Proceedings of COBEM 2011
21st Brazilian Congress of Mechanical Engineering
October 24-28, 2011, Natal, RN, Brazil

FATIGUE BEHAVIOUR OF RIVETED AND BOLTED CONNECTIONS MADE OF PUDDLE IRON - PART II: NUMERICAL INVESTIGATION

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Abstract. *In Europe and North America there are a number of old riveted highway/road and railway bridges, constructed during the second half of the 19th century up to the middle of the 20th century which, due to economic reasons, are still in operation. Since they have been subjected to increasing traffic intensity along their operational lives, both in terms of vehicle gross weights/axle loads as well as truck/train frequencies, their damage levels need to be assessed in order to decide about possible repairs. Thus, the maintenance and safety of these existing bridges is a major concern of governmental agencies. In order to assure high safety levels in old riveted steel bridges, highway and railway authorities have to invest heavily in their maintenance and retrofitting. Fatigue failures are a concern for riveted steel bridges since they were not originally designed taking into account fatigue. The fatigue phenomenon was only intensively investigated after the half of the 20th century, when the riveted construction was no longer applied in new bridge structures. Therefore, there is a lack of a comprehensive methodology for the fatigue assessment of riveted bridges motivated by limited knowledge on the fatigue behavior of this type of construction as well as a deficient understanding on the fatigue behavior of the old materials (wrought-iron, puddle iron or old steels) and riveted connections. This paper presents a numerical study concerning the fatigue modeling of riveted and bolted joints made of puddle iron from the centenary Portuguese Fão bridge. A finite element model is proposed using solid and contact finite elements. The effect of the clamping stresses is taking into account on the model as well as friction. The fatigue crack initiation and propagation are both modeled using, respectively, the local strain and Fracture Mechanics approaches. Numerical S-N curves are predicted and compared with the experimental data available for the investigated joints.*

Keywords*: Riveted joints, Bolted joints, Fatigue behavior, Ancient riveted bridge, Finite element modeling*

1. INTRODUCTION

In Europe and North America there are a number of old riveted highway and railway bridges, constructed during the second half of the 19th century up to the middle of the 20th century which, due to economic reasons, are still in operation. Since they have been subjected to increasing traffic intensity along their operational lives, both in terms of vehicle gross weights/axle loads as well as truck/train frequencies, their damage levels need to be assessed in order to decide about possible repairs. Thus, the maintenance and safety of these existing bridges is a major concern of governmental agencies.

In order to assure high safety levels in old riveted steel bridges, highway and railway authorities have to invest heavily in their maintenance and retrofitting. In particular, fatigue failures are a concern for riveted steel bridges since they were not originally designed taking into account fatigue. The fatigue phenomenon was only intensively investigated after the half of the 20th century, when the riveted construction was no longer applied in new bridge structures. Therefore, there is a lack of a comprehensive methodology for the fatigue assessment of riveted bridges motivated by limited knowledge on the fatigue behavior of this type of construction as well as a deficient understanding on the fatigue behavior of the old materials (wrought-iron, puddle iron or old steels).

A number of fatigue assessment methodologies for riveted railway bridges have been proposed in the past (DiBattista *et al*., 1998)(Geissler, 2002), some being of probabilistic form (Bruhwiler & Kunz, 1993) (Kunz & Hirt, 1993)(Tobias & Foutch, 1995). The proposed probabilistic approaches seek the probability of failure or the reliability index, being inherent the comparison of the probabilistic fatigue strength data with the probabilistic fatigue loading. Some of the proposed methodologies for fatigue assessment of riveted bridges aimed the estimation of the remaining life of the primary members of the bridge (main girders, stringers, cross-girders) (DiBattista *et al*., 1998) (Geissler, 2002)(Bruhwiler & Kunz, 1993)(Kunz & Hirt, 1993)(Tobias & Foutch, 1995) and are supported by simplified global models of the bridge. However, most of the fatigue-damage related cases that have been reported for the riveted bridges were observed on riveted connections between the primary members and the fatigue damage has been attributed to secondary effects (e.g. out of plane deformation) (Fisher *et al*., 1987)(Al-Emrani, 2002). Very few fatigue assessments of riveted connections have been based on a detailed stress analysis such as that provided by finite element models (DePiero *et al*., 2002)(Al-Emrani & Kliger, 2003)(Imam, 2006).

