**Extreme response based reliability analysis of composite risers for applications in deepwater**

H.A.Ragheba, M. Goodridgea, D.C. Phamb and A.J. Sobeya,c

a Maritime Engineering Group, University of Southampton, Boldrewood Innovation Campus, Southampton, SO16 7QF

b Global Engineering and Materials Inc., 1 Airport Place, Princeton, NJ 08540

c Marine and Maritime Group, Data-centric Engineering, The Alan Turing Institute, The British Library, London, NW1 2DB

**ABSTRACT**

As current oil reserves start to deplete, companies are looking to exploit deeper deposits. At these greater depths composite risers, with their high strength-to-weight ratio, reduce the effective tensions and bending moments compared to steel risers. However, there is still limited research into their behaviour, with one key missing element being a comparison with traditional riser designs which accounts for variances in material properties and wave loads. This paper therefore conducts a strength-based reliability analysis of composite catenary risers operating between 1,500m and 4,000m. A static global catenary model is combined with Classical Laminate Theory to determine the extreme response and its performance is verified against FEA. This response is evaluated with the Tsai-Wu failure criterion to determine first-ply failure. The effect of laminate moisture absorption on the long-term reliability of submerged composite-based risers is also investigated as it can cause a significant reduction in the strength of composite risers. The reliability analysis is conducted using the Monte Carlo Method, revealing that the composite risers perform well at 4000m. The degradation in performance from moisture absorption becomes increasingly important at greater depths and needs further investigation for these applications.

Keywords: Monte Carlo Simulation, Composite Risers, Catenary, Durability, Hygrothermal Aging

# 1. Requirement for COMPOSITE RISERS

The rapid growth in the demand for energy has increased the desire to access new hydrocarbon reserves below the ocean floor. Approximately 12% of global conventional oil reserves lie between 1,000 to 4,000 meters under water**.** This equates to a total reserve of approximately 300 billion barrels of oil, which could produce 9 million barrels of oil per day by 2035.

Risers are an integral component of this offshore hydrocarbon extraction, allowing oil and gas to be transported from seabed wells to floating platforms for refinement and transfer to shore. Traditionally, these riser elements have been constructed from metal alloys, predominantly steel, which are susceptible to corrosion in the ocean environment, and expensive non-corrosive metal-alloys based on titanium, copper-nickel, duplex and super duplex stainless steel. These conventional materials have had great success in providing safe structures for depths up to 1500m however, Tarnopol’skii et al. (1999) and Ochoa (2006) highlight the potential advantages of composite-based risers at greater depths. Composite materials, especially carbon-epoxy, have potential benefits for offshore applications due to a unique combination of their high specific strength, thermal conductivity and low maintenance requirements. Due to these benefits there have been a growing number of investigations into the use of these materials but the lack of research literature and current applications has resulted in the use of large safety factors, such as in DNV-RP-F202 DNV (2009) which are between 15-50. These large safety factors demonstrate that these materials are not well understood for use in these applications, requiring further investigation. One important area highlighted by Pham et al. (2016), is the lack of available studies investigating the reliability of such systems. This is despite the fact that DNV (2010a) outlines a reliability-based design in their recommendations for composite risers which should meet the target safety levels, outlined in Table 1, which reflect the probability of structural failure due to normal variability in load and resistance. Further motivation comes from Skogdalen and Vinnem (2011 and 2012) who describe the risks that can occur in offshore scenarios such as riser breakage which can have major consequences and who advocate a proactive risk-based approach to safety, requiring a greater quantitative understanding of new technologies.

#### Table 1:target annual failure rate for composite risers as recommended by DNV (2010)

|  |  |
| --- | --- |
|  | **Failure Consequence** |
| **Failure Type** | ***Low Safety Class*** | ***Normal Safety Class*** | ***High Safety Class*** |
| **Ductile**  | *Pf* = 10-3 | *Pf*= 10-4 | *Pf*= 10-5 |
| **Brittle**  | *Pf*= 10-4 | *Pf* = 10-5 | *Pf* = 10-6 |

Whilst there is limited published evidence of stochastic analyses for composite risers, steel designs have been utilised for longer and methods are already available to investigate their reliability. Carrillo et al. (2011) presents a methodology to analyse the structural reliability of the ultimate limit strength of a steel catenary riser (SCR) under conditions present in the Gulf of Mexico. The results indicate that the lowest probability of failure occurs at the contact point and submarine connection, with annual failure probabilities of 2.958 x 10-5 and 7.318 x 10-5, with the elements connected with the TLP and Catenary Transition areas experiencing failure rates of 1.731 x 10-14 and 1.133 x 10-11 respectively. Li and Low (2012) performed a fatigue reliability analysis for steel risers utilising FORM on a response surface method generated from an Orcaflex model and outlines a number of other studies in this area demonstrating the importance of these studies.

While reliability studies have been conducted on steel catenary risers, application of these to a large-scale composite riser still remains an important challenge, Pham et al. (2016). To help address the gap in the literature this paper performs an analysis of composite risers to determine failure at maximum loads and compares the trends in behaviour to those of steel catenary risers. A static global catenary analysis is performed with the addition of Classical Laminate Theory which is shown to be accurate to FEA within 10% and its performance is empirically adjusted to further reduce this error. A strength-based assessment is selected to provide an initial understanding of risers at larger depths and it is also rare for fatigue failures to occur in composite materials where first-ply failure is a common mode of analysis. In addition, this analysis is extended to investigate the effect of moisture absorption on the composite properties between wet and dry conditions as all current riser analysis addresses only the intact condition. The paper then incorporates the effect of moisture absorption on the composite extreme failure based on the Tsai-Wu failure criterion.

# 2. MONTE CARLO SIMULATION OF a RISER

The analysis of riser systems has been conducted by various studies using a number of different techniques. DNV (2010) recommends a global-local procedure, where a global analysis is first conducted to extract effective tensions, bending moments, thermal loads and pressure loads, which act as boundary conditions for the subsequent local analysis. The local analysis then yields the local stresses and strains which are applied to a particular failure criteria to evaluate possible failure mechanisms of the riser elements. Tan et al. (2015), Kang et al (2015) and Zhan (2010) all conducted studies utilising static global catenary models to determine equilibrium positions, force extraction or the basis for a further dynamic study. Kang et al. (2015) noted that the bending moments generated were larger than those calculated via other methodologies along with some discrepancy in the static position of the riser as compared to other models. However, Bridge (2005) concludes that the use of the simple catenary equations is considered a good approximation. In a similar application Da Silva et al. (2013) utilised this type of model to optimize a composite laminate structure for a riser system. An analytical catenary solver was used for the static global model due to the faster run times compared to FEA while also providing representative results. The global analysis was used to extract the riser shape and effective axial tensions, based on the weight per unit length, top/departure angle and the operating depth of the riser system. Classical Laminate Theory (CLT) was then utilized to conduct a local analysis of critical sections of the riser and to extract the stresses and strains acting on a segment of the riser. The resulting stresses from the Classical Laminate Theory were then assessed by the Tsai-Wu criterion to determine possible failure. The local model is utilised as the basis for the reliability analysis with the addition of the von Mises failure criterion when metallic risers are considered.

