Fatigue Life and Fatigue Crack Growth Behavior of Nonthrough Cracks in Friction Stir-Welded 2024-T3 Aluminum Alloy

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Friction stir-welded (FSW) joints are evaluated under two welding conditions to investigate their properties for application to aircraft structures. It is found that the welding condition affects the hardness profile, static strength, and fracture location. The tool mark near the burr, the kissing bond, the burr, and the edge of the specimen within the base material are found to be the origins of fatigue fractures in FSW joints, and the welding condition affects these origins and fatigue life. The kissing bond is not the origin of a fracture when low stress is applied. The fatigue life of a FSW joint is longer than that of a riveted joint. An evaluation of fatigue crack growth for each case via observation of the fracture surface indicates that the crack growth rate when the kissing bond is the origin of the fracture is close to that of the base material. The crack growth rate when the tool mark near the burr is the origin of the fracture is different from that of the base material: Overestimating the stress intensity factor range based on assuming the crack geometry contributes to the difference.

Key Words: Mechanical Property, Friction Stir-Welded Joint, Origin of Fracture, Fatigue Crack Growth, Riveted Joint

Nomenclature

- a: crack length in thickness direction
- b: half specimen width
- c: crack length along width direction
- F: boundary correction factor
- f_w: finite width correction factor
- f_θ: angular function
- g_{ij}: curve fitting function
- ΔK: stress intensity factor range
- ΔK_{eff}: effective stress intensity factor range
- K_{ij}: stress concentration factor
- M_{ij}: curve fitting function
- N: number of fatigue cycles
- Q: shape factor of elliptical crack
- R: stress ratio
- t: specimen thickness
- φ: parametric angle of ellipse
- λ: function defined in text
- σ: stress
- Δσ: stress range
- ξ: normalized crack length

Subscripts

- x: x direction
- y: y direction
- i: 1 or 2
- j: 1, 2 or 3

1. Introduction

It is necessary to identify the mechanical behaviors and environmental resistances of the structural configurations used in aircraft. A riveted joint, which is a typical mechanically fastened joint, has been used in aircraft structures for several decades. Despite this long period of time, changes to airworthiness requirements prompted by incident and accident investigations require increased knowledge of the properties of this type of joint, and many research activities have been conducted to evaluate its mechanical properties.1–3) Xiong and Bedair evaluated the stress carried at each rivet row using an analytical and numerical procedure, and showed a good correlation between them.4) Rahman et al. evaluated the boundary correction factor of the stress intensity factor for a crack emanating from a countersunk rivet hole in order to evaluate the crack growth behavior under several stress conditions.5) The results showed that a variety of boundary correction factors can be used for evaluating widespread fatigue damage, and that the boundary correction factor is the highest for a crack close to the boundary condition. Chen et al. obtained the crack tip opening angle using elastoplastic finite element analysis in order to evaluate the residual strength of a crack in a fuselage panel, and showed good agreement between the test and analysis results.6) Bakuckas7) and Wang et al.8) evaluated the multisite damage to a riveted lap joint. Muller,9) Terada,10) and Li et al.11) evaluated the effects of squeezing a rivet and the interference fit between the rivet and rivet hole, and showed that these parameters affect the fatigue properties of riveted joints. One of the authors of this paper has evaluated the effects of the side edge due to tight clamping and using stiffeners on riveted joints,12) as well as fatigue crack growth up to the first link of up to two adjacent cracks in wide riveted joints with a skin having a thickness of 1.27 mm.13)
In addition, other types of joints have been developed to reduce the manufacturing cost. Friction stir welding was invented in 1991, and it was intended to be applied to primary aircraft structures. Therefore, friction stir welding has been the subject of similar investigations of riveted joints. Sato and Kokawa,14) Rhodes et al.,15) and Sutton et al.,16) evaluated the microstructure formed by friction stir welding. It was observed that the global yielding and ultimate strengths were affected by the hardness profile of the joint. The banded microstructure observed affected the fracture process of the joint. Jones et al.17) investigated the hardness of a friction stir-welded (FSW) 2024-T351 aluminum alloy and proved that the microstructure affects the hardness profile of the joint. The fatigue properties of an FSW joint were evaluated by Lemmen et al.,18) Ali et al.,19) Dickerson and Przydatek,20) Uematsu et al.,21) Besel et al.,22,23) and Gemme et al.,24) and the fatigue crack growth properties have been evaluated by several researchers.25–30) Kumagai31) reported the static and fatigue properties, and the resistance to stress corrosion cracking of FSW joints for 2000- and 7000-series aluminum alloys. In addition, the FSW subcommittee in the Japan Light Metal Welding and Construction Association has evaluated the properties of FSW joints in order to determine the prevailing FSW technique and standardize the FSW process.32–38) The authors and collaborators have evaluated the static and fatigue properties of FSW joints39) and several fatigue crack growth properties of through cracks in FSW joints.40)

