

Fatigue assessment of tubular structures

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General

An essential component of current rules for fatigue design of steel structures, e.g., [4, 6, 9], etc. is the linear damage summation, Palmgren-Miner, rule. It fits the widely used approaches, when the S-N criteria, or «Strain-Life» criteria for fatigue crack initiation are applied, and also when the residual fatigue life should be assessed in the crack propagation phase.

The damage assessment is recommended to carry out in the common form when the S-N criteria for fatigue are applied:

$$D = \sum_i n_i / N_i = C^{-1} \sum_i n_i \cdot (S_i)^m = (N^* / C) \sum_i p_i \cdot (S_i)^m \leq \eta, \quad (1)$$

where i is the number of equivalent¹ cyclic stress components in the stress block, n_i is the number of equivalent stress cycles in stress block components, N^* is the number of stress «cycles» which experiences the structure through the service life, $p_i = n_i / N^*$ is the fraction of the stress cycles n_i in the life-long loading history, N_i is the number of cycles to failure at constant stress range S_i , η is the «usage factor», total time of exposure to fluctuating loading related to prescribed service life, C and m are parameters of a fatigue failure criterion, e.i.:

$$N(S) = C / S^m. \quad (2)$$

Bolotin [1] considered characterization of the loading history by continuous function of distribution of probabilities and proposed transformation of the damage summation scheme (1) into the integral form by defining the number of stress cycles n_i in the step-wise block form as the following:

$$n_i = N^* \cdot p(S) dS, \quad (3)$$

where N^* is the total number of load (stress) fluctuations over the intended service life (or a specified period of time), $p(S)$ is the probability of the stress range to occur in a range of dS . Respectively, the sum (1) is expressed as

$$D = \sum_i n_i / N_i = N^* \int_{S_{\min}}^{S_{\max}} (p(S) / N(S)) dS. \quad (4)$$

This relationship can be readily used to estimate the total fatigue damage, which may be accumulated through service life of an industrial or marine structure or, alternatively, the number of stress excursions, N^* , over the intended service life, fatigue life, instead of the summation format (1).

By this reason development of a controversial procedure, i.e. transforming of the right-hand part of (4) into the common form (1) may seem needless. This may be true, when the S-N criteria are applied for fatigue analysis of structures and loading history is given by the stress probability distribution function.

However, the mentioned transforming may be helpful in assessment of residual fatigue life of redundant structures where cracks are allowed within the safe limits, especially when reduction of irregular loading history into equivalent cyclic loading should be done as recommended by present codes [4, 6, 7]. Also, it may be a necessary component of fatigue analysis based on «Strain-Life» approach

¹ The term «equivalent» is applied here since the irregular loading is substituted by cyclic loading successions

which includes an appropriate Strain-Life criterion and the Neuber's formula-based technique of evaluation the local strain range [17].

The procedure is exemplified in case of application of the «Strain-Life» approach in this paper. Therefore it may be reasonable first to display the technique of reducing the long-term stress distribution into a set of cyclic loading successions (histogram) equivalent by fatigue damaging to the random loading «history» through the service life.

Description of the procedure

Application of the damage summation procedure (1) presumes, as said in above, expressing the long-term stress range probability distribution function in the form of stress histogram, consisting of blocks of equivalent cyclic loading successions at stress ranges S_i with number of stress repetitions n_i . It is noted [4] that the number of equivalent stress successions, «steps», should be selected «large enough to ensure reasonable numerical accuracy, and should not be less than 20...». However, the explicit recommendations on evaluation of representative stress ranges S_i and respective number of stress cycles n_i in every of the block «steps» are missing.

To solve this problem, a procedure is proposed, as follows. In the context of recommended reducing the long-term distribution to the block-type composition of equivalent cyclic loading successions, first the whole range of service stress in the accepted stress distribution should be subdivided into several sub-ranges, steps, the number of which may be provisional, not necessarily equal 20. Then the partial damage corresponding to every of the i steps in the block-type damage summation (1) should be calculated using the general form (4):

$$d_i = N * \int_{S_{\min,i}}^{S_{\max,i}} (p(S)/N(S))dS. \quad (5)$$

in which $S_{\min,i}$, $S_{\max,i}$ are the minimum and maximum stress ranges of the « i » step of the block form (1).

