ASSESSMENT OF FRETTING FATIGUE IN HIGH STRENGTH STEEL
BOLTED CONNECTIONS WITH SIMPLIFIED FE MODELLING
TECHNIQUES

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Abstract: The fretting fatigue (FF) damage on contact surfaces constitutes a major problem in High Strength Steel (HSS) bolted connections when subjected to cyclic loadings, in that it significantly lowers the fatigue life. Moreover, the estimation of damage in the contact areas is challenging and laborious through experiments. The present paper proposes a simplified modelling technique to estimate the fretting fatigue damage in high cycle regime by considering the pretension effect and contact properties of bolted lap connections. To do so, a three-dimensional solid model is developed and validated. Afterwards, simplified FE models are validated using the three-dimensional model and test results to identify the most computationally efficient model for bolt connection in HSS under static and fatigue loading conditions. It was observed that the newly developed FE model could retain all the components on contacting surfaces while saving of up to 97% of the computational time over full solid models. These results successively provided insight into the crack initiation location and the fatigue lifetime of the joint and showed good agreement with experimental and three-dimensional solid model data.

1. Introduction

In recent years, the use of high strength steels (HSS) with high yield strength (> 460MPa) as a base material for the design of lightweight automotive and heavy machinery components is on the increase [1-5]. With a reduction of weight, HSS offers better ultimate tensile strength, wear-resistant, reduction in ductility and greater resistance to corrosion when compared with mild steel grades. Even the higher manufacturing costs of HSS compensate for the benefits of upgraded HSS, which include reduced weight, improved fuel efficiency and extended product lifetime – all of which effectively contribute to reduce carbon emissions.

However, the efficient use of HSS grades faces various challenges at the structural designing phase. Due to their susceptibility to defects, especially under fatigue loading; the fatigue life of widely used welded HSS connection is barely larger than that of mild steel [6-8]. Consequently, solutions are

sought by replacing welded connections by bolted ones. Although several design rules are available for bolted connections in different standards, these rules often do not take into account the specifics of the grades that are connected. Notably, the fatigue strength ratio of the HSS connections differs from the connections made of mild steel ones.

Even though many different models and technologies have been developed, the failure of mechanical connections remains hard to predict. The challenge faced by the designer community so far is the damage caused in contact regions because the loads transferred inside them are very complex. Many factors have to be taken into account when defining damage in contacting bodies. One of the main reasons is the frictional problem emerging in contact, and there can be a partial slip, lack of stability or vibrations so on. Another significant factor is the clamping force (pre-tension load), which creates a tensile force inside the bolt and leads to compression between the plates. It can control the frictional forces between the surfaces in contact [9-11].

Experiments have been already carried out to provide an insight into the load-carrying capacity of bolted connections made of steel grades from S235 up to S960. The investigation by Puthli and Fleischer [12] has to lead to the specified design rules of Eurocode 3 [13] for steel grades S235 and S355 regarding the minimum edge distance and bolt spacing on the bearing resistance. They concluded that Eurocode 3 [13] requires a reduction of the design bearing resistance of bolted connections under shear loading when the edge distance (end to bolt hole center) is less than 1.5 times the bolt hole diameter (d_o) or bolt inner spacing is less than $3d_o$. In 2005, Sofia et al. [14] did the same series of tests using the design rules of Eurocode 3 in order to investigate the HSS grade S690. The design of connections made from HSS up to S700 is covered in EN1993-1-12 [15]. However, the code does not allow the HSS connections to design within the semi-continuous/partially restrained concept. Coelho and Bijlaard [16] focused on research for the high deformation demands that are implemented on bolted connections. Here, special attention is given to validate the design rules to understand what is applicable to the high strength steel material. A report on comparative research of multi bolted connection was carried out by Moze et al. [17, 18] to evaluate the bearing resistance of S690 double shear joints.

Regardless of material strength, the behaviour of bolted connections is predictable, unless a sudden fatigue failure occurs as a result of fretting [19, 20]. Fretting arises because of an oscillatory relative displacement between two surfaces and also under the action of a normal pressure or load, which results in surface degradation and initiation of crack known as fretting fatigue damage. Based on past literature [21-23], micro-cracks are initiated during the partial sliding of the contacting surfaces and lead to a visible crack when it reaches the gross sliding. In essence, this implies that the fatigue lifetime can be affected by the partial slip region. Due to the increase in stress rate on the surfaces in contact, the fatigue lifetime is considerably reduced compared to fatigue without fretting. During fretting conditions, crack initiation can happen with low stresses which are below the fatigue limit, rather than with typical fatigue damage [24].