A damage-tolerant design mostly requires the fatigue crack growth behavior of the initial defect, such as a non-through crack. The location of the initial defect is usually based on the possibility of fatigue crack growth. In addition, the limit of validity (LOV) corresponds to the average fatigue life of the structure when it does not incur widespread fatigue damage in the as-is manufacturing condition. However, most research on the fatigue crack growth of FSW joints has focused on the fatigue crack growth of through cracks in the residual stress field formed by friction stir welding. Therefore, the crack growth behavior of nonthrough cracks in FSW joints has to be evaluated before enabling application to aircraft structures. Moreover, evaluation of the crack growth behavior should be based identifying the origin of the fracture in the FSW joint. This knowledge is essential to enable the evaluation of friction stir welding in terms of the requirements for type certification of aircraft structures, even if the welding quality has been improved according to the studies mentioned above.

In this study, hardness, static, and fatigue tests were carried out for FSW joints consisting of a 2.0-mm-thick 2024-T3 aluminum alloy under two welding conditions in order to evaluate their properties for application to aircraft structures. The origins of fracture and the fatigue crack growth behavior of the joint were identified by observation of the fracture surface. A fatigue test of a conventional riveted joint with the same skin thickness was also conducted in order to compare its mechanical properties to those of the FSW joint.

## 2. Experimental Procedure

### 2.1. FSW joints

The FSW panels used for this research were 2024-T3 aluminum alloy sheets with a thickness of 2 mm and supplied by Japanese aircraft manufacturers. The rolling direction of these sheets was parallel to the welding direction, and FSW specimens were cut from the welded panels. Two types of FSW joints were manufactured for this research. The tool and rig for welding were independently prepared by the manufacturers. Each welding condition was selected to be adequate for each welding tool on the basis of the experience of the manufacturers; thus, the detailed welding conditions are confidential. However, it was confirmed that other welding conditions such as the tilt angle of the welding tool and the control procedure for the protruding depth of the welding tool were identical for both welding conditions. On the basis of the cross-section of the FSW joint, the sizes of the pin and shoulder for welding condition A were about 4 and 10 mm, and those for welding condition B were 2 and 6 mm, respectively. The ratios of the tool rotation speed to the tool traveling speed for each welding condition are 1.97 and 5.53, respectively.

Figure 1 shows the residual stress distributions along and perpendicular to the weld line for both welding conditions. The stresses in x and y directions indicate the stresses perpendicular and along the welding direction. To measure the residual stress distribution, strain gauges were attached to both sides of the jointed panel after welding. First, the strain was measured with gauges attached to the joint. Next, the pieces with the strain gauges were cut from the joint, and the strain was measured again. The difference between these strain measurements corresponds to the residual stress. The residual stress distribution parallel to the weld line for weld B was mostly symmetric with respect to the weld line, whereas that for weld A exhibited a difference outside of the weld line in the region of compressive residual stress. The residual stress parallel to the welding direction had a peak tensile stress at the center of the weld line and a compressive peak outside of the weld line. The residual stress distributions perpendicular to the weld line for each welding condition were mostly symmetric with respect to the weld line. The peak

![Fig. 1. Residual stress distributions of the FSW joints.](image)
tensile stress was observed outside of the weld line.

Metallographic observations of welding conditions A and B were presented in Okada et al.39) and Okada et al.,40) respectively.

2.2. Hardness test of the FSW joints

The hardness distribution in the welding area was measured to evaluate the mechanical features perpendicular to the weld section. A microindentation hardness test procedure (ASTM E-384) was utilized. The hardness profile was measured at the middle in the thickness direction. The indentation force was 9.8 N.

2.3. Static test of the FSW joints

The specimen geometry for the static test is shown in Fig. 2. Specimens were cut from an FSW panel such that the welding direction was perpendicular to the loading direction. Further, the weld line was located at the center of the specimen. No additional surface treatment was applied to the specimens. A static test based on ASTM E-8 was conducted. Five specimens were tested for each condition, and the average values of the yield strength, ultimate strength, and elongation were obtained.

