Dynamic response of deep-water catenary risers made of functionally graded materials

J.C.R. Albino\textsuperscript{a}, C.A. Almeida\textsuperscript{b}, I.F.M. Menezes\textsuperscript{b}, G.H. Paulino\textsuperscript{c}

\textsuperscript{a}Department of Mechanical Engineering, SENAI CIMATEC, BA 41650-010, Brazil
\textsuperscript{b}Department of Mechanical Engineering, Pontifical Catholic University of Rio de Janeiro, RJ 22453-900, Brazil
\textsuperscript{c}School of Civil and Environmental Engineering, Georgia Institute of Technology, Atlanta, GA 30332, USA

\section*{A R T I C L E   I N F O}

Article history:
Received 6 October 2019
Revised 29 December 2020
Accepted 1 January 2021
Available online 7 January 2021

Keywords:
Functionally graded material
Steel catenary riser
Finite element
Nonlinear dynamic analysis

\section*{A B S T R A C T}

This paper examines the structural behavior of a steel catenary riser (SCR) with a segment composed of a functionally graded material (FGM) at the touchdown zone (TDZ). A co-rotational beam formulation is employed to evaluate the influence of material gradation on the static and dynamic nonlinear behavior of the riser. A finite element model is developed to predict the riser response considering environmental and operational conditions such as self-weight, buoyancy, current, soil contact, waves, buoys, and ship motion. Moreover, a comparison of SCR configurations is made between those constructed from FGM and those constructed from standard homogeneous materials. The study indicates that an SCR configuration with an incorporated FGM segment improves the curvatures near the TDZ, where designing to mitigate fatigue damage is critical.

© 2021 Elsevier Ltd. All rights reserved.

\section*{1. Introduction}

Riser technologies constantly face new challenges imposed by the recent deep- and ultra-deep-water oil discoveries and the associated harsh environmental conditions. Compared to other riser configurations, steel catenary risers (SCRs) are attractive owing to their simplicity, reliability, and cost effectiveness. Moreover, SCRs are resistant to high pressures and temperatures, and thus larger diameter models can be deployed more successfully when compared to flexible risers [1].

The dynamic representation of SCRs requires nonlinear numerical models owing primarily to the significant motion experienced at the hang-off point, fluid drag loadings, and riser-seabed interaction. To study the effect of these nonlinearities in the global dynamic response of the riser, finite element approaches have been extensively reported in the literature [2–9].

There are two main concerns in the design of catenary risers for applications in deep-water conditions: a) the fatigue damage assessment at the points of the riser that touch the seabed, known as the touchdown zone (TDZ) [10]. This problem is influenced by the cyclic riser-seabed interaction in the TDZ and is dependent on the seabed stiffness and damping characteristics [11–13]; and b) the risk of riser corrosion due to the presence of corrosive contaminants (CO\textsubscript{2} and H\textsubscript{2}S) in the fluid (oil) [14,15].

The use of composite materials in riser applications has increased recently due to the potential advantages that they offer over metallic risers, including their high stiffness-to-weight ratio, high tensile and fatigue strength, improved structural damping, low thermal conductivity, acceptable corrosion resistance, and low maintenance requirements [16,17]. However, delamination is the most common failure associated with composite risers, as has been reported in numerous research studies [18–20]. Unlike traditional composite materials, functionally graded materials (FGMs) present a smooth and gradual compositional variation with no sharp interfaces, thus eliminating the possibility of classical delamination. Typical FGMs, of interest in this study, are a mixture of ceramic and metal, aiming to combine the best properties of each phase, such as the hardness and corrosion resistance of the ceramic phase, and the toughness, machinability, and weldability of the metallic phase [21,22]. Owing to their unique properties, FGMs are promising materials for application in risers such as SCRs under deep-water conditions. In this context, and under the assumption that the FGM is available at the engineering scale of interest, then they could be applied in the TDZ to improve the SCR fatigue performance as a potential alternative solution to the concept of weight-distributed SCRs [23–26].