Despite the S-N approach is widely used to assess the fatigue damage for riveted steel constructions (DiBattista *et al*., 1998)(Geissler, 2002)(Kulak, 2000)(Kim *et al*., 2001), Fracture Mechanics (Wang *et al*., 2006)(Paasch & DePiero, 1999) appears as an alternative approach to perform residual life calculations. However, the use of the Fracture Mechanics is very often limited to the application of simplified formulae for stress intensity factors evaluation, available in standard handbooks (Tada *et al.*, 2000). For example, the stress intensity factor in a cracked plate is calculated by considering an isolated plate rather than a plate integrated in a riveted structural member. No interaction is taken into account between the cracked plate and the remaining components of the member. This may result in inconsistent residual life evaluations (ex. residual life overestimation), motivating the search for more accurate stress intensity factors evaluation.

Very few works can be found in literature (Moreno & Valiente, 2004) regarding the stress intensity evaluation for riveted built-up beams. Riveted built-up beams are typical from the end of 19th /beginning of 20th centuries, when technology did not offer manufactured hot rolled beams nor welding techniques for making welded connections. Moreno & Valiente (2004) proposed an analytical model to assess the stress intensity factors for cracked web of a riveted T beam. The proposed model neglects friction effects and the clamping stresses on rivets and is limited to the specific investigated geometry. In a few number of cases, detailed 3D finite element models have been used in stress analysis of uncracked riveted connections (DePiero *et al*., 2002)(Al-Emrani & Kliger, 2003)(Imam, 2006)(Imam *et al*., 2007)(Righiniotis *et al*., 2008)(De Jesus *et al*., 2010). The analysis of cracked riveted geometries, using 3D finite element models is almost inexistent. Authors of the paper presented recently such approach for fatigue assessment of a single riveted joint (De Jesus & Correia, 2008).

This paper presents a numerical study concerning the fatigue modeling of riveted and bolted joints made of puddle iron from the centenary Portuguese Fão bridge. A finite element model is proposed using solid and contact finite elements in order to compute the elastic stress concentration factor as well as the stress intensity factors for crack emanating from the rivets or bolted joints. The stress intensity factors are computed by means of the virtual crack closure technique (VCCT), as described by Krueger (2004). The effect of the clamping stresses is taking into account on the model as well as friction. The fatigue crack initiation and propagation are both modeled using, respectively, the local strain and Fracture Mechanics approaches. Numerical S-N curves are predicted and compared with the experimental data available for the investigated joints (De Jesus *et al.,* 2011).

2. FATIGUE MODEL

The fatigue life of the riveted and bolted joints is computed assuming that fatigue life is computed taking into account the crack initiation and crack propagation periods:

$$N_f = N_i + N_p \tag{1}$$

where N_f is the total number of cycles to failure, N_i is the number of cycles required to initiate the crack and N_p is the number of cycles required to propagate the crack. While the crack initiation is modeled using strain-life relations, the crack propagation is modeled using Linear Elastic Fracture Mechanics. The transition from the crack initiation period to the crack propagation is a controversial subject in literature, but has been assumed by several authors that the transition is dictated by a crack size of a certain size. A crack size between 0.25 and 1 mm has been considered the usual range for this parameter.

The fatigue crack initiation may be computed using the Morrow's equation (Morrow, 1965):

$$\frac{\Delta\varepsilon_{loc}}{2} = \frac{\sigma'_f - \sigma_{loc,med}}{E}\left(2N_i\right)^b + \varepsilon'_f\left(2N_i\right)^c \tag{2}$$

where $\Delta\varepsilon_{loc}$ and $\sigma_{loc,med}$ are, respectively, the local elastoplastic strain range and average local stress; σ'_f and b are, respectively, the cyclic fatigue strength coefficient and exponent; ε'_f and c are, respectively, the fatigue ductility coefficient and exponent; E is the Young modulus. The application of the Morrow's equation requires the local elastoplastic strain range and average stress, which have to be computed using elastoplastic analysis. This paper uses the Neuber (1961) approach which is applied together with the cyclic curve of the material, defined according the Ramberg-Osgood (1943) equation of the material. The following system of equations is used to compute the maximum local stress (σ_{loc}) and strain (ε_{loc}):

$$\begin{cases} \dfrac{\sigma_{loc}^2}{E} + \sigma_{loc}\left(\dfrac{\sigma_{loc}}{K}\right)^{1/n} = \dfrac{K_t^2\sigma_{nom}^2}{E} \\ \varepsilon_{loc} = \dfrac{\sigma_{loc}}{E} + \left(\dfrac{\sigma_{loc}}{K}\right)^{1/n} \end{cases} \tag{3}$$

where K and n are, respectively, the monotonic strain hardening coefficient and exponent; K_t is the elastic stress concentration factor and σ_{nom} is the nominal stress. For cyclic loading, Eq. (3) may be rewritten as follows, replacing the maximum values by range values:

$$\begin{cases} \dfrac{\Delta\sigma_{loc}^2}{E} + 2\Delta\sigma_{loc}\left(\dfrac{\Delta\sigma_{loc}}{2K'}\right)^{1/n'} = \dfrac{K_t^2\Delta\sigma_{nom}^2}{E} \\ \Delta\varepsilon_{loc} = \dfrac{\Delta\sigma_{loc}}{E} + \left(\dfrac{\Delta\sigma_{loc}}{2K'}\right)^{1/n'} \end{cases} \tag{4}$$

where K' and n' are, respectively, the cyclic strain hardening coefficient and exponent. The average local stress ($\sigma_{loc,med}$) required for the Morrow's equation, is computed as follows:

$$\sigma_{loc,med} = \sigma_{loc} - \frac{\Delta\sigma_{loc}}{2} \tag{5}$$

The proposed elastoplastic analysis is a simplified approach, but it is significantly less time consuming than a fully elastoplastic finite element analysis. Nevertheless, the elastic stress concentration factor is required which is computed for the riveted and bolted joints under investigation using finite element analysis, assuming materials as linear elastic. The elastic stress concentration factor is computed taken into account friction and preload/clamping effects introduced by rivets or bolts.

The crack propagation is modeled using the Linear Elastic Fracture Mechanics concepts. In this paper, the crack propagation law proposed by Paris (Paris & Erdogan, 1963) is applied:

$$\frac{da}{dN} = C(\Delta K)^m \tag{6}$$

where da/dN is the crack growth rate, ΔK is the stress intensity factor range, C and m are material constants. The number of cycles to propagate a constant depth crack (through thickness crack) from the initial size, a_i, to the final size, a_f, is computed thru the integration of the Paris's law:

$$N_p = \frac{1}{C}\int_{a_i}^{a_f} \frac{1}{\Delta K^m}\,da \tag{7}$$

The crack initiation size, a_i, has a significant influence on the number of cycles to propagate the crack since the stress intensity range assumes small values at the beginning of the crack propagation stage. The final crack size, a_f, may be considered the size of the net section of the connection or be evaluated as the crack size for which the maximum stress intensity factor reaches the material toughness. Both criteria should give approximately the same number of cycles required to propagate the crack, since there is important crack acceleration at the final stage of the crack propagation meaning a small number of cycles to increment the crack. The integration of the Eq. (7) may be performed using an approximate procedure of small crack increments, for which the stress intensity factor is assumed constant. The increment in the number of cycles, for each crack increment, may be computed using the following equation:

$$\Delta N = \frac{1}{C}\frac{1}{\Delta K^{m}}\Delta a \tag{8}$$

The computation of the number of propagation cycles, N_p, requires the stress intensity factors evaluated as a function of the crack size. The stress intensity factors were computed using the finite element model of the connections, using the VCCT (Krueger, 2004).

3. SUMMARY OF EXPERIMENTAL DATA

Riveted and bolted joints made of puddle iron from the Portuguese ancient riveted Fão bridge were fatigue tested in order to evaluate constant amplitude S-N data. Double shear splices, as illustrated in Fig. 1, were tested under load control and R=0. Figure 2 illustrates the S-N data obtained for three tested series, namely a test series of riveted joints and two test series of bolted joints, one without preloaded bolts and the other with preloaded bolts. The preload on bolts was defined by a torque of 70N.m, which corresponds approximately to a preload of 16kN or 52MPa in the bolt. The preload on bolts/rivets have a very significant influence on fatigue strength, as illustrated by Fig. 2. Details about the fatigue tests of the riveted and bolted joints and results discussion are given by De Jesus *et al.* (2011).

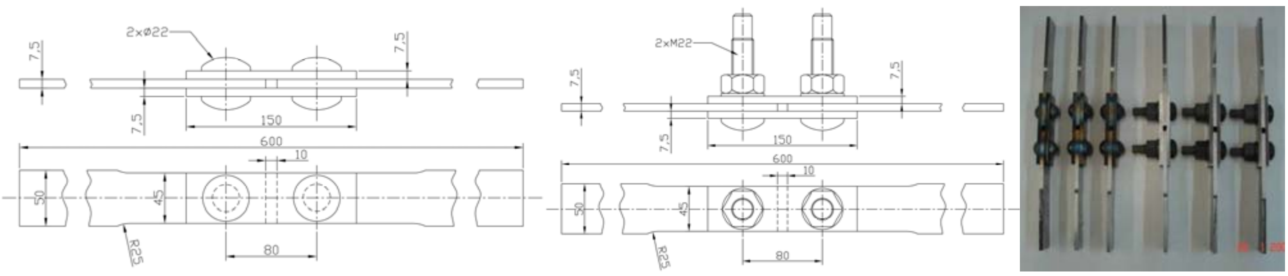

Figure 1. Riveted/bolted joints made of puddle iron from the Fão bridge.

