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The Effects of Surface Pits and Intermetallics on the Competing Failure Modes in Laser Shock Peened AA7075-T651: Experiments and Modelling

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Abstract

The effects of laser shock peening (LSP) on the fatigue life of AA7075-T651 were investigated. The combined influence of surface imperfections (i.e. pits and intermetallics), compressive residual stresses (CRS) and the applied stress on crack initiation sites (surface or subsurface) and the associated fatigue life were investigated. Critical surface imperfections were found to significantly reduce the benefits of LSP in life improvement, by promoting surface crack initiation despite the resisting effects of CRS. To facilitate quantifying the effects of LSP on fatigue life, a finite element (FE) model was developed to simulate residual stress distribution induced by LSP, as well as its redistribution caused by the formation of surface pits. Based on the FE results, a method identifying whether the specified surfaces pits and intermetallics are critical to lead to surface cracking at given stress conditions was proposed, based on the Smith-Watson-Topper method and the Murakami’s model respectively. The interaction between surface imperfections, CRS and the applied loads were taken into account in this method. In addition, a fatigue life assessment framework was proposed based on the prediction of crack initiation sites, which was validated to be reliable in efficiently evaluating the efficacy of the applied LSP in improving fatigue life.

Key words: Laser shock peening; pits; intermetallics; compressive residual stress; crack initiation

1. Introduction

Competing failure modes in fatigue refer to distinct failure distributions (i.e. surface, subsurface and internal areas) in the material tested under different conditions. The variety of failure modes have been attributed to the variance in surface conditions, the size and distribution of inclusions, temperature, stress levels, etc [1, 2]. It has

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been summarised that subsurface and internal failure tends to result in longer fatigue life than the failure originating from the surface. This phenomenon is closely related to the imperfection of material surface which usually contains grooves, dimples, or microcracks introduced by the applied surface treatments [3, 4]. These surface imperfections, as well as inclusions located at the surface, are local stress raisers which favour crack initiation at the early stage of fatigue life [5-8]. This process is additionally facilitated by the lack of constraints at the free surface of the material [9, 10]. Therefore, surface crack initiation normally leads to low-cycle fatigue (LCF). By contrast, when the stress is reduced to a level being too low to activate surface crack initiation, the failure dominating area is transferred to subsurface or internal sites where cracks initiate due to local strain accumulation. This tends to result in a longer crack initiation process, leading to high-cycle fatigue (HCF) [2, 11].

According to the mechanism of competing failure modes, resisting surface crack initiation by improving the surface quality and reducing the surface stress concentration are effective ways of enhancing the fatigue resistance of material. Laser shock peening (LSP) is a relatively new surface treatment method following this mechanism. Compared to conventional shot peening, LSP uses high power laser pulses to generate a surface compressive residual stress (CRS) distribution in structural components, instead of impacting the surface using particles. Typically, the CRS layer generated by LSP is 1-2 mm, which is about 3 times deeper than that generated by shot peening [12-14]. In addition, LSP normally leads to a better surface finish with lower surface roughness than shot peening, which is advantageous in retaining the benefits of CRS in improving fatigue life [12, 13].

It has been reported that the CRS generated by LSP is effective in mitigating surface crack initiation and the subsequent crack growth behaviour [15]. At relatively low load levels, surface failure in baseline samples (i.e. without LSP treatment) is usually moved to the subsurface area where the effects of CRS become weakened in LSP samples, achieving 5-10 times life improvement [16, 17]. However, surface crack initiation and growth can be reactivated when the applied load is increased to a level being sufficient to counterbalance the surface CRS. Under this condition, the effects of CRS are limited, degrading the benefit of LSP in life improvement to 2-5 times [16]. It has also been reported that existing surface imperfections or corrosion effects tend to accelerate this degradation by introducing additional stress concentration features at the surface [18-20].

Although the mechanisms of the competing failure modes occurring in materials treated with LSP have been well understood, investigations on predicting such behaviour and the associated life assessment are still relatively scarce. Some published researches focus on the development of the Kitagawa-Takahashi diagram, which builds up the relationship between defect size and fatigue limit [13, 21-23]. However, it cannot be used in life prediction at different load levels and the effects of CRS are rarely considered. This impedes the effective evaluation of the efficacy of the applied LSP. In the present study, a modelling-based method is proposed to predict the failure mode (i.e. safety-critical area) in laser shock peened AA7075-T651 at varying load levels, considering the effects of surface pits, intermetallics and CRS. Life assessment is also carried out based on the
stress and strain data at the predicted safety-critical area, using the Smith-Watson-Topper method.