To perform the reliability analysis a Monte Carlo simulation was employed, shown in figure 1, to analyse variations in mechanical and manufacturing properties of laminate materials, as well as environmental loads, following a similar procedure to Sobey et al. (2013). The Monte Carlo simulation generates a large number of random values for the stochastic variables (*Xi),* which are then inserted into the limit state function *G(X)* to determine if the structure will fail. The method allows for the calculation of the probability of failure over the entire domain of load inputs. In addition to its simplicity and accuracy, the Monte Carlo simulation is also robust in its applicability to various situations. However, in cases of small probabilities of failure, the required number of simulations increases significantly, resulting in additional computational time.



Figure 1: Monte Carlo Simulation Methodology

The material properties for the riser are based on Carbon/Epoxy T700/X4201 manufactured by Torayca in Japan with statistical distributions taken from Philippidis et al. (1999). Properties for steel are taken from Xia et al. (2008), which alongside the mechanical properties, are given in Table 2, where Xt and Xc are the carbon fibre tensile strength in tension and compression in the fibre direction; similarly Yt and Yc are the strengths in the transverse direction; S is the shear strength; Ex and Ey are the elasticity modulus in the fibre and transverse directions; G is the shear modulus and ν is the Poisson’s ratio. It is expected that composite material parameters exhibit co-variation between these properties, however, the available in the literature doesn’t account for this co-variation, therefore, and therefore, it is assumed that the material properties are statistically independent.

Two different types of risers, from Tan et al. (2015) and from Wei (2015), are assessed with the topology and layup for each riser given in Table 3 and illustrated in figure 2.The steel riser was developed to have the same thickness as the larger composite riser found in Wei (2015) with a corresponding unit mass of 115 kg/m. The risers were subjected to a horizontal tension of 1.3×105 N and were assumed to have an internal fluid density of 700kg/m3. The riser designs are selected to determine trends in behaviour, not as direct comparisons between each other.

#### Table 2: Statistical variations and material properties for carbon/epoxy and steel

|  |  |  |  |
| --- | --- | --- | --- |
|  | Composite | Steel | Distribution |
| Mean Wei (2015)  | CoVPhilippidis et al. (1999)  | Mean Xia (2008)  | CoV Xia (2008) |
| Xt (MPa) | 2450 | 5% | N/A | N/A | Normal |
| Xc (MPa) | 1570 | 16% | N/A | N/A | Normal |
| Yt (MPa) | 70 | 18% | N/A | N/A | Normal |
| Yc (MPa) | 133 | 16% | N/A | N/A | Normal |
| S (MPa) | 98 | 11% | N/A | N/A | Normal |
| Yield Strength (MPa) | N/A | N/A | 550 | 6% | Normal |
| Ex (GPa) | 125 | 10% | 197.5 | 2% | Normal |
| Ey (GPa) | 9.588 | 16% | 197.5 | 2% | Normal |
| G (GPa) | 5.4 | 20% | 78 | 3% | Normal |
| ν | 0.35 | 11% | 0.3 | 1% | Normal |

Table 3: Layup structure & Global properties of each composite pipe under study

|  |  |  |  |
| --- | --- | --- | --- |
| **Pipe structure** | **Steel** | **Tan et al. (2015)** | **Wei** **(2015)** |
| **Number of Laminae** | N/A | 20-ply | 56-ply |
| **Layup** | N/A | [45/-45/15/80]5 | [90/15/-15/90/45/-45/45/-45/45/-45]5 +[45/-45]3 |
| **Thickness of Steel liner (mm)** | 18.3 | 5 | 7 |
| **ID (m)** | 0.1834 | 0.2796 | 0.1836 |
| **OD (m)** | 0.22 | 0.31667 | 0.22 |
| **Ply Thickness (mm)** | N/A | 0.675 | 0.075 |
| **Laminate Wall thickness (mm)** | N/A | 13.5 | 11.2 |
| **Mass in air (kg/m)** | 91 | 57 | 46 |
| **Bending Stiffness, EI (MN.m2)** | 11.9 | 27.5 | 8.83 |
| **Axial Stiffness, EA (MN)** | 2319.19 | 2500.96 | 1756.65 |

#

OD

ID

Composite Laminate

Steel Liner

Figure 2: Composite Riser Cross-section Design

# 3 GLOBAL Response Model

The static catenary model is modelled using an analytical approach based on the work of Faltinsen (1990), describing the behaviour of catenary mooring lines and is chosen for computational efficiency.

## **3.1 Catenary Model**

The global model used to determine the static equilibrium position of the riser was derived from the steel catenary riser models employed in a number of other analyses including Duan et al. (2011), Bridge (2005) and Kang et al. (2015). The catenary model allows a simple representation of the risers including the specification of the geometry of the system as well as the extraction of axial tensions. While bending stiffness is inherently neglected in this approach, bending moments can be extracted by including material properties and curvature at each point.

The governing mathematical equations for the catenary shape assume neglected bending stiffness, in-extensible cable, infinite axial stiffness, and no hydrodynamic forces. The equation requires the following inputs: *w*, submergedweight per unit length of the cable; *TH,* horizontal tension; *ρl*, density of constituent laminate structure; *ρi*, density of internal fluids; *ρf*, density of external medium; *h,* operational depth and the geometry of the cylindrical pipe to evaluate the second order differential equation, eq. 1,

 (1)

where α is the catenary parameter,The boundary conditions are imposed such that eqs. 2-4 are valid:

 (2)

 (3)

 (4)

where is the angle of the seabed at the touchdown point. Assuming the angle of the seabed is 0 leads to eq. 5,

 (5)

which is a catenary curve with a departure point at *y =0*, sea level and a touchdown point at a depth of *h* metres below the sea surface.