Further, the number of equivalent stress cycles in every of the i steps is found as

$$n_i = N * \int_{S_{\min,i}}^{S_{\max,i}} p(S)dS. \quad (6)$$

Since the partial damage is defined in the linear summation procedure (1) as $d_i(S_i) = n_i(S_i)/N(S_i)$, in which S_i may be regarded an equivalent cyclic stress range of the « i » step, this stress range is obtained using the partial damage definition and fatigue criterion (2) as:

$$S_{i,eq} = (Cd_i / n_i)^{1/m}. \quad (7)$$

The loads, e.g., on bridge structures are typically classified into permanent loads due to self-weight of structural members, fixed equipment, variable loads caused by traffic, wind, diurnal and season changes of ambient temperature, and accidental loads caused by feasible vehicle impacts. Depending on the technique of recording and representation of service variable loads, the loading history may be given, alternatively, in the form of stress histogram or of stress continuous probability distribution function.

In marine applications, e.g., the wave loads and, respectively, local stress probability distributions are typically approximated in the form of the two-parameter Weibull «law» ([4, 17], etc.):

$$Q(S > S_1) = \exp(-(S_1/a_s)^k). \quad (8)$$

which is read as the probability to exceed an arbitrary stress range S_1 ; a_s, k are the scale and shape of the distribution parameters, respectively. The probability density function, $p(S)$, of the Weibull distribution (8) is given by:

$$p(S) = -\frac{dQ}{dS} = \frac{kS^{k-1}}{a_s^k} \exp(-(S/a_s)^k). \quad (9)$$

Substituting (9) into (6), one finds the number of equivalent stress cycles in every of the i steps:

$$n_i = \frac{kN^*}{a_s^k} \int_{S_{\min,i}}^{S_{\max,i}} S^{k-1} \exp(-(S/a_s)^k) dS. \quad (10)$$

And respective partial damage values, when the S-N criterion (2) is applied are defined as:

$$d_i = \frac{kCN^*}{a_s^k} \int_{S_{\min,i}}^{S_{\max,i}} S^{m+k-1} \exp(-(S/a_s)^k) dS. \quad (11)$$

Further, the equivalent stress ranges for every of the block scheme steps i in (1) should be found from equation (7). And then the total fatigue damage is calculated substituting (10) and (7) into the sum (1):

$$D = \sum_i n_i / N_i = \frac{1}{C} \sum_i n_i S_{i,eq}^m = \frac{N^* S_{0,eq}^m}{C} \sum_i p_i \bar{S}_{i,eq}^m, \quad (12)$$

where $\bar{S}_{i,eq}^m = S_{i,eq}^m / S_{0,eq}^m$, in which the reference stress range, $S_{0,eq}$, is selected from the set of obtained values (12), components of the block scheme.

In the following, the above described procedure is implemented in fatigue assessment of brace and chord welded joint detail of the tubular frame bridge structure when the «Strain-Life» approach is applied.

Application to fatigue assessment of a bridge tubular frame welded joint detail

The steel tubular girder structure of a composite bridge is shown in Fig.1. The previous experience of marine community indicated development of high stress concentration in welded joints of chords and braces, of stabilizing columns and braces, i.e., in welded joints of tubular components, menacing reliability of structures.



Figure 1. Composite bridge with tubular frame and integral abutments ([15])

This is due to the large difference of stiffness of the connected components: the chord is typically loaded in direction of minimal stiffness whereas the brace is loaded along its longitudinal axis, i.e., in direction of maximum stiffness. The procedure of fatigue assessment of marine structures, focused, in particular, on design of tubular components is given, e.g., in DNV² Rules [4], in [6], etc.

The procedure consists commonly of the two basic components, design S-N curves of typified welded joints («categories of fatigue strength») and linear damage summation rule.

Characterization of fatigue properties of welded joints in tubular structures by S-N classes may be regarded an approximation, a simplified approach, since local conditions (e.g., loading composition) for the damage in details attributed to the same S-N class (category) may substantially differ.

² DNV – Det Norske Veritas, Norwegian Classification Society

This difference would result in uncertainty of assessment of bridge structure fatigue life. The rules for fatigue analysis of marine structures [4] employ S-N curves for base and weld materials only (with differentiation by corrosion protection); the shape of details in connection, specifics of the stress flow transfer through the joint is considered by evaluation of the stress concentration factors for the expected critical locations. The approach seems offering reduced range of uncertainties pertaining to the stress analysis of structure, and respectively, a more convincing evaluation of fatigue life.