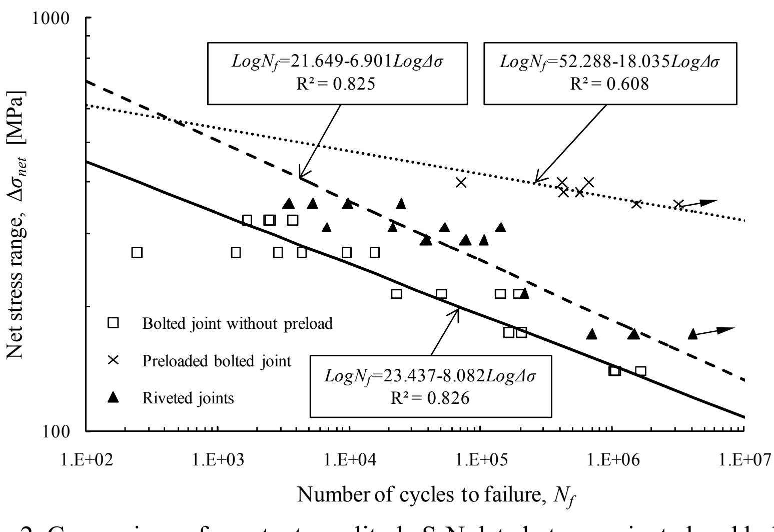

Figure 2. Comparison of constant amplitude S-N data between riveted and bolted joints.

The fatigue data of the puddle iron required for the fatigue modeling proposed in this paper, is described in detail by De Jesus et al. (2011). Table 1 summarizes the strain-life and crack propagation constants for the plain material. The strain-life data derived for repeated strain was adopted. The crack propagation constants were obtained for null stress range. The maximum stress intensity factor range observed in the experimental crack propagation tests was about 1200 $N.mm^{-1.5}$, for R=0.0. The simulation of the crack propagation test, carried out in this work, was terminated as soon as the stress intensity factor reaches the maximum stress intensity factor. It is worth mentioning that the CT specimens and the side plates of the connection exhibits approximately the same thicknesses, which makes the maximum stress intensity registered in the crack propagation tests, a reliable measure of toughness.

Table 1. Strain-life constants and crack propagation data of the puddle iron from the Fão bridge.

K (MPa)	n	K' (MPa)	n'	σ'_f (MPa)	b	ε'_f	c	$C^{(1)}$	m
477.53	0.13	818.47	0.14	635.80	-0.09	0.044	-0.48	4.2047×10^{-18}	4.8038

(1) : da/dN in mm/cycle and ΔK in $N.mm^{-1.5}$

4. FINITE ELEMENT ANALYSIS OF RIVETED AND BOLTED JOINTS

A 3D parametric finite element of the riveted/bolted connections was built to compute the elastic stress concentration factor as well as the stress intensity factor for a propagating fatigue crack. The same base finite element model was used to model the riveted and bolted connection. The riveted connection is modeled with null clearance between the rivet shoulder and the hole. The bolted joint is modeled with a diametric clearance between the bolt shoulder and the hole of 2 mm (bolt diameter of 22 mm and hole diameter with 24 mm). The FE model was built using the ANSYS® commercial code (SAS, 2010). Details about the FE model are given by Silva (2009). The contacts between plates and between the rivet and plates were modeled using contact finite elements. Surface-to-surface and flexible-to-flexible contact technology was used. In general, ANSYS® default contact parameters were used in the analysis (SAS, 2010). For the stiffness penalty factor, FKN, a value equal to 0.1 was adopted and a penetration tolerance factor, FTOLN, equal to 0.1 was used. The selection of the contact parameters, FKN and FTOLN, was based on a sensitivity analysis described in detail by Silva (2009). The proposed combination of FKN and FTOLN parameters is the one leading to more physical consistent evolution of the stress concentration with the clamping stress generated by the rivet or bolt (Silva, 2009). Figure 3 shows the finite element mesh of the riveted connection. Only ¼ of the geometry was modeled, taking advantage of the existing planes of symmetry. The bolted connection shows very similar mesh. The shape of the head of the connecter was not changed.