Hämäläinen et al. [25] investigated fatigue tests on double bolted lap joints made of HSS steel grades S355 and S960QC. The author implemented fretting criteria to compare the results of steel grades and concluded that the S355 joints showed no tendency towards fretting fatigue. However, S960QC failed at the connection due to fretting fatigue below two million cycles. Jiménez-Peña et al. [26] carried out similar experiments on a single lap bolted connection of HSS S500MC under different pre-tension loads. In their study, the results reveal that an increase in the pre-tension force considerably improves the fatigue lifetime of the joint and the fretting fatigue damage was observed in all tests of S500MC connection. In spite of the materials, many researchers [27-29] have used multiaxial fatigue criteria to estimate the fatigue damage in bolted connections. The overwhelming majority of the literature available [25, 26, 28, 29] concludes that the fretting fatigue failure occurs near the bolt hole at the plate-plate contact interface. Studies reveal that the significant part of fatigue life is consumed in the

crack initiation phase (85% approx.) and the rest during the crack propagation phase [30-32]. Such results clearly indicate the importance of understanding and estimating the crack initiation process in fatigue related mechanisms.

When trying to assess the fatigue life of bolted connections via experimental tests, the prediction of crack initiation in contact areas is challenging and complicated to validate because damage and cracks are hidden between the two contact surfaces. While geometrically detailed 3D solid models can reproduce this fretting [25, 26, 28, 29, 33], they are also computationally expensive and not suitable for large-scale structural analysis of assemblies. Considering the increasing interest in HSS bolted connections, this present research aims to deliver an advanced and computationally efficient simplified FE model with an equivalent bolt to predict the fretting fatigue contact problems. This research explores two simplified versions of bolted joint models, mesh dependent beam element representing bolt with solid elements and shell elements plate. These simplified equivalent models are used to estimate the fretting fatigue life in the high cycle fatigue regime using multiaxial fatigue damage criteria. Then the simplified equivalent models were validated using experimental data of S500MC bolted connections under shear fatigue loading and also with benchmarked three-dimensional finite element bolt models. All models are developed and analysed using the commercial FE-solver Abaqus Standard. The implicit load steps are used, as only the linear elastic behaviour of the joint under static loading was of initial interest. This force-controlled step provides a steady-state loading condition during the fatigue cycle.

2. Experimental observation

2.1. Results overview

To validate and evaluate the simplified FE models, an experimental test which exposes the fatigue behaviour of single lap bolt joint is needed. The test setup consists of two identical plates, shaped like a dog bone, made of S500MC HSS fastened by a bolt M16 grade 10.9, a nut (EN14399-4) [34] and two washers (EN14399-6) [35] on each side. The 3D view of the assembly is schematically depicted in Figure 1. More details of the specimen's geometrical dimensions are shown in Figure 2 and are described in Section 3. The mechanical properties of the material S500MC HSS are to be found in the literature and are shown in Table 1.

Three series of fatigue tests were carried out for each nominal stress range at an axial stress ratio $R = 0.1$ and a cyclic test frequency of 25 Hz. In addition, the SLBJ was tested under quasi-static loading conditions up to a load of 90kN at a load rate of 0.1 kN/s. The details of the experiment's programme were published by Jiménez-Peña [26]. Figure 3 represents the axial load versus displacement computed between the clamped edges using Digital Image Correlation (DIC) technique. The force-displacement behaviour observed was very similar for all the tested samples. In the elastic phase, it is assumed that relative displacement is accommodated by micro slip between the surfaces in contact and by the elastic deformation of the contacting solids. The elastic phase mentioned is taken into consideration for the initial step of validating the FE models. However, by increasing the axial load, the joint slips and the bolt starts to bear on the plate. Eventually, the elongation of the bolt hole and the plate is noted. Each specimen was tested until failure, defined as a complete fracture of the connection, appeared. The damage was observed in detail in the contact area at the plate-plate and the plate-washers due to the application of the clamping force, as indicated in Figure 4. However, there was no sign of fretting damage or wear found between washer-plate (bolt/nut side) interface. Although a clear indication of the fretting fatigue failure mechanism between the plate-plate contact interface was observed and appeared to be constant in all samples tested, as shown in Figure 4. The surface of the plates displayed two distinguished concentric regions of sticking and slipping around the hole, with fretting scars at the slip region. Based on the experimental overview, the upper plate (completely fixed

one) fractured 50% of the times while the lower plate (loaded plate) 38% of the times. For the remaining samples (12%), the fracture was observed for both plates simultaneously. In the research at hand, the FE models focused on the crack initiation stage as it is assumed that the major part of fatigue life is spent in this stage.

Figure 1. Schematic diagram of single lap bolt joint specimen.

Figure 2. Geometry of the HSS plate (dog-bone shaped specimen).

2.2. Evaluation of fatigue results

The loading conditions and the total number of cycles to failure (N_{t-exp}) are presented in Table 2. For the extraction of $\Delta S-N$ curves, all stress ranges are calculated corresponding to Eurocode-3 [13] and plotted against its number of cycles. While the fatigue lives are estimated within the range of $10^4 - 10^7$ cycles. Additionally, International Institute of Welding (IIW) recommendation [7] was considered for the estimation of characteristic curves, since the numerical investigation was focused on the crack initiation stage. The proposed fatigue characteristic curves with tested fatigue results of Single Lap Bolted Joint (SLBJ) are presented in Figure 5. In the S-N curves, the characteristic curve that represents 97.7% survival probability was considered as a design curve and the survival

probability of 50% as a natural mean curve. The characteristic curves were estimated using equation (1) [7].

$$\log N = \log C - 2SD - m.\log \Delta S \quad (1)$$

whereby ΔS , N , m , C and SD are the stress range, the number of cycles to failure, slope, load intensity value and standard deviation (capacity) respectively.