The length of the catenary section, *s,* based on the horizontal, *x*, distance from touch-down point can be determined using eq. 6,

. (6)

Forces acting on the riser include tension, both vertical and horizontal, as well as the bending moments, which were estimated using eq. 7 and 8,

 (7)

, (8)

where *Tv (x)* is the distribution of the vertical component of tension along the length of the riser. In addition, the bending moments acting on a segment of the riser can be estimated by multiplying the curvature by the bending stiffness inherent to the material and the geometry, which is shown in eq. 9,

. (9)

This model does not consider any environmental factors, and represents the static position as a free hanging chain which excludes sea current and wave effects. These factors were then included via statistical distributions based on the findings of DNV(2010b), Chu (2008) and Zhan (2010) where the forces related to undersea currents are modelled as additions to the constant *TH*, while wave and current variability is modelled as additions to the sea depth usingthe Weibull probability density function shown in equation 10, where the parameters αH  and β are the scale and shape parameters of the Weibull distribution as shown in table 4.The forces related to current flow were applied using the following simplified assumptions:

1. Tangential current velocity is neglected; this is recommended by DNV (2010b), as it is an insignificant consideration for riser applications.
2. Normal current velocity is constant in the Y, vertical direction or depth. This simplifies the application of an ocean current, without losing generality.
3. Acceleration of ocean currents is excluded. It is assumed that the normal velocity of the ocean currents remain constant which eliminates inertial loading effects as predicted by Morison’s Formula.

 (10)

The Weibull shape parameter β is equal to the slope of the probability plot where β>1 indicates that the value is close to the mean wave height and slightly positively skewed with a tail to right of the distribution curve, while the scale parameter αH  shrink or squeeze the density function and affect if the function is narrow or wide banded.

#### Table 4: characteristics of ocean current velocity and wave height, DNV (2010b) and Chu (2008)

|  |  |  |
| --- | --- | --- |
|  | **Distribution** | **Suggested Parameters** |
| **Wave Height**  | Weibull | αH = 0.681, β = 2.126 |
| **Current Velocity** | Weibull | αH=0.3, β = 2 |

Therefore, the force acting on the riser per unit length for constant current is equivalent to the drag term of the Morison formula in eq. 12. Where is the riser velocity relative to the water particle velocity, accounting for wave, , and current velocities, as given by eq. 11,

 (11)

As this study is based on the static catenary model, the riser velocity , is equal to zero in equation 11. The dynamic effect of the wave and the hang-off excitation is accounted for in the Dynamic Amplification Factor, which is equal to unity for wave conditions that are described in section 3.2 and are the basis of this study.

, (12)

where *Fn* is the force per unit length, is the density of surrounding fluids, *CD* is the normal drag coefficient, is the diameter and is the current velocity. The *CD* for the purpose of this analysis a value of 1.25 was chosen.

## **3.2 Verification of Riser Model**

The outputs of the developed model using the properties listed in Table 5 are shown in figures 4 and 5 in comparison to the FEA model utilised in Zhan (2010). Both models reflect similar results for the static geometric profile and characteristic forces acting on the riser. Maximum tensions and bending moments as well as their distribution along the length of the riser are similar and almost identical between the models. A sensitivity study is carried out using FLEXCOM software to quantify the maximum dynamic amplification factor due to waves and the FPSO motion**.** A 270m FPSO is chosen and exposed to a range of sea states with wave periods close to its natural frequency **for heave motion**, of 15.7 secs, and water depths, of 1500, 2000, 3000 and 4000m, to replicate the worst-case scenario. This is performed using the thicker, 56-ply riser configuration proposed for the later studies, documented in figure 6, and top-tensions in table 6.The sensitivity results in figure 3 show that the dynamic model using regular waves can be approximated by the simple static approach for regions which are dominant by waves of up to 4m wave height, at which the dynamic amplification factor is close to unity. This approximation is acceptable for regions, such as Gulf of Guinea, where the short-term environmental conditions matches the criteria of the 4m maximum wave height.In such cases thevariations between the simple static analytical approach and the FEA approach are less than 1%. The effects of wave height are not included in the final model as they are found to increase the computational expense but with a limited increase in accuracy, while current effects are still included.

#### Table 5: SCR Verification Study Properties

|  |  |
| --- | --- |
| **Parameter** | **Value** |
| **Outer diameter (m)** | 0.273 |
| **Wall thickness (m)** | 0.0127 |
| **Weight in air (kg/m)** | 125 |
| **Internal fluid density ()** | 700 |
| **Length (m)** | 2240 |
| **Water depth (m)** | 1000 |
| **Hang-off to point of no motion****(Horizontal distance)(m)** | 1500 |
| **Modulus of Elasticity (GPa)** | 207 |
| **Boundary condition**  | Pinned-Pinned |
| **Analysis type** | Static |

Figure 3: Amplification factor sensitivity between the static analysis and a dynamic case

Figure 4: tension forces from static global model in comparison to Zhan (2010)

Wellhead

Touchdown Point

Departure Point

## **Investigation into riser response**

A simple comparison between the steel and composite risers, shown in Table 6, gives an indication for the differences between the maximum bending moments that are observed at the touch-down zone and maximum tensions at the hang-off point. Water depths of 1500m are chosen to represent current deep-water applications with incremental increases to 4000m, indicating the maximum depth for which future riser applications are likely to be aiming.

Figure 5:bending moments from static global model in comparison to Zhan (2010)

Wellhead

Touchdown Point

Departure Point

Composite catenary risers exhibit greater horizontal displacements than the traditional steel catenary risers with similar operating depths as a result of the lower density and transverse stiffness of composite systems as shown in figure 6. This also has the consequence of increasing the necessary length of composite systems compared to steel catenary risersto reach the surface elevation starting from the same touch-down point, due to lighter apparent weight of composites. This is most evident for the 20-ply composite system which has the lowest density, and requires a total length of 2079m to reach its operating depth of 1500m. It is observed that the maximum bending moments remain constant for those models with the same materials, and is thus independent of operating depth, as opposed to tensions which are weight, and hence depth, dependant. As a result, steel catenary risers incur significantly larger bending moments, which peak at the touchdown point.