As a proof of concept, the use of an SCR with an FGM segment at the TDZ is proposed (as illustrated in Fig. 1). The static and dynamic nonlinear behavior of the riser is evaluated using a recently
proposed co-rotational formulation [9]. The results obtained herein demonstrate that this new FGM SCR riser approach leads to a significant reduction in the riser curvature at the TDZ. Hence, it could be applied to SCRs in harsh environmental conditions where, in addition to controlling the dynamic stresses, the use of FGMs could help prevent collateral problems such as corrosion and the formation of paraffin deposits.

2. Finite element formulation

In this section, a short review of the finite element formulation for nonlinear dynamic analysis of FGM risers, as proposed in [9], is presented. Using the principle of virtual work (PVW), the static equilibrium condition of the riser at time \( (t + \Delta t) \), for an updated Lagrangian formulation [27], is given by

\[
\int_V \left( t^{+\Delta t} S_{ij} \varepsilon_{ij} + t^{+\Delta t} \delta e_{ij} \right) dV = \int_S t^{+\Delta t} \varepsilon_{ij} \delta u_i dS + \int_V t^{+\Delta t} \delta e_{ij} \delta u_i dV
\]  

(1)

where \( t^{+\Delta t} S_{ij} \) is the second Piola-Kirchhoff stress tensor, \( t^{+\Delta t} \varepsilon_{ij} \) is the Green-Lagrange strain tensor, \( t^{+\Delta t} f_i^B \) is the surface force, \( t^{+\Delta t} \delta u_i \) is the body force, and \( \delta e_{ij} \) is the virtual displacement vector component.

The PVW can be extended to dynamic problems using the D’Alembert principle [28]. Therefore, by incorporating inertia and viscous damping into Eq. (1), the incremental equation of dynamic equilibrium is obtained as

\[
\int_V \left[ 0 \rho^{+\Delta t} \ddot{u}_i dV + \int_V k^{+\Delta t} \varepsilon_{ij} \dot{u}_i dV + \int_V c_{ijrs} \Delta e_{rs} \Delta e_{ij} dV \right] \nonumber \\
+ \int_V t^{+\Delta t} \delta e_{ij} \dot{u}_i dV = \int_S \frac{t^{+\Delta t}}{t^0} f_i^B \delta u_i dS + \int_V \frac{t^{+\Delta t}}{t^0} f_i^B \delta u_i dV
\]  

\[
- \int_V t^{+\Delta t} \delta e_{ij} \dot{u}_i dV
\]  

(2)

After the discretization of the riser using beam element kinematics, the global dynamic equilibrium equation can be written in the matrix form in terms of the incremental nodal displacements (\( \Delta \mathbf{U} \)), updated nodal accelerations (\( \mathbf{0} \)), and velocities (\( \mathbf{0} \)) as

\[
\mathbf{M}^{+\Delta t} \ddot{\mathbf{U}} + \mathbf{D}^{+\Delta t} \dot{\mathbf{U}} = \left( \mathbf{K}_L + \mathbf{K}_{NL} \right) \Delta \mathbf{U} = \mathbf{t}^{+\Delta t} \mathbf{R} - \mathbf{F}
\]  

(3)

where \( \mathbf{M} \), \( \mathbf{D} \), \( \mathbf{K}_L \), \( \mathbf{K}_{NL} \), \( \mathbf{t}^{+\Delta t} \mathbf{R} \) and \( \mathbf{F} \) represent the global mass matrix, global damping matrix, global linear incremental stiffness matrix, global nonlinear geometric stiffness matrix, updated global vector of the external nodal forces, and global vector of internal nodal forces, respectively, which can be expressed as

\[
\mathbf{M} = \sum_m \int_{V(m)} 0 \rho^{(m)} \mathbf{H}^{(m)\top} \mathbf{H}^{(m)} dV^{(m)},
\]