2. Experiments

2.1. Materials

The investigated material in this study was AA7075-T651 aluminium alloy. The bend bars used in fatigue testing were cut from the longitudinal – long transverse (L-LT) plane from the AA7075-T651 plate, as illustrated in Fig. 1. The dimensions of the bend bar are also shown in Fig. 1. All samples were mechanically ground with 1200 SiC grit paper before the laser shock peening treatments.

![Fig. 1. Schematics of the LSP sample geometry and the LSP area.](image)

Fig. 2(a) shows the microstructure of AA7075-T651 on the L-LT, L-ST and LT-ST planes, including the dark features representing intermetallics. The JEOL JSM-6500F scanning electron microscope (SEM) for secondary electron imaging (SEI) and backscatter electron imaging (BEI), including energy dispersive X-ray spectroscopy (EDS) was used to identify these intermetallics, which were found to be mostly Al7Cu2Fe and Al23Fe4Cu, and occasionally Mg2Si.

The size distribution of the intermetallics was measured in terms of the square root area (i.e. $\sqrt{\text{area}}$) of them on the LT-ST plane. This parameter was chosen since its critical value was directly related to the fatigue resistance of the material, according to the Murakami’s model which quantitatively predicted the effects of inclusions on fatigue strength [24]. The measurement was taken by means of micro- X-ray computed tomography ($\mu$-CT) at the $\mu$-VIS X-ray imaging centre at the University of Southampton. All of the detected intermetallics were collected and arranged in the ascending order in terms of the measured $\sqrt{\text{area}}$, being
expressed as $\sqrt{\text{area}_i}$ ($\sqrt{\text{area}_1} \leq \sqrt{\text{area}_2} \leq \cdots \leq \sqrt{\text{area}_n}$), where $n$ was the total number of the detected intermetallics. The measured results can be represented by a Gumbel distribution, which can be expressed as the cumulative probability of $\sqrt{\text{area}_i}$ [21, 25]:

$$G(\sqrt{\text{area}_i}) = \frac{i}{n+1}$$  

(1)

where $G(\sqrt{\text{area}_i})$ represents the probability that a random variable $\sqrt{\text{area}}$ is smaller than the specified $\sqrt{\text{area}_i}$. Eq. 2 describes a linear relationship between $\{-\ln[-\ln G(\sqrt{\text{area}_i})]\}$ and $\sqrt{\text{area}_i}$, where $\alpha$ and $\lambda$ can be obtained using the least square method. The fitted results are shown in Fig. 2(c), from which the maximum $\sqrt{\text{area}}$ in AA7075-T651 is approximated to be 20.4 µm by setting $G(\sqrt{\text{area}_i})$ as 99.9%.

$$\sqrt{\text{area}_i} = \alpha \cdot \{-\ln[-\ln G(\sqrt{\text{area}_i})]\} + \lambda$$  

(2)

Fig. 2. (a) 3D representation of AA7075-T651 microstructural planes, (b) intermetallics on the LT-ST plane and (c) the size distribution of the intermetallics in terms of the $\sqrt{\text{area}}$ of their cross sections on the LT-ST plane.

2.2. Laser shock peening treatment

Ablative laser shock peening (LSP) and non-ablative laser shock peening (LSPwC) were performed at the Council for Scientific and Industrial Research (CSIR), South Africa. As shown in Fig. 1, both the top surface and the chamfered sides of the sample were peened to avoid crack formation outside the top flat surface. Compared to LSP, a thin oxide layer was mechanically removed during the LSPwC process to improve the surface CRS. More details of the applied LSP process are reported in [26, 27] hence are not repeated here.