Rivets may apply a clamping effect on joints due to the plastic deformation and cooling process inherent to the riveting process. This clamping effect was simulated in the FE model assuming a non-null thermal expansion coefficient for the rivet in its axial direction. A first load step, consisting on a temperature variation applied to the rivet, was applied prior to the tensile loading in order to result the clamping stresses. The exact value of the clamping stresses developing in rivets from riveted connections is an unknown. It must be emphasized that clamping stresses on rivets are not significant as in preloaded high strength bolts.

Figure 4 shows the evolution of the elastic stress concentration factor in the riveted joint with the clamping stress. Also, Fig. 4 shows the evolution of the elastic stress concentration factor for the bolted joint, with the clamping stress, allowing the comparison between solutions. The elastic stress concentration factor is defined as the ratio between the maximum stress at the central plate, in the surface of the hole of the connector, and the net stress evaluated as the average stress in the resisting section containing the rivet axis and is perpendicular to the load. The values of the elastic stress concentration factors, presented in Fig. 4 were computed for a friction coefficient μ=0.35, which is assumed as a very likely value for this type of joints. The difference between solutions for the riveted and bolted joints is essentially due to the clearance between the bolt and the hole. It is clear a significant effect of the clamping stress on the stress concentration factors.

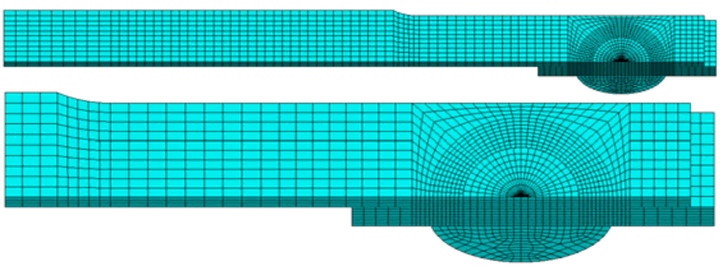

Figure 3. Finite element mesh of the riveted/bolted connection.

A through thickness constant depth crack was modeled at the central plate, starting at the stress concentration at the surface of the hole of the rivet and propagating perpendicularly to the loading (see Fig. 5). Symmetry in crack propagation was assumed, which allowed the use of the same finite element model of ¼ of the geometry. Figure 6 illustrates the stress distributions, along the loading direction, for the cracked and uncracked specimens. The stress intensity factor was evaluated for several crack lengths (a) using the VCCT. Although the VCCT allows the evaluation of the stress intensity across the thickness, average values were computed. Figures 7 shows the evolution of the stress intensity factor with the clamping stress for the riveted and bolted joints, for μ=0.35. The stress intensity factor was normalized using the average stress at the net section of the central plate. Higher clamping stresses were simulated for the bolted joints since they are more plausible in riveted joints. The clamping stress of 58 MPa applied to the bolt corresponds to the theoretical clamping stress applied to the bolt, in the experimental program. In general, the stress intensity factor reduces with the clamping stress. The stress intensity factor follows a sigmoidal type function.

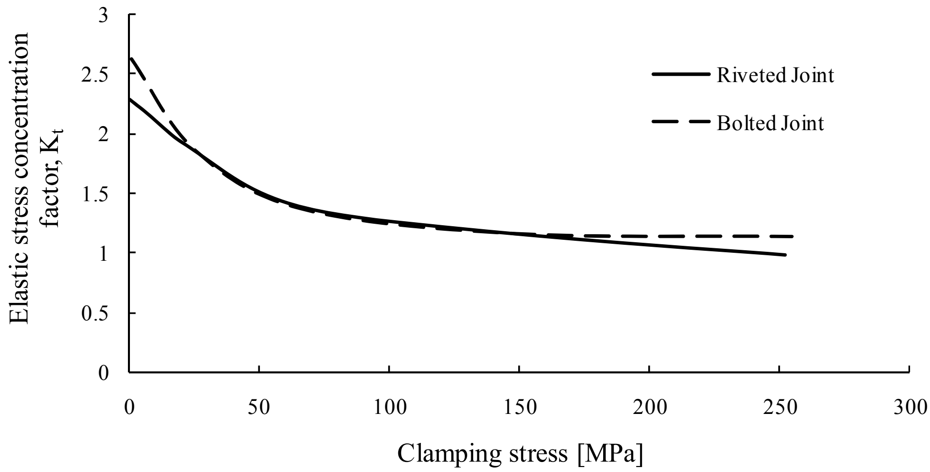

Figure 4. Evolution of the elastic stress concentration factor for riveted and bolted joints (μ=0.35).