Meanwhile the S-N criteria-based procedures operate with implication of linear elastic material behavior when stresses at critical locations are evaluated, whether the hot-spot or local stress via the respective stress concentration factors are used [7, 9, 10]. By this the physical nature of fatigue damage process in polycrystalline material structure, which realizes through cyclic microplasticity, is underestimated. This may result in excessive conservatism in estimation of fatigue life, in particular, in the range of the most frequent (moderate) service load excursions.

One more issue should be considered when the S-N criteria are applied: this is uncertainty in definition of the scale of fatigue damage in structure related to exhaustion of fatigue life. The S-N criteria are related almost to separation in two pieces of test specimens; the testing machines are supplied with devices which terminate test when stiffness of specimen would substantially drop down. By this moment fatigue crack may grow over large part of the specimen cross-section (which is typically, according to the IIW³ recommendations, 100 by 22 mm). When the S-N criterion is applied to fatigue analysis of welded joints with specific geometry and stress field, the respective crack size occurs uncertain. The rules [2, 4, 9] assume (but did not prove it) that exhaustion of design fatigue life of tubular welded joints may be related to a through-thickness crack. Ever since, this subject was not commented in the design codes.

In the sense of mentioned problems, the «Strain-Life» criteria-based approach may be preferred. It allows considering material cyclic plasticity, the leading mechanism of fatigue damage (although with problems in high-cycle range), and to almost exclude ambiguity of the damage indication: fatigue crack corresponding to properties of criteria is about the stress concentration zone. In welded joints the depth of the zone is around the radius of the weld toe, i.e. about 1 mm.

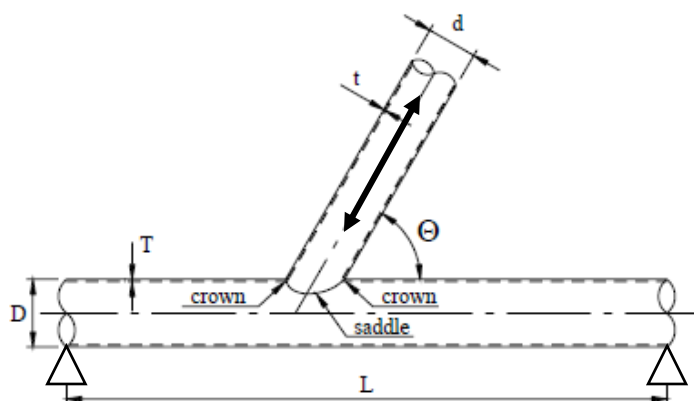


Figure 2. Scheme of the chord and brace welded joint

For illustration, an example of fatigue assessment of welded joint of tubular details, chord and brace, Fig. 2, is outlined. The scheme of loading is simplified: zero-to-tension random loading of the brace is assumed, with load (stress) range probability distribution function suggested as the Raleigh «law».

The number of load fluctuations through the service life is taken in accordance with recommendations of the code [6]: $N_d = 5 \cdot 10^6$.

Material of structure – higher strength steel of the 09G2 grade ($\sigma_y = 300 \text{ MPa}$, $\sigma_u = 450 \text{ MPa}$). Geometry of the detail characterize the following parameters: the chord diameter is $D = 330 \text{ mm}$; the wall thickness, $T = 18 \text{ mm}$; the brace diameter $d = 250 \text{ mm}$ and wall thickness $t = 18 \text{ mm}$. The brace angle is $\theta = 90^\circ$. It is assumed that the brace ending is adjusted to the chord shell shape and the weld is full penetration with weld throat 16 mm at the crown points and 8 mm at the saddle points depending on geometry of the joint circumference.

³ IIW = International Institute of Welding

The «Strain-Life» approach in its simplest form consists of the two obtained empirically principal components: the Strain-Life criterion for fatigue of material under the scope of analysis, and generalized cyclic diagram giving the generalized description of elastic-plastic cyclic properties of the same material. Several versions of the criterion are known [3, 5, 11]; in the present analysis a simplified modified form [17] is selected:

$$\Delta\varepsilon = \Delta\varepsilon_p + \Delta\varepsilon_e = CN^{-\alpha} + 2\beta\sigma_{-1}K_t/EK_f, \quad (13)$$

where for the steel of the 09G2 grade $C = 0.340$, $\alpha = 0.654$; $\sigma_{-1} \approx 90$, MPa, is the fatigue limit stress of the heat-affected zone, rolling skin removed [12, 17, 18]; $\beta = 0.55$, is the correction for the fatigue resistance decreasing under irregular loading [6]; K_t is the theoretical («elastic») stress concentration factor (SCF) and K_f is the fatigue notch factor.