2.4. Fatigue test of the FSW joints

The specimen geometry used for the fatigue test is shown in Fig. 3. The requirements for preparing the fatigue test specimens were the same as those for the static test specimens. Fatigue tests based on ASTM E-466 were conducted. The specimens were subjected to cyclic loading with a load frequency of 10 Hz at room temperature and normal relative humidity. The fatigue test conditions utilized in this test were as follows: $\Delta \sigma = 135, 180, 225, \text{ or } 270 \text{ MPa for } R = 0.1$. The stress range for the FSW joint was calculated using the thickness of the plate. This means that the thickness reduction at the weld section resulting from FSW was not considered in this case. After the fatigue test, the fatigue life was recorded, and the fracture surfaces were examined by the CCD camera.

3. Results and Discussion

3.1. Observations of the welding sections

Figure 5 shows optical micrographs for each welding condition. The section is etched to see the microstructure, and the bright area corresponds to the stir zone (SZ) and thermo-mechanically affected zone (TMAZ). As shown in the figure, the heights of the tool mark and burr for welding condition A were about 0.05 and 0.45 mm, respectively, and the thickness reduction at the weld line was about 0.15 mm. For welding condition B, the heights of the tool mark and burr and thickness reduction at the weld line were about 0.02, 0.22, and 0.06 mm, respectively.

3.2. Hardness profile

For comparison with the fatigue properties of the FSW joints, riveted lap joints were fabricated by a Japanese aircraft manufacturer using plates of the same 2 mm-thick 2024-T3 aluminum alloy used for the FSW joints. A countersunk rivet made of a 2117-T4 aluminum alloy was used to fasten the joint. Figure 4 shows the geometry of a single lap joint specimen riveted by three rows of three fasteners. The width and length of the joint were 60 and 420 mm, respectively. The diameters of the rivets and rivet holes were 3.96 and 4.0 mm, respectively. The pitch of the rivets in the vertical and parallel directions was 20 mm. In order to obtain the fatigue properties under the as-is condition, no artificial flaws were applied to the specimens. A constant-amplitude fatigue load was applied to the lap joints. Five types of stress ranges, 123.5, 100.0, 79.8, 69.8, and 59.8 MPa with a stress ratio of 0.05, were utilized for this test. For the riveted joint, the stress range was calculated using the thickness and width of the joint, and reduction of the section as the result of the rivet hole was not considered. The test frequency was 5 Hz. After the fatigue test, the fatigue life was recorded and the fracture surfaces were examined by the CCD camera.
Fratini et al. evaluated the hardness profile of the section in 2024-T341 FSW joint and obtained W-shaped distribution. The hardness profile in FSW precipitation-hardened aluminum alloys depends on the temper of the alloy, and the hardness distribution observed is one of the typical shapes. In the reference, the increased hardness at the center of the weld was considered to be reprecipitation based on the natural aging after welding. In addition, severe aging in the heat-affected zone (HAZ) would cause hardness to decrease. As reported in Jones et al. and Lemmen et al., the hardness of a 2024-T3 FSW joint at SZ is, at most, the same as that of the base material.

In Okada et al., the authors evaluated the hardness distribution of welding condition A at 0.5, 1.0, and 1.5 mm from the bottom. The average hardness of the base material at 0.5 and 1.5 mm from the surface was 137 HV and that in the middle was 127 HV. As shown in Fig. 6, the hardness in SZ and TMAZ for welding condition A is around the average hardness of the base material at 0.5 and 1.5 mm from the surface.

The pin diameter for welding condition A was about twice that for welding condition B. The plastic flow during welding for welding condition A would be larger than that for welding condition B. The hardness of the SZ and TMAZ was close to that of the base material at the surface. This is because a part of the metallic structure around the surface moved around the middle during stirring and reprecipitation occurred around the center of the weld. The FSW panel in this research is 2.0 mm thick and is thinner than the plate in other references. For thinner plate, heat input during welding is transferred to the backing plate and rig sooner, which inhibits aging and decreasing hardness in the HAZ.

### 3.3 Static test results

Table 1 summarizes the static test results for the FSW joints and base material. In order to obtain elongation at the fracture, we fit ends of the fractured specimen lightly together and measure the distance between the gauge mark. The elongation is obtained as the ratio of the difference before and after the test divided by that of the initial distance.