Figure 6:Comparison of configurations for risers manufactured using different material properties

#### **Table 6**: maximum bending moments and tensions related to global models

|  |  |  |  |
| --- | --- | --- | --- |
| **Water Depth (m)** | **Arc-Length (m)** | **Max. Tension (N)** | **Max. Bending Moment (Nm)** |
| **56-ply composite Wei (2015)** |
| **1500** | 1957 | 485914 | 28049 |
| **2000** | 2471 | 613628 | 27526 |
| **3000** | 3490 | 866054 | 28019 |
| **4000** | 4498 | 1116779 | 28049 |
| **20-ply composite Tan et al. (2015)** |
| **1500** | 2079 | 392603.7 | 66068 |
| **2000** | 2601 | 491228.6 | 66893 |
| **3000** | 3625 | 684859 | 66922 |
| **4000** | 4642 | 876417 | 66068 |
| **Steel** |
| **1500** | 1659 | 1323057 | 79672 |
| **2000** | 2160 | 1723999 | 80664 |
| **3000** | 3176 | 2522527 | 79863 |
| **4000** | 4181 | 3323526 | 79672 |

# 4 Pipe model

Global loads are obtained from the catenary equation but because the method is based on a one-dimensional beam formulation, that only represents the centre line of the riser, there is missing information for the remaining two spatial dimensions of the pipe cross-section. Two interface equations are needed to recover this information for the cross-sectional local analysis and to calculate the equivalent in-plane force, N, and the equivalent induced moment, M,. If an infinitesimal section of the composite pipe wall is considered at the maximum curvature location, then the in-plane forces at this location is due to the tension in the beam and the tension induced by the curvature at the location of the outer ply.



Figure 7:schematic of theglobal loads applied to the local cross-section

The tension, T, and the bending moment, are calculated from the catenary equation. The equivalent in-plane force, N, is the equivalent force that is applied to the laminate x direction and causes the same stress at the outer ply that results from the tension force and bending moment. The calculations are based on equation 13,

 (13)

which is dependant on , the equivalent force at the outer ply, defined in equation 14,

 (14)

where r is distance from the pipe neutral axis to the outer ply centre, Ac is the cross-sectional area and I is the second moment of area of the pipe.

The difference between the stress at the outer ply and the mid-plane of the composite laminate generates a rectifying moment about the y axis of the laminate that is located at the neutral axis of the laminate section as shown in figure 7. Therefore, the equivalent moment is calculated using equation 15,

 (15)

where t is the thickness of the riser. The equivalent force at the mid-plane, can be found by replacing with in equation 14. The principal stresses obtained by this analytical method are verified against a finite element model constructed using continuum shell elements, where the load from the static model is applied and the end of the pipe are constrained using a multi-point constraint which are tied to a point on the neutral axis. The highest principal component stress at the outer ply is found to be 10% higher than the analytical values, and therefore a multiplier of 1.1 is applied to the analytical model to adjust for this difference.

The pipe analysis is conducted to determine the stresses and strains acting on the laminate structure of the riser. This is performed using Classical Laminate Theory (CLT), defined in equation 16, using the notation from Nijhof (1993),

 (16)

where *A* is the extension stiffness matrix; *B* is the bending-extension coupling effects between in plane stresses and curvatures and between bending and twisting moments and in plane strains; *D* is the stiffness of the laminate in the perpendicular direction under the influence of bending and twisting moments; *ε0* is the vector of strain at a particular point in the laminate; *κ* is the vector of curvatures induced in the laminate by the external forces; *N* represents the in-plane forces acting on the segment of the composite pipe and *M* representing the corresponding induced moments about the laminate mid-plane. The in-plane strain vector *{ε}k*­ for the kth lamina is given by equation 17,

. (17)

The stress-strain relationship can be determined for the kth lamina by employing equation 18,

 (18)

where is the distance from the midplane of the laminate in the thickness direction, Q’ is the transformed reduced stiffness matrix for each lamina, dependent on the lamina’s angle relative to the principle direction of the laminate, and *ε ={ εx, εy, εxy)* is a vector of in-plane strains experienced by the laminate. The stress components of *{σ}k* are evaluated by the Tsai-Wu failure criterion, in equation 19, and is used to determine the reliability,

. (19)

The Tsai-Wu criterion is chosen as a good predictor of first-ply failure, and it is assumed that after this initial failure that the riser is unsafe and that the failure will propagate. The left-hand side of the equation is evaluated, at each node along the riser length**,** and the utilisation factor can take values between 0 and 1, where 1 indicates first ply failure. The highest Tsai-Wu value for each riser is found near the touchdown point (x=0) or departure point which agrees with the findings of Wang et al. (2014) and Buberg (2014), where bending moments and tension forces are maximized respectively. As the operational depth increases the Tsai-Wu value closest to the departure point fails, until by 4000m, the Tsai-Wu value is lowest at the departure point. This analysis is selected to provide initial guidance on the probability of failure for composite risers, which are unlikely to fail in the same manner as steel, and to ascertain the importance of water saturation. The steel analysis forms a point of reference and in reality extreme bending moments are an unlikely reason for failure in steel catenary risers that are in service because the riser configuration is often designed to avoid low bending radii and that the most likely form of failure is due to fatigue at the touch-down zone.

# 5. Reliability of Risers

An analysis is performed to compare the trends in reliability between conventional steel risers with composite alternatives across different configurations. This analysis is also extended to investigate the change in reliability estimation of FRP composite risers in wet and dry conditions. The number of runs used for each simulation is 108 with values below this probability judged to be due to numerical phenomena rather than an accurate estimate of the reliability.

**5.1 Sensitivity to dynamic effects**

One challenge when combining a dynamic FEA analysis and Monte-Carlo simulation, is the computational expense which makes the approach infeasible. However, a dynamic amplification factor study, shown in figure 3, predicts values close to unity for significant wave height conditions that are lower than 4m. To verify this assumption the following steps are followed and the results compared to the quasi-static approach for dry and wet conditions:

1. A load case matrix is constructed covering 1 to 6 m wave heights and wave periods ranging between 12 to 22 seconds. A Flexcom finite element riser model, that is used in the dynamic amplification study in figure 3, is utilised to perform the detailed analysis of the load matrix cases. The dynamic amplification factors are estimated for maximum tension and curvature using regular waves. The dynamic amplification factors are estimated as the ratio between the maximum tension or curvature along the riser length divided by the static value for each load case; these values are listed in tables 9 and 10 in the Appendix.
2. For conservatism, extreme short-term omni-directional wave conditions are assumed. The annual waves distribution is predicted by Weibull distribution in equation 10 and parameters listed in table 4. This distribution is found to represent the short-term wave height in the Gulf of Guinea region. The maximum short-term significant wave height is found to be 2.67m, Akinsanya et al. (2017). The environmental directions are assumed to be in the far and near directions for conservatism. In-line waves, currents and offset directions are considered to capture both extreme cases of maximum top-tensions and curvatures.
3. The drag force is calculated using a quasi-static approach by summing the constant velocity current and the maximum wave particle velocities along the riser length.
4. The 2nd Order FPSO response is captured with varying the offset as a function of the wave height as shown in equation 20, the bottom tension is re-calculated for each run with new configurations after adding the offset and the corresponding top tension is estimated. The load case offset, is given by the following equation (20),

 (20)

where the maximum offset, is defined as 0.9% of the depth.

