁹⁾. The interfacial conditions in these partial slip contacts are often linked to degradation by fretting wear—conditions corresponding to an axial load, $L=0$, in Fig. 1(a).

Figure 7 presents a cross-section of the three-dimensional mesh used to model the elastic, partial slip contact between a flat specimen surface and an indenter with a radius of curvature of 127 mm. The mesh of each body was extruded 6.35 mm in the z -direction, so that each node on the corresponding free z -faces of the bodies had the same initial z -coordinates. A symmetry condition was placed along the plane $z=6.35$ mm to represent a specimen/indenter pair with a depth of 12.7 mm. The assembled mesh was composed of approximately 230 000 nodes comprising over 211 000 eight-noded brick elements with elastic constants representative of a common aluminum alloy ($E=75.1$ GPa and $\nu=0.3$).

A second plane of symmetry was imposed along the midplane $x=0$ of the specimen to represent an opposing contact configuration similar to Fig. 1(a). Yet unlike Fig. 1(a), the model did not rely on an axial bulk stress at one end of the specimen to generate a tangential loading, Q , through resistance to spring elements attached to the pad. Instead, a shear traction was induced at the interface by prescribing a remote displacement to the indenter in the x -direction, with appropriate boundary conditions on the specimen and pad to prevent additional rigid body displacements and rotations. The magnitudes of the resultant tangential loads were limited to less than the global frictional resistance offered by the contact, μP , leading to the division of the surface into a central region of no relative motion (stick) flanked by zones of small-amplitude relative motion (microslip). In these zones of microslip, the shear traction, $q(x, z)$ was equal to the product of the coefficient of friction

Fig. 8 Contours of the evolution of interfacial shear traction during a complete tangential load cycle from the finite element model of the Mindlin partial slip problem with a finite depth contact. For the results shown, $P = 489 \text{ N/mm}$, $\mu = 0.65$

Fig. 9 The surface of a titanium alloy (Ti-6Al-4V) pad used in a fretting fatigue test. Note the annular slip zone associated with the three-dimensional effects resolved in the finite element results presented in Fig. 8

and contact pressure, $\mu p(x, z)$, thereby initiating the relative motion.

Figure 8 presents a sequence of shear traction contours at the surface of the specimen as the tangential load follows a fully reversed tangential load cycle of amplitude $Q_{\max}$. As noted on the sequence, the finite element simulation begins with application of a normal load, P , to the indenter, while the coefficient of friction at the interface was set to zero. The loss of constraint at the free face of the specimen is apparent

Fig. 10 Contours of $\Delta\sigma_{kk}$ on the driven-head surface of a riveted single lap joint obtained by the thermal imaging technique. The remote applied load is along the vertical axis of each figure

immediately by examining the local expansion of the face in the z -direction, as highlighted in the plot of the deformed interface in Fig. 8. A similar deformation was noted on the free face of the pad. While not captured in this initial contour of $q(x, z) \equiv 0$, this deformation led to a decrease in the contact pressure near the ends of the contact.

These free end effects were also evident in the variation of the shear traction through one complete cycle of the tangential load. As reflected in the contours of Fig. 8, the regions near the edges of contact, $x = \pm a$, witnessed relative motion, as $|q(x, z)| = \mu p(x, z)$. The distribution of this shear traction was independent of z over approximately 80% of the pad half-depth. Near the free ends, though, the distributions of the shear traction and the related zones of microslip became nearly annular, as the contact pressure decreased near the free faces of the pad. With this reduced distribution of contact pressure, the zone of slip extends to increasingly smaller values of $|x|$ with increasing z , so that relative motion existed almost entirely across the contact width near the free edge of the pad. Experimental evidence of this effect is provided in Fig. 9, which shows an annular wear scar on the surface of a nominally flat titanium alloy fretting pad. As demarcated on the photograph, this microslip zone expands to cover nearly the entire contact width near the free face of the indenter, as indicated by the wear debris associated with the small-amplitude relative motion.

4. Application : Fretting Fatigue of Riveted Aircraft Structure

The near-surface conditions resulting from fretting contacts are recognized widely as a persistent cause of crack nucleation in structural components, including riveted lap joints that comprise typical primary structural elements in aircraft^{(10),(11)}. Previous work by the authors has indicated that the through-thickness restraint offered by installed rivets may have a crucial influence on the load transfer process and related development of fretting damage in riveted lap joints⁽¹²⁾. The residual stresses induced during the riveting process also play an important role in fretting crack nucleation and subsequent propagation of this damage.