The rules recommend formulae to calculate (SCF) addressed to the hot-spot stress approach, which is not applicable in the Strain-Life format. Respectively, the stress concentration factor values should be obtained for the critical locations in the joint («crown» and «saddle» points) by the finite element analysis (FEA).

Fatigue notch factor, K_f , may be approximated by Peterson's formula $K_f = 1 + (K_t - 1)/(1 + g/r)$ [16], where r is the notch root (weld toe) radius, assumed here as $r = 1$ mm, g is the «material structure parameter», a volume at the weld toe which depth approximately corresponds to fatigue crack transition from the shear into the normal opening mode [13]. For structural steels with the yield stress in the range 235 – 390 MPa this parameter may be defined by relationship: $g = 0.38(350/\sigma_u)^{1.16}$ [16, 17], where σ_u is the ultimate strength, MPa.

Modified criterion (13) provides somewhat conservative estimations of the damage in the «transition» number of load cycles, $N \approx 10^5$; however, to partly compensate it, the criterion is completed with corrections to the «elastic term», $\Delta\varepsilon_e$, to account for effects of material microplasticity at the stress concentration area, (K_t/K_f) , and irregular loading, β , on the damage accumulation.

The cyclic strains at the weld toe are estimated by applying the Neuber's formula-based technique [17]. Basically, Neuber's formula presents heuristic relationship between stress and strain concentration factors when elastic-plastic material behavior is assumed [14]:

$$K_t^2 = K_\sigma K_\varepsilon, \text{ or } \Delta\sigma \cdot \Delta\varepsilon = (K_t \cdot \Delta\sigma_n)^2 / E, \quad (14)$$

where $\Delta\sigma_n$ is the nominal stress range, $\Delta\sigma$ and $\Delta\varepsilon$ are the stress and strain ranges, respectively; E is the elasticity modulus.

Applicability of (14) for fatigue analyses was proved since 1960s for stress concentrations with rather unconstrained cyclic plasticity [5, 12, 17], etc. The usefulness of the (14) in engineering procedures is due to necessity of performing only once elastic analysis of a structure (for a given loading mode), in particular, by FEA. Current FEA packages allow cyclic elastic-plastic analysis; however, it has to be carried out many times to cover the whole range of service stresses, which turns a laborious issue.

The stress and strain ranges at a stress concentration in (14) are interrelated by the cyclic stress-strain diagram, which readily allows evaluation of the cyclic strain range at the notch, $\Delta\varepsilon$.

The technique of evaluation stress range $\Delta\varepsilon$ differing from typically recommended ([5, 8], etc.) is illustrated in Fig. 3.

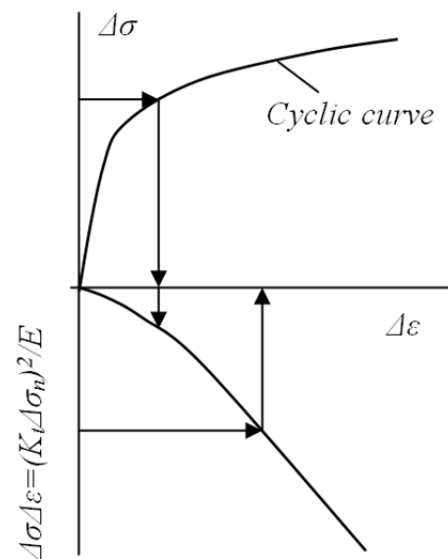


Figure 3. Evaluation of the local strain range by Neuber's formula and cyclic curve

Stress and corresponding strain ranges of the cyclic curve are multiplied and the products $\Delta\sigma\Delta\varepsilon$ are plotted with respect to the strain range $\Delta\varepsilon$ axis, as shown in Fig. 3.

As follows from (14), $\Delta\sigma\Delta\varepsilon = (K_t\Delta\sigma_n)^2 / E$; one needs to calculate nominal stress and stress concentration factor, and the respective strain range can be obtained from the graph.

If nominal stress can not be defined, e.g. when the beam theory is not applicable, the local stress at a critical location calculated on assumption of elastic behavior of material may be used instead of product $K_t\Delta\sigma_n$ in (14).