1. The maximum axial force that propagates along the riser, due to the FPSO pitch and heave motions, is approximated using the tension dynamic amplification factor.
2. The maximum curvature that occurs due to the transverse waves propagating along the riser length, due to the FPSO motion, is approximated using the curvature dynamic amplification factor.
3. Two distinctive dynamic amplification factors are used, one for the tension and another for curvature to capture the difference in peak dynamic amplification factor noticed around different wave periods. For environmental conditions that lies between the pre-simulated dynamic amplification factors provided in the Appendix, tables 9 and 10 a linear interpolation is incorporated.

The reliability analysis results, shown in figure 8, are the probability of failures calculated using the quasi-static approach, compared to the detailed dynamic amplification factor predicted by the FEA analysis. A slight increase of the probability of failure is observed, however, the figure shows that the quasi-static approach remains a reasonable approximation for the short-term extreme environmental conditions in the Gulf of Guinea region.

Figure 8: Probability of failure for the 56-ply composite riser using a quasi-static analysis and dynamic analysis, for dry and after water absorption aging at increasing water depths

## **5.2 Steel Catenary Risers vs Composite Risers**

The reliability of two Steel Catenary Risers, one at 1500m and another at 4000m, are analysed and compared to the results of a composite-based riser system to establish a benchmark for comparison. The results show negligible probability of failure and justifies why such risers are in widespread use for offshore hydrocarbon extraction. Further simulations are not performed as the results demonstrate that the probability of failure is low and simulations where failures only occur deep in the tails of the distributions may not be significant from a practical perspective. The reliability of these simulations is similar to the dynamic results from Carrillo & Cicilia (2011), which is assessed to be 1.33×10-11 in the catenary transition zone and 1.73×10-14 at the connection to the tension leg platform for a riser of unknown length, but who quote a reliability on the order of 10-5 at the weakest points near the Touchdown Zone which are not exhibited in this model. The simulation of the Steel Catenary Riser operating in ocean depths of 4000m reveals a significantly higher top tension much larger than that of the of the Steel Catenary Riser operating at 1500m. However, the probability of failure for these risers is still lower than composites.

Figure 9: probability of failure for the 56-ply composite, Wei (2015)**,** operating at 1500m

To determine the probability of failure, a convergence study is performed to select the appropriate number of runs required for the Monte-Carlo simulation, illustrated in Figures 9 and 10.In comparison, Figure 9 reveals the rate of failures for the 56-ply composite layup operating at 1,500m. These simulations provide a probability of failure of 4.4×10-7 which is higher than the steel catenary riser operating at 1500m which recorded no failures. However, this is still determined to be a safe value according to the DNV rules, Table 1. This increases to 1.6×10-5 for depths of 4000m which is still safe, but more importantly shows only a small increase in probability of failure but where the rate in change of failure for the steel catenary is unknown as no failures have occurred.

Figure 10: probability of failure for the 20-ply composite riser, Tan et al. (2015), operating at 1,500m

Figure 10 shows that the probability of failure for the 20-ply riser, Tan et al. 2015, has not converged but reaches a value of 6.0×10-8 . However, this can be considered to be a numerical anomaly as they are occurring in the tails in the normal distribution and unlikely to represent real properties as they are unrealistically far from the mean value. Therefore, the simulations are stopped at this point and assumed to be a very low value. This demonstrates a similar probability of failure to that of a steel catenary riser with similar thickness and operating depth. For the 20-ply case the probability of failure increases to 1.2×10-7 at 4000m demonstrating convergence and a similar low probability of failure.

Table 7 compares the different systems with respect to the unit weight and the total weight of the entire riser system. Steel catenary risers show smaller deflections than the composite risers and the low stiffness exhibited by composite materials increases the overall length of the composite system. A larger thickness for the riser might improve the performance of the riser system but will also require higher tension loads and the results in this analysis demonstrate the opposite effect, thin and light risers are less likely to fail. The Marginal Reliability, the improvement in probability of failure for each kilogram of additional material, of Carbon/Epoxy improves reliability by 9.07x10-13 and 1.53x10-13 for the 56- and 20-ply composite riser respectively and at 4000m this increased to 1.43x10-11 and 1.37x10-13. The probability of failure per kilogram, Pf/kg is a normalized failure probability that is a parameter to allow easier comparison of the cross-sectional design by addition or reduction of riser material to indicate the change in the probability of failure.

#### Table 7: comparison of weight and probability of failure for steel catenary and composite risers

|  |  |  |
| --- | --- | --- |
|   | 1500m | 4000m |
| Steel | 56-ply | 20-ply | Steel | 56-ply | 20-ply |
| Submerged weight full of product (N/m)  | 794 | 248 | 189 | 794 | 248 | 189 |
| **Arc**-Length (**m**) | 1659 | 1957 | 2079 | 4181 | 4498 | 4642 |
| Total Weight | 1.3E+06 | 4.9E+05 | 3.9E+05 | 3.3E+06 | 1.1E+06 | 8.8E+05 |
| Pf | <1.0E-8 | 4.4E-07 | 6.0E-08 | <1.0E-8 | 1.6E-05 | 1.2E-07 |
| Pf/kg | <1.0E-13 | 9.1E-13 | 1.5E-13 | <1.0E-13 | 1.4E-11 | 1.4E-13 |

Both composite layups fulfil the DNV’s lower safety class recommendation*,* Pf = 10-5 DNV (2010a), at depths of 1500m and 4000m. This builds confidence in the ability of composite risers to be utilized in deep-water conditions and the potential to benefits from lower weight, and therefore reduced installation costs. However, the analysis shows limited benefits, in terms of reliability, over steel under extreme conditions. The steel catenary riser provides a higher level of reliability than the design using a composite material statically. However, the two composite riser and steel catenary riser designs are chosen arbitrarily and the analysis shows that thinner thicknesses of pipe provide a safer design but that the steel benefits from a higher stiffness. This analysis therefore needs to be extended with an analysis performed on optimally designed pipes for each distance, supported by the findings from the parametric study performed in this paper, and a dynamic analysis.

