2002021

Theoretical Analysis of Beneficial and Detrimental Effects of Controlled Shot Peening in High Strength Aluminium Alloys

Chris A. Rodopoulos [1] Rachel E. Edwards [1], Sean Curtis [1], Jose Solis Romero [1], Jung-Hee Choi [1,2], Eduardo de los Rios [1] and Andrew Levers [3]

[1] Division of Aeronautical Applications, Department of Mechanical Engineering, University of Sheffield, Sheffield, UK

[2] Hyundai Motor Company, S. Korea.

[3] Airbus UK, Chester, UK

1 Introduction

For many years controlled shot peening (CSP) was considered as a surface treatment of questionable benefits. This impression was fuelled by contradictory results from fatigue experiments [1,2]. It is now clear that the performance of CSP in terms of fatigue depends on the balance between its beneficial (compressive residual stress and work hardening) and detrimental effects (surface roughening) [3,4]. Hence, in order to achieve a favourable fatigue performance, the role of those effects has to be analysed and understood. To achieve such undertaking it is essential to consider their interaction with other parameters such as the nature of the target material and the loading conditions.

This work brings together two micromechanical models, (i) for notch sensitivity [5] and (ii) for fatigue life [6]. The former assesses the effect of surface roughening, whilst the latter incorporates the residual stress distribution and work hardening on fatigue life calculations. Combination of the two models allows the determination of the residual stress distribution to meet specific improvements in fatigue life (improvement life factor, ILF). Using the ILF methodology, the effects of CSP can be scrutinised against stress level, surface roughness and ILF value.

2 Modelling the Surface Roughness

On shot peened surfaces, cracks are likely to form at micro-notches (dents). Early studies from Smith [7] and Tanaka [8] indicate that the propagation of cracks from notches depends on the bluntness of the notch, given by $(\alpha/\rho)^{0.5}$ (ρ is the notch radius). Despite the numerous models published in the literature, as illustrated by the work quoted in [9], most models fail to provide a relationship between the geometry of the notch and the microstructure of the material, except by that provided by Vallellano et al [5,10]. According to their work, the nominal stress in a notched member is given by,

$$\sigma_i^{nom} = \frac{\sigma^{app}}{Z_i} \tag{1}$$

where σ^{app} is the applied stress, σ_i^{nom} is the distribution of the nominal stress ahead of the notch root as a function of the distance from the notch i, mapped as $i = 2\,a/D$ (a: crack length) and Z_i is the notch factor given by,

$$Z_i = \frac{\sqrt{i}}{\bar{\alpha}+\bar{\beta}}\left[\frac{\bar{\beta}}{\lambda_i}+\frac{\bar{\alpha}}{\sqrt{1+\lambda_i^2}}\right]^{1/2} \quad (2)$$

$$\lambda_i = \frac{1}{\alpha^2-\beta^2}\left[\alpha\sqrt{\left(\alpha+iD^2/2\right)^2-\alpha^2+\beta^2}-\beta\left(\alpha+iD/2\right)\right]$$

where i=1,3,5,...

The parameters $\bar{\alpha} = \frac{2\alpha}{D}$ and $\bar{\beta} = \frac{2\beta}{D}$ represent in a dimensionless form the notch depth α and the notch half width β. The parameter D represents the distance between two successive barriers. In the case where grain boundaries are considered being the dominant barrier, D is regarded as the grain diameter.

Li et al [11], proposed that the elastic stress concentration K_t introduced by multiple micro-notches in CSP, is somehow lower than the one determined in the case of a single notch of similar depth and width. The above finding reflects the uniformity of the micro-notches on the surface. According to Li, the resulting K_t from CSP is given by,

$$K_t = 1+2.1\left(\frac{R_t}{S}\right) \quad (3)$$

where the parameters R_t and S are respectively the mean of peak-to-valley heights and the mean spacing of adjacent peaks in the surface roughness profile. In the case of a semi-elliptical notch and a high degree of uniformity (CSP coverage percentage of more than 100%), Eq.(3) can be written as,

$$K_t = 1+2.1\left(\frac{\alpha}{2\beta}\right) \quad (4)$$

At the beginning of this section it was pointed out that the bluntness of the notch could significantly affect the strain generated at the root of the notch and consequently the propagation rate of the crack. In light of that, Smith and Miller [7] proposed that K_t should be determined by,

