

Partially Displacement Controlled conditions in the overbend

During S-lay pipeline installation

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PARTIALLY DISPLACEMENT CONTROLLED CONDITIONS IN THE OVERBEND

DURING S-LAY PIPELINE INSTALLATION

by

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PREFACE

This thesis is the final work for my Master of Science study of Offshore and Dredging Engineering at the Delft University of Technology and has been performed in cooperation with the Pipeline Engineering Department of Allseas Engineering B.V. in Delft.

I would like to thank the members of my graduation committee, Andrei Metrikine and Antonio Jarquin Laguna, for their guidance and getting me back on the right track when I lost goal of the research.

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A special thanks for my friends, who were always willing to give me reasons to skip studying and made my time truly amazing. Last but not least, I want to thank my parents for never losing faith in me and supporting me all the way. I would not have made it this far without you.

*J.J. Jansen
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ABSTRACT

The S-lay pipeline installation method is characterised by its fast installation process and its application in all water depths. Offshore standards and design codes provide local buckling checks for combined loading criteria for load controlled and displacement controlled conditions, where a load controlled condition is defined as a case in which the structural response is primarily governed by the imposed loads and a displacement controlled condition is defined as a case in which the structural response is primarily governed by imposed geometric displacements. It is recognised by the offshore pipelaying industry that the condition with the pipeline on a rigid stinger in S-lay is not fully displacement controlled. Using the methods by DNV only allows for situations that are either 100% load controlled or 100% displacement controlled to be checked. In order to account for partially displacement controlled conditions, a method is needed to calculate the level of displacement control in a specific situation and to translate this to a partially displacement controlled buckling check.

This graduation thesis proposes a method to calculate the level of partial displacement control of an S-lay pipeline installation situation using pipe stiffness variations in beam elements. First, a static pipelaying configuration is modelled using beam elements. Next, the same situation is modelled with a different pipe stiffness over the stinger section. The changes in moment and curvature at the rollerbox locations between the base case and the modified model give an indication of the level of displacement control on the stinger. An approach to translate the calculated level of displacement control to a modification of the displacement controlled buckling check by DNV is presented.

Detailed finite element models composed of shell type elements are created to validate the proposed method. A first step towards validation of the proposed method has been taken with satisfactory results. The calculated levels of displacement control with the proposed method are within 2% of the calculated level of displacement control in the detailed partially displacement controlled model using shell elements. The use of the proposed method allows the calculation of the level of displacement control using Euler-Bernoulli beam element models and with reduced computational time.

A parametric study is performed using the proposed method to study the effect of most relevant variables in the S-lay configuration on the degree of partial displacement control. The effects of the stinger radius; rollerbox spacing; rollerbox load; lay tension; pipe diameter; and pipe diameter to wall thickness ratio have been studied, giving greater understanding of the partially displacement controlled behaviour.

The research done in this thesis provides an engineering approach to determine the partially displacement controlled conditions in the S-lay configuration using stiffness variations in Euler-Bernoulli beam element models. First steps have been taken to validate this engineering approach with satisfactory results. A method for modifying the displacement controlled local buckling check to account for partially displacement controlled conditions is presented, as well as an understanding of the influence of most relevant pipelaying variables on the partially displacement controlled condition. The findings in this research have proposed a step forward in designing with partially displacement controlled conditions in the overbend during pipeline installation.

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LIST OF ABBREVIATIONS

ALS	Accidental Limit State
ASD	Allowable Stress Design
DC	Displacement Control
DNV	Det Norske Veritas
DP	Dynamic Positioning
ERW	Electric Resistance Welding
FE	Finite Element
FEM	Finite Element Model
FLS	Fatigue Limit State
JIP	Joint Industry Project
LC	Load Control
LPF	Load Proportionality Factor
LRFD	Load and Resistance Factor Design
LSD	Limit-State Design
PDC	Partial Displacement Control
SAWL	Longitudinal Submerged Arc-Welding
SLS	Serviceability Limit State
SMTS	Specified Minimum Tensile Strength
SMYS	Specified Minimum Yield Stress
TRB	Three Roll Bending
ULS	Ultimate Limit State
UO	pipe fabrication process for welded pipes
UOE	pipe fabrication process for welded pipes, expanded
UTS	Ultimate Tensile Stress