## **5.3 The Impact of Moisture Absorption of Riser Reliability**

Whilst the results of the previous study indicate the potential for composites for deep-water applications, it is unlikely that these materials will perform for extended periods with the performance of testing in dry conditions. To account for this, the riser properties are degraded to represent the wet state according to experiments from the open literature. Malmstein et al. (2013) provide data for glass/epoxy composites in conditions lasting up to 6 weeks in distilled water. The material properties of the degraded material are shown to be more dependent on the composite matrix resin material, so findings related to the degradation percentage of glass provides can be extrapolated to carbon, due to the small quantity of data available in the open literature. The findings of these experiments show that the riser flexural strength is reduced by 50% and the weight is increased by 2%; the flexural modulus is also reduced by 1.6% but this reduction is ignored. The degradation of composites in distilled water is often higher than in salt water and the results are also for flexure, rather than tension, and so this degradation factor is considered as a worst-case scenario. Experiments are also performed on carbon/epoxy specimens by Zafar et al. (2012) who found an increase in weight of 2.12% in conditions lasting up to 300 days in salt water. The corresponding loss of tensile strength was 20% and Young’s modulus was 10%. These values are therefore included into the model where Zafar et al. (2012) represents more realistic values for risers due to the similar materials and tensile properties alongside experiments conducted in salt water. The reliability analysis for different depths is reported in figure 11 where the probability of failure is shown in logarithmic scale, for the 56-ply case, and similarly, figure 12, for the 20-ply case. The results are assumed to have converged as the probability of failure for the wet simulations should be higher than those in the dry condition, requiring fewer runs to converge and therefore the same number of simulations are performed.

Figure 11: probability of failure for the 56-ply composite riser, Wei (2015), after water absorption aging at increasing water depths

These results imply that, as expected, there is a negative relationship between water retention and reliability. The reduction in ultimate tensile strength and Young’s Modulus combined with the increased weight per unit leads to a combined effect of increasing the tension while reducing the materials resistance to external loadings. This explains why the probability of failure is higher for the moisture absorbed riser systems. Comparing the two types of moisture absorption the Zafar et al. (2012) case reports higher probability of failures for all of the risers in comparison to the dry properties; at lower depths this increase is mild however at the larger depths it is more significant. This trend also occurs for the less realistic properties from Malmstein et al. (2013) but the probability of failure is higher.

Figure 12: probability of failure for the 20-ply composite riser, Tan et al. (2015), after water absorption aging at increasing water depth

The results for the wet composites, summarised in Table 8, are more indicative of the number of failures to be expected as the system matures over time, and may be used to establish the upper limits for the operating lifetime for FRP riser systems. Even in the worst-case material degradation scenario, the Malmstein et al. (2013) aging and the thicker 56-ply case, the probability of failure for the 1,500m is still 7.56x10-6 and is only predicted to pass the lower safety limit at 4,000m with a probability of failure of3.05x10-3, which could be reduced with enhanced cross-sectional design.

#### Table 8: probabilities of failure for composite risers with moisture absorption

|  |  |  |
| --- | --- | --- |
|  | 56-ply | 20-ply |
| Dry | Malmstein (2013) | Zafar (2012) | Dry | Malmstein (2013) | Zafar (2012) |
| 1500m |
| Pf | 4.40E-07 | 7.56E-06 | 5.20E-07 | 6.00E-08 | 3.74E-06 | 5.00E-08 |
| Weight (kg) | 4.85E+05 | 4.89E+05 | 4.89E+05 | 3.92E+05 | 3.99E+05 | 3.99E+05 |
| Pf/kg | 9.07E-13 | 1.55E-11 | 1.06E-12 | 1.53E-13 | 9.37E-12 | 1.25E-13 |
| 2000m |
| Pf | 9.30E-07 | 2.87E-05 | 1.18E-06 | 6.00E-08 | 3.75E-06 | 5.00E-08 |
| Weight (kg) | 6.13E+05 | 6.18E+05 | 6.18E+05 | 4.91E+05 | 4.99E+05 | 4.99E+05 |
| Pf/kg | 1.52E-12 | 4.65E-11 | 1.91E-12 | 1.22E-13 | 7.51E-12 | 1.00E-13 |
| 3000m |
| Pf | 3.64E-06 | 3.52E-04 | 6.08E-06 | 6.00E-08 | 9.50E-06 | 1.80E-07 |
| Weight (kg) | 8.65E+05 | 8.72E+05 | 8.72E+05 | 6.84E+05 | 6.97E+05 | 6.97E+05 |
| Pf/kg | 4.21E-12 | 4.04E-10 | 6.97E-12 | 8.77E-14 | 1.36E-11 | 2.58E-13 |
| 4000m |
| Pf | 1.60E-05 | 3.05E-03 | 3.18E-05 | 1.20E-07 | 8.95E-05 | 7.20E-07 |
| Weight (kg) | 1.12E+06 | 1.12E+06 | 1.12E+06 | 8.76E+05 | 8.92E+05 | 8.93E+05 |
| Pf/kg | 1.43E-11 | 2.71E-09 | 2.82E-11 | 1.37E-13 | 1.00E-10 | 8.06E-13 |

The increase in probability of failure due to the hygrothermal aging is higher for the thicker composite, where the values increase by a factor of 62-746 over the intact riser for the extreme case of Malmstein et al. (2013) and by 1-6 for the more realistic aging case of Zafar et al. (2012), than for the thinner riser, with an increase by a factor of 17-190 for the extreme aging case of Malmstein et al. (2013) and 1.18-1.98 for the more realistic aging case of Zafar et al. (2012), where these values are highest at the higher depths. This underscores the need for effective and robust waterproof layering to protect the laminate from direct contact with the surrounding saline environment as suggested by Tan et al.(2015) for their composite system. This system will extend the operating lifetime of the pipeline, and potentially reduce maintenance and operational expenses in the long run by reducing moisture contact with the laminate. However, it is unlikely that it will be totally able to remove the moisture from the environment and determining the levels of absorption over time appears to be a critical characteristic in reducing safety factors for composite risers in deep-water conditions, where even the more realistic water uptake estimates give an increase in probability of failure of 6 for the thicker riser and 1.98 for the thinner riser.

**6. Conclusions**

Composite risers are increasingly being investigated for industrial applications with some current applications already in place. However, there is limited literature documenting the stochastic behaviour of risers made from these materials. Therefore, this paper conducts a strength-based reliability assessment of full-scale carbon/epoxy composite risers in comparison with traditional steel catenary risers. A dynamic amplification factor is derived, and verified, using an FE model to predict a similar probability of failure compared to the quasi-static approach, confirming the suitability of the quasi-static model to predict the failure probability in the Gulf of Guinea region.The results confirm the benefits of FRP composites risers for depths approaching 4,000m over conventional steel risers, in that they provide a safe but lighter weight system which is therefore cheaper to install; thinner risers are shown to have a lower probability of failure due to the lighter weight and lower tensions. However, the risers are shown to have a higher probability of failure in comparison to the steel equivalents. Additional studies are performed to account for the effect of water absorption on the material degradation and the performance of the composite risers. The results show that the reliability of composite risers is reduced due to moisture absorption and that this becomes more pronounced at depths exceeding 2,000m.