As shown in Fig. 7, the entire fracture surface of the base material was oriented at an angle with respect to the loading direction and was a typical feature of the thin sheet. As stated in Okada et al., the average yield strength of a specimen for welding condition A is 339.4 MPa, 6% higher than that of the base material, and its ultimate strength is 435.2 MPa, 10% lower than that of the base material. The elongation at failure is 8.0% and is 40% of that of the base material.

Observations of the fracture surface for welding condition A identified a kissing bond on the root side. As shown in Fig. 8(d), it became the origin of static failure, and the fracture surface was oriented at an angle with respect to the loading direction, except for the surface by the kissing bond. The kissing bond was considered to be one of the key reasons for the decrease in the ultimate strength. The height of the kissing bond for each specimen was measured from the fracture surface. The average and variation coefficient of the kissing bond height were 0.152 mm and 0.034, respectively. Additionally, the variation coefficient of the yield stress and tensile strength were 0.009 and 0.052, respectively. The test
specimens for each welding condition were cut out from the same FSW panel. Based on the test results, the variability of the property within the FSW panel is considered to be small. For welding condition B, the average yield strength is 333.4 MPa, 4% higher than that of the base material, and the ultimate strength is 6% lower than that for the base material. Observations of the fracture surface and fracture location indicated that the origin of fracture was not the kissing bond. It was approximately 1.2 mm away from the center of the weld line and a little outside of the bright area in Fig. 5. Therefore, the fracture origin is considered to be in the HAZ. Thus, the reduced elongation due to welding contributed to reducing the ultimate strength.

3.4. Fatigue test results

Figure 10 shows the fatigue test results for the FSW specimens and the base material in as-received surface conditions. In Fig. 10, the slope of the S–N curve when the kissing bond is the origin of the fracture is indicated by a thick solid line, and the curve when the tool mark near the burr is the origin of the fracture is indicated by a thick dashed line. The fatigue life of the FSW joint was significantly lower than that of the base material. For a stress range of 135 MPa, the specimens did not fail for 5 million cycles.

Observation of the fracture surface for welding condition A identified two origins of fractures, the kissing bond and tool mark near the burr, as shown in Figs. 11 and 12. Failure due to a fracture originating from the kissing bond was observed for stress ranges of 270 and 225 MPa. The fatigue life was longer when the fracture originated at the tool mark near the burr.

For a stress range of 180 MPa, only failure due to a fracture originating by the tool mark near the burr was observed. As shown in Fig. 10, the S–N curve for the tool mark near the burr was steeper than that for the kissing bond, and the fatigue life when the kissing bond was the origin of the fracture was shorter than that when the tool mark near the burr was the origin of the fracture.

The fatigue life of welding condition B was higher than that of welding condition A. However, it was still lower than that of the base material. The origin of the fracture was around the burr (Fig. 13) or at the edge of the end point of the gauge length (Fig. 14).

As described in Sec. 3.1, the height of the burr and reduction in thickness at the weld line for welding condition B are...
smaller than those for condition A. Thus, their effects on the origin of the fracture and fatigue life become less detrimental, and some specimens do not fracture at the burr.

The fatigue test results for the riveted lap joint are indicated by the plus (+) symbols in Fig. 10. As shown in the figure, the fatigue lives of the riveted joints are much shorter than those of the FSW joints, and the riveted joints fractured at a stress range of 59.8 MPa. The test results do not show the fatigue limits of the riveted joints.

Photographs of the specimens after the fatigue test show that the origin of the fracture was the manufacturing side of the joint (Figs. 15 and 16). Inspection of the failed joint also revealed fretting damage to the matting surfaces, and between the sides of the rivet holes and shanks of the rivets. Fatigue cracks grew from this fretting damage in all cases. Therefore, fretting damage was found to be the origin of fracturing in the riveted joints. After a crack forms from fretting damage, the surface crack grows in the thickness and width directions.

The S–N data for several stress concentration ratios, $K_t$, are collected in MMPDS-10, and some of the data points are plotted in Fig. 10. The graph shows that the data for welding condition A when the origin of the fracture is the tool mark near the burr are close to the data for $K_t = 2.0$. 