With the validated ability to combine thermography with the finite element method to resolve multi-axial states of stress and the irreversible effects of friction, this approach was applied to the assessment of the effects of fretting on fatigue failures in riveted aircraft structures. To characterize the localized conditions in these joints responsible for this fatigue damage, the distribution of load among the rows must

be determined accurately by the finite element model. The infrared thermal imaging system provided accurate estimates of both the load transfer and related stress distribution in lap joint test specimens, thus allowing accurate boundary conditions to be prescribed in the finite element model. The remainder of this document will describe the experimental program and the modeling results.

4.1 Experimental details

The specimen used in the experimental program was a single lap joint assembled with nine 2117-T4 aluminum alloy universal head rivets (MS2047AD6-6). The two plates of the lap joint were produced from bare 2024-T3 aluminum sheet (2.3-mm thickness). The nine rivets were distributed in three rows with an equal spacing of 25.4 mm between each rivet. The experimental program targeted quantification of the effects of both manufacturing and applied loads on these riveted lap joints. The manufacturing parameter studied was the maximum quasi-static squeeze force used to buck the rivet. The cyclic stress applied to the assembled joint was representative of actual aircraft service conditions. Three stress levels between 69 MPa and 110 MPa were used on joints assembled with maximum squeeze forces of 15.5 kN, 20.0 kN and 22.2 kN, while four levels between 69 MPa and 110 MPa were used with joints produced with the lowest squeeze force of 11.1 kN. The R -ratio (ratio of minimum applied stress to maximum applied stress) in each test was 0.01. Two repetitions of each set of experimental conditions were performed, with two additional specimens manufactured with squeeze forces of 11.1 kN and 22.2 kN used for the thermal imaging experiments. A more complete summary of these experimental details may be found elsewhere^{(13),(14)}.

4.2 Fretting damage in riveted lap joints

Post-mortem inspection of the lap joint specimens clearly revealed regions of fretting damage at both the rivet/hole interface and the interface between the two plates forming the lap joint. Also, as the cyclic applied stress was increased, a reduction in the fatigue life was recorded. Similarly, increased squeeze forces correlated with increases in joint lives. Additional details of these observations may be found elsewhere^{(13),(15)}.

To analyze these experimental results and provide a design tool for riveted lap joint structures, a finite element model of a single lap joint was formulated. As alluded to earlier, application of the correct boundary conditions was critical to an accurate modeling effort. To determine the appropriate model boundary conditions, a thermal imaging system was used to record temperature changes associated with

the stress-related thermoelastic effect.

4.3 Thermal imaging of lap joints

By using the infrared imaging system presented earlier, it was possible to measure small changes in temperature during cyclic loading of the lap joint specimens that corresponded to the changes in the hydrostatic stress in the sheet material. The surfaces of each specimen were imaged using an infrared thermal sensor with an indium-antimonide focal plane array similar to that presented earlier. The specific infrared imaging system employed in this study used a synchronization signal obtained from the load waveform signal generator as a basis for filtering the raw intensity data collected by the focal plane array. This filtering technique yielded a high signal-to-noise ratio over the 128×128 -pixel field of view, enabling accurate determination of the intensity amplitude at the loading frequency.

The changes in the measured intensity during the loading cycle were then related to the changes in the hydrostatic stress through the use of a calibration for the constant C in Eq.(1). This calibration was obtained by measuring the infrared response over a region far removed from the rivet rows where the applied stress was a uniform, known amplitude and correlating this amplitude to the measured change in intensity.

Figure 10 shows the contours of the hydrostatic stress change, $\Delta\sigma_{hh}$, on the side of the driven rivet heads at two points in the load history for an experiment conducted at 10 Hz. The remote applied stress amplitude, directed along the vertical axis of each of these images, was 110 MPa, with an R -ratio of 0.10. By providing detailed information regarding the stress distribution in each of the rivet rows, this technique offered the ability to easily identify the percentage of load transferred through each row of rivets without the use of multiple strain gage rosettes. These images reveal that the first or top row of rivets in each image carries the highest percentage of the total load, thereby identifying it as the critical row for fatigue crack nucleation in this joint configuration.