LIST OF SYMBOLS

Symbol	Description	Units
A	Ramberg-Osgood coefficient	–
B	Ramberg-Osgood exponent	–
c	Menger curvature	m^{-1}
D	nominal outer diameter	m
D_{max}	greatest measured inside or outside diameter	m
D_{min}	smallest measured inside or outside diameter	m
E	Young's modulus	Pa
f_o	ovality	–
f_u	characteristic tensile strength	Pa
$f_{u,temp}$	de-rating value due to temperature of the tensile strength	Pa
f_y	characteristic yield stress	Pa
$f_{y,temp}$	de-rating value due to temperature of the yield stress	Pa
I_c	cross-sectional moment of inertia	m^4
K	curvature	m^{-1}
K^*	normalised curvature	–
K_F	functional curvature	–
K_s	roller stiffness	N/m
K_{Sd}	design curvature	m^{-1}
K_y	yield curvature	m^{-1}
M	bending moment	Nm
M^*	normalised moment	–
$M_{buckling}$	buckling moment	Nm
M_{dc}	allowable moment for the displacement controlled situation	Nm
M_F	functional moment	Nm
M_{Ic}	allowable moment for the load controlled situation	Nm
M_p	plastic moment capacity	Nm
M_{pdc}	allowable moment for the partially displacement controlled situation	Nm
M_{ref}	reference moment	Nm
M_{Sd}	design moment	Nm
M_y	yield moment	Nm
N	material constant	–
P_0	dead load	–
p_b	pressure containment resistance	Pa
p_c	characteristic collapse pressure	Pa
p_e	external pressure	Pa
p_{el}	elastic collapse pressure	Pa
p_{min}	minimum internal pressure that can be sustained	Pa
p_p	plastic collapse pressure	Pa
P_{ref}	reference load	–
r	pipe radius	m
R	stinger radius	m

Symbol	Description	Units
R_m	tensile strength	Pa
R_s	roller support reaction	N
$R_{t,05}$	yield strength	Pa
S_0	material constant	–
S_p	plastic effective axial force capacity	N
S_{sd}	design effective axial force	N
S_{MTS}	specified minimum tensile strength	Pa
S_{MYS}	specified minimum yield stress	Pa
t	nominal wall thickness	m
z	level of displacement control	–
α_c	flow stress parameter	–
α_{fab}	fabrication factor	–
α_{gw}	girth weld factor	–
α_h	strain hardening factor	–
α_U	material strength factor	–
γ_ϵ	strain resistance factor	–
γ_m	material resistance factor	–
γ_{pdc}	partial displacement control factor	–
γ_{SC}	safety class resistance factor	–
δ_s	roller deformation	m
ϵ	uniaxial strain	–
ϵ_c	characteristic bending strain resistance	–
ϵ_{dc}	strain capacity for the displacement controlled situation	–
$\epsilon_{engineering}$	engineering strain	–
ϵ_F	functional strain	–
ϵ_{lc}	strain capacity for the load controlled situation	–
ϵ_{pdc}	strain capacity for the partially displacement controlled situation	–
ϵ_{sd}	design compressive strain	–
ϵ_{true}	true strain	–
λ	proportionality factor	–
ν	Poisson's ratio	–
σ	uniaxial stress	Pa
$\sigma_{engineering}$	engineering stress	Pa
σ_{true}	true stress	Pa
σ_y	yield stress	Pa

DEFINITIONS

A lot of the terms used in this report are exclusive to the offshore pipelaying industry. This section aims to take away misunderstandings that can occur as a result of this. This section is not required to be read, but can help if there is anything unclear regarding definitions.