\[
\mathbf{D} = \sum_m \int_{V(m)} 0 k^{(m)} \mathbf{H}^{(m)\top} \mathbf{H}^{(m)} dV^{(m)},
\]

\[
\mathbf{K}_L = \sum_m \int_{V(m)} \left( \mathbf{B}_L^{(m)\top} \mathbf{C}^{(m)} \mathbf{B}_L^{(m)} \right) dV^{(m)},
\]

\[
\mathbf{K}_{NL} = \sum_m \int_{V(m)} \left( \mathbf{B}_{NL}^{(m)\top} \mathbf{t}^{(m)} \mathbf{B}_{NL}^{(m)} \right) dV^{(m)},
\]

\[
\mathbf{t}^{+\Delta t} \mathbf{R} = \sum_m \int_{V(m)} \mathbf{H}^{(m)\top} \mathbf{t}^{+\Delta t} \mathbf{f}_i^B dV^{(m)}
\]

\[
+ \sum_m \int_{V(m)} \mathbf{H}^{(m)\top} \mathbf{t}^{\Delta t} \mathbf{f}_i^B dV^{(m)},
\]

\[
\mathbf{F} = \sum_m \int_{V(m)} \left( \mathbf{B}_L^{(m)\top} \mathbf{t}^{(m)} \right) dV^{(m)}
\]

(4)

Here, \( \mathbf{H}^{(m)} \) is the m-element interpolation matrix (for further information on these matrices see [9]).

The material gradation throughout the riser thickness is included in the constitutive matrix \( \mathbf{C} \) and the mass matrix \( \mathbf{M} \). Thus, we use a nonhomogeneous material model where the elastic modulus \( \mathbf{E} \) and density \( \rho \) vary according to a power law along the radial coordinate through the riser thickness \( t \), as follows:

\[
E = E_0 \left( \frac{t}{t_0} \right)^a, \quad \rho = \rho_0 \left( \frac{t}{t_0} \right)^b, \quad t_i \leq t \leq t_o
\]

(5)

where \( a \) and \( b \) are the non-homogeneity parameters of the Young’s modulus and material density, respectively, \( t_i \) is the riser internal radius, and \( t_o \) is the riser outer radius.

In Eq. (3), a Newmark time integration scheme is used to express the displacements, velocities, and accelerations at time \( (t + \Delta t) \) in terms of the known variables at time \( t \). Thus,

\[
\mathbf{t}^{+\Delta t} \ddot{\mathbf{U}} = \frac{1}{\beta \Delta t^2} \Delta \mathbf{U} - \frac{1}{\beta \Delta t} \dot{\mathbf{U}} - \left( \frac{1}{2 \beta} - 1 \right) \ddot{\mathbf{U}}
\]

\[
\mathbf{t}^{+\Delta t} \dot{\mathbf{U}} = \left( \dot{\mathbf{U}} + \Delta t (1 - \gamma) \ddot{\mathbf{U}} + \gamma \Delta t \left( \mathbf{t}^{+\Delta t} \ddot{\mathbf{U}} \right) \right)
\]

\[
\mathbf{t}^{+\Delta t} \mathbf{U} = \left( \mathbf{U} + \Delta \mathbf{U} \right)
\]

(6)
where $\Delta t$ is the time step. In this work, the Hilber, Hughes and Taylor (HHT) algorithm [29] is used, where $\beta$ and $\gamma$ are constants defined as

$$\beta = \frac{1}{4} (1 - \alpha)^2 \quad \text{and} \quad \gamma = \frac{1}{2} (1 - \alpha), \quad -1/3 \leq \alpha \leq 0 \quad (7)$$

The external forces considered in this study are the effect of the self-weight, buoyancy, hydrodynamic loads (due to maritime waves, currents, and added mass inertia), prescribed displacements at the floating unit, and forces exerted by the floaters (for further details, see [7]).