# ACKNOWLEDGEMENTS

This work was supported by EPSRC Centre of Doctoral Training grant no. EP/L015382/1 and by the Lloyd’s Register Foundation.

# REFERENCES

Akinsanya, A., 2017. Swell description for Bonga offshore Nigeria location. *Ocean Systems Engineering*, 345-369

Bridge, C., 2005. *Effects of Seabed Interaction on Steel Catenary*. University of Surrey.

Buberg, T., 2014. *Design and Analysis of Steel Catenary Riser Systems for Deep Waters*. Norwegian University of Science and Technology.

Carrillo, C.C., Cicilia, F.B. and del Angel, F.C. 2011. Structural Reliability in Catenary Upstream Pipelines. *El Portulano De La Ciencia*, III, pp.907–914.

Chu, P.C., 2008. Weibull distribution for the global surface current speeds obtained from satellite altimetry. *International Geoscience and Remote Sensing Symposium (IGARSS)*, 3(1).

Det Norske Veritas, 2009. *Recommended Practice for Composite Risers, DNV-RP-F202, Oslo, Norway.*

Det Norke Veritas, 2010a. *Recommended Practice Composite Risers, DNV-RP-F202*, Oslo, Norway.

Det Norske Veritas, 2010b, *Environmental Conditions and Environmental Loads, DNV-RP-C205, Oslo, Norway.*

Duan, M., Chen, J. & Li, Z., 2011. *Mechanics of Deepwater Steel Catenary Riser*, Shanghai.

Faltinsen, O., 1990. *Sea Loads on Ships and Offshore Structures*, Cambridge: Cambridge University Press.

Kang, Z., Zhang, L. & Zhang, X., 2015. Analysis on J lay of SCR based on catenary and large deflection beam theory. *Ocean Engineering*, 104, pp.276–282. Available at: http://linkinghub.elsevier.com/retrieve/pii/S0029801815001936.

Li, F.Z. & Low, Y.M., 2012. Fatigue reliability analysis of a steel catenary riser at the touchdown point incorporating soil model uncertainties. *Applied Ocean Research*, 38, pp.100–110. Available at: http://dx.doi.org/10.1016/j.apor.2012.07.005.

Malmstein, M., Chambers, A.R. & Blake, J.I.R., 2013, Hygrothermal ageing of plant oil based marine composites, *Composite Strutures*, vol. 101, pp. 138-143.

Nijhof, A.H.J., 1993. Analysis of Laminated Composites. In R. A. Shenoi & J. F. Wellicome, eds. *Composite Materials in Maritime Structures. Volume 1: Fundamental Aspects*. Cambridge: Cambridge University Press.

Ochoa, O.O. & Salama, M.M., 2005. Offshore composites: Transition barriers to an enabling technology. *Composites Science and Technology*, 65, pp.2588–2596.

Ochoa, O.O., 2006. Composite riser experience and design guidance, Final Project Report prepared for the Minerals Management Service under the MMS/OTRC Cooperative Research Agreement 1435-01-04-CA-35515, Task Order 35985, MMS Project Number 490, , Texas A&M University.

Pham, D.C., Narayanaswamy, S. Qian, X. Sobey, A.J. Achintha, M. and R.A.Shenoi, 2016. A review on design, manufacture and mechanics of composite risers, *Ocean Engineering,* vol 112, pp. 82-96*.*

Philippidis, T.P., Lekou, D.J. & Aggelis, D.G., 1999. Mechanical property distribution of CFRP filament wound composites. *Composite Structures*, 45(1), pp.41–50.

Silva, R.F. Da et al., 2013. Optimization of composite catenary risers. *Marine Structures*, 33, pp.1–20.

Skogdalen, J.E. Vinnem, J.E. 2011. Quantitative risk analysis offshore - Human organizational factors, *Reliability Engineering and System Safety*, vol. 96, pp. 468-479

Skogdalen, J.E. Vinnem, J.E. 2012. Quantitative risk analysis of oil and gas drilling, using Deepwater Horizon as case study, *Reliability Engineering and System Safety*, vol. 100, pp. 58-66

Sobey, A. J., Blake, J.I.R. & Shenoi, R. A., 2013. Monte Carlo reliability analysis of tophat stiffened composite plate structures under out of plane loading. *Reliability Engineering and System Safety*, 110, pp.41–49. Available at: http://dx.doi.org/10.1016/j.ress.2012.08.011.

Tan, L., Chen, Y. & Jaiman, R.K., 2015. Coupled fluid–structure simulations for evaluating a performance of full-scale deepwater composite riser. *Ocean Engineering*, 94, pp.19–35. Available at: http://linkinghub.elsevier.com/retrieve/pii/S0029801814004193.

Tarnopol’skii, Y.M., Kulakov, V.L. & Mungalov, D.D., 1999. Composites in offshore technology in the next century. *Mechanics of Composite Materials*, 35(5), pp.365–372.

Wang, Y., Gao, D. & Fang, J., 2014. Static analysis of deep-water marine riser subjected to both axial and lateral forces in its installation. *Journal of Natural Gas Science and Engineering*, 19, pp.84–90. Available at: http://dx.doi.org/10.1016/j.jngse.2014.04.019.

Wei, Z., 2015 *Mechanical and Fatigue Test of CFRP Risers*, National University of Singapore Report.

Xia, J. Das, P.K. and Karunakaran, D. (2008) A parametric design study for a semi/SCR system in Northern North Sea, *Ocean Engineering*, vol. 35, pp. 1686-1699

Zhan, J.P., 2010. *Review and verification of marine riser analysis programs : Global response analysis*. Norwegian Univeristy of Science and Technology.