Fig. 11. Fracture surface for welding condition A when a kissing bond is the origin of the fracture ($\Delta \sigma = 275$ MPa, failed at 5,000 cycles): (a) top view, (b) fracture surface between the kissing bond and final failure surface, (c) 0.30 mm from the bottom surface, and (d) 0.74 mm from the bottom surface.

Fig. 12. Fracture surface for welding condition A when the tool mark near the burr is the origin of the fracture ($\Delta \sigma = 225$ MPa, failed at 86,000 cycles): (a) top view, (b) fracture surface between the tool mark near the burr and the final failure surface, (c) 0.72 mm from the top surface, and (d) 1.20 mm from the top surface.
As mentioned earlier, the height of the tool mark is 0.45 mm, which is quite large compared to the width of the weld line. Thus, it is difficult to obtain the stress concentration factor using a numerical evaluation. However, an equivalent level of stress concentration could be predicted by comparing the S–N data with stress concentration. On the other hand, the equivalent level of stress concentration for welding condition B seems to be a certain value between 1.5 and 2.0.

For the riveted joint, the fatigue test results were close to the MMPDS data for $K_t = 5.0$. The stress concentration factor of an open hole is 3.0, and that for a filled hole, which is the case for a riveted joint, is somewhat lower. Observation of the fracture surfaces identified that the fretting damage formed by relative slip between the mating surfaces was the origin of the fracture for the riveted joint. It is well-known that fretting damage to the surface degrades the fatigue life of an aluminum alloy. This means that stress concentration is not the only reason for the lower fatigue life of riveted joints. Owing to these two characteristics, the fatigue limit of a riveted joint is lower than that of a FSW joint.

Compared to the FSW joint, the origin of the fracture for the riveted joints is fretting damage, as identified by observations of the fracture surfaces. Fretting damage is formed due to a contact force and the relative movement at the mating surface.
surfaces of the skins or holes in the skin and rivet shanks during cyclic loading. As discussed by many researchers,\textsuperscript{1,9,12} this is affected by the clearance of the rivet and rivet hole, and the squeezing force of the rivet. The contact force causes fretting stress on the mating surfaces. At areas where the stress applied is lower than the fretting stress, there is no relative slip at the mating surface, and fretting does not occur. When the stress applied is greater than the fretting stress, relative slip arises, and fretting occurs on the mating surfaces. When a higher stress is applied, relative slip occurs over larger areas; consequently, the edge of the slip area moves toward the centerline of the rivets. This can be confirmed by observation of the specimen after fracturing occurs. The origin of the fracture is located close to the centerline of the rivet holes at higher stress levels: this was 0.9 mm away from the center of the rivet hole with a stress range of 100 MPa, as shown in Fig. 15. On the other hand, the location of fracture origin deviates 2.0 mm from the centerline in the vertical direction at a stress range of 69.8 MPa. Furthermore, as shown in Figs. 15 and 16, applying a lower stress results in a smaller area of the ligament for final failure.

In this research, there are two origins of fracturing in FSW joints for welding condition A: a kissing bond and a tool mark near the burr. SEM observations reveal striations on the fatigue surfaces in both cases (Figs. 11 and 12). Striations were observed between 0.25 and 0.82 mm from the bottom along the thickness direction when the kissing bond was the origin of the fracture and between 0.62 and 1.25 mm from the top along the thickness direction when the tool mark near the burr was the origin of the fracture.

The fatigue crack growth behavior of the specimens when the origin of the fracture was the kissing bond was obtained from SEM observations, and is indicated by open circles in Fig. 17. Five photos in each stress intensity factor range were obtained. The average for several striation spacing in each photo is measured first, and then the average in each stress intensity factor range is calculated and plotted in the figure. The behavior when the tool mark near the burr was the origin of the fracture was also evaluated using SEM observations, and is indicated by open squares in Fig. 17.

In Okada et al.,\textsuperscript{40} the fatigue crack growth rate of the base material for the middle tension specimen was obtained, and the relationship between the fatigue crack growth rate and effective stress intensity factor range was evaluated. The crack opening ratio was also calculated in Okada et al.,\textsuperscript{40} using the equation proposed by Newman.\textsuperscript{43} Using the $da/dN−ΔK_{eff}$ curves and crack opening ratio, the $da/dN−ΔK$ curves of the base material for $R = 0.1$ was obtained and is shown in as the solid line in Fig. 17. The stress intensity factor range for welding condition A was obtained for the edge crack with equal crack length along the width direction of the specimen. The stress intensity factor was calculated using the equation described in Tada et al.\textsuperscript{44} This assumption is based on observation of the fracture surface.

\[
ΔK(a) = Δσ\sqrt{πaF(ξ)}
\]

\[
F(ξ) = \frac{2}{πξ}\tan\left(\frac{πξ}{2}\right)\left(0.752 + 2.02ξ + 0.37\left(1 - \sin\left(\frac{πξ}{2}\right)\right)\right)/\cos\left(\frac{πξ}{2}\right)
\]
The panel. Therefore, the stress intensity factor does not include the effect of residual stress in this case.