For the extraction of ΔS - N curves through the numerical approach, the fatigue properties of the S500MC HSS were needed. It was deduced [36, 37] from the stress-strain controlled hysteresis loop and fatigue life data. Table 3 listed the fatigue properties of the base material. Additionally, the obtained fatigue lifetime of HSS S500MC SLBJ was compared with the fatigue lifetime of its base material HSS S500MC, as illustrated in Figure 6. The mean value of the base material fatigue strength was identified with a slope of 5, ruling out two run-outs. The plots show a definite shift in the ΔS - N curve, with a substantial decrease in the number of cycles to failure for the SLBJ. This shows the importance of analysing the bolted joints by choice of HSS, especially in the case of fatigue loading. Different parameters can influence the fatigue resistance of the SLBJ and are needed to be investigated for joint efficiency improvement. Thus, this paper aims to introduce simplified modelling approaches for a bolted connection, so its fatigue performance can be estimated beforehand without the need for experimental testing.

Table 1. Material properties of S500MC HSS.

Material grade	Mechanical Properties			
	Elastic modulus, E [MPa]	Poisson's ratio, ν	Yield stress, σ_{yield} [MPa]	Ultimate tensile stress, σ_u [MPa]
S500MC	219000	0.29	562.7	658

Table 2. Fretting fatigue experimental results for SLBJ, $F_p = 93.75$ kN.

n	t [mm]	F_{max} [kN]	F_{min} [kN]	$\sigma_{\text{max (nom)}}$ [MPa]	ΔS (nom) [MPa]	ΔS (eurocode) [MPa]	$N_{\text{t-exp}}$ (cycles)
1	6	25	2.5	122.55	110.29	73.53	5000000
2	6	25	2.5	122.55	110.29	73.53	5000000
3	6	25	2.5	122.55	110.29	73.53	4277241
4	6	30	3.0	147.06	132.35	88.24	1931065
5	6	30	3.0	147.06	132.35	88.24	1936088
6	6	30	3.0	147.06	132.35	88.24	1356807
7	6	35	3.5	171.57	154.41	102.94	1285050
8	6	35	3.5	171.57	154.41	102.94	691815
9	6	35	3.5	171.57	154.41	102.94	595501
10	6	40	4.0	196.08	176.47	117.65	359068
11	6	40	4.0	196.08	176.47	117.65	434557
12	6	40	4.0	196.08	176.47	117.65	873288
13	6	50	5.0	245.10	220.59	147.06	151852
14	6	50	5.0	245.10	220.59	147.06	156987
15	6	50	5.0	245.10	220.59	147.06	197536

Table 3. High cycle fatigue properties of S500MC HSS tested under R = 0.1.

Grade	Fatigue strength coefficient σ_f [MPa]	Fatigue strength exponent b
S500MC	11463.0	-0.271

Figure 3. Static axial load versus displacement for SLBJ.

Figure 4 Damage assessment in SLBJ during fatigue test at $F = 50$ kN at $F_p = 93.75$ kN; (a) plate-washer interface and (b) plate-plate interface.

Figure 5. ΔS - N curve of the fatigue testing results, including survival probability curves according to the IIW recommendation, evaluated with $m = 4.79$.

Figure 6. ΔS - N characteristics curves obtained from the S500MC HSS and its SLBJ.

3. Multiaxial high cycle fatigue criteria

From the experimental results, it is concluded that the fretting fatigue damage was observed for all the tested samples in the plate-plate interface. The damage process localised at the frontier of the contact between two contacting bodies subjected to fatigue loadings. Thereby assuming that the prediction of damage causing stresses at contact is of major importance in determining the lifetime of bolted connection.

In this study, 3 different multi-axial approaches have been used to predict the crack initiation location and fatigue lifetime. The flow chart presented in Figure 7 illustrates the methodology for a typical FF problem, which focuses on the estimation of crack initiation lifetime under elastic conditions. The initial step was to determine the crack initiation site. This was accomplished by identifying the location where given multi-axial fatigue damage parameter achieves its maximum value. In this work, stress and strain are calculated along the crack region as elastic. Consequently, the maximum damage parameter values extracted from numerical results and fatigue properties have been employed into multi-axial fatigue damage equations to compute the fretting fatigue life estimation.

The multi-axial fatigue criteria are implemented by post-processing the FEA results using the python scripting interface in Abaqus and the used multi-axial fatigue criteria are reviewed in the following sections.