$$K_t = 1+2\sqrt{\frac{\alpha}{\rho}} \quad (5)$$

where ρ is the notch root radius. In the case of a semi-elliptical notch, the notch root radius can be approximated by $\rho = \alpha^2/\gamma$ and thus Eq.(5) can be rewritten as,

$$K_t = 1 + 2\sqrt{\frac{\gamma}{\alpha}} \tag{6}$$

where γ is the notch half width that considers the bluntness of the notch. By equating Eq.(6) with Eq.(4), the stress concentration due to multiple micro-notches can be expressed in terms of a single notch by,

$$K_t = 1 + \frac{\alpha}{\beta} \tag{7}$$

3 Modelling the Fatigue Life in CSP Components

In [4,6] it was proposed that the fatigue life of polycrystalline materials can be determined by,

$$N = \frac{1}{A_2}\sum_{i=1}^{i_c}\int_{n_s^i}^{n_c^i}\frac{\left(\frac{iD}{2}\right)^{1-m_2} dn_1^i}{CTOD^{m_2}} \tag{8}$$

where A_2, m_2 are parameters from the Paris law of crack propagation, CTOD is the crack tip opening displacement and n_s^i, n_c^i are limit values of n_1 as defined in Figure 1.

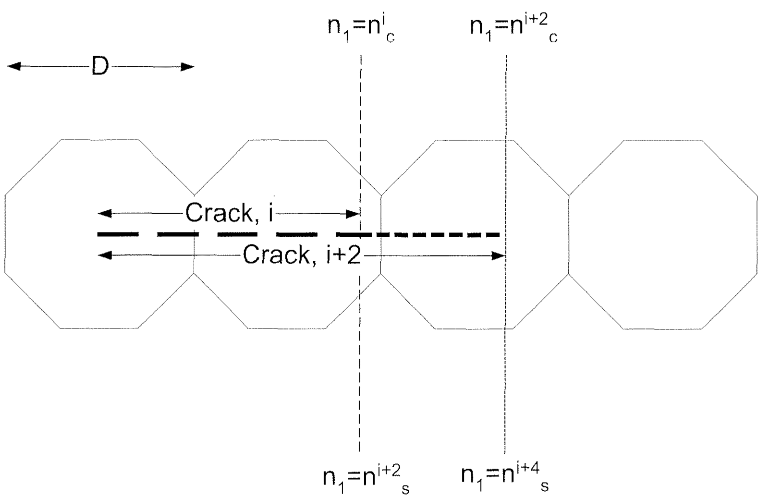


Figure 1: Schematic showing the position of n_1 prior (n_c) and after (n_s), the unblocking of the crack tip plasticity

In Eq.(8) the parameter n_s^i and n_c^i represent respectively, the position of the crack tip at the beginning and end of each interval i of crack growth. These two parameters are calculated by,

$$n_c^i = \cos\left(\frac{\pi}{2}\frac{\sigma - \sigma_{i\ arrest}^p}{\sigma_2}\right)$$
$$n_s^i = n_c^{i-2}\frac{i-2}{i} \tag{9}$$

where σ_2 is the flow resistance of the material and $\sigma_{i\ arrest}^p$ is the Kitagawa-Takahashi formula for a plain specimen,

$$\sigma_{i\ arrest}^p = \frac{m_i}{m_1}\frac{\sigma_{FL}}{\sqrt{i}} \tag{10}$$

with σ_{FL} denoting the fatigue limit of the plain material. In Equation 10 the parameter m_i/m_1 represents the effect of the grain orientation factor. More details can be found in [4,12].