Stress analysis aimed at evaluation of the stress concentration factors at the mentioned critical locations was carried out using the ANSYS software. The finite-element (FE) model of the detail was designed so that the necessary stress resolution was attained at the weld toes⁴ of the joint circumference. The minimum finite element size at the assumed rounded weld toe was taken equal 0.2 of the weld toe radius, in coherence with past experience of numerical and experimental studies of stress concentrations [17].

Fig.4 shows fragment of the FE-model of the joint at the crown point (points, indicating the two critical locations, high stressed areas). The dark line along the weld toe at the chord indicates the narrow highly stressed area, whereas the weld toe at the chord is essentially less stressed. The FE mesh and the results of analysis at the saddle point is shown in Fig. 5.

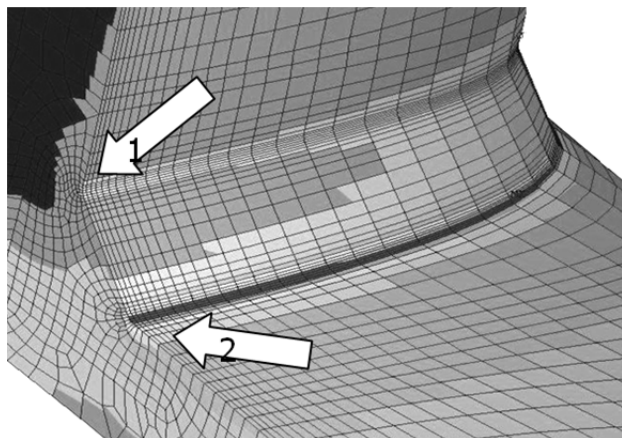


Figure 4. Crown point: (1) - weld toe at the brace shell, (2) – weld toe at the chord shell

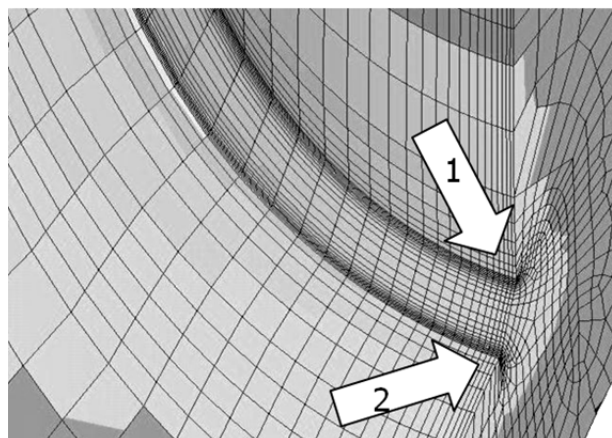


Figure 5. Saddle points of the joint: (1) – weld toe at the brace shell, (2) – weld toe at the chord shell

The values of stress concentration factor (SCF) are obtained by relating maximum equivalent (von-Mises) stresses at the mentioned critical locations found by FEA to the nominal stress in the brace. The results are presented in Table 1. For comparison, respective SCF values are calculated using approximate formulae [4] given in the below; however, the Rules define so-called «hot-spot» stress concentration factors attributed to the general geometry of a detail, and the weld shape is not considered. The local stress increase due to the weld shape may be included into the definition of the «hot-spot design S-N curve»; otherwise, the hot-spot SCFs may be corrected to account for effect of the weld shape multiplying by the «weld shape» stress concentration factor, recommended as $K_w = 1.6$ [4, 9].

The hot-spot SCF formulae [4] and calculated values are shown in Table 2, where $\alpha = 2L/D = 18$; $\beta = d/D = 0.75$; $\gamma = D/2T = 10$; $\eta = t/T = 1.0$.

Table 1. Stress concentration factors (SCF) at the critical locations

Location	SCF, FEA		Rules [3], Hot-spot SCF		HSS SCF, corrected	
	Crown	Saddle	Crown	Saddle	Crown	Saddle
Brace	7.0	12.9	3.433	6.351	5.49	10.16
Chord		22.24	5.404	9.513	8.64	15.22

⁴ Weld toes at the chord shell and at the brace shell

Table 2. Hot-spot SCF formulae [4]