Incremental centre hole drilling was used to measure the residual stresses resulting from the LSP and LSPwC treatments, using a Stresscraft three axis drilling device. The distribution of both the longitudinal ($S_{11}$) and transverse ($S_{33}$) stresses beneath the peened surface were measured. The measured surface residual stress data were conservatively corrected by X-ray diffraction (XRD), utilising the $d$-$\sin(2\chi)$ technique. Further details of the applied methodology for the residual stress measurement are provided in [26]. The obtained results are presented in Fig. 3, showing that the maximum CRS generated by LSPwC is 378 MPa, which is greater than 308 MPa by the LSP. However, the LSPwC results in lower CRS at the surface than the LSP process by ~50 MPa.
Fig. 3. Residual stress distribution measured in two orthogonal directions in the LSP and LSPwC samples: longitudinal stress $S_{11}$, transverse stress $S_{33}$.

It was found that both the LSP and LSPwC treatments are likely to generate some pits at the surface of the sample. The Alicona™ variable focus microscope was used to capture the pit distribution at a small sampling of bend bar surfaces, demonstrating that the depth of most pits ranged between 2 – 10 µm, as shown in Fig. 4.

Fig. 4. Depth information of the pits generated by the LSP process at the surface of the bend bar, captured by Alicona™ variable focus microscopy.

2.3. Corrosion treatment

To investigate the effects of corrosion on the fatigue behaviour of laser shock peened AA7075-T651, the surface of some LSP and LSPwC samples were electrochemically corroded to generate some corrosion pits at the peened surface. All the electrochemical tests were performed in ambient temperature and atmospheric conditions. Detailed procedures of carrying out the electrochemical tests are reported in [26]. The corroded peened surfaces were investigated using the Alicona™ variable focus microscope and the results are presented...
in Fig. 5. It suggests that there is no clear difference regarding the variance of the depth of corrosion pits in the LSP and LSPwC samples, averagely ranging between 10 – 20 µm in depth, which is deeper than the LSP-induced pits presented in Section 2.2. However, more critical pits being deeper than 35 µm occasionally exist in both cases (as shown in Fig. 5(a)), which might significantly affect the fatigue resistance of the peened sample. More details are provided in Section 2.4.

![Fig. 5. Depth information of the corrosion pits generated at the peened surface of (a) LSP and (b) LSPwC bend bar, captured by Alicona™ variable focus microscopy.](image)

2.4. Fatigue test and fractography

Four-point bend fatigue tests were performed using the chamfered sample (as illustrated in Fig. 1) on a servo-hydraulic Instron machine at a frequency of 20 Hz and a load ratio of $R = 0.1$. The fatigue life of samples with different surface treatments are plotted in Fig. 6, in terms of the experienced surface stress range ($\Delta S_{11}$) during fatigue cycles.
In Fig. 6, the baseline samples represent those without laser shock peening treatment. Their fatigue lives scatter between $10^4$ and $10^5$ with $\Delta S_{11}$ ranging between 576 and 353 MPa. The typical crack initiation site for baseline samples is at surface intermetallics, as shown in Fig. 7(a), regardless of the applied load level. It can be seen from Fig. 6 that LSP and LSPwC effectively improve the fatigue life of the baseline samples at all considered load levels. The improvement was particularly significant (20–50 times) at low load levels when $\Delta S_{11} = 407$–440 MPa, where surface crack initiation at intermetallics is completely resisted and the crack initiation sites are transferred to the subsurface area at a depth of 280–430 µm below the peened surface, as shown in Fig. 7(e). No clear defects were observed at those subsurface crack initiation areas. However, there are some exceptions among the peened samples tested at $\Delta S_{11} = 407$ MPa, whose life improvement is much reduced (0–5 times). The reason for this phenomenon is attributed to the surface pits generated during the peening process (i.e. the peening-induced pits), as introduced in Fig. 4. Some of these pits are likely to result in severe surface stress concentration which is sufficient to counterbalance the CRS induced by the peening process, bringing the crack initiation site from the subsurface area back to the surface. Two typical examples are provided in Fig. 7(c) and Fig. 7(d). The depth of the LSP-generated pit shown in Fig. 7(c) is 12 µm, which is consistent with the most critical pit shown in Fig. 4. By contrast, the LSPwC-generated pit shown in Fig. 7(d) is only 6.5 µm and the intermetallic beneath is supposed to also contribute to the local stress concentration.