Figure 7. Flow chart showing the numerical approach of fretting fatigue damage lifetime prediction.

3.1. Smith Watson Topper (SWT) parameter

Smith [14] proposed an experimental damage parameter on the basis of stress effects on the plain fatigue. The SWT parameter is used for the analysis of both proportional and non-proportional loaded components which materials that fail primarily due to mode I tensile cracking. The parameter is expressed as a function of the number of cycles to initiation (N_i) of cracks and is defined by equation (2).

$$SWT = (\sigma_{\max} \epsilon_a)_{\max} = \frac{(\sigma'_f)^2}{E} (2N_i)^{2b'} \quad (2)$$

The SWT parameter is the product of principal strain amplitude (ϵ_a) and maximum normal stress ($\sigma_{\max}$) at the critical plane. Where, σ'_f is the fatigue strength coefficient and b' is the fatigue strength exponent. In this study, the maximum value of the product in the contact node has been used. Then the maximum values of these products will be defined in equation (2) to estimate the fatigue life. According to SWT parameter, the crack initiation is possible to occur on the critical plane where a combination of principal strain range and normal stress reaches its maximum value.

3.2. Fatemi-Socie (FS) parameter

The multi-axial criterion is modified from Brown and Miller's [38] critical plane formulation, which is based on strain values of the planes. Fatemi and Socie [39] [10] modified the formulation to identify the multi-axial fatigue damage by replacing the normal strain with the maximum normal stress. FS parameter is developed on the principle of fatigue damage caused by the strain and maximum normal stress to the critical plane. The frictional force blocking the crack growth between slip planes got decreased by crack opening. For this criterion, the critical plane is defined where the shear strain range is maximized. FS parameter is expressed as,

$$FS = \frac{\Delta\gamma_{\max}}{2} \left[1 + k \frac{\sigma_n^{\max}}{\sigma_{\text{yield}}} \right] \quad (3)$$

where $\frac{\Delta\gamma_{\max}}{2}$ and $\sigma_n^{\max}$ are the maximum shear strain amplitude and maximum normal stress on the critical plane. σ_{yield} is the yield strength of the material and k is a material constant computed by fitting data from the simple uniaxial and simple torsion test result. By taking into account of a simple case of plain fatigue with reverse load cycle and combining Basquin Coffin Manson equation [40, 41], the number of cycles to initiation (N_i) of crack can be expressed as

$$FS = \frac{1 + \nu}{E} \sigma'_f (2N_i)^{b'} + \frac{k(1 + \nu)}{2 E \sigma_{\text{yield}}} \sigma'_f (2N_i)^{2b'} \quad (4)$$

where elastic deformation has been considered. In this paper, k value is considered as 0.8, extracted from literature [26], for the prediction of crack location. According to the sensitive study [26] on k value, the predicted crack locations by FS parameter are insensitive to the constant k . Also, the study indicates that the locations remain unchanged for a value higher than 0.1.

3.3. Crossland parameter

Crossland [42] proposed this criterion based on the stress invariants which is the maximum amplitude of the square root from the second invariant of deviatoric stress tensor and maximum hydrostatic stress. Crossland parameter is expressed as

$$CP = \sqrt{J_{2,a}} + \frac{I_{1,\max}}{3} \left(\frac{3t_f}{b_f} - \sqrt{3} \right) \quad (5)$$

$$CP = \sigma'_f (2N_i)^b \cdot \left(\frac{t_f}{b_f} \right) \quad (6)$$

where J_2 is the amplitude of the second invariant of the deviatoric stress tensor, $I_{1,\max}$ is the maximum of the first invariant of the stress tensor, t_f is the shear fatigue limit, b_f is the bending fatigue limit and k , $\left(\frac{3t_f}{b_f} - \sqrt{3} \right)$, is the material-dependent constant. The value of k is assumed to be unit for this research work.

3.4. McDiarmid parameter

McDiarmid proposed multi-axial fatigue criteria [43] for finding a critical plane on which fatigue failure will occur. In this criterion, the critical plane is subjected to the plane where the shear stress is maximum. McDiarmid equivalent stress is expressed as:

$$\sigma_{\text{eq}} = \frac{\Delta\tau_{\max}}{2} + \frac{\tau_f}{2\sigma_u} \sigma_n^{\max} \quad (7)$$

where $\Delta\tau_{\max}$ is the maximum shear stress. This parameter is only based on cyclic stress state rather than the combination of stress and strain.

3.5. Ruiz criterion

Ruiz [44] proposed two formulations for assessing the damages during fretting fatigue loading condition. The first parameter is a function of relative slip displacement (δ) and maximum shear stress (τ_f). Since this parameter provides only crack nucleation, combining the influence of tangential stress ($\sigma_{11}^{\max}$) with the product of two surface damage driving factors (δ and τ_f) at contact provides the crack initiation location. The second parameter is denoted as Fretting Fatigue Damage Parameter (FFDP) and is expressed as

$$\text{FFDP} = \sigma_{11}^{\max} \tau_f \delta \quad (8)$$

This criterion considered that the maximum stress controls the crack growth in tangential direction at the interface.