From Eq.(8) it is clear that the number of cycles required by the crack to propagate an i number of half grains, depends solely on the parameter n_c^i. For CSP components, the parameter n_c^i has to be modified in a way that it would take into account the roughening of the surface and the crack closure stresses generated by the residual stresses.

$$n_c^{i,CSP} = \cos\left(\frac{\pi}{2}\frac{\frac{\sigma}{Z_i} - \sigma_{i\ \mathrm{arrest}}^{CSP}}{\sigma_2 - \sigma_1^i}\right) \tag{11}$$

where the parameter Z_i is given by Equation 2. The Kitagawa-Takahashi formula for the case of CSP, $\sigma_{i\ arrest}^P$, is given by [4],

$$\sigma_{i\ \ arrest}^{CSP} = \left(\frac{m_i}{m_1}\frac{\sigma^{CSP}{}_{FL} - \sigma_1^{i=1}}{\sqrt{i}} + \sigma_1^i\right)Z_i \tag{12}$$

where $\sigma^{CSP}{}_{FL} = \sigma_{FL} + \sigma_1^{i=1}$. Hence, Equation 12 is rewritten as,

$$\sigma_{i\ \ arrest}^{CSP} = \left(\frac{m_i}{m_1}\frac{\sigma_{FL}}{\sqrt{i}} + \sigma_1^i\right)Z_i \tag{13}$$

4 Introducing the Improvement Life Factor (ILF)

In order to increase the life consumed at each grain and consequently the overall life of the CSP component, we make use of a predetermined ILF,

$$ILFxN = \frac{1}{A_2} \int_{n_s^{i,CSP}}^{n_c^{i,CSP}} \frac{\left(\frac{id}{2}\right)^{1-m_2} dn_1^{i,CSP}}{CTOD^{m_2}} \tag{14}$$

where the values of ILF are in percentage. Solution of Equation 14 in terms of CTOD yields,

$$\ln(CTOD) = \frac{0.69(m_2 - 1)}{m_2} + \frac{\ln\left[\frac{\left(n_c^{i,CSP} - n_s^{i,CSP}\right)(Di)^{(1-m_2)}}{A_2 \times ILF \times N}\right]}{m_2} \tag{15}$$

In the case of a plain/unpeened material, Equation 15 is written as,

$$\ln(CTOD) = \frac{0.69(m_2 - 1)}{m_2} + \frac{\ln\left[\frac{\left(n_c^{i,p} - n_s^{i,p}\right)(Di)^{(1-m_2)}}{A_2 N}\right]}{m_2} \tag{16}$$

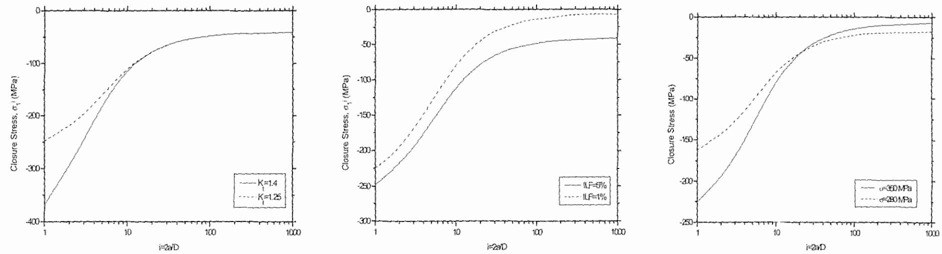
The fact that the value of CTOD at the position n_C, where the crack tip plasticity is able to overcome the microstructural barrier, is identical for both the peened and the unpeened material (for the same loading conditions) allows Equations 15, 16 to be equated,

$$\frac{0.69(m_2 - 1)}{m_2} + \frac{\ln\left[\frac{\left(n_c^{i,CSP} - n_s^{i,CSP}\right)(Di)^{(1-m_2)}}{A_2 \times ILF \times N}\right]}{m_2} = \frac{0.69(m_2 - 1)}{m_2} + \frac{\ln\left[\frac{\left(n_c^{i,p} - n_s^{i,p}\right)(Di)^{(1-m_2)}}{A_2 N}\right]}{m_2} \tag{17}$$

Simplification of Equation 17 gives,

$$n_c^{i,CSP} = ILF \times \left(n_c^{i,p} - n_s^{i,p}\right) + n_s^{i,CSP} \tag{18}$$

From Equation 18, the closure stress σ_1^i can be determined. It should be noted that due to the complexity of Equation 18, a computational solution is advised. Figure 2 shows the calculated crack closure, σ_1^i, for several conditions of loading and treatment.