At higher stress levels ($\Delta S_{11} > 540$ MPa), no subsurface crack initiation was observed in peened samples; the failure mechanism in baseline samples was recovered, with cracks initiating from surface intermetallics, as show in Fig. 7(b). Under such conditions, the benefits of LSP in life improvement is limited to 2.5–5 times. Surprisingly, no pits-led surface failure was found at these high stress levels. This was probably due to that the critical pits were only occasionally generated during the peening process, as suggested by the results obtained at $\Delta S_{11} = 407$ MPa.
In addition, the surface of some LSP and LSPwC samples were corroded following the process introduced in Section 2.3. All of these samples containing corrosion pits were tested at $\Delta S_{11} = 407$ MPa. It was found that there was marginal life improvement of these samples compared to the baseline ones, owing to similar mechanisms as those samples containing peening-induced pits.

Fig. 7. Images of typical crack initiation sites for different fatigue test conditions: (a) all baseline samples, LSP samples with surface crack initiation tested at (b) high stress levels and (c) $407$ MPa, (d) LSPwC samples with surface crack initiation tested at $407$ MPa, (e) LSP/LSPwC samples with internal crack initiation tested at $407$ MPa, (f) corroded LSP/LSPwC samples tested at $407$ MPa [27].

3. Modelling

In this study, finite element (FE) modelling was employed to investigate the competing failure modes observed
in the laser shock peened AA7075-T651 samples. It aimed at helping quantitatively unveil the behind mechanisms and contributing to the development of reliable life assessment methods that were applicable to this complex condition.

3.1. Residual stress and pit modelling

A quarter of the chamfered bend bar used in fatigue tests was modelled using ABAQUS by applying symmetry boundary conditions (BCs), as shown in Fig. 8. A fine-meshed sub-model containing the pit was developed at the top corner of the model, corresponding to the top centre of the real specimen. Surface-to-surface tie constraints are defined at the interface between the sub-model and the global model. The pit model is composed of two parts: the pit plug and the pit base. In order to represent the surface condition without pits, the plug and the base models are initially integrated by defining a ‘rough’ and ‘hard’ interface between them, which constrains the sliding movement and allows the pressure transfer between the two parts. As illustrated in Fig. 8, the formation of the pit can be simulated by deactivating the BCs defined at the plug-base interface, separating them as two single parts without any contact. Similar sub-modelling methodology has been adopted in [28] for crack modelling, which validates that the defined BCs at the interface between parts effectively maintain the integrity of the whole model and have negligible effects on the overall stress distribution.

In order to more appropriately simulate the effects of the pit, the shape of the cross section of the pit was assumed to be represented by two semi-ellipses, as presented in Fig. 9(a). Compared to the conventional semi-elliptical shape shown in Fig. 8, this shape was demonstrated to be more consistent with the actual pit geometry and to better describe the critical stress condition around the pit [29]. In this study, this definition is suitable for both the peening-induced and the corrosion pits, as shown in Fig. 9(b) and Fig. 9(c) respectively.

In the modelling work, before the pits were activated, the residual stresses generated by laser shock peening were reconstructed in the FE model, using the inverse eigenstrain method introduced in [27, 30, 31]. The reconstructed results for both the LSP and LSPwC conditions are compared to the measured data in Fig. 10(a), showing good consistency.
Fig. 9. (a) Schematic diagram showing the description of the cross section geometry of the pit using two semi-ellipses, and the dimensions of (b) a LSP-induced pit (Fig. 7(c)) and (c) a corrosion pit (Fig. 7(f)).

The formation of the pit was then simulated by releasing the plug-base BCs and removing the pit plug in the model, obtaining the redistributed residual stresses around the pit. The simulated results for a peening-induced and a corrosion pits (Fig. 9(c) and Fig. 9(f)) in LSP samples are shown in Fig. 10(b), displaying that the beneath CRS becomes greater after the formation of the pit. This is attributed to that the relaxed CRS at the pit plug area needs to be redistributed to the surrounding area, maintaining overall stress equilibrium. Similar phenomenon was also observed in [32], which studied the residual stress redistribution after foreign object damage in an LSP aerofoil. However, it is not supposed that the greater CRS beneath the pit leads to more benefits in mitigating fatigue, since the CRS tends to be quickly counterbalanced when tensile stress is applied due to the stress concentration effects of the pit. In addition, Fig. 10(b) also suggests that the peening-induced and the corrosion pits result in similar maximum CRS, despite their significant difference in dimensions. This to some degree is reflected by the similar fatigue life (at $\Delta \sigma_{11} = 407$ MPa) obtained for the LSP samples failed due to the two types of pits respectively, as shown in Fig. 6.