Furthermore, the influence of the two squeeze forces used during rivet installation is clearly seen in Fig. 10, with the higher squeeze force producing larger changes in the hydrostatic stress near the rivets. Figure 10 also shows that the specimen geometry and small variations in the riveting process resulted in a non-uniform stress distribution just ahead of the top row. This observation was incorporated into the finite element model of this test to accurately preserve the boundary conditions.

4.4 Riveted single lap joints

To accurately model the local conditions near the

rivet/hole interface, the finite element model incorporated the residual stresses induced during rivet bucking or forming. These data were obtained from an elastic-plastic axisymmetric model of squeeze force-controlled rivet installation that captured the strong through-thickness variation of the residual stress field⁽¹⁶⁾. It was noted from the model results, though, that the residual through-thickness clamping effects were negligible for rivets installed with the range of squeeze forces used in the test program. This observation allowed two-dimensional plane strain conditions to be employed in the subsequent joint model.

Figure 11 presents the mesh used for the finite element analysis of the single riveted lap joint. The mesh generation used the final results of the riveting model to determine the initial geometry of the rivet/plate interface. The related residual stress field from the riveting results was then mapped to the nodes of the joint model. An equilibrium step followed this mapping to resolve the contact conditions at the interface. Note that upon consideration of symmetry of the rivet rows, only one and one-half rivets were modeled. By attaching elastic spring elements to the edge of the model and adjusting the value of the applied load, the load transfer percentage through any of the three rows of rivets could be modeled. However, since all failures were observed at the two extreme rows, subsequent results are presented only for these critical rows.

The model of 9200 elements and 9185 nodes was solved using the commercial finite element code ABAQUS/Standard. Coulomb friction, with a coefficient of friction of 0.5, was used in modeling the contact interaction between the rivet and plate. Furthermore, an isotropic hardening model was employed to capture small-scale yielding behavior at and around the rivet/hole interface. A complete summary of the material properties used in the model is contained in Table 1. Additional details of the finite element model may be found in the literature⁽¹⁵⁾.

4.5 Lap joint model results

Upon successful completion of the equilibrium step, the remote cyclic tensile stress was applied in the form of loads distributed along the right vertical boundary. These loads were varied for two complete cycles, after which time the model settled into a periodic solution known as the shakedown state. The stiffness of the spring elements connected to the support layer was varied to obtain the correct load transfer for the rivet row of interest. Figure 12 presents a comparison between the contours of $\Delta\sigma_{hh}$ from the thermal imaging experiments and the finite element results. The solid agreement between the measured and predicted results serves to validate the modeling

Table 1 Mechanical properties for plate and rivet material

$E_{\text{plate}}, \nu_{\text{plate}}$	72.4 GPa, 0.33
$E_{\text{rivet}}, \nu_{\text{rivet}}$	71.7 GPa, 0.33
$\sigma_{\text{yield, plate}}$	275 MPa
$\sigma_{\text{yield, rivet}}$	165 MPa
tangent modulus, plate	9.65 GPa
rivet hardening, $\sigma = C(\epsilon)^m$	$C = 544 \text{ MPa}, m = 0.23$

Fig. 11 Finite element mesh used for solution. Dimensions shown are in millimeters.

approach, which included the use of the thermal imaging technique to determine appropriate boundary conditions for the finite element model. These finite element results have been used in conjunction with a multiaxial fatigue life parameter^{(1),(2)} and the characterization of the residual stresses induced during riveting to predict the observed fatigue lifetimes of single lap joint test articles manufactured under controlled conditions⁽¹³⁾.

5. Conclusions

The experimental tools detailed here have guided and validated the formulation of a mechanics-based analysis for fretting fatigue in an array of structural systems. This integrated approach is capable of predicting fretting fatigue lives of riveted aircraft single lap joints from monitored load histories, the associated stress fields in the joints and uniaxial fatigue data.

Fig. 12 Comparison of $\Delta\sigma_{kk}$ obtained from thermal imaging experiments (top) and the finite element model (bottom) for rivet row carrying the greatest percentage of transferred load. With the remote cyclic stress $\Delta\sigma_o$ applied at the top of each panel, this critical row corresponds to the top rivet row of the test articles.