- **Stinger:** Frame structure hanging from the pipelaying vessel. Its function is to prevent the pipeline from buckling when it is bending from a nearly horizontal position on the vessel through the first bend of an S-curve towards the seabed.
- **Rollerbox:** Pipe supports consisting of a set of rollers in pairs. Its function is to support the pipeline vertically and allow the pipe string to travel in longitudinal direction of the vessel.
- **Tensioner:** Tracks that exert a controlled pressure on the pipe. It is a central element in most pipelay systems and its function is to hold the pipe in suspension between the end of the stinger and the seabed.
- **Overbend:** The overbend is the section of the pipeline where the pipeline is curved concave-down.
- **Sagbend:** The sagbend is the pipeline section where the pipeline is curved concave-upward.
- **Stinger radius:** The stinger radius is the radius of a circle formed by the pipe supports on the stinger.
- **Lift-off angle:** The lift-off angle (or departure angle) is the angle, relative to the horizontal plane, of the pipeline at the point where the pipeline is no longer in contact with the rollers on the stinger.
- **Firing line:** The firing line is the complete line of work stations and equipment from the line up station to the last station on the vessel.
- **Load controlled:** A load controlled condition is one in which the structural response is primarily governed by the imposed loads.
- **Displacement controlled:** A displacement controlled condition is one in which the structural response is primarily governed by imposed geometric displacements.
- **Partially displacement controlled:** A condition in which the structural response is governed by a combination of imposed loads and imposed geometric displacements.

1

INTRODUCTION

1.1. ALLSEAS

Allseas Group S.A. is a Swiss-based company founded by Edward Heerema in 1985. The company is a global leader in offshore pipeline installation, heavy lift and subsea construction. Worldwide Allseas has executed more than 275 projects and installed over 20.000 kilometres of subsea pipeline. The company employs approximately 3000 people worldwide and operates a fleet of six vessels. The fleet consists of the dynamically positioned (DP) pipelay vessels Solitaire, Audacia, and Lorelay, the shallow water/ multi-purpose work barge Tog Mor, the DP support vessel Calamity Jane and the DP platform installation/decommissioning and pipelay vessel Pioneering Spirit.

1.2. PIPELINE INSTALLATION METHODS

1.2.1. S-LAY METHOD

The installation method used by Allseas is the S-lay method and is characterised by its ability to lay a wide variety of pipe diameters quickly over a large range of water depths. With the S-lay method, the pipeline starts with pipeline sections in a horizontal position on the vessel. On board the vessel the pipeline sections are welded together and given corrosion protection in the firing line. Due to this horizontal work method, the line leaves the vessel in a horizontal orientation and is then guided along a long boom-like structure called the stinger.

The stinger is a frame structure hanging from the stern of the vessel that supports the line intermittently on v-shaped rollers, providing a controlled curvature of the pipeline from the horizontal to the suspended section. Modern stingers are articulated, consisting of several segments connected by hinges. This allows the stinger shape to be set to a prescribed curvature by setting the segments at chosen angles. The suspended section of pipeline is held by tensioners located at the end of the firing line.

The upper part of the pipeline that is supported by the stinger is known as the overbend and the section of the pipeline bending towards the seabed is known as the sagbend. The shape of the pipeline as it is being installed resembles the shape of the letter S, giving this lay method its characteristic name. A schematic of this lay method is shown in figure 1.1.

