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STRAIN-BASED DESIGN PROCEDURES FOR SPIRAL-WELDED STEEL TUBES IN COMBINED WALLS

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Abstract: Spiral-welded steel tubes with diameter to wall thickness ratios between 60 and 140 are often employed in combined wall systems with local buckling as governing failure mode. The current design rules in Eurocode 3 (EN 1993-5 and EN 1993-1-6) are not capable to obtain a good estimate of the real strength and deformation capacity. In a European RFCS project called Combitube, the structural behaviour of spiral-welded steel tubes has been investigated. An analytical model has been developed that gives more economic designs with a better balanced safety level.

1. Introduction

Spiral-welded steel tubes are often employed in combined wall systems, e.g. for quay wall systems as indicated in Fig. 1. In these applications, the diameter to wall thickness ratio (D/t) ranges between 60 and 140. The main loading is bending in combination with normal force, earth loads and tensile or compressive loads from the infill sheeting. The governing failure mode is local buckling in the inelastic range of the steel.

The current design rules in Eurocode 3 (EN 1993-5 and EN 1993-1-6) follow a design approach based on stress resultants, rather than strains and deformations. It has been shown that these current design rules are not well suited for tubes in combined wall systems. They are not capable to obtain a good estimate of the real strength and deformation capacity and lead to uneconomic designs. Within the framework of a European RFCS project called Combitube, the structural behaviour of spiral-welded tubes for application in combined walls has been investigated. Full-scale four-point bending tests and extensive numerical parametric studies have been performed [1][7][8][9]. Based on the analytical formulation initially developed for buried pipelines [3][6] and the results of the Combitube research, an analytical model has been developed for tubes in combined walls including the effect of the following parameters:
• Diameter to wall thickness ratio (for tubes in CombiWalls usually between 60 and 140).
• Stress-strain properties of the steel, in particular the strain hardening properties.
• Presence of normal (axial) force and shear force in the tube.
• Transverse forces from infill sheeting.
• Cross-sectional ovalisation.
• Initial pipe wall short-wave wrinkling patterns in longitudinal direction.
• Other geometrical Imperfections: offset at girth welds (“high-low”), dimples.
• Residual stresses due to the manufacturing cold-bending process.

![Fig. 1: Schematic and photo of a combined wall of tubes and infill sheeting, also called CombiWall; the infill sheeting is connected to the tubes with welded slots](image)

2. Bending moment curvature diagram

First the bending moment curvature diagram is constructed in pure bending without the effect of other influences such as ovalisation, imperfections, residual stresses, etc. Thereafter the effect of other influences is considered. The equations are set up for thin walled tubes. This allows simplified equations for the cross-section quantities with sufficient accuracy for the present D/t range.

2.1 Moment curvature without other influences

A bilinear stress strain diagram is assumed (no strain hardening). For the elastic part it gives:

\[ M = \kappa \cdot EI \quad \text{with} \quad EI = E \pi r^3 t \quad \text{and} \quad \kappa = \frac{\varepsilon_{\text{max}}}{r} = \frac{\sigma_{\text{max}}}{Er} \]

\[ M_y = \sigma_y \cdot \pi r^2 t \quad \kappa_y = \frac{\varepsilon_y}{r} = \frac{\sigma_y}{Er} \quad \text{with} \quad r = (D-t)/2 \]

And for the elastic-plastic part [6]:

\[ M = M_y \cdot 0.5 \left( \frac{\theta}{\sin \theta} + \cos \theta \right) = 4r^2 \sigma_y \cdot 0.5 \left( \frac{\theta}{\sin \theta} + \cos \theta \right) \quad ; \quad \kappa = \frac{\varepsilon}{r} = \frac{\varepsilon_y}{r \cdot \sin \theta} \]

where:

\[ \theta = \arcsin \left( \frac{\varepsilon_y}{\varepsilon} \right) \quad \text{with} \quad \varepsilon \geq \varepsilon_y \]

Fig. 2 gives the stress distribution in the elastic-plastic part and the bending moment curvature diagram without other influences. The curvature is normalized by dividing it by the following quantity:

\[ \kappa_j = \frac{t}{D_m^2} = \frac{t}{(D-t)^2} \]
2.2 Ovalisation

2.2.1 Elastic behaviour

The ovalisation in the elastic part of the bending moment curvature diagram is [6]:

\[ a = \frac{r^5}{\rho^2 t^2} = \kappa^2 \frac{r^5}{t^2} \]  

This equation can be derived with the model in Fig. 3.