The riser-soil interaction is modeled by associating bilinear springs to all the nodes in the structure. Whenever a node, in its deformed position, comes into contact with the soil, the soil stiffness $k_i$ is activated. The friction force due to the riser-soil contact is represented by an idealized elastoplastic formulation that allows the consideration of anisotropic friction, with the definition of different soil friction coefficients for the axial and lateral directions of the riser $\mu_x$ and $\mu_y$, respectively. The complete interaction (coupled model) between the friction forces in these directions follows the formulation presented in Reference [30]. The elastoplastic behavior of the friction force is illustrated in Fig. 2, where $F_i = \mu N$ is the friction force and $d$ are the limits of friction force and displacement that define the elastic behavior; $\mu$ is the friction coefficient; and $N$ is the normal contact force. For further details about the equations that govern the riser-soil interaction, we refer the reader to the work of Silva et al. [30].

3. Stress evaluation in functionally graded risers

This section presents the details of the stress analysis of the FGM risers used in the present study. The references of interest are [31–33].

3.1. Stresses due to internal and external pressures in thick-walled FGM cylinders

The exact solution for stress distribution in thick-walled FGM cylinders under internal pressure $p_i$ and external pressure $p_b$ is presented. This problem was originally solved by Tutuncu and Ozturk [31]; however, they only considered the effect of internal pressure. The FGM model is isotropic with a constant Poisson’s ratio ($\nu$), and the Young’s modulus ($E$) varies according to the power law presented in Eq. (5).

Considering a cylindrical coordinate system $(r, \theta, z)$, and asymmetry about the x-axis (along the axial direction), the differential equation of the stress equilibrium in the radial direction can be written as [32]

$$\frac{d\sigma_r}{dr} + \frac{\sigma_r - \sigma_\theta}{r} = 0 \quad (8)$$

where $\sigma_r$ and $\sigma_\theta$ are the radial and circumferential stresses of the cylinder, respectively. An illustration of the thick-walled FGM cylinder is presented in Fig. 3.
**Table 1**

Steel catenary data.

<table>
<thead>
<tr>
<th>Characteristic</th>
<th>Differential</th>
<th>Substituting by</th>
<th>Alized displacement</th>
</tr>
</thead>
<tbody>
<tr>
<td>External diameter, ( D_e )</td>
<td>0.219 m</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Internal diameter, ( D_i )</td>
<td>0.175 m</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Morison’s inertia coefficient, ( C_m )</td>
<td>2.0</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Morison’s drag coefficient, ( C_D )</td>
<td>1.0</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Floater’s weight</td>
<td>0.453 kN/m</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Floater’s buoyancy force</td>
<td>0.628 kN/m</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Specific weight of internal fluid</td>
<td>6 kN/m²</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Specific weight of sea water</td>
<td>10.055 kN/m³</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

**Homogeneous material (Steel):**

- Young’s modulus: \( E = 207 \text{ GPa} \)
- Specific weight: \( \gamma = 77 \text{ kN/m}^3 \)
- Poisson’s ratio: \( \nu = 0.3 \)

**Functionally graded material (Ni-Al-TiC):**

- Young’s modulus: \( E(t) = 420.7 \left( \frac{t}{t_0} \right)^{0.26} \text{ GPa} \)
- Specific weight: \( \gamma(t) = 49.2 \left( \frac{t}{t_0} \right)^{1.05} \text{ kN/m}^3 \)
- Poisson’s ratio: \( \nu = 0.3 \)

**Soil parameters:**

- Axial elastic deflection limit, \( d_a \): 0.030 m
- Lateral elastic deflection limit, \( d_l \): 0.219 m
- Axial soil resistance coefficient, \( \mu_a \): 1.54
- Lateral soil resistance coefficient, \( \mu_l \): 1.63
- Soil stiffness, \( k_s \): 1269 kN/m

**Table 2**

Static environmental loads.