Zafar, A. Bertocco, F. Schjodt-Thomsen, F. Rauhe, J.C. (2012) Investigation of the long term effects of moisture on carbon fibre and epoxy matrix composites, *Composite Science and Technology*, vol. 72, pp.656-666

# Appendix

#### Table 9: Tension Dynamic Amplification Factors (1500/3000/4000 m)

|  |  |
| --- | --- |
|  | **Wave Period (s)** |
| **12** | **13** | **14** | **15** | **16** | **17** | **18** | **19** | **20** | **21** | **22** |
| **Maxim Wave Height (m)** | **1** | 1.02/1.02/1.02/1.01 | 1.03/1.03/1.03/1.02 | 1.04/1.03/1.03/1.03 | 1.04/1.04/1.03/1.03 | 1.04/1.04/1.03/1.03 | 1.04/1.04/1.03/1.03 | 1.04/1.04/1.03/1.03 | 1.04/1.04/1.03/1.03 | 1.04/1.03/1.03/1.03 | 1.03/1.03/1.03/1.03 | 1.02/1.02/1.02/1.01 |
| **2** | 1.04/1.04/1.03/1.03 | 1.06/1.06/1.05/1.05 | 1.07/1.07/1.06/1.06 | 1.08/1.08/1.07/1.06 | 1.08/1.08/1.07/1.06 | 1.08/1.08/1.07/1.06 | 1.08/1.08/1.07/1.06 | 1.08/1.08/1.07/1.06 | 1.07/1.07/1.06/1.06 | 1.07/1.07/1.06/1.05 | 1.04/1.04/1.03/1.03 |
| **3** | 1.06/1.06/1.05/1.04 | 1.09/1.09/1.08/1.07 | 1.11/1.11/1.09/1.09 | 1.12/1.12/1.11/1.1 | 1.12/1.12/1.11/1.1 | 1.13/1.12/1.11/1.1 | 1.12/1.12/1.1/1.09 | 1.12/1.12/1.1/1.09 | 1.12/1.11/1.09/1.08 | 1.11/1.11/1.09/1.08 | 1.06/1.06/1.05/1.04 |
| **4** | 1.08/1.08/1.07/1.06 | 1.12/1.12/1.1/1.09 | 1.15/1.14/1.12/1.11 | 1.17/1.16/1.14/1.13 | 1.17/1.17/1.14/1.13 | 1.18/1.17/1.14/1.13 | 1.17/1.17/1.14/1.12 | 1.18/1.17/1.13/1.12 | 1.17/1.16/1.13/1.11 | 1.16/1.16/1.12/1.11 | 1.08/1.08/1.07/1.06 |
| **5** | 1.1/1.1/1.08/1.07 | 1.15/1.15/1.13/1.12 | 1.18/1.18/1.16/1.14 | 1.22/1.21/1.18/1.16 | 1.22/1.22/1.18/1.16 | 1.24/1.24/1.18/1.16 | 1.24/1.23/1.18/1.15 | 1.24/1.23/1.18/1.15 | 1.23/1.23/1.17/1.14 | 1.23/1.22/1.16/1.13 | 1.1/1.1/1.08/1.07 |
| **6** | 1.12/1.12/1.1/1.09 | 1.18/1.18/1.16/1.14 | 1.23/1.22/1.19/1.17 | 1.27/1.27/1.22/1.2 | 1.29/1.28/1.22/1.19 | 1.31/1.31/1.23/1.19 | 1.31/1.3/1.22/1.19 | 1.32/1.31/1.22/1.18 | 1.31/1.3/1.22/1.17 | 1.3/1.29/1.21/1.16 | 1.12/1.12/1.1/1.09 |

#### Table 10: Curvature Dynamic Amplification Factors (1500/3000/4000 m)

|  |  |
| --- | --- |
|  | **Wave Period (s)** |
| **12** | **13** | **14** | **15** | **16** | **17** | **18** | **19** | **20** | **21** | **22** |
| **Maxim Wave Height (m)** | **1** | 1.02/1.02/1.01/1.01 | 1.03/1.03/1.02/1.01 | 1.04/1.03/1.03/1.01 | 1.04/1.04/1.03/1.02 | 1.04/1.04/1.04/1.02 | 1.05/1.05/1.04/1.02 | 1.05/1.05/1.04/1.02 | 1.05/1.05/1.04/1.02 | 1.05/1.05/1.04/1.02 | 1.04/1.05/1.04/1.02 | 1.02/1.02/1.01/1.01 |
| **2** | 1.04/1.03/1.02/1.01 | 1.06/1.05/1.04/1.02 | 1.07/1.07/1.05/1.03 | 1.08/1.08/1.06/1.03 | 1.09/1.09/1.07/1.04 | 1.1/1.09/1.08/1.04 | 1.1/1.1/1.08/1.04 | 1.1/1.1/1.08/1.04 | 1.1/1.1/1.08/1.04 | 1.1/1.1/1.08/1.04 | 1.04/1.03/1.02/1.01 |
| **3** | 1.05/1.05/1.04/1.02 | 1.08/1.08/1.06/1.03 | 1.11/1.1/1.08/1.04 | 1.13/1.13/1.1/1.05 | 1.14/1.14/1.11/1.05 | 1.16/1.16/1.12/1.06 | 1.16/1.16/1.12/1.06 | 1.17/1.17/1.13/1.06 | 1.17/1.17/1.13/1.07 | 1.17/1.17/1.13/1.07 | 1.05/1.05/1.04/1.02 |
| **4** | 1.07/1.06/1.05/1.02 | 1.11/1.1/1.08/1.04 | 1.15/1.14/1.11/1.05 | 1.19/1.19/1.14/1.07 | 1.21/1.21/1.16/1.07 | 1.23/1.24/1.18/1.08 | 1.24/1.25/1.19/1.09 | 1.25/1.26/1.2/1.09 | 1.25/1.27/1.2/1.09 | 1.26/1.27/1.2/1.09 | 1.07/1.06/1.05/1.02 |
| **5** | 1.09/1.08/1.06/1.03 | 1.14/1.13/1.1/1.05 | 1.2/1.19/1.14/1.07 | 1.26/1.26/1.2/1.09 | 1.29/1.3/1.22/1.1 | 1.33/1.35/1.26/1.11 | 1.34/1.37/1.27/1.12 | 1.37/1.39/1.29/1.13 | 1.37/1.4/1.29/1.13 | 1.37/1.4/1.29/1.13 | 1.09/1.08/1.06/1.03 |
| **6** | 1.1/1.09/1.07/1.03 | 1.18/1.17/1.13/1.06 | 1.25/1.25/1.19/1.08 | 1.35/1.36/1.26/1.11 | 1.39/1.42/1.3/1.13 | 1.46/1.5/1.36/1.15 | 1.48/1.52/1.38/1.16 | 1.51/1.57/1.41/1.17 | 1.51/1.57/1.41/1.17 | 1.52/1.57/1.41/1.18 | 1.1/1.09/1.07/1.03 |