The uniform ovalisation load \( q \) and the ovalisation \( a \) follow from:

\[ q = \frac{df}{\rho d\theta ds} = \frac{rtE}{\rho^2} = \kappa^2 rtE; \quad a = \frac{qr^4}{12EI_{wall}} = \frac{qr^4}{12E \frac{1}{12}t^3} = \kappa^2 \frac{r^5}{t^2} \]  

Fig. 2: Stress distribution in bending and bending moment curvature diagram

Fig. 3: Ovalisation forces due to linear elastic bending - comparable to a uniform load \( q \) [6]

Fig. 4: Ovalisation forces give plate bending moments and plate normal forces
The ovalisation forces cause plate bending moments and plate normal forces in the tube wall (Fig. 4). The model for determining the ovalisation and the plate bending moments enables to take into account and combine these plate bending moments and normal forces with the effect of other loads perpendicular to the tube wall, e.g. soil loads, internal or external pressure and loads from infill sheeting.

2.2.2 Elastic-plastic behaviour

The calculation of the ovalisation in the elastic-plastic part of the bending moment curvature diagram is more complicated. For the full plastic cross section, the normality principle can be applied for the relation between curvature, ovalisation and plate bending moments [6].

In Fig. 5 the yield surface is given. The plastic moment capacity $M_{pr}$ decreases with increasing values of the plate bending moments. In the right figure, the full plastic cross section with plastic hinges loaded by ovalisation forces and soil loads as in buried pipelines [6] is given.

Because the slope of the yield surface is not a constant, a step wise procedure is followed.

In this equation, $\psi$ is the slope of the yield surface.

For practical applications an approximate equation is proposed. It is based on the “elastic” model for ovalisation as in Eq. 6. In the elastic part of the bending moment curvature diagram, the ovalisation forces increase due to increasing curvature and increasing axial bending stresses. In the elastic-plastic part, the increase of the ovalisation forces due to bending stresses in axial direction becomes smaller and therefore the ovalisation forces increase slower. For the elastic-plastic part the following equation can be applied for the ovalisation $a_p$:

$$a_p = \kappa^{1.5} \cdot \kappa^{0.5} \cdot \frac{r^5}{I^2} \quad \text{with} \quad \kappa = \frac{\varepsilon_p}{r}$$

The area of high plate moments at the sides of the cross section, mainly remains elastic. The plate bending stiffness at the top is governed by the normality principle.

2.3 The effect of ovalisation on the bending moment capacity

Ovalisation gives a reduction in the section modulus and it causes plate bending moments that reduce the tube bending moment capacity. The effect is expressed in next equations:

$$M_{\text{max,oval}} = g \cdot h \cdot M_p \quad \text{with} \quad h = 1 - \frac{2}{3} \frac{a}{r}$$

where $g$ expresses the effect of the plate moments and plate normal forces.
The stresses due to the plate bending moments and plate normal forces are given in Fig. 6. They are optimized to obtain the highest tube bending moment (optimal distribution of stresses according to the theory of plasticity). Plate bending moments in circumferential direction \( m_y \) give also plate bending moments in longitudinal direction called \( m_x \) (Poisson's ratio \( \nu \) is assumed as 0.3). Plate normal forces in circumferential direction are \( n_y \) and in longitudinal direction \( n_x \). The plate normal forces \( n_x \) give the tube bending moment \( M \).

\[ \sigma_{ym}, \sigma_{xn}, \sigma_{ym}, \sigma_{xm} \]

\[ n_y, m_y, n_x, m_x \]

\[ \text{Fig. 6: Plate bending moments and plate normal forces with optimal stress distribution in the tube wall} \]