4. Model development and its validations

To reduce the modelling complexity and to streamline the analysis, simplified FE models were introduced for the numerical analysis of SLBJ. A detailed full solid model reflects all the aspects of the physical problem, including all contact relations, geometric details and physical behaviour of the bolt. So, a full solid model was used as a benchmark for testing the accuracy of the newly proposed equivalent bolt models. Therefore, in the present research, two types of simplified equivalent models are introduced applicable to different joint configurations and loading conditions. They should replicate the dominant factors on the behaviour of bolted joints in holding the moderately thick/thin plates together. Finally, the results can be used to estimate the fatigue strength of the SLBJ and the number of cycles to fretting fatigue crack initiation. For the present purpose, all models were created and analysed using the commercial software Abaqus [45].

4.1. Numerical modelling strategies

4.1.1. Full solid model

A detailed three-dimensional (3-D) FE model of the SBLJ shown in Figure 8 (a), was created and used for the initial development of the bolt model. The full solid model results are considered to be the closest prediction of the experimental results. The dimensions were kept the same as the tested specimen presented in the previous section. Figure 8 (a) equally shows the assembly of the full solid model assuming symmetry [along the XY plane](#) to reduce the computational cost. The modelled parts paid attention to details such as plates (dog bone shaped), bolt (head and shank), nut and two plain washers on both sides. The solid plates are meshed by 8-node linear brick elements (C3D8R) with a reduced integration to prevent shear locking. The region around the hole and washer contact interface has the maximum stress concentration, so the mesh is refined sufficient on the region in successive iterations. Considering the mesh sensitivity study, a mesh size of 0.3x0.3 mm was sufficient around the hole and gradually [increased to the clamping edge](#) of the plate. The defined mesh is sufficient enough to capture global and local response for all models.

For attaching the parts, tie constraints were used between the bolt head to the washer, nut to washer and bolt shank to nut. The parts were assigned HSS S500MC material properties, as tabulated in Table 1, to reproduce the elastic behaviour of the joint. The material is defined to be elastically isotropic. The contact interaction between the components in bolted connections can significantly affect the FE results. The contact between the plates was modelled using the master-slave algorithm considering the upper plate as master surface. The contact properties between these surfaces are modelled with the

penalty contact approach assuming a static coefficient of friction (COF) to represent the tangential behaviour; the normal behaviour is employed by assuming hard contact respectively. The value of COF ($\mu = 0.77$) was adopted from the previous study of Jiménez-Peña [18], which was computed by dividing the tangential force at the onset of gross sliding by the normal force. A small sliding surface-surface discretization approach was defined, i.e. any rotation or deformation and geometric nonlinearities were allowed.

Proper representations are made to employ the boundary and load conditions in the FE models as close as possible to the test setup. Initially, the plate ends are constrained to a single reference node with a kinematic coupling constraint by keeping all the degrees of freedom. Then, the loads are applied in two stages with the specified boundary conditions. In the first stage, both sides of the plate were constrained completely and the bolt load/clamping force (F_p), estimated from experimental tests is applied to the shank of the bolt model. The implementation of the bolt load is done by splitting the bolt shank into two parts and applying the magnitude in the middle surface. In Abaqus Standard, the bolt load function creates a tension across the specified surface and results in tightening effect of the bolt [45]. In the second stage, loading direction (right side) of the lower plate is freed and concentrated forces (F) are applied gradually up to the maximum value of 50 kN (till bolt slip). Additionally, the bolt length after the previous step is assigned to be fixed during the second step execution. This conditions in the bolt model will act like a real fastener and the plates are matted together during loading time. Due to the symmetry boundary conditions, the full solid model is partly loaded in both stages.

4.1.2. Solid Equivalent model

To reduce complexity, the model excluded detailed structures such as bolt-head, nut and washers. Figure 8 (b) shows the full assembly of a solid equivalent bolt model. This model consists of two three-dimensional (3-D) element plates, dog-bone-shaped, held together by a beam bolt model. Similar to a full solid model, the plates are modelled linear solid elements with reduced integration. The beam bolt was modelled with a set of linear beam elements of B31 in Abaqus, similar to the work of Gray et al. [46].