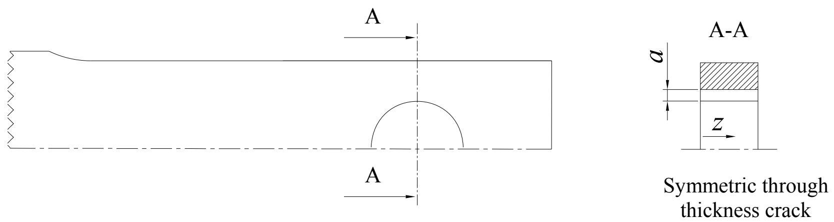

Figure 5. Through thickness constant depth crack configuration: crack depth a.

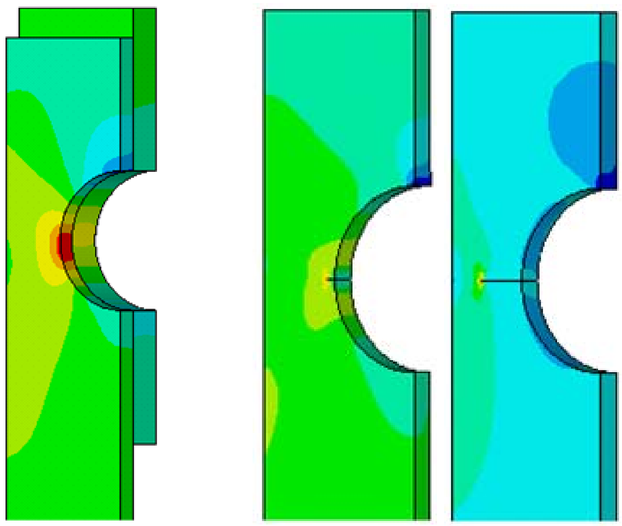

Figure 6. Illustration of the stress fields in the cracked and uncracked riveted joint (null clamping).

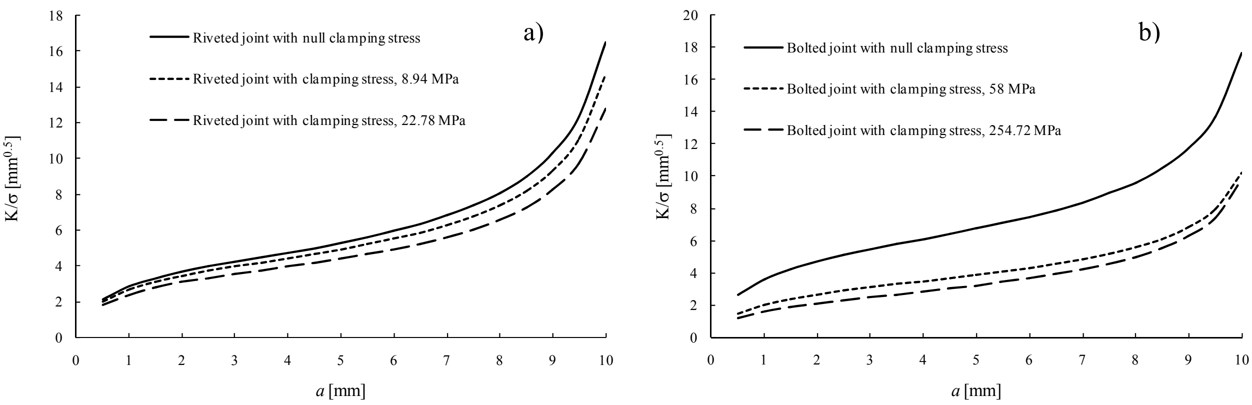

Figure 7. Stress intensity functions for constant depth cracks under distinct clamping stresses: a) riveted joint; b) bolted joint.

5. FATIGUE LIFE PREDICTION OF RIVETED AND BOLTED JOINTS

Using the proposed fatigue model, the experimental fatigue data of the base material and the results of the stress analysis carried out using the finite element model, S-N curves were modeled for the riveted and bolted joints. A criterion for crack initiation based on a crack of 0.5 mm was assumed. The proposed S-N curves were compared with the available experimental data. Since the clamping stresses introduced by rivets are unknown, three simulations were performed for distinct clamping stresses. In particular, clamping stresses equal to 0.63, 8.94 and 22.78 MPa were considered for the riveted joints. These clamping stresses correspond, respectively, to elastic stress concentration factors of 2.28, 2.14 and 1.89. Figure 8 exhibits the predicted S-N curves and compares them with the experimental data. Furthermore to the global S-N curves, crack initiation and crack propagation S-N curves were proposed. The analysis of the predicted S-N curves shows that the propagation S-N curve has a higher slope than average experimental S-N curve. The predicted S-N curve, taking into account the fatigue crack initiation phase, shows a slope a very close to the average mean S-N curve. The crack initiation period is always the governing damage process, assuming a significant influence for high-cycle fatigue. The predicted global S-N curve shifts upward with increasing clamping stress. Introducing a small clamping stress, which is plausible for riveted connections, the theoretical S-N curve becomes very close to the experimental mean S-N curve.