Once the plate bending moments and plate normal forces in circumferential direction are known, the stresses \( \sigma_{yn} \) and plate normal force \( n_x \) can be calculated using the Von Mises yield criterion (Fig. 7). The factor \( g \) for tube bending moment capacity can be calculated with:

\[ g = \frac{c_1}{6} + \frac{c_2}{3} \] (10)

where

\[ c_1 = \sqrt{4 - 3 \left( \frac{n_y}{n_p} \right)^2 - 2\sqrt{3} \frac{m_y}{m_p}} \] \quad and \quad \[ c_2 = \sqrt{4 - 3 \left( \frac{n_y}{n_p} \right)^2} \] (11)

with

\[ n_y \leq n_p = t f_y \quad \text{and} \quad m_y \leq m_p = 0.25 t^2 f_y \] (12)

The equations for \( n_y \) and \( m_y \) depend on the type of loading and can be found in [6][3][7]. For pure bending, \( n_y \) and \( m_y \) are:

\[ n_y = 0.20 \frac{M_{\text{max}} \kappa}{r} \quad \text{and} \quad m_y = 0.071 \cdot M_{\text{max}} \cdot \kappa \] (13)

\[ \text{Fig. 7: Plate normal forces in axial direction that give the bending moment in the tube - in the figure also plate normal forces as a result of a normal force in the tube are given} \]

Safe estimates for \( n_y \) and \( m_y \) can be found by assuming \( M_{\text{max}} = M_p \) and \( \kappa = \frac{t}{D^2} \). The effect of \( n_y \) is very small for tubes without internal or external pressure. The effect of ovalisation (reduction of the section modulus) and plate bending moments on the bending moment curvature diagram is depicted in Fig. 8.
2.4 The effect of residual stresses on the bending moment curvature diagram

Depending on the forming process of the tubes, residual stresses will be present in the tube walls. These residual stresses result in yielding of the tube wall almost from the start of bending. From a bending moment of about $0.5M_e$ the effect can be clearly visible. The effect of residual stresses can be taken into account using a modified expression for the curvature.

$$\kappa_{res} = \alpha_{res} \frac{\varepsilon}{r} = \alpha_{res} \frac{\varepsilon_y}{r \cdot \sin \Theta}$$

with

$$\alpha_{res} = 1,0 \quad \text{for} \quad M \leq 0,5M_e$$

$$\alpha_{res} = 1,0 + \left( \frac{M}{M_p} - \frac{0,5M_e}{M_p} \right)^2 \quad \text{for} \quad M > 0,5M_e$$

The resulting bending moment curvature diagram is given in Fig. 9. It is noted that where the ovalisation is limited, e.g. near endplates, the bending moment capacity will be larger. However, this advantage is only relevant for (very) short tubes and not relevant for tubes in structural applications. Another issue to be taken into account near endplates and connections are the distortions due to welding and uneven introduction of stresses due to variations in stiffness of the supporting structure. In tests it often happens that local buckling occurs near endplates.
3. Critical curvature for local buckling

3.1 General equations

Starting from the critical strain according to [6][4][3][10], equations have been developed to take into account the various influences that have an effect on the strain at which local buckling occurs. The starting equations are:

\[
\varepsilon_{cr} = 0,25 \frac{t}{r_o} - 0,0025 \quad \text{for } \frac{r_o}{t} \leq 60
\]

\[
\varepsilon_{cr} = 0,10 \frac{t}{r_o} \quad \text{for } \frac{r_o}{t} > 60
\]

where \( \varepsilon_{cr} \) is the critical compressive strain and \( r_o \) is the local radius in the compressed part of the cross section as is indicated in Fig. 10.

\[
r_o = \frac{r}{1 - \frac{3a}{r}}
\]

\[
\kappa_{cr} = \frac{\varepsilon_{cr}}{r_o}
\]

For pure bending the effect of ovalisation can be neglected because the validation of these equations is done on pure bending tests where ovalisation due to pure bending was included. Therefore, in this case: \( r_o = r \). The next equations take into account the other influences.