Fig. 10. (a) Comparison between the reconstructed and measured residual stresses for LSP and LSPwC conditions, (b) simulated residual stress redistribution after a LSP-induced and a corrosion pits are activated respectively in the LSP condition.

3.2. The effects of surface pits

As discussed in Section 2.4, the formation of both the peening-induced and corrosion pits is likely to degrade
the effects of laser shock peening in extending fatigue life, by introducing severe stress concentration at the peened surface. In order to quantitatively study the effects of the pits on fatigue life, a method based on fatigue limit was proposed in the present study to determine whether the specified pit was critical at the given load level. The influence of the applied peening process was considered using the FE model introduced in Section 3.1.

3.2.1. Prediction of fatigue limit

As the first step, the fatigue limit of baseline AA7075-T651 was determined using the test data obtained under uniaxial loading, which was reported in [33, 34]. However, as shown Fig. 11(a), these data were obtained under various load ratios (R) and it was difficult to directly derive a unified fatigue limit from them. Therefore, the Smith-Watson-Topper (SWT) method was applied to correlate the fatigue life data for different R values. The expression for the SWT method is shown by Eq. 3, where \( \sigma_{11,max} \) and \( \Delta \varepsilon_{11} \) are the maximum stress and the strain range experienced during a cycle respectively, \( A_1, A_2, a_1 \) and \( a_2 \) are parameters required to be calibrated using the test data.

\[
SWT = \frac{1}{2} \sigma_{11,max} \Delta \varepsilon_{11} = A_1 N_f^{a_1} + A_2 N_f^{a_2}
\] (3)

As presented in Fig. 11(b), the fatigue life data reported in [33, 34] are replotted in terms of the SWT parameter based on Eq. 3. It suggests that the SWT method is effective in unifying the S-N curve, being independent of the applied R values when \( R > -2.5 \). The calibrated parameters are listed in Table 1. SWT

\[
\text{Fatigue limit: 0.33 MPa}
\]

However, Fig. 11 also suggests that the calibrated equation becomes invalid for the data obtained at \( R < -2.5 \). This is attributed to that the conventional definition of SWT was proposed for tension dominated conditions, which was less applicable for compression dominated failure. Some researchers attempted to modify the definition of SWT by taking into account the effects of shear stress on compressive failure [35]. But the resultant error bands at HCF still needs improvement before it is appropriate to be applied in life assessment. In the

![Fig. 11. (a) S-N curve for AA7075-T651 under uniaxial loading at different R ratios, and (b) correlation of the fatigue life data using the SWT method. The S-N curve is reproduced from [33, 34].](image-url)
In the present study, a simple method was tried by recalibrating the data obtained at $R < -2.5$ using Eq. 3. The obtained parameters for Eq. 3 are also listed in Table 1. Fig. 11(b) demonstrates that most data are within the error band (factor of three) of the recalibrated Eq. 3. However, it is noted that the fatigue limit (for $R < -2.5$) predicted by this relation tends to be lower than reality, since most cases tested at $R < -2.5$ were accelerated in the second phase of testing by changing the $R$ to 0, particularly for those data around $10^6$ cycles [34].

**Table 1. The calibrated parameters for the SWT equation.**

<table>
<thead>
<tr>
<th>Parameter</th>
<th>$R &gt; -2.5$</th>
<th>$R &lt; -2.5$</th>
</tr>
</thead>
<tbody>
<tr>
<td>$A_1$ (MPa)</td>
<td>$A_2$ (MPa)</td>
<td>$a_1$</td>
</tr>
<tr>
<td><strong>Value</strong></td>
<td>0.812</td>
<td>135.287</td>
</tr>
</tbody>
</table>

3.2.2. Stress concentration caused by surface pits

Before the effects of the pits were considered, the distribution of the SWT parameter within the surface layer in both the LSP and LSPwC samples was calculated for different load levels, using the FE model (with pit inactivated) introduced in Section 3.1. The results are displayed in Fig. 12. It shows that the surface material experiences a stress level lower than $SWT_0$ at $\Delta S_{11} = 407$ MPa in both LSP and LSPwC samples, implying the high possibility for subsurface failure. However, with increasing $\Delta S_{11}$, the surface SWT gradually becomes greater than $SWT_0$, suggesting the dominance of surface failure at high load levels despite the beneficial effects of CRS. This trend is consistent with experimental observations discussed in Section 2.4.