Figure 9 presents the results of the numerical simulations for the bolted joint, without preload on bolts. Again, the predicted global S-N curve is in very close agreement with the experimental data, namely with the average experimental S-N curve. Again, the crack initiation is the governing damage process.

Finally, Fig. 10 presents the results for the bolted joints with a preload applied on bolts. In this case, two clamping stresses were applied on bolts, namely 58 MPa which is the theoretical value applied in the test series, and a significant higher value of 254.72 MPa. In this case, the predictions based on the proposed model are not so accurate as resulted for the other test series. The predicted S-N curves show very distinct slopes of the experimental mean S-N curve, which makes the predictions based on theoretical clamping stresses very conservative. Very high stress levels were applied in the preloaded bolted joints in order to produce fatigue failures before the conventional run-out of 3 million cycles. This high stress levels produced initial plasticity which may contribute for a significant initial increase in the clamping stresses. Consequently, a higher clamping stress was modeled and the predicted S-N curve becomes more close to the experimental S-N data. Nevertheless, the slope of the resulting S-N curve still differs significantly from the experimental ones. It is worthwhile mention that there is a very limited number of experimental data points (6+1 run-out) covering a small variation of stress ranges, which reduces the confidence of the average experimental S-N curve.

6. CONCLUSIONS

A model for fatigue life assessment of riveted and bolted joints made of puddle iron from the Fão bridge was proposed and tested using available experimental data. In order to support the fatigue model, a finite element model of the riveted and bolted joints was proposed which was able to compute the elastic stress concentration and stress intensity factors for cracks as a function of friction and clamping stresses on rivets/bolts.

In general, the fatigue crack ignition was the governing process in the fatigue process prediction, for a crack initiation definition based on a crack of 0.5 mm depth. The prediction S-N curves for the riveted and non-preloaded bolted joints were very accurate, when compared with the experimental test data. A relative small clamping force was applied to the riveted joints, which is very consistent with the expected experimental values. The S-N curve predicted for the preloaded bolted joints exhibited significantly distinct slope of the mean experimental S-N curve. Nevertheless, there were a limited number of experimental data points which reduces the confidence in the slope of the experimental data.