(a) **(b)** **(c)**

Figure 2: The effect of (a) surface roughness (b) ILF and (c) applied stress on the distribution of closure stress in a 2024-T351 CSP component under mode I loading. The parameters used in the calculations are: ILF = 5 %, s = 350 MPa, s_2 = 450 MPa, s_{FL} = 220 MPa and D = 52 mm.

5 Discussion and Conclusions

In this work the effects of CSP on fatigue damage are analysed and modelled. Surface roughness is modelled as a local increase in the far-field stress. Hence, the treated surface has a higher propensity to initiating and propagating short fatigue cracks. Compressive residual stresses are considered as crack closure stresses and regarded as one of the beneficial effect of CSP. Residual stresses tend to reduce the intensity of the far-field stress by introducing a closure stress on the crack flanks. Thus, crack propagation rate of peened material is expected to be lower than that of unpeened material. Finally, strain hardening is expected to reduce the propagation of short fatigue cracks by increasing the resistance of the material to the generation of crack tip plasticity.

A predetermine improvement in terms of fatigue life can be calculated by introducing the ILF factor into the number of fatigue cycles consumed in every grain. The above approach allows the mathematical modelling of the balancing between the beneficial and detrimental CSP effects. At first the analysis reveals that the magnitude of the closure stresses should always attain a maximum at the surface. Such distribution minimises the premature initiation of a "visible" fatigue crack. Secondly, the depth distribution should be able to counteract the stress gradient generated by the surface roughness. Further analysis allows the assessment of parameters such as the far-field stress level, the ILF and the surface roughness. From Figure 2, the effect of the above parameter is quantified in the following order, starting from the most decisive: a) *Surface Roughness*. The analysis reveals that a 12% increase, measured in terms of K_t, in the surface roughness requires a 47% increase in the closure stress magnitude to allow a 5% increase in per grain fatigue life. Additionally, a higher K_t would require deeper closure stresses; b) *Far-Field Stress Level*. In principle, high far-field stress levels require high magnitude and deeper closure stresses. This agrees with findings published extensively in the literature, that CSP will have a minimum effect, or in some cases a detrimental effect, on the low cycle fatigue region; and c) *ILF*. The analysis reveals that CSP components are not so sensitive to different ILF values. The above conclusion agrees with experimental data showing that short cracks propagate almost irrespective of the crack closure stress levels.

It should be noted that since the methodology is expressed in terms of crack length, it can be easily adjusted to incorporate relaxation profiles of residual stress and strain hardening.

6 Acknowledgements

The authors are indebted to numerous industrial organisations and governmental bodies which supported their research for many years. Special citation should be given to the EPSRC, The Royal Academy of Engineering, The British Council, Airbus UK, The Hellenic Aerospace Industry and the Mexican CONACYT.

7 References

[1] P. O'Hara in Surface Treatment IV, (Ed. . Brebbia and J. M. Kenny) WIT Press, 1999, 321–330.
[2] L. Wagner and G. Lütjering in Shot Peening, Pergamon press, 1981, 453–460.
[3] P. K. Sharp and G. Clark DSTO-RR-0208, Defence-Science and Technology Organisation, Royal Australian Air Force, Australian Ministry of Defence, 2001, (declassified).
[4] S. Curtis, E. R. de los Rios, C. A. Rodopoulos and A. Levers, Inter. J. of Fatigue, 2002, in press.
[5] C. Vallellano, A. Navarro and J. Domínguez, Fatigue Fracture Engineering Materials Structures, 2000, 23, 113–121.
[6] E. R. de los Rios, M. Trull and A. Levers, Fatigue Fracture Engineering Materials Structures, 2000, 23, 709–716.
[7] R. A. Smith and K. J. Miller, Inter. J. Mech. Sci., 1978, 20, 201–206.
[8] K. Tanaka, Inter. J. Fract., 1983, 22, R39–R45.
[9] S. Suresh, Fatigue of Materials, Cambridge University Press, 1991.
[10] C. Vallellano, A. Navarro and J. Domínguez, Fatigue Fracture Engineering Materials Structures, 2000, 23, 123–128.
[11] J. K. Li, M. Yao, D. Wang., R. Wang, Fatigue Fracture Engineering Materials Structures, 1999, 15(12), 1271–1279.
[12] E. R. de los Rios and A. Navarro, Philosophical Magazine 1990, 61, 435–449.