Figure 8 (b) depicts the beam bolt model, cylindrical section of the fastener. The model used linear Timoshenko beam elements with a uniform circular beam profile to represent the bolt shank. This allows transverse shear deformation. In Abaqus [45], the shear stiffness was defined by the available inbuilt option and calculated from the assigned elastic material properties of the bolt (as listed in Table 1) during analysis. Along the bolt shank, the cross-sectional diameter is set to the nominal bolt diameter, M16 bolt. The two independent nodes at each end of the beam element are considered as the master nodes for the control of bolt-head and nut. The nodes corresponding to the bolt-head and the nut are connected to the beam element through coupling constraints. More specifically, both the bolt-head and nut are represented with circular distributed coupling constraints, web-like design, connected to the washer bearing contact surface of each plate. The use of kinematic coupling and mesh independent connectors leads the FE models to have high stiffness compared with the experimental results, as shown in Figure 9. Hence, the distributed coupling constraint is enforced by distributed loads so that the resultant forces at the coupling nodes are equivalent to the reference node. In the action of transmission of forces and moments, force distributes like a classic bolt-pattern distribution, where the load distribution can be described with a moment-of-inertia expression [45]. In this model, the mechanical properties of the beam element are set to be exactly the same as the solid bolt. However, the contact properties and interaction details between the bolt's end features (head or nut) cannot be accounted for result extraction. The research mainly focuses on the critical contact areas of

the SLBJ. Accordingly, the geometric details of end features of bolt or washers are omitted for more efficiency. Nevertheless, the modelling technique addresses the important aspects of lap bolted connection like pre-loading condition, contact contour between plates, frictional coefficient and joint stiffness.

The plates are fully modelled like the sample plate tested, mainly due to the limitation in symmetry modelling of beam bolt model. The constraints represent the bolt-head and the nut are coupled to each node within the projected washer contact area on the surface of the plates, see Figure 8 (b). The elastic behaviour of the components was defined using the properties mentioned above in Table 1. For preloading, the bolt load is directly applied to the pre-tension section defined at the middle of the beam bolt shank, as shown in Figure 8 (b). Afterwards, the bolt length after the bolt load step is assigned to be fixed during the axial loading. The boundary and axial loading conditions are defined as same as in the previous section.

4.1.2.1. Shell equivalent bolt model

Further simplifying the model, shell elements have been modelled to replace the solid plates. This model consists of two shell element plates held together by a beam bolt model mentioned in the previous section, as illustrated in Figure 8 (c). Similar to solid equivalent model, the shell plates are meshed using reduced integration shell elements, S4R (linear 4 sided) and S3 (linear triangular), and the beam bolt was modelled with a set of linear beam elements.

Initially, the shell element is defined with the homogeneous shell section and a corresponding integration method is adopted through the thickness of plates. For the accuracy of the contact variables, the reference surface of the thickness is considered at the bottom section (offset is negative) of the shell. In this study, to account the rotation and geometric nonlinearities at the contact, the finite sliding approach was used. The material properties, interaction approach and boundary conditions are kept the same as the solid plates which are discussed in the previous section. The two master nodes of the beam bolt are constrained with the washer contacting areas of the shell plates using uniform distributed coupling. These coupling constraints simulate the effects of the clamping surface of the bolt head, nut and the washers.

Figure 8. Finite element models for SLBJ; (a) Full solid model, (b) Solid equivalent model and (c) Shell equivalent model.

Figure 9. Axial load versus displacement of a different modelling approach for Bolt-head/nut/washers in accordance with Abaqus standard.

4.2. Model verification

Before proposing the simplified model of SLBJ for fatigue assessment, it is essential to validate the FE models results versus experimental observations. Figure 10 illustrates the axial load versus the displacement curve, load up to 40 kN, for all FE models and the experimental one. Initially, the slopes show a linear behaviour and are well correlated to each other within the elastic regime. It can be noticed that there is a sudden increment in the displacement and also a significant drop in load when the experimental result reaches its maximum applied load. This small difference is always observed between results from experimental and numerical tests due to various reasons (e.g. assumed stiffness of the bolt, relative slip between the plates, variation in material property and so on), which is also observed in the results presented in the existing literatures [26, 47].

For a quantitative comparison, the variation of contact status and the stress variables at the contact interface of the plates were extracted. The obtained results show good agreement between the different FE models at different load levels. Figure 11 illustrates the contact status between the plates, which has been divided into three regions: sticking, slipping and open. The plots revealed that around the hole edge of the plate-plate interface is fully sticking and the presence of slipping is located at a distance from the hole edge (i.e. the side of loading edge). To validate the models, contact status between the plates have been measured at a path, shown in Figure 11 (a). The status characteristics measured are similar in all FE models, as observed in the tested specimen.

Figure 12 (a) and (b) shows the tangential stress and frictional shear stress around the bolt hole at the contact interface. The peak stresses are located at some distance from the edge of the hole and are comparatively the same for the solid equivalent model and the full solid model. However, the model with shell element shows 20% lower at the peak stress location. That can be explained by the fact that the solid model has a continuous thickness with gradual transitions. Additionally, while using the shell elements, the stresses are assumed to be linear through the thickness, which neglects the peak stresses. Overall, the peak stresses locations correspond with the partial slip region and the crack initiation region during the experimental test, as depicted in Figure 11.