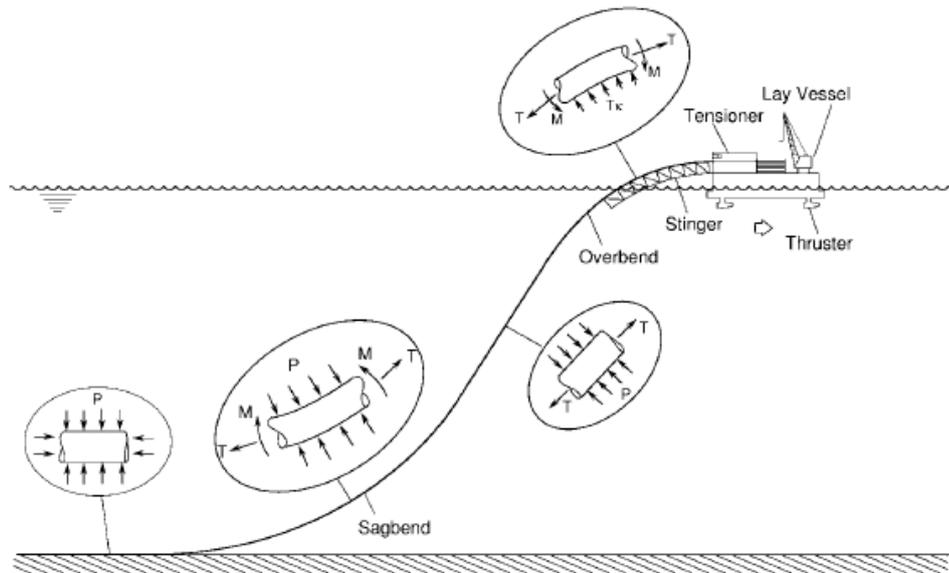


Figure 1.1: Schematic representation of S-lay pipeline installation and associated pipeline loadings. [1]

1.2.2. OTHER METHODS

Other lay methods used in the industry are J-lay, reeling and towing. With the J-lay method, pipe sections are welded together and inspected vertically. This method is slower than S-lay because usually there is only one workstation for welding the pipe sections. The benefit of this method is that the pipeline leaves the vessel near vertically, which means that the vessel does not need large thrusters to be able to deliver the required horizontal tension. This method is also easier to apply to deeper waters.

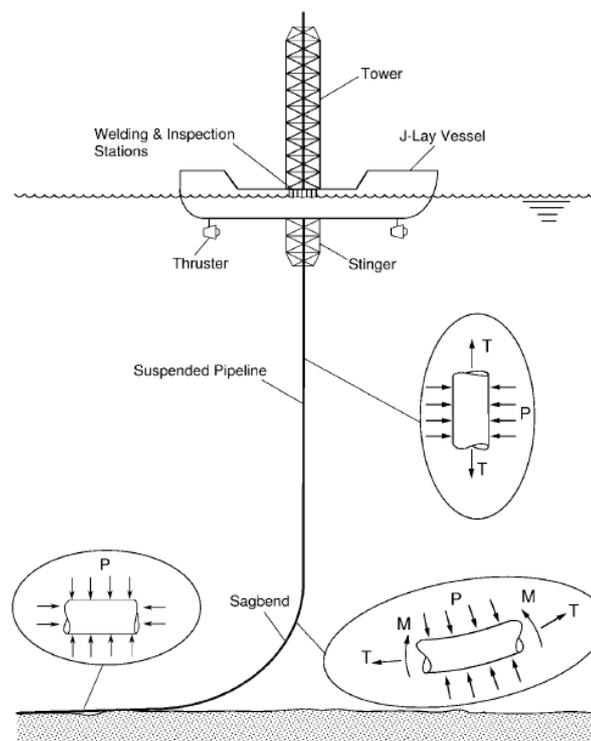


Figure 1.2: Schematic representation of J-lay pipeline installation and associated pipeline loadings. [1]

Reeling is one of the most efficient methods for laying subsea pipelines. In this method a section of pipeline, usually several kilometres long, is wound onto a reel that is mounted on a vessel. With the pipeline on the reel the vessel travels to the installation site and installs the pipe by unspooling the pipeline and pulling it through a straightener to get rid of any residual curvature caused by the reeling process. High installation speeds can be achieved but the method is limited to smaller diameter pipelines.