The da/dN–ΔK curves for the specimens were obtained from the data, and the fatigue crack growth life was evaluated. For a specimen that failed from the kissing bond at approximately 5,000 cycles at a stress range of 270 MPa, observations of the fracture surface showed that the height of the kissing bond was approximately 0.24 mm. The maximum length of the fatigue crack in this case was approximately 0.92 mm, and the calculated fatigue life between these lengths based on the da/dN–ΔK curves is approximately 1,800 cycles. Therefore, the fatigue life until the formation of cracks at the kissing bond is about approximately 3,200 cycles. On the other hand, for a specimen that failed at approximately 86,000 cycles from the tool mark near the burr for a stress range of 225 MPa, multiple cracks were observed along the tool mark, and they merged approximately 0.62 mm from the surface. The maximum length of the fatigue crack was approximately 1.3 mm. The fatigue crack growth life for the crack between 0.62 mm and 1.3 mm was also calculated, and is approximately 1,300 cycles. Below a crack length of 0.62 mm, multiple cracks that form around the tool mark grow simultaneously. Therefore, the fatigue life developing a 0.62-mm crack from the tool mark is approximately 84,700 cycles.

These results show that the kissing bond separates and becomes a source of cracks much earlier than the tool mark near the burr. As shown in Fig. 10, the kissing bond does not act as the origin of a crack for a stress range of 180 MPa; the tool mark near the burr becomes the origin of the fracture in this case. At lower stress levels, the kissing bond will not separate during fatigue loading. Although it is not as strongly joined as other regions of the weld, the kissing bond is still a physical joint; thus, the changes in the stress around the kissing bond seem to be gradual as long as it is not opened. On the other hand, tool marks near a burr causes stress concentrations around the area under a wider stress level, and they could act as the origin of fractures at a lower stress levels in this case.

For welding condition B, two origins of fractures were also observed: the burr and the edge of the specimen. Observations of the fracture surface identified a quarter-ellipse shape for fatigue failure in both cases. Striations were also observed by SEM, as shown in Figs. 13 and 14. The crack growth rates obtained by the striations are also plotted in Fig. 17. The stress intensity factor for the corner crack was calculated using the equations proposed by Newman and Raju.45

\[
\Delta K(a) = \Delta \sigma \sqrt{\pi a F(a/c, a/t, c/b, \phi)}
\]

\[
F\left(\frac{a}{c}, \frac{a}{t}, \frac{c}{b}, \phi\right) = \frac{1}{Q^{0.5}} \left( M_1 + M_2 \left(\frac{a}{t}\right)^2 + M_3 \left(\frac{a}{t}\right)^4 \right) g_2 \phi f_w
\]

\[
Q = 1 + 1.464 \left(\frac{a}{c}\right)^{1.05}
\]

\[
M_1 = 1.08 - 0.03 \frac{a}{c}
\]

\[
M_2 = -0.44 + 1.06 \left(0.3 + \frac{a}{c}\right)
\]

\[
M_3 = -0.5 + 0.25 \frac{a}{c} + 14.8 \left(1 - \frac{a}{c}\right)^{15}
\]

\[
g_1 = \left(1 + \left(0.08 + 0.4 \left(\frac{a}{t}\right)^2\right) (1 - \sin \phi)^3\right)
\]

\[
g_2 = 1 + \left(0.08 + 0.15 \left(\frac{a}{t}\right)^2\right) (1 - \cos \phi)^3\]

\[
f_\phi = \left(\frac{a}{c} \left(\cos \phi\right)^3 + (\sin \phi)^2\right)^{0.25}
\]

\[
f_w = 1 - 0.2 \lambda^2 + 9.4 \lambda^2 - 19.4 \lambda^3 + 27.1 \lambda^4
\]

\[
\lambda = \frac{c}{b} \left(\frac{a}{t}\right)^{0.25}
\]

In these equations, \(\phi\) is a parametric angle of the ellipse. The same as for welding condition A, the effect of residual stress is not included in the evaluation of the stress intensity factor for welding condition B. Based on observation of the fracture surface, the aspect ratio of the quarter-ellipse fatigue crack at final failure was measured, and the ratio was used to calculate the stress intensity factor for the entire length of the crack.