Figure 12 (c) shows the contact pressure around the hole edge between the plates. The presence of high contact pressure around the edge of the hole and it drops gradually after some distance from the hole. Overall, the stress distribution trend is similar for all numerical models. The contact pressure is notably high at the edges of the hole for models used beam bolt. The 15% increase in pressure

variation may be due to the coupling constraints that it defined at edge nodes of the hole that is not in contact with the bolt or due to the absence of washer modelling. However, the differences in results made by equivalent FE models were not influential in affecting the methodology to estimate fretting fatigue lifetime, as it will be discussed later on. Apart from the negative differences between different developed models, the obtained results of the equivalent FE models are in good agreement with the full solid model and experimental test results, which confirms the validity of the proposed modelling strategies.

Figure 10. Comparison between experimental results and FE models of SLBJ at $F_p = 93.75$ kN.

Figure 11. Status of the contact region (between plates) during fatigue loading at $F = 50$ kN with $F_p = 93.75$ kN; (a) Experimental; (b) Shell equivalent model; (c) Solid equivalent model and (d) Full solid model.

Figure 12. Stress distributions in contact interface between the plates at $F = 50$ kN with $F_p = 93.75$ kN: (a) tangential stress; (b) frictional shear stress and (c) contact pressure.

5. Results and discussions

A multi-axial fatigue life prediction approach is used to predict the fatigue life of SLBJ. The precise determination of stress and strain components and their distribution at the joint is necessary in order to use multi-axial fatigue theories. Therefore, finite element-based techniques are required to come up with a solution to stress-strain calculations. Based on the numerical results from the simplified models, one can obtain the stress/strain variations, slip amplitude and the contact status along the contact surface, which will be a good input to the prediction of the FF damage. A comparison between the different FE models versus the experimental fatigue life predictions is carried out.

5.1. Estimation of crack initiation location

The stress concentration at the edge of the bolt hole is reduced by bolt pretension, but this leads to fretting and develops a stress concentration at a certain distance away from the bolt hole. This promotes early crack initiation in the contact region as shown in Figure 13 and Figure 14. As mentioned previously, crack initiation is identified in the plate-plate contact interface for all tested specimen. Figure 13 and Figure 14 illustrates the prediction of crack initiation locations using (a) SWT, (b) FS, (c) Crossland, (d) McDiarmid and (e) FFDP. The maximum value of these multi-axial fatigue damage parameters locates the crack initiation region. These are similar to the crack initiation region observed in the tested samples and the region with the maximum shear stress indicates the shift between the stick and slip status. **Therefore, by assuming the red contour plot in FE models as the maximum multiaxial damage stress, the location of maximum parameters can be related to the location of crack measured, S_i , from the bolt hole.** As explained in the previous section, the calculations of the parameters are based on the maximum stress and strain on the contact plane of the loaded plate and in particular at the trailing edge (sticking – slipping transition region) of the contact status. However, the location of maximum stress and strain changes during the load steps. This explains the multiple crack initiation sites identified along the fracture surface of the tested specimens.

Figure 15 (a) and (b) plots the multi-axial fatigue criteria along the centerline (extracted path) of the loaded specimen. The transition of parameters from sticking to slipping region, measured from contact status, can clearly be identified. In addition, the maximum value of the damage parameters is plotted in Figure 15. Although, the equivalent models differ up to 6% in the identification of the parameter in the extracted path, which is due to the small variation of stress levels. Overall, the equivalent models provide similar results that the maximum parameters have remained in the sliding region. **These results enabled the capability** of proposed FE models to replace the full solid model in estimating the locations of the crack initiation. Furthermore, the crack initiation position at a distance (S_i) from the hole edge is measured from tested specimens and are compared, shown in Table 4, with the equivalent models at the point of maximum parameters in the extracted path. These results from the models indicate the fact that fretting fatigue is the dominant failure mechanism in S500MC HSS SLBJ, especially under shear loading.

The study also revealed that the contact around the holes between the plates **is fully sticking**, which reveals the influence of bolt pretension. As reported before, the stress concentration was moved away from the edge of the hole due to the preload force. Again, differences are visible in the solid equivalent model compared to the shell equivalent model, due to the gradual thickness transition exclusive to the solid element modelling.

Figure 13. Predicted crack initiation location for SLBJ using solid equivalent model with (a) SWT, (b) FS, (c) Crossland, (d) McDiarmid and (e) FFDP at $F = 50$ kN with $F_p = 93.75$ kN.

Figure 14. Predicted crack initiation location for SLBJ using shell equivalent model with (a) SWT, (b) FS, (c) Crossland, (d) McDiarmid and (e) FFDP at $F = 50$ kN with $F_p = 93.75$ kN.

Table 4. Crack initiation position measured from test specimens and numerical models.

ΔS [MPa]		S_i mm				S_i (avg) mm
147.06	Experiment tests	3.27	3.86	4.10	-	3.75
		FS	SWT	Crossland	McDiarmid	FFDP
	Solid Equi. model	3.05	3.66	3.06	3.36	2.75
	Shell Equi. model	3.97	4.27	3.96	3.92	2.75

Figure 15. Multiaxial damage parameters result along the specified path at the plate-plate interface: (a) Solid equivalent model and (b) Shell equivalent model.