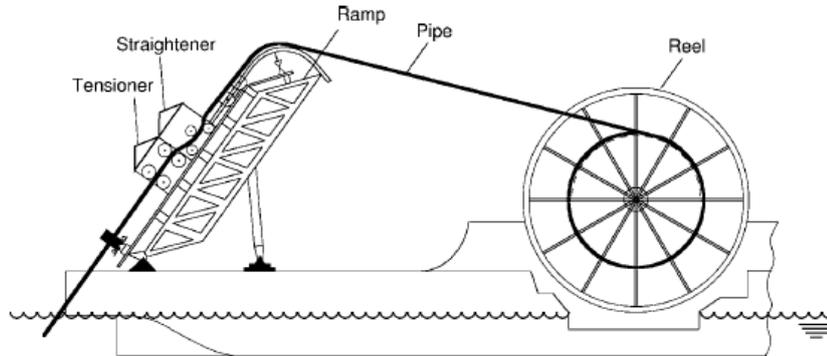


Figure 1.3: Schematic representation of reeling pipeline installation. [1]

The last method of installation is towing. When using the towing method a section of pipeline is welded and inspected onshore and then towed to the installation site. This technique is ideal for shorter pipelines. The pipeline is transported to the installation location by a tugboat, while either a second tugboat keeps the line taut or the free end of the pipeline drags across the bottom. Once at location the line is lowered by flooding buoys in a controlled manner.

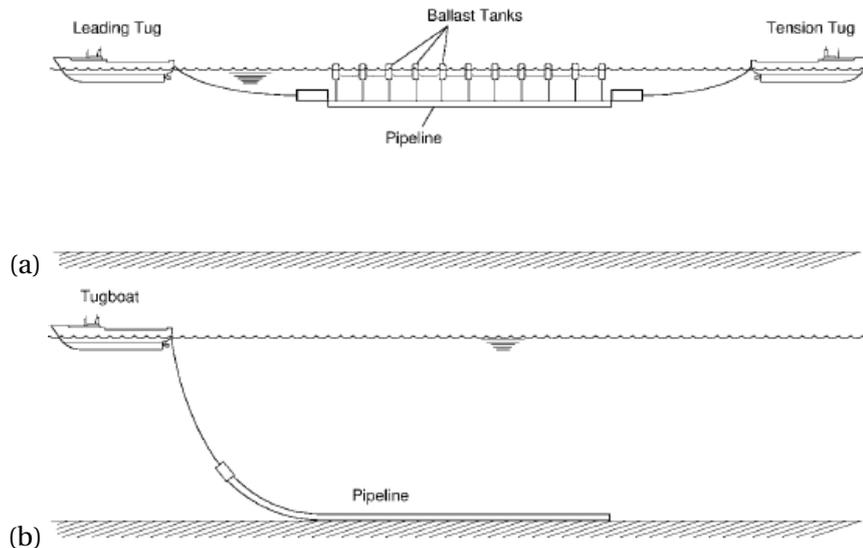


Figure 1.4: Schematics of (a) surface tow and (b) bottom tow. [1]

1.3. THESIS OBJECTIVES

S-lay is characterised by its fast installation process and its application in all water depths. Allseas has installed over 20,000 km of subsea pipeline worldwide using S-lay technology, with diameters ranging from 2" to 48", without a single overbend buckle during normal installation conditions. The vast majority of this pipe has been installed in accordance with DNV-OS-F101 [2] while applying displacement controlled overbend buckling checks.

It is recognised that the condition with the pipeline on a rigid stinger in S-lay is not a 100% displacement controlled condition. On a large scale the configuration of the pipeline has to conform to the rollers, and in that sense is displacement controlled. On a local scale however, bending of the pipe between rollers is determined by the interaction between weight and tension and is closer to load controlled. For a 100% displacement controlled condition, the stinger would have to consist of a more or less continuous ramp with a constant radius whereas stingers are equipped with rollerboxes and as such intermittently support the pipe.

DNV-OS-F101 provides buckling checks for load controlled and displacement controlled conditions (Section 5 D600). Initially, this means that either 100% load controlled or 100% displacement controlled conditions can be checked. JIP report DNVGL 2014-0185 [3] recommends the use of load controlled conditions while applying a reduced γ_c of 0.8 for overbend conditions in order to account for a certain extent in terms of being partially displacement controlled (PDC). It should be noted that this reduced γ_c is not project or case dependent.