Figure 17 shows that the crack growth behavior when the origin of the fracture is the kissing bond for welding condition A is close to that for the base material. The crack growth rates for welding condition B relatively coincides with that of the base material. However, the crack growth rate for welding condition A when the tool mark near the burr is the origin of the fracture is somewhat different from that of the base material, and the difference is larger for longer crack lengths.

In this paper, the authors simulate the crack for welding condition A as an edge crack from the top or the bottom depending on the origin of the fracture. In the case of the origin being the tool mark near the burr, the actual crack front along the width direction was curved, not straight. The stress intensity factor range calculated would be overestimated based on this assumption. The stress intensity factor range for the semi-elliptical surface crack of which the width was the same as the specimen width was calculated using NASGRO,56
and this was compared with the results obtained for the edge crack. The ratio of the stress intensity factor for the semi-elliptical surface crack to the edge crack was 0.83 for a 0.62-mm crack and was 0.45 for a 1.26-mm crack. The accurate stress intensity factor has to be obtained using the three-dimensional finite element method (3D FEM), because the crack length is not zero at either end, and then the actual crack shape is not semi-elliptical. However, the stress intensity factor range for the actual crack is less than that of the edge crack. This implies that the $da/dN=\Delta K$ curves for welding condition A in the case when a tool mark near the burr is the fracture origin would shift left and become close to that of the base material.

In the case when a kissing bond was the fracture origin, the crack in Sz grew along the thickness direction. As shown in Fig. 6 and Okada et al.,$^{39}$ the hardness in Sz is approximately 140 HV and there is little variability along the thickness direction. In other cases, the crack in HAZ grew or the base material, and the hardness around the surface and in the middle was 137 and 127, respectively. As shown in Figs. 11–14, no cleavage surface was observed and it is considered a ductile-type striation. As indicated in Fig. 17, there is little difference in the fatigue crack growth rate. Based on the results, a difference in hardness would not affect the fatigue crack growth feature in this case.

It is well known that the growth rate of a small crack has large scatter because of plasticity, metallurgy, roughness of the crack surface, and/or oxide/corrosion on the crack surface. Therefore, the evaluation of small crack growth using the stress intensity factor range based on linear elastic fracture mechanics is not appropriate.$^{37–49}$ In the NASTRO Reference Manual,$^{46}$ the scatter of the crack growth rate for 2024-T3 aluminum alloy at $R = 0$ was observed for $\Delta K < 10 \text{ MPa m}^{0.5}$. Most of the stress intensity factor range in this research was above $10 \text{ MPa m}^{0.5}$. In addition, the minimum crack length in thickness direction in case the striations were measured was 0.3 mm and it was one order higher than the grain size in thickness direction. Therefore, the crack growth behavior in this research was evaluated using the stress intensity factor range based on linear elastic fracture mechanics.

The NRCC and JAXA have conducted collaborative research work to assess the fatigue life of countersunk riveted joints.$^{13}$ The fatigue test results identified fretting damage to the matting surfaces of the joint as the origin of fractures that formed during fatigue loading. The life of the first fatigue crack observed, that of the first merging of adjacent cracks, and the final fatigue life were 1.95 million, 2.11 million, and 2,113,534 cycles with a stress amplitude of 60.5 MPa and $R = 0.069$. In Li et al.,$^{13}$ the fatigue life of a riveted joint up to the first merging of two adjacent rivet holes was divided in two regions: the fatigue life to form a 0.5-mm corner crack at a rivet hole in the matting surface, and the fatigue crack growth life at the 0.5-mm corner crack to first merging. On the basis of fatigue tests of riveted joints and 3D FEM analysis of a joint from manufacturing processes subjected to fatigue loading, the fatigue life to the formation of a 0.5-mm corner crack was predicted using the Smith–Watson–Topper (SWT) strain life equation. Further crack growth from a 0.5-mm crack to the first merging was predicted using the fatigue crack growth characteristics of the aluminum alloy and the effect of an adjacent crack using an in-house NRCC code, Can-GROW. Good correlation was obtained between the fatigue test results and the predicted service life.