![Fig. 12. The FE-calculated distribution of the SWT parameter for (a) LSP and (b) LSPwC samples without pits at different load levels.](image)

The effects of pits were numerically investigated under $\Delta S_{11} = 407$ MPa, using the FE model with activated surface pit. Both the peening-induced (LSP and LSPwC) and corrosion pits observed in experiments, as shown in Fig. 7(c)(d)(f), were considered in this analysis. The modified SWT distribution resulting from the presence of the pit is shown in Fig. 13(a) and Fig. 13(b) for LSP and LSPwC samples respectively, compared to the
original SWT distribution before the pit is activated; the baseline SWT distribution for the unpeened case is also included as a reference. Fig. 13 indicates that the SWT at the base of the pit is significantly increased due to local stress concentration. According to the critical distance theory, fatigue life is dominated by the stress or strain averaged over a certain distance ($L_p$) at the stress raiser, rather than the peak value. It was also reported that $L_p$ decreased when the small stress raiser (with notch root radius smaller than 0.5 mm) became sharper [36]. In the present study, $L_p$ was assumed to be 25 µm, which was approximately the size of the grain of AA7075-T651 on the LT-ST plane. The resultant SWT ($p$) (i.e. the averaged SWT over $L_p$ for a pit) was calculated for each considered pit and is indicated in Fig. 13. It demonstrates that SWT$_p$ is always above SWT$_0$, implying that the considered pits are sufficiently critical to lead to surface crack initiation. This coincides well with experimental observations. It is also noted that the actual SWT$_p$ related to the LSPwC-induced pit might be underestimated in Fig. 13(b), since in reality this pit acted together with the intermetallics beneath it, as shown in Fig. 7(d).

![Fig. 13. The influence of the surface pit on the distribution of the SWT parameter (at $\Delta S_{11} = 407$ MPa): (a) LSP and (b) LSPwC conditions.](image)

3.3. The effects of intermetallics

According to Fig. 7, when the samples are free from the effects of surface pits, intermetallics that result in local material discontinuity become popular surface crack initiation sources. The stress distribution around an intermetallic is complex and highly depends on the shape of it, thus is difficult to directly simulate. In order to quantify the effects of intermetallics on fatigue resistance, the Murakami’s model was applied in the present study [24]. This model was developed based on the concept of fracture mechanics and regarded intermetallics as existing cracks. It assumes that the stress intensity factor of a crack depends on the $\sqrt{\text{area}}$ of the crack face instead of the crack length. Based on this assumption, an empirical relation between the stress fatigue limit ($\sigma_0$) and $\sqrt{\text{area}}$ of the critical intermetallic ($\sqrt{\text{area}_{c}}$) was also built, as expressed by Eq. 4, where $H_v$ is the Vickers hardness of the material, $C$ is a parameter depending on material properties. The Murakami’s model has been successfully applied to a variety of materials [21, 37-39]. Although there is no report for AA7075-T651, the value of $C$ is estimated to be ranging between 0 and 75 for aluminium alloys according to the literatures [23, 40-43].
\[ \sigma_0 = \frac{1.43(H_v + C)}{\sqrt{\text{area}_c}}^{1/6} \]  \hspace{1cm} (4)

To validate the applicability of the Murakami’s model for AA7075-T651, \( \sigma_0 \) under symmetric uniaxial loading \((R = -1)\) was predicted using Eq. 4 and was compared to the experimental results reported in [33, 34]; \( \sqrt{\text{area}_c} \) was approximated to be 20.4 \( \mu \text{m} \) according to Fig. 2(c), \( H_v \) was determined to be 171 according to microhardness measurements [27], \( C \) was set to be 38 and varied between 0 and 75. Table 2 shows that the measured \( \sigma_0 \) is 158 MPa, which fits well within the predicted range of \( \sigma_0 \) (148.8 – 213.7 MPa).

Table 2. Comparison between the measured and predicted fatigue limit of AA7075-T651 under uniaxial loading \((R = -1)\).