1.3.1. THESIS GOAL

This master thesis aims at developing and implementing a method to determine the (project specific) level of partial displacement control, and to apply this PDC percentage to the buckling checks in DNV-OS-F101 such that the current load controlled and displacement controlled formulations can be used to include this project specific PDC condition.

1.3.2. THESIS RESEARCH QUESTIONS

The questions of this research project are defined as follows:

1. How can the level of displacement control in the stinger overbend situation be determined?
2. How can this level of displacement control be implemented in a buckling check?
3. How is the level of displacement control influenced by the pipe and stinger configuration?

1.4. APPROACH AND SCOPE OF WORK

The research subject is first approached by performing a literature study on partially displacement controlled conditions in situations found during and after the pipelaying process. From this literature study a general method for determining the level of displacement control is found. With the findings from the literature study a modification of the found general method is proposed to make it applicable to the stinger overbend situation. Next, steps are taken to validate the proposed modified method using detailed finite element models in the Abaqus software. Lastly, the modified method is used for parametric analyses to determine the effect of pipe and stinger configurations on the level of displacement control.

The scope of this research is limited to the partially displacement controlled conditions on the

stinger overbend during S-lay pipeline installation. For the development of a method for determining the level of displacement control and its validation only homogeneous, continuous pipeline is considered. This means that the effect of discontinuities in the pipeline such as inline structures or welds fall outside the scope. Furthermore, only bare pipelines without concrete coating are investigated. Finally, all analyses in this thesis are taken as static situations.

1.5. STRUCTURE OF WORK

Chapter 2, DNV codes and standards, covers the current local buckling checks by DNV for pipeline installation. The definitions of load control and displacement control and its implications on pipeline design are explained. The buckling checks are explained step by step and all relevant equations and factors are explained in detail. In the next chapter past publications on partial displacement control in pipelaying are discussed. The weak link method for determining the level of displacement control is explained and the elaborations by various authors on this method are described.

With the background from the DNV codes and standards and the findings from the literature review a method is proposed in chapter 4. A modification is proposed to make the method suitable for analysis in the stinger overbend region. The steps necessary to compute the degree of displacement control, to calculate the partial displacement buckling limit, and to determine the new case-specific partial displacement control buckling check are explained. Also, the implementation of this method in the software package used at Allseas is briefly touched upon. In chapter 5 the method proposed in the previous chapter is validated using the Abaqus FE software. Buckling in detailed shell element models is compared to the calculated buckling limit in less detailed beam element models. Lastly, the results from the Abaqus models are compared to results from an OFFWIN model. Using the validated method in OFFWIN, several models with varying parameters are analysed to study the effect of pipeline, stinger, and laying characteristics on the level of displacement control in chapter 6.

In the final chapter the results of the research are commented upon. Recommendations are given for future research and for the practical implementation of the designed method and obtained results in future engineering projects.

In the appendices several mathematical calculations and derivations are explained in detail.

2

DNV CODES AND STANDARDS

The Submarine Pipeline Systems Offshore Standard by Det Norske Veritas (DNV) gives criteria and recommendations on concept development, construction, operation and abandonment of submarine pipeline systems. In the past DNV used an Allowable Stress Design (ASD) method for pipelines, meaning that the stress in the pipeline should stay below a predetermined fraction (based on safety factors) of the yield stress. The stress has to stay below the yield stress, which means that using this method one can only design in the elastic region of your material. The DNV submarine pipelines standard was a pioneer code that introduced the Limit-State Design (LSD), also known as Load and Resistance Factor Design (LRFD), to replace the ASD method. This method focuses on the conditions that threaten the safety and operability of the structure. The basis of this design method is that it compares the load effects to the design capacities. The load effects consists of all the characteristic functional, interference, environmental and accidental loads, multiplied by their corresponding load effect factors. The design capacity is determined from the resistance capacity divided by the corresponding material and safety class factors. The philosophy behind the LSD method is illustrated in figure 2.1. For some cases, the stresses may be allowed to exceed the limits from ASD. Besides limits on stress, limits on strains may also be determined to protect the structure from failure due to buckling or rupture.