Observation of the fracture surfaces of the riveted joints in this study identified fretting damage to the joint as the origin of the fracture. Because fretting damage is located around rivet holes, it is discretely distributed. The merging of adjacent cracks was associated with tensile failure of the rest of the ligament at a higher applied stress (Fig. 15), whereas it occurred through the merging of fatigue cracks under a lower applied stress (Fig. 16). This difference is only related to the mechanism of merging adjacent cracks. The splitting of the fatigue phase into the two regions mentioned above seems to be adequate given the observations of the fracture surfaces. This means that the crack growth behavior in a riveted joint could be modeled using the behavior in the base material when applying an appropriate stress intensity factor. As described in Li et al.,$^{13}$ detailed modeling of the riveted joint, including the manufacturing process, and the modeling of the effects of an external load and adjacent crack are imperative for evaluating the stress intensity factors of cracks in a riveted joint.

In 2011, the LOV was mandated as a new requirement for transport-category aircraft. The LOV is the period of time up to which it has been demonstrated that widespread fatigue damage (WFD) is unlikely to occur in an airplane structure by virtue of its inherent design characteristics and maintenance actions. In order to meet this requirement, a possible scenario for WFD must first be established. Then, it has to be demonstrated that WFD based on the scenario does not occur before the LOV. Although its validation does not necessarily require the prediction of WFD, a technique for predicting the occurrence of WFD is imperative for the development of lighter aircraft structures with a longer operation life. The procedure described above for testing riveted joints would be one candidate technique for predicting WFD. The test results for the FSW joints obtained in this research show several types of fatigue scenarios. However, additional evaluations comparing the FSW joints to riveted joints would be necessary to comply with the WFD requirement. As our research demonstrates, the possibility of locating the origin of fatigue fractures within the base material depending on the welding condition and higher fatigue limit as compared to that of the riveted joint would mitigate the number of evaluations.

4. Conclusion

In order to evaluate the features of FSW joints for the purpose of applying them to aircraft structures, hardness, static, and fatigue tests are conducted for two welding conditions. The hardness profile indicates that the welding condition affects the hardness distribution within the weld line, and a rel-
atively stable hardness profile can be obtained depending on the welding condition. No typical hardness reduction outside the weld line was observed in this research.

Static test results identified that a kissing bond reduces the ultimate strength of the FSW joint in cases where one existed. It also reduces the yield stress of the joint. In the case where a kissing bond did not form, the heat-affected zone became the origin of the fracture, reducing the ultimate strength of the FSW joint; however, there was no significant reduction in yield stress.

Fatigue tests and observation of the fracture surfaces identified a tool mark near a burr and a kissing bond as the origins of fractures for welding condition A, whereas a burr and an edge within the base material were identified as the origins of fractures for welding condition B. The differences in the sizes of the tool mark and burr for each welding condition affect the difference in the origin of the fracture for cases in which fractures occurred. For welding condition A, a tool mark near a burr can be the origin of a fracture at stresses above the fatigue limit because the stress concentration around it is a source of fatigue failure. On the other hand, the kissing bond is not an origin of fractures at lower stress levels because it does transfer stress at such stress levels. When the kissing bond is the origin of a fracture, the fatigue life of the joint is much shorter than that when a tool mark near the burr is the origin of fracture. From an evaluation of fatigue crack growth for each case based on observations of the fracture surfaces, the crack growth rate when a kissing bond is the origin of the fracture is close to that of the base material obtained from the middle tension specimen. The crack growth rate when a tool mark near a burr is the origin of the fracture is different from that of the base material; overestimating the stress intensity factor range based on assuming the crack geometry contributes to the difference.

For a riveted lap joint, the locations of the origins of fractures in the areas of fretting damage along the loading direction are close to the centerlines of the rivet holes at higher stress levels and are separated from the centerlines at lower stress levels. When higher stresses are applied, the area of relative slip between the mating surfaces is larger, and the edge of the slip area moves toward the centerline of the rivets. The FSW joint is found to have a longer fatigue life than that of the riveted joint. Although several origins of fracture and failure behaviors demand more resources for their evaluation, the possibility of locating the origin of fatigue fracture within the base material depending on the welding condition and the higher fatigue limit of an FSW joint compared to that of the riveted joint would mitigate the drawbacks regarding the WFD requirement.

References