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References

- (1) Szolwinski, M.P. and Farris, T.N., Mechanics of Fretting Fatigue Crack Formation, *Wear*, Vol. 198 (1996), pp. 93-107.
- (2) Szolwinski, M.P. and Farris, T.N., Observation, Analysis and Prediction of Fretting Fatigue in 2024-T351 Aluminum Alloy, *Wear*, Vol. 221 (1998), pp. 24-36.
- (3) Johnson, K.L., *Contact Mechanics*, (1985), Cambridge University Press, Cambridge.
- (4) Szolwinski, M.P., Harish, G., Farris, T.N. and Sakagami, T., In-Situ Measurement of Near-Surface Fretting Contact Temperatures in an Aluminum Alloy, *ASME Journal of Tribology*, Vol. 121, No. 1/2 (1999), pp. 11-19/340.
- (5) Boley, B.A. and Weiner, J.H., *Theory of Thermal Stresses*, (1960), John Wiley and Sons, New York.
- (6) Harish, G., Szolwinski, M.P., Harish, G., Farris,

- T.N. and Sakagami, T., Evaluation of Fretting Stresses Through Full-Field Temperature Measurements, *Fretting Fatigue: Current Technology and Practices*, ASTM STP 1367, edited by Hoepfner, D.W., Chandrasekaran, V. and Elliott, C.B., (2000), pp. 423-435, American Society for Testing and Materials, West Conshohocken, PA.
- (7) Szolwinski, M.P., The Mechanics and Tribology of Fretting Fatigue with Application to Riveted Lap Joints, Ph.D. Thesis, School of Aeronautics & Astronautics, Purdue University, (1998), West Lafayette, IN.
 - (8) Szolwinski, M.P., Tieche, C., Harish, G., Farris, T.N., Sakagami, T. and Heinstein, M., Mechanics of Finite-Depth Fretting Contacts, 1999 Society for Experimental Mechanics Spring Conference, (1999), Society for Experimental Mechanics, Cincinnati, OH.
 - (9) Mindlin, R.D., Compliance of Elastic Bodies in Contact, *Journal of Applied Mechanics*, Vol. 16 (1949), pp. 259-268.
 - (10) Hoepfner, D.W., Elliott III, C.B. and Moesser, M.W., The Role of Fretting Fatigue on Aircraft Rivet Hole Cracking, Tech. Rep. DOT/FAA/AR-96/10, FAA, October, (1996).
 - (11) Piascik, R.S. and Willard, S.A., The Characteristics of Fatigue Damage in the Fuselage Riveted Lap Splice Joint, Tech. Rep. NASA/TP-97-206257, NASA Langley Research Center, November, (1997).
 - (12) Harish, G. and Farris, T.N., Shell Modeling of Fretting in Riveted Lapjoints, *AIAA Journal*, Vol. 36, No. 6 (1998), pp. 1087-1093.
 - (13) Szolwinski, M.P., Harish, G., McVeigh, P.A. and Farris, T.N., Experimental Study of Fretting Crack Nucleation in Aerospace Alloys with Emphasis on Life Prediction, *Fretting Fatigue: Current Technology and Practices*, ASTM STP 1367, edited by Hoepfner, D.W., Chandrasekaran, V. and Elliott, C.B., (2000), pp. 267-281, American Society for Testing and Materials, West Conshohocken, PA.
 - (14) Harish, G., Farris, T.N., Sakagami, T. and Szolwinski, M.P., Observing and Modeling the Onset of Widespread Fatigue Damage in Riveted Aircraft Structures, 1999 Society for Experimental Mechanics Spring Conference, (1999), Society for Experimental Mechanics, Cincinnati, OH.
 - (15) Harish, G. and Farris, T.N., An Integrated Approach for the Prediction of Fretting Crack Nucleation in Riveted Lap Joints, A Collection of Technical Papers, Proceedings of 40th AIAA/ASME/ASCE/AHS/ASC Structures, Structural Dynamics and Materials (SDM) Conference, St. Louis, MO, (1999), AIAA 99-1340, CD-ROM.
 - (16) Szolwinski, M.P. and Farris, T.N., Linking Riveting Process Parameters to the Fatigue Performance of Riveted Aircraft Structures, *Journal of Aircraft*, Vol. 37, No. 1 (2000), pp. 130-137.