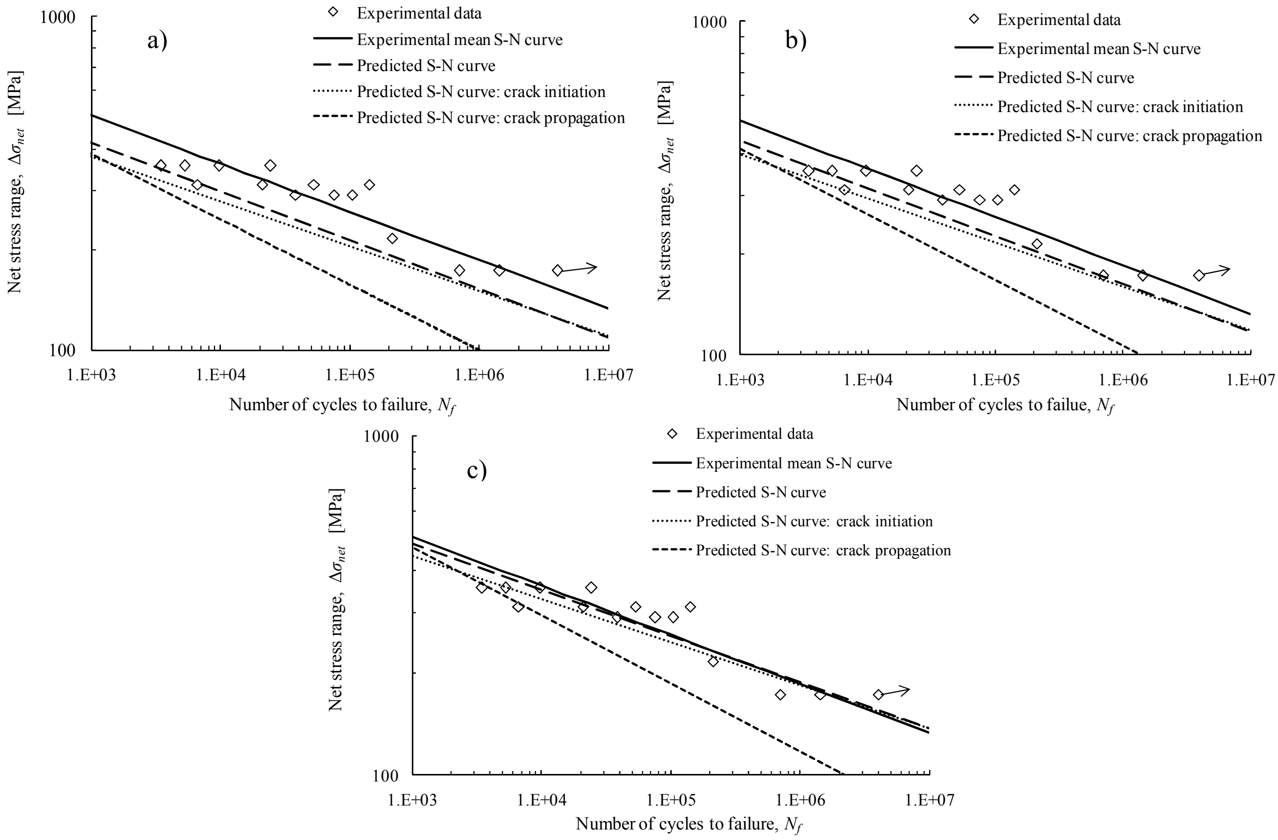

Figure 8. Fatigue life prediction for the riveted joint: a) clamping stress equal to 0.63 MPa; b) clamping stress equal to 8.94 MPa; c) clamping stress equal to 22.78 MPa.

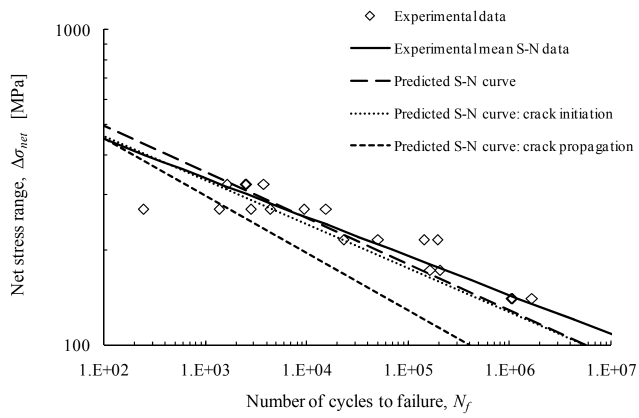

Figure 9. Fatigue life prediction for the bolted joint without preload on bolts.

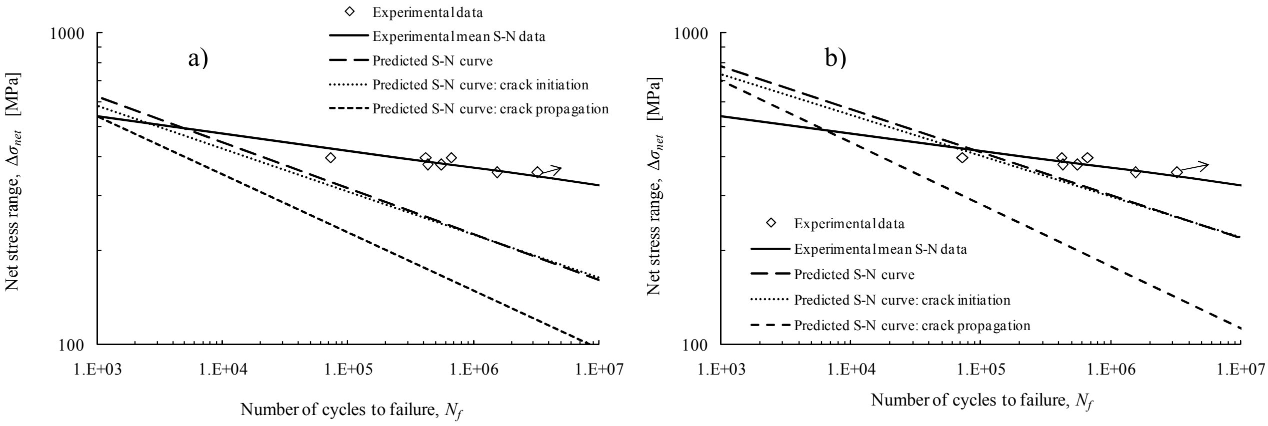

Figure 10. Fatigue life prediction for the bolted joint: a) clamping stress equal to 58 MPa; b) clamping stress equal to 254.72 MPa.

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