Critical «hot-spot» location	Hot-spot SCF
Chord saddle	$K_{tcs} = \gamma\eta^{1.1}(1.11-3(\beta-0.52)^2) = 9.513$
Chord crown	$K_{tcc} = \gamma^{0.2}\eta(2.65+5(\beta-0.65)^2) + \beta\eta(0.25\alpha-3) = 5.404$
Brace saddle	$K_{tbs} = 1.3 + \gamma\eta^{0.52}\alpha^{0.1}(0.187 - 1.25\beta^{1.1}(\beta - 0.96)) = 6.351$
Brace crown	$K_{tbc} = 3 + \gamma^{1.2}(0.12 \exp(-4\beta) + 0.01\beta^2 - 0.045) + \beta\eta(0.1\alpha - 1.2) = 3.433$

As seen from Table 1, the maximum local stress occurs in the saddle point related to the chord shell, the respective weld toe, $K_t = 22.24$. Corrected HSS SCF at the same location is also very high, $K_{tcs} = 15.22$, although it is somewhat smaller than obtained by FEA.

It is assumed in the present study that alternating axial loading history of the brace is described by the Raleigh nominal stress range distribution with parameters in (8): $k = 2$ and scale parameter $a_s = 2\sqrt{2}\sigma_s$, where σ_s is the standard deviation of the stress amplitudes. The latter is taken rather provisionally, equal to $\sigma_s = 1.75$ MPa, although considering very high stress concentration at the chord saddle point. The once in the stress history maximum nominal stress range is $S_{max} = \sigma_{s,nom}\sqrt{-8\ln Q} \approx 19.44$ MPa provided that probability to exceed this value corresponds to the number of load fluctuations through the service life, i.e., $Q = 1/N_d = 2 \cdot 10^{-7}$.

According to the procedure the Raleigh distribution of the nominal stress should be transformed into the step-wise equivalent histogram. The whole range of the nominal stress is subdivided into 6 steps, sub-ranges: 0-3.24; 3.24 -6.48; 6.48-9.72; 9.72-12.96; 12.96-16.2 and 16.2-19.44 MPa. To estimate the equivalent number of cyclic loadings in every of the sub-ranges, the above parameters of the Raleigh distribution are substituted into (10):

$$n_i = \frac{kN}{a_s^k} \int_{S_{min,i}}^{S_{max,i}} S^{k-1} \exp(-(S/a_s)^k) dS = (N_d / 4\sigma_s^2) \int_{S_{min,i}}^{S_{max,i}} S \exp(-(S^2 / 8\sigma_s^2)) dS$$

The respective equivalent stress ranges for every of the sub-ranges are calculated using the appropriate design S-N curve of the F₃ class [4] (load-carrying welded joint) with parameters: $m = 3.0$, $C = 3.51 \cdot 10^{11}$: $S_i^{eq} = (d_i C / n_i)^{1/m}$, where partial damage values d_i are found by using (11). The equivalent number of cyclic loadings in every of the sub-ranges and corresponding equivalent nominal stress ranges are presented in Table 3.

Table 3. The equivalent stress histogram parameters, $\sigma_s = 1.75$ MPa (Raleigh distribution) S-N curve of the F₃ class with parameters: $m = 3.0$, $C = 3.51 \cdot 10^{11}$

S, sub-ranges	n _i , cycles	S _{eq} nominal, MPa
0 – 3.24	$1.742 \cdot 10^6$	2.313
3.24 – 6.48	$2.357 \cdot 10^6$	4.876
6.48 – 9.72	$7.951 \cdot 10^5$	7.764
9.72 – 12.96	$1.005 \cdot 10^5$	10.758
12.96 – 16.2	$5.156 \cdot 10^3$	13.818
16.2 – 19.44	110	16.942

To prove the procedure, S-N curve of the B₂ class (parent material, [4]) was used to obtain equivalent stress ranges for every of sub-ranges of the stress histogram. The results are presented in Table 4. It may be seen the difference between equivalent stress values practically is negligible. It means
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that evaluation of the equivalent stress ranges in composing the service stress histogram may be based on selection of any of the design S-N criteria; it should be noted the one-slope approximation is applied.

Table 4. The equivalent stress histogram parameters, $\sigma_s = 1.75 \text{ MPa}$ (Rayleigh distribution)

S-N curve of the B₂ class with parameters: $m = 4.0$, $C = 7.67 \cdot 10^{14}$.