<table>
<thead>
<tr>
<th>( \sqrt{\text{area}_c} ) (( \mu \text{m} ))</th>
<th>( H_v )</th>
<th>Fatigue limit ( \sigma_0 ) (MPa)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Predicted</td>
<td>20.4</td>
<td>171</td>
</tr>
<tr>
<td>Test</td>
<td>/</td>
<td>/</td>
</tr>
</tbody>
</table>

In order to apply the Murakami’s model in laser shock peening conditions where the value of \( R \) varies depending on the CRS and applied loads, Eq. 3 was combined with the Walker’s equation to allow its application when \( R \neq -1 \), as expressed by Eq. 5, where \( \sigma_{0,eq} \) represents the equivalent \( \sigma_0 \) when \( R \neq -1 \), \( \alpha \) is a material constant and was set as 0.445 for AA7075-T651 according to [44].

\[ \sigma_{0,eq} = \frac{1.43(H_v + C)}{\sqrt{\text{area}_c}}^{1/6} \left( \frac{1 - R}{2} \right)^\alpha \]  \hspace{1cm} (5)

The effects of surface intermetallics in both the baseline and laser shock peened samples, which were tested with known fatigue lives shown in Fig. 6, were investigated based on Eq. 5. For samples with known dimensions of the surface intermetallics leading to failure, such as those shown in Fig. 7(a)(b), the measured \( \sqrt{\text{area}_c} \) was regarded as \( \sqrt{\text{area}_c} \); for some LSP and LSPwC samples with subsurface failure, an intermetallic with the critical size of \( \sqrt{\text{area}_c} = 20.4 \mu \text{m} \) (Fig. 2(c)) was assumed to be located at the surface. Samples with surface pits dominating the fatigue behaviour were excluded from this analysis. An FE model without surface pit was used to predict the maximum nominal surface stress (\( \sigma_{max} \)) experienced during cyclic loading for samples with different surface treatments; the effects of CRS on \( \sigma_{max} \) in LSP/LSPwC samples were considered via residual stress modelling as shown in Fig. 10(a). \( \sigma_{0,eq} \) was predicted based on the determined \( \sqrt{\text{area}_c} \) for each considered sample using Eq. 5. It is noteworthy that in LSP/LSPwC samples, \( R \) value at surface varies with the level of the applied stress due to the presence of CRS, hence an iteration process was applied to determine \( \sigma_{0,eq} \) and the corresponding \( R \) based on the FE results. Subsequently, \( R_{lms} \) was defined as comparing \( \sigma_{max} \) to \( \sigma_{0,eq} \) as expressed by Eq. 6. \( R_{lms} > 1 \) indicates that the applied load is sufficiently high to trigger failure originating from the specified surface intermetallics. By contrast, \( R_{lms} < 1 \) implies that such failure is less likely
to occur under given conditions due to the low stress level.

\[ R_{IMS} = \frac{\sigma_{max}}{\sigma_{0,eq}} \]

The S-N curve presented in Fig. 6 (excluding the data with failure originating from surface pits) are replotted in Fig. 14 in terms of the calculated \( R_{IMS} \). The error bands were obtained by varying the value of \( C \) in Eq. 5 according to Table 2. It can be seen that the calculated \( R_{IMS} \) is consistent with the experimentally-observed failure mode of each sample; two images of fracture surface are used in Fig. 14 to illustrate the typical failure modes. Particularly, this method is demonstrated to be accurate in predicting the load level corresponding to the subsurface-surface transition of failure sites in LSP/LSPwC samples. In addition, Fig. 14 also implies that the fatigue life of samples tends to be reduced when \( R_{IMS} \) increases, despite the loading and surface treatment conditions.

![Fig. 14. Prediction of surface or internal crack initiation based on surface stress conditions and the size of intermetallics (IMs), compared to experimental observations.](image)