<table>
<thead>
<tr>
<th>Current profile:</th>
<th>Speed (m/s)</th>
<th>( \theta_r )</th>
</tr>
</thead>
<tbody>
<tr>
<td>Depth (m)</td>
<td></td>
<td></td>
</tr>
<tr>
<td>0.01</td>
<td>1.57</td>
<td>180</td>
</tr>
<tr>
<td>1.01</td>
<td>1.57</td>
<td>180</td>
</tr>
<tr>
<td>2.01</td>
<td>1.53</td>
<td>180</td>
</tr>
<tr>
<td>4.01</td>
<td>1.39</td>
<td>180</td>
</tr>
<tr>
<td>4.00</td>
<td>0.41</td>
<td>180</td>
</tr>
<tr>
<td>7.00</td>
<td>0.29</td>
<td>270</td>
</tr>
<tr>
<td>11.40</td>
<td>0.29</td>
<td>225</td>
</tr>
<tr>
<td>15.40</td>
<td>0.29</td>
<td>225</td>
</tr>
<tr>
<td>19.40</td>
<td>0.38</td>
<td>225</td>
</tr>
<tr>
<td>21.40</td>
<td>0.00</td>
<td>225</td>
</tr>
</tbody>
</table>

**Offset vessel excursion (m):**

- Displacement in the x-direction, -138.4
- Displacement in the y-direction, 57.3

Assuming a state of plane strain \( (\varepsilon_2 = 0) \), and using the strain-displacement relations for small strains, together with the generalized Hooke’s law, the stress-displacement relationships are given by

\[
s_{ir} = \frac{E_0(t/r_0)^a}{(1 + v)(1 - 2v)} \left[ (1 - v) \frac{du_r}{dr} + \frac{v u_r}{r} \right]
\]

\[
s_{ir} = \frac{E_0(t/r_0)^a}{(1 + v)(1 - 2v)} \left[ v \frac{du_r}{dr} + (1 - v) \frac{u_r}{r} \right]
\]

Eqs. (9) and (10) into Eq. (8), we obtain the governing differential equation for the radial displacement \( u_r \), i.e.,

\[
r^2 u_{rr} + (a + 1) ru'_r + (\nu^2 a - 1) u_r = 0
\]

where \( v^2 = v/(1 - v) \).

A standard solution for Eq. (11) is \( u_r = r^m \), where \( m \) is a constant. Substituting this solution into Eq. (11) we obtain the characteristic equation \( m^2 + am + (\nu^2 a - 1) = 0 \), whose roots are

\[
m_1 = \frac{1}{2} \left( -a - \sqrt{a^2 - 4(\nu^2 a - 1)} \right)
\]

\[
m_2 = \frac{1}{2} \left( -a + \sqrt{a^2 - 4(\nu^2 a - 1)} \right)
\]

For typical numerical values of \( a \) and \( \nu \) (where \( a, \nu \in \mathbb{N} \)), distinct roots are considered. Therefore, a general solution for \( u_r \) is obtained as

\[
u_r = C_1 r^{m_1} + C_2 r^{m_2}
\]

Using the stress boundary conditions \( \sigma_r(t_0) = -p_i \) and \( \sigma_r(t_0) = -p_o \), constants \( C_1 \) and \( C_2 \) are obtained, and thus we obtain the re-
The sum of the stresses caused by the real axial force, \(F_r\), and the bending moments, \(M_y\) and \(M_z\), thus,

\[
\sigma_x = E(r) \left( \frac{F_r}{EA} - \frac{M_y}{EI} + \frac{M_z}{Et} \right) 
\]

(16)

where the expressions of the equivalent structural rigidities \(EA\) and \(Et\) are given in [9], and \(y\) and \(z\) are the local coordinates of a point defined within the riser thickness \(r_1 \leq \sqrt{y^2 + z^2} \leq r_o\), with \(r_1\) and \(r_o\) being the inner and outer radii, respectively.