\[
\varepsilon_{cr}^* = \varepsilon_{cr} \cdot \alpha_{geo} \cdot \alpha_{sh} \cdot \alpha_{sand} ; \quad \kappa_{cr}^* = \frac{\varepsilon_{cr}^*}{r_o}
\]

where

- \( \varepsilon_{cr} \) is the critical compressive strain in pure bending
- \( \alpha_{geo} \) is the smallest of the following geometrical imperfections effects:
  - \( \alpha_{un} \) is the effect of tube surface undulations
  - \( \alpha_{high-low} \) is the effect of misalignment (high-low) at welds
  - \( \alpha_{dimples} \) is the effect of dimples or dents
  - \( \alpha_{local-load} \) is the effect of local deformations due to local loads, e.g. waling beams
- \( \alpha_{sh} \) is the effect of strain hardening
- \( \alpha_{sand} \) is the effect of sand fill

Below a summary is given of the set of equations to determine these factors.

3.1.1 Surface undulations

For surface undulations the reduction factor for the critical curvature is:

\[
\alpha_{un} = a_{un19} \frac{D/t - 119}{52} (a_{un67} - a_{un19})
\]

with

\[
\alpha_{un67} = 1,37 - 1,2 \left( \frac{\delta_{un}}{2t} \right)^{0.3} \quad \text{and} \quad \alpha_{un19} = 1,15 - 0,5 \left( \frac{\delta_{un}}{2t} \right)^{0.3}
\]
In these equations is $\delta_{un}$ the depth of the undulation (difference between valley and adjacent tops). For undulations $\delta_{un}$ equal to 0.04$t$, the factor $\alpha_{un} = 1.0$. This is considered normal quality fabrication. For larger surface undulations, the critical curvature becomes smaller.

<table>
<thead>
<tr>
<th>$\delta_{un}$ / t</th>
<th>0.02</th>
<th>0.04</th>
<th>0.08</th>
<th>0.12</th>
<th>0.16</th>
<th>0.20</th>
<th>0.30</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\alpha_{un}$ (D/t = 67)</td>
<td>1.07</td>
<td>1.00</td>
<td>0.91</td>
<td>0.86</td>
<td>0.81</td>
<td>0.77</td>
<td>0.69</td>
</tr>
<tr>
<td>$\alpha_{un}$ (D/t = 93)</td>
<td>1.05</td>
<td>1.00</td>
<td>0.94</td>
<td>0.89</td>
<td>0.86</td>
<td>0.83</td>
<td>0.78</td>
</tr>
<tr>
<td>$\alpha_{un}$ (D/t = 119)</td>
<td>1.02</td>
<td>1.00</td>
<td>0.96</td>
<td>0.94</td>
<td>0.92</td>
<td>0.90</td>
<td>0.87</td>
</tr>
</tbody>
</table>

The above equations are based on parameter studies in the Combitube project. Canadian research reported by the Center for Reliable Energy Systems (CRES) [12] has resulted in:

$$\alpha_{un\text{-CRES}} = 1.84 - 1.6 \left( \frac{\delta_{un}}{t} \right)^{0.2}$$ (22)

This factor gives about the same reduction for D/t ratios till about 40. It does not depend on the D/t ratio. For high D/t ratios the CRES reduction factor is much lower than the Combitube result. The field of application in CRES is for D/t between 22 and 104.

### 3.1.2 Misalignment at girth welds (high-low)

For misalignment at girth welds the following reduction factor can be applied:

$$\alpha_{\text{high-low}} = 2.0 - 1.6 \left( \frac{\delta_{hl}}{3t} \right)^{0.2}$$ and $\alpha_{hi-lo} = 1.0$ (23)

Where $\delta_{hl}$ is the misalignment at the girth weld as indicated in next table.

<table>
<thead>
<tr>
<th>$\delta_{hl}$ / t</th>
<th>0.096</th>
<th>0.15</th>
<th>0.20</th>
<th>0.30</th>
<th>0.40</th>
<th>0.50</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\alpha_{\text{high-low}}$</td>
<td>1.0</td>
<td>0.905</td>
<td>0.840</td>
<td>0.742</td>
<td>0.668</td>
<td>0.607</td>
</tr>
</tbody>
</table>

The negative effect of misalignments till $\delta_{hl} = 0.096t$ is assumed to be compensated by the higher yield strength at the weld (overmatched weld metal) and the extra thickness of the tube wall at the weld (the weld cap). The proposed design rule is based on research in [11] and [12]. It was found that for weld high-low misalignments up to 0.50t, the effect of the misalignment on the compressive strain capacity is equivalent to a 0.15t geometry imperfection (surface undulation) in a plain pipe with zero pressure. Parameter studies in the Combitube project have shown that without the extra thickness of the weld cap and without the overmatched weld strength the effect of misalignments is much larger, as could be expected.