### 4. Life assessment framework and discussion

The methods introduced in Section 3.2 and 3.3 can be used to predict the critical area where cracks are prone to develop under given conditions; i.e. from surface pits, surface intermetallics, or subsurface area. Based on this prediction, a fatigue life assessment framework is proposed which integrates the work associated with experimental testing, modelling and life prediction. This framework is presented in Fig. 15, the first level of which is composed of experimental characterisations in three aspects: the mechanical properties of the material, the morphology of surface pits and the \( \sqrt{\text{area}_c} \) of surface intermetallics. The obtained three types of information are fed into the fatigue limit prediction, the FE model and the Murakami’s model respectively in the second level of the framework. In the third level, the stress and strain at surface pits or intermetallics are calculated by
the FE model at given loads, considering the applied surface treatments. The obtained stress and strain are then compared to the equivalent fatigue limit to determine whether the considered pits or intermetallics are sufficiently critical to become the failure origin, following the procedures detailed in Section 3.2 and 3.3 respectively. Subsequently, for a sample with given test conditions and surface characterisations, the safety-critical area can be determined, where the stress and strain data can be used for assessing the fatigue life based on the SWT method using Eq. 3 and the parameters listed in Table 1. For the condition with cracks being predicted to originate from the subsurface area, the sensible SWT parameter used in life prediction can be obtained by averaging it over $2L_c$ from the depth where the curve of SWT distribution intersects with the fatigue limit line, as shown in Fig. 12; $L_c$ is estimated to be 50 $\mu$m for AA7075-T651, based on the critical distance line method applicable for life assessment when macroscopic gradient stress distribution is present [45].

Following the framework proposed in Fig. 15, the fatigue life of both LSP and LSPwC samples that are presented in Fig. 6 were calculated. The results are shown in Fig. 16, demonstrating an acceptable accuracy with the factor of three error band compared to experimental results.
This life assessment framework is helpful in practice in that the efficacy of the applied LSP in life improvement can be effectively evaluated by characterising the surface imperfections. Furthermore, it can potentially provide guidance on establishing the surface quality standard that helps maximise the life improvement resulting from LSP. However, the main difficulty in practice is that the cross section geometry of the critical pit is not always available as the input. To solve this problem, a pit reconstruction method based on the scanned volume, the projected area and the depth of the pit was proposed. The shape of the pit was approximated using the scanned information, based on the assumption of two semi-ellipses as shown in Fig. 9. Fig. 17 illustrates an example of reconstructing the LSP-induced pit displayed in Fig. 7(c) using this method, compared to its counterpart reconstructed based on the cross section geometry as introduced in Fig. 9(b). The comparison demonstrates a good consistency between the two methods in terms of the critical stress and the fatigue life resulting from the reconstructed pits. Therefore, it is promising to select surface scanning as a means to provide essential information on pits as the input for the life improvement framework, which is expected to facilitate the evaluation process.

![Graph showing life prediction using the SWT method](image)

*Fig. 16. Life prediction using the SWT method, based on the stress and strain obtained at safety-critical areas (i.e. surface pits, intermetallics, and subsurface area).*
Fig. 17. (a) The volume, projected area and depth of a LSP-generated pit obtained by surface scanning using Alicona™ variable focus microscope (on two broken parts after fatigue testing). Comparison between the pits reconstructed using the scanned data and the cross section geometry (Fig. 9(b)) respectively in terms of (a) the stress distribution, and (b) the SWT and the predicted fatigue life when ΔS_{11} = 407 MPa.

5. Conclusions

This paper investigated the fatigue life of laser shock peened AA7075-T651 with various surface conditions. It was found that the benefits of LSP in life improvement tend to be maximised when surface cracking was resisted by the introduced compressive residual stresses, transferring the crack initiation site to the subsurface area. It was also found that surface pits and intermetallics, particularly those with critical dimensions, were prone to counteract the effects of compressive residual stresses, and to be activated as surface cracking sources during fatigue loading, leading to reduced life improvement by LSP. At high load levels, this trend was promoted, with subsurface crack initiation being rarely observed in LSP samples.

Numerical studies have been carried out to successfully predict the crack initiation sites (i.e. surface pits, surface intermetallics or subsurface area) in samples treated with LSP, by quantifying the competition between surface stress concentration and compressive residual stresses. Two methods, which were based on the Smith-Watson-Topper (SWT) method and the Murakami’s model respectively, were proposed to quantify whether the specified surface pit and intermetallics were critical enough to lead to surface crack initiation at given loads.

A fatigue life assessment framework has also been proposed based on the prediction of crack initiation sites. The fatigue lives of samples with different LSP treatments, surface pit morphologies, intermetallic dimensions or load levels were reasonably predicted based on this framework, with predicted fatigue lives being located within the factor of three error band.

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