Defining \(M_{th} = \sqrt{M_y^2 + M_z^2}\) as the resulting bending moment, the axial stress may assume the following two extreme values:

\[
\sigma_{s1} = E(r) \left( \frac{F_r}{EA} + \frac{M_{th}}{Et} \right) \quad \text{and} \quad \sigma_{s2} = E(r) \left( \frac{F_r}{EA} - \frac{M_{th}}{Et} \right) 
\]

(17)

Thus, the von Mises equivalent stress, \(\sigma_{vM}\), along the riser can be expressed as

\[
\sigma_{vM} = \max \left\{ \frac{1}{2} \left( (\sigma_r - \sigma_\theta)^2 + (\sigma_r - \sigma_{s1})^2 + (\sigma_{s1} - \sigma_\theta)^2 \right)^{1/2} \right\} 
\]

(18)

where the radial \(\sigma_r\) and circumferential \(\sigma_\theta\) stresses are defined by Eqs. (9) and (10), respectively.

In Eq. (18), the shear stresses are neglected according to the Euler-Bernoulli beam model adopted in this work. This approach is more appropriate to model slender structures such as risers [33].

4. Case study

This example presents a real case scenario of an SCR, 4053 m in length, submerged to a depth of 2140 m. The upper end of
the riser was connected to a floating production vessel 10 m below the still water level (SWL) with a hang-off angle of 10°; the other end was anchored onto the seabed, as indicated in Fig. 4. The catenary’s plane was located at a 20° angle with respect to the global XZ plane. The initial deformed configuration considered a simple catenary with stiffness along its axial direction and under self-weight and buoyancy forces. The solution was obtained by integrating the differential equation of equilibrium of an elastic cable. Using this initial configuration, as illustrated in Fig. 4, static and dynamic loads were then applied. The riser was modeled using 1351 equally long beam elements; the physical and geometric properties, and the characteristics of the riser-seabed interaction, are presented in Table 1.

Static and dynamic analyses were performed considering the following effects: current, self-weight, forces exerted by the floaters, static vessel offset, and prescribed displacements at the top of the connection due to wave action. In the first analysis, the riser was made entirely of steel with a specific weight 77 kN/m³ and a modulus of elasticity of 207 GPa. For the static analysis, the loadings were applied in 21 steps as illustrated in Fig. 5. In the first step, only the self-weight and floaters were considered; in the next 20 steps, a vessel offset of 150 m (corresponding to 7% of the water depth) and the current load were applied. The current profile data and vessel offset are given in Table 2. The current direction (θt) is measured counterclockwise from the positive x-axis. At every load step, a numerical tolerance of 10⁻³ for displacements, and 10⁻² for forces, were used as the convergence criteria for the Newton-Raphson method. Fig. 6 illustrates the initial and final deformed configurations of the riser during the static analysis.

The final static equilibrium configuration was set as the initial configuration for the dynamic simulations and all static loads were held constant. The prescribed harmonic motions, given in Table 3, were applied at the riser’s top connection. The amplitude of the prescribed motions was gradually increased according to a sinusoidal function, during the first two loading periods (i.e., 26,484 s), to minimize the transient axial vibrations. The total simulation time was set as 70 s and a time step of 0.01 s was used. The HHIT method [29] was employed to integrate the differential equations of motion with an alpha parameter of 0.1.