S_i, sub-ranges	n_i, cycles	S_{eq} nominal, MPa
0 – 3.24	$1.742 \cdot 10^6$	2.395
3.24 – 6.48	$2.357 \cdot 10^6$	4.956
6.48 – 9.72	$7.951 \cdot 10^5$	7.813
9.72 – 12.96	$1.005 \cdot 10^5$	10.790
12.96 – 16.2	$5.156 \cdot 10^3$	13.838
16.2 – 19.44	110	16.947

Following the procedure, the equivalent nominal stress ranges are multiplied by the maximum stress concentration factor ($K_t = 22.24$, saddle point, weld toe at the chord) to find the product $\Delta\sigma \Delta\varepsilon$. According (14) $\Delta\sigma \cdot \Delta\varepsilon = S_{eq,i} \Delta\varepsilon_i = (K_t \cdot S_{n,eq})^2 / E$; strain ranges in every of the histogram sub-ranges, $\Delta\varepsilon_i$, are estimated from the lumped (generalized) cyclic curve for the steel [17], fragment of which is given in Table 5.

Table 5. Stabilized lumped cyclic curve of the 09G2 grade steel

$\Delta\sigma$, MPa	105.0	210.0	303.2	363.2	435.0	482.1
$\Delta\varepsilon$	0.0005	0.0010	0.0015	0.0020	0.0025	0.0030
$\Delta\varepsilon \Delta\sigma$	0.0525	0.2100	0.4548	0.7264	1.0875	1.4463

The strain range values $\Delta\varepsilon_i$ are applied to calculate the number of cycles prior to material failure at every of the stress histogram sub-ranges using the criterion (13) and respective values of partial damage. The results are given in Table 6.

Table 6. Histogram parameters, Neuber's strain ranges and partial damages

S_n, sub-ranges, MPa	S_{nom.eq}, MPa	$\Delta\varepsilon_i$	n_i, cycles	N_i, cycles	d_i
0 – 3.24	2.313	-	$4.617 \cdot 10^6$	-	-
3.24 – 6.48	4.876	-	$3.539 \cdot 10^5$	-	-
6.48 – 9.72	7.764	0.0007927	$2.713 \cdot 10^4$	$1.188 \cdot 10^5$	0.2283
9.72 – 12.96	10.758	0.001138	$2.079 \cdot 10^3$	$2.109 \cdot 10^4$	0.0986
12.96 – 16.2	13.818	0.001507	159	$9.166 \cdot 10^3$	0.0173
16.2 – 19.44	16.942	0.001931	12	$5.017 \cdot 10^3$	0.0024

It follows from the results of analysis that the total damage $D \approx 0.347$ which means that expected fatigue life of the detail may be almost 3 times longer compared to the designed, certainly, on condition the intensity of traffic loading would remain unchanged through the service life and the structure would be effectively protected from corrosion.

The nominal stress in the brace may seem too small; on condition the fatigue resistance would be expired by the end of designed life of the detail ($D = 1$) the maximum nominal stress may be increased approximately up to 30 MPa. Still, it remains small depending on the very high stress concentration at the joint. The local stress at the saddle point of the chord may be effectively lowered by increasing stiffness of the chord shell (thickness of the shell) in the joint area.

Conclusions

1. A procedure of the continuous long-term stress distribution explicit transformation to the step-wise block-type format is developed. It provides unambiguous technique of composing the histogram of service loading and allows reducing the number of equivalent cyclic stress fragments compared to those required by the rules for fatigue design scheme and provides accuracy of fatigue analysis of welded structures.

2. The procedure may seem unnecessary when the damage index is calculated by using the damage summation in the integral form (4). However, it is necessary when the damage should be analyzed by the means of the «Strain-Life» approach and application of the Neuber's formula based approach and may be helpful when the crack propagation has to be assessed.

3. Efficiency of the developed approach is illustrated in example of fatigue analysis of a detail of welded tubular structure. The analysis may be used to select sufficiently reliable versions of the detail.

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Fatigue assessment of tubular structures

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Key words

Fatigue damage accumulation; «Strain-Life» approach; S-N criteria; Fatigue of tubular structures; FEA modeling of tubular and pipeline structures

Abstract

Fatigue assessment of welded joints in tubular structures in industrial applications according to the current design codes is based on the block-type presentation of the loading histories. An approximate procedure of the long-term stress distribution transformation to the block-type format is developed. It allows reducing the number of equivalent cyclic stress fragments compared to those required by the rules for fatigue design and provides accuracy of fatigue analysis of tubular frame structures.

Efficiency of the developed approach is illustrated in example of fatigue assessment of the tubular frame bridge structure. Technique of analysis may be used to choose sufficiently reliable versions of the detail.

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