### 3.1.3 Dimples

For dimples the same reduction factor may be applied as for surface undulations.

$$\alpha_{dpl} = 1.84 - 1.6 \left( \frac{\delta_{dpl}}{t} \right)^{0.2}$$ (24)

The measurement length is according to EN1993-1-6:

$$l_{dpl} = 4\sqrt{rt}$$ (25)

### 3.1.4 Effect of strain hardening

Strain hardening has a positive influence on the local buckling behaviour. The $\sigma_y / \sigma_t$ ratio is a measure for the strain hardening.
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\[ \alpha_{sh} = 1,33 - \frac{0,04}{1 - \frac{\sigma_y}{\sigma_t}} \]  \hspace{1cm} (26)

The factor \( \alpha_{sh} \) is based on parameter studies in the Combitube project. The value of \( \alpha_{sh} \) is normalised to 1,00 for \( \sigma_y/\sigma_t = 0,85 \). In the DNV rules [13] the following equation is given:

\[ \alpha_{\text{strain-hard}} = \left( \frac{\sigma_y}{\sigma_u} \right)^{-1,5}, \text{ normalised to } 0,85 \text{ it gives } \alpha_{sh-DNV} = 0,825 \left( \frac{\sigma_y}{\sigma_u} \right)^{-1,5} \]  \hspace{1cm} (27)

Another formulation is on basis of the strain hardening modulus \( E_{sh} \). According to the parameter studies in the Combitube project:

\[ \alpha_{sh-combitube} = 1,8 \cdot h^{-0,14} \text{ with } h = E / E_{st} \]  \hspace{1cm} (28)

### 3.1.5 Effect of sand fill

Sand fill has a positive effect on the critical compressive strain, the bending moment capacity and the post buckling behaviour [14][15].

\[
a_{\text{sand}} = a_{\text{empty}} \frac{k_{\text{steel}}}{k_{\text{steel}} + k_{\text{sand}}} \]

\[ k_{\text{steel}} = \frac{12EI}{r^4} \left[ \frac{N}{m^3} \right] \text{ with } EI = \frac{1}{12} E_{\text{steel}} r^3 \quad k_{\text{sand}} = \frac{E_{\text{sand}}}{r} \left[ \frac{N}{m^3} \right] \]  \hspace{1cm} (29)

### 4. Comparison with test results

In Fig. 11 the calculated and measured bending moment curvature diagrams are compared for two of the 14 tested tubes. The column \( \kappa_{cr}/\kappa_I \text{ excl.} \) in the table on next page gives the critical curvature without the effect of surface undulations and the effect of strain hardening. The column with \( \kappa_{cr}/\kappa_I \text{ incl.} \) gives the critical curvature including these effects. The lines with K1, K2 and K3 give the measured curvatures along the test tubes [1]. Variations in the bending resistance along the tubes cause variations in the measured curvatures.

Local buckling occurs in the section with the largest curvature. The critical curvature also depends on the measuring length. Test T6D10 had a “coil-connection weld” where the plates from two coils were welded during the spiral-tube production. At that spot large undulations were measured, resulting in a low value of the calculated critical curvature [1] and [7].

### 5. Concluding remark

An analytical model is presented that is capable of taking into account many more parameters that have an influence on the bending moment capacity and the deformation capacity than the present equations in the European standards. Addressing these influences enables to much better predict the structural behaviour and lead to less scatter in statistical evaluations of test
data and so enhance economic and safe designs with better balanced safety level.

**Fig. 11:** Test T4D4 and test T6D10 compared with the calculated behaviour, showing good agreement between the tests and the calculations - also showing safe estimates for the local buckling curvature

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