During the dynamic analysis, a periodic reduction in the radii of curvature was observed. Fig. 7(a), (b), and (c) illustrate the minimum values of the radius of curvature along the riser length for three critical time intervals: 30–37s, 43–50s, and 57–63s, respectively. The gray region in Fig. 7 corresponds to the critical section of the riser with the smallest radii of curvature and, therefore, the greatest values of the bending moments (in the FEM model, this section of the riser is 399 m long and is located between nodes 832 and 965). To address the effects of the small radius of curvature,
the critical section of the riser was replaced by a Ni$_3$Al-TiC (Nickel Aluminide/Titanium Carbide) FGM riser, with Ni$_3$Al and TiC phases in the internal and external radius, respectively. The power laws for the modulus of elasticity and specific weight of the FGM are given in Table 1. It is important to mention that the critical section of the riser, where the FGM was placed, encompassed the TDZ and, in addition to small radii of curvature, this region was subjected to the greatest amplitude variation of the axial forces (i.e., from tension to compression), as depicted in Fig. 8, for a typical node of each riser (homogeneous and FGM). Both static and dynamic analyses were performed considering the new FGM riser, following the same approach described previously for the homogeneous riser. The results obtained with the FGM riser indicate a significant increase in the radii of curvature (see blue curves in Fig. 7(a),
Table 4 presents the minimum values of the radius of curvature, for both risers, considering the same time intervals indicated in Fig. 7. Notice that the use of FGM increased the radii of curvature, in each time interval, by approximately 55.4%, 42.3%, and 72.1%, respectively. However, we acknowledge that this is a preliminary study and thus further investigation is needed.

Table 5 displays the nodes of each riser that experienced the minimum value of the radius of curvature and Fig. 9 illustrates the corresponding values of the resultant bending moments in each time interval. The results obtained herein demonstrate a significant reduction in the values of bending moments for the FGM riser compared to the homogeneous riser. Fig. 10 illustrates the deformed configurations of the risers corresponding to the critical time instants, i.e., the times when the bending moments achieved their maximum values (according to Fig. 9). It is important to mention that, for the FGM case, the deformation of the riser, specifically in the region near the TDZ, was limited to the catenary's plane, thereby reducing and redistributing the overall stresses in this region.

Figs. 11 and 12 display a comparison between the FGM and homogeneous risers with respect to the envelope of maximum von Mises stresses in the region near the TDZ. A significant reduction in the values of von Mises stresses is observed in the FGM riser. Considering that a conventional homogeneous riser is made of Carbon Steel API 5L X70 (whose yield strength is equal to 482.6 MPa), this material could fail. Conversely, the Ni$_3$Al-TiC FGM riser, although subjected to a high level of stresses in the external radius, can be designed with a higher flexural strength depending on the parameters for the melt infiltration process and volume fraction of Ni$_3$Al. Therefore, the flexural strength at 22°C for a Ni$_3$Al-TiC composite fabricated using a simple melt infiltration, prepared at 1300°C and with 20 vol%Ni$_3$Al, could achieve a level of 1080 MPa [34].

5. Concluding remarks

In this study, the use of FGMs at critical regions of an SCR, such as the TDZ, proved to be an effective procedure in reducing dynamic effects. This idea can also be applied to other riser configurations to improve their dynamic behavior. The results from this work confirm that FGM risers are an acceptable alternative for deep-water applications under extreme loading conditions.

It is also important to highlight that the use of FGM risers can address technological challenges beyond their dynamic behavior. As FGMs combine thermal insulation and corrosion-resistance characteristics of ceramics with the mechanical strength and hardness of metals, they are promising materials to be used in Brazil's deep-water pre-salt fields, where materials are required to withstand the high pressures and corrosion associated with the CO$_2$ and H$_2$S produced. Therefore, further studies are encouraged to verify the viability of FGM risers to address such problems.

Author Contributor

All authors have participated in (a) conception and design, or analysis and interpretation of the data; (b) drafting the article or revising it critically for important intellectual content; and (c) approval of the final version.
Declaration of Competing Interest

None.

Acknowledgments

JCRA is thankful for the financial support provided by CNPq (Conselho Nacional de Desenvolvimento Científico e Tecnológico - Brazil) and the Pontifical Catholic University of Rio de Janeiro. IFMM acknowledges the support provided by CNPq, Tecgraf/PUC-Rio (Institute of Technical-Scientific Software Development), and Coordenção de Aperfeiçoamento de Pessoal de Nível Superior - Brasil (CAPES) - Finance Code 001. GHP acknowledges support from the Raymond Allen Jones Chair at the Georgia Institute of Technology.

References