5.2. Prediction of crack initiation lifetime

In all cases, computed data was at high cycle fatigue, and the theorisation was that the significant part of the total fretting fatigue lifetime was crack initiation phase. Figure 16 shows the ΔS -N curves of S500MC HSS SLBJ obtained from experimental tests (reported in Table 2) and FE models. The ΔS -N curves of FE models are plotted against the 97.7% survival probability of failure. To compute the crack initiation lifetime in SLBJ, three multi-axial fatigue parameters have been used. The estimated ΔS -N curve from different FE models was able to evaluate the fatigue lives close to the experimental results. However, it can be seen in Figure 16 (b) that the predictions made by SWT is underestimated. Figure 16 (c) shows that the Crossland criterion overestimates at the lowest fatigue cycle and underestimates at the higher fatigue cycles.

Figure 17 depicts the predicted fretting fatigue crack initiation lifetime versus the experimental lives. Both are plotted in log scale. It is pointed out that for lives ranging between 10^4 and 10^7 , FS parameter predicts accurately for this specimen. The obtained accuracy in the current investigation may be due to the shear loading conditions. Also, the predictions of Crossland criterion fall in the range of life factor 2. However, SWT leads to a conservative prediction for all FE models.

Overall, the estimated numerical results of simplified FE models fall within the scatter band of ± 2 N for FS and Crossland approach. Table 5 indicates that the (up to 84%) crack initiation life consumes a major part of total test life for partial slip boundary condition. The results in terms of a piece of evidence to confidently estimate fretting fatigue crack initiation lifetime with the proposed simplified equivalent models. From the generated results of FE models, the shell equivalent model can reproduce the results like a full solid model with a better stress approximation. The computational effort for the full solid model was considerably higher. It is worth mentioning that, keeping the same level of accuracy the shell equivalent model saves computational time up to 96% with the full solid model and 92% with the solid equivalent model, see Figure 18. The equivalent model leads to the reduction in computational cost and the need for experimental testing, especially for large-scale structures having multi-bolts.

Table 5. Lifetime prediction of crack initiation using FS parameter for Shell equivalent model.

n	ΔS (Eurocode) [MPa]	N_{i-FS} (cycles)	N_{i-exp} (cycles)	N_{t-exp} (cycles)	$(N_{i-exp}/N_{t-exp})\%$	$(N_{i-FS}/N_{t-exp})\%$
1	73.53	2.18E+06	2.52E+06	4.28E+06	58	50
2	88.24	9.50E+05	1.07E+06	1.93E+06	55	49
3	102.94	5.00E+05	5.14E+05	5.96E+06	86	83
4	117.65	2.90E+05	2.73E+05	3.59E+05	76	81
5	147.06	1.11E+05	9.52E+04	1.52E+05	62	73

Figure 16. Comparative curve for fatigue strength received experimentally and with the predictions made by (a) FS, (b) SWT and (c) Crossland at $F_p = 93.75$ kN.

Figure 17. Identified experimental fatigue life versus predicted fatigue lives using simplified FE bolt models.

Figure 18. Computational efficiency of different FE models proposed for fatigue assessment.

6. Conclusion

In this paper, different series of high efficient finite element analyses have been carried out on SLBJ made of HSS plates. From the numerical point of view, by setting-up the FE model, there are plenty of parametric studies that can be performed which provide extra information such as contact status, relative slip and stress distributions at the contact interface of a bolted connection. This information could not be monitored directly using experimental tests. However, these are generally captured by the 3D solid model, which are computationally expensive and complicated. Hence, two kinds of the simplified equivalent FE models were suggested to capture the physical and damage characteristics of single bolt lap joints.

Finally, the fretting fatigue behaviour of the SLBJ has been investigated using the proposed equivalent bolt models. The fatigue crack initiation lifetime was calculated as a function of stress and strain fields at each point of the plate subjected to damage. In the current work, the damage parameter is analysed only in the contact surface. It has been shown that the simplified FE model increases the possibility to quickly investigate the effects of different damaging parameters between the counterparts plates of the SLBJ when subjected to the fretting fatigue loading conditions. Among the

applied multi-axial criteria, the Fatemi-Socie parameter has the best accuracy over high cycle fatigue lives for a single lap bolt joint.

When the full solid model was downgraded to a simplified model, computational time was significantly saved, and the complexity of the geometry was also reduced. The simplified FE models can accurately predict the mechanical behaviour of the bolted connection and the response of the base material. Validation studies have shown that in many cases, the shell equivalent model efficiently provides realistic results. It is expected that the shell equivalent modelling approach will apply to multi-bolted connections.

Overall, the results of this study indicate that the maximum stress and strain amplitudes at the contact interface are an essential factor in the development of parameters to predict fatigue damage in a bolted connection. Therefore, the simplified equivalent models should retain all the effects of contact surfaces in order to increase the predictive accuracy of the fatigue life. With a view to effectiveness and usefulness, the shell equivalent model is recommended for further studies.

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