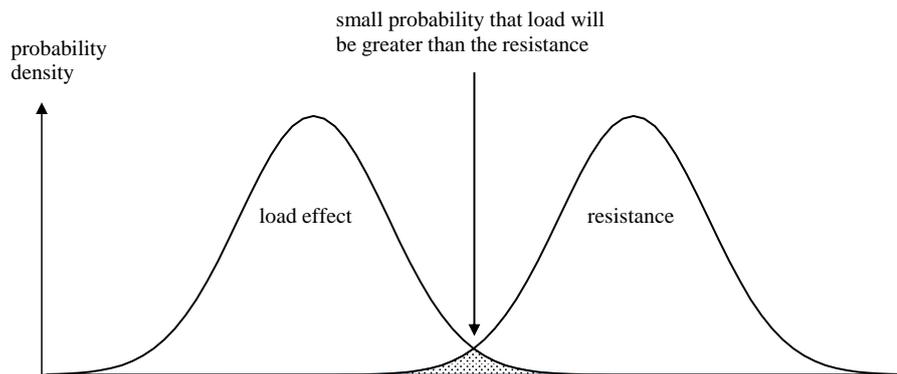


Figure 2.1: Limit state design

2.1. DEFINITIONS

For the limit state criteria for local buckling with combined loading a differentiation is made between checks for Load Controlled conditions (LC) and Displacement Controlled conditions (DC). Different limit states apply to these two conditions. The definitions DNV [2] gives for load controlled and displacement controlled conditions are as follows:

- A load controlled condition is one in which the structural response is primarily governed by the imposed loads.
- A displacement controlled condition is one in which the structural response is primarily governed by imposed geometric displacements.

It is important to distinguish between these two loading conditions when analysing the response of a structure to a certain loading situation. The difference in structure response to the two loading conditions can be illustrated with a material stress-strain curve such as in figure 2.2. In the elastic (linear) region the increase in stress is proportional to the increase in strain. In the plastic (non-linear) region a small increase in stress leads to a large increase in strain and vice versa a large increase in strain leads to a small increase in stress. If the loading condition is load controlled (i.e. travelling along the vertical axis), it can be seen that after the yield point the strain rapidly increases for increasing stress. The stress can be increased up until the Ultimate Tensile Stress (UTS) point. When the stress increases further the material will fail. If the loading condition is displacement controlled the material can be utilised beyond the UTS point, up to the fracture stress point. In essence this means that for a situation where the stress and strains are just before the UTS point, a load controlled condition will be very close to failure and caution is needed while for a displacement controlled condition the point of failure is still far away.

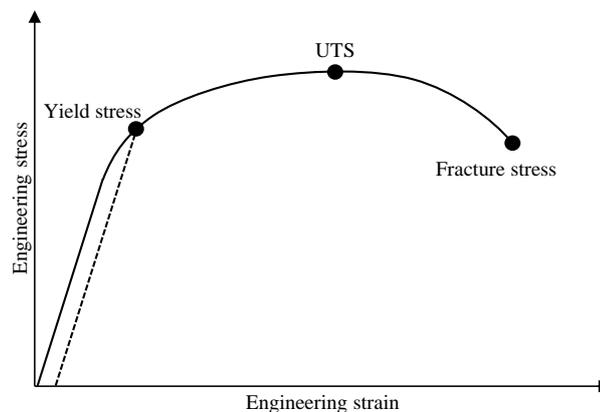


Figure 2.2: Stress-strain curve

The design capacities for LC and DC conditions follow from the characteristic capacity of the pipe. In figure 2.3 a moment-curvature curve is shown, which is similar in shape as a stress-strain curve. It can be seen that when determining the LC and DC capacities with comparable safety factors, the DC limit allows higher utilisation than the LC limit. DNV notes that if there are uncertainties about the loading condition, a load controlled design criterion can always be applied in place of a displacement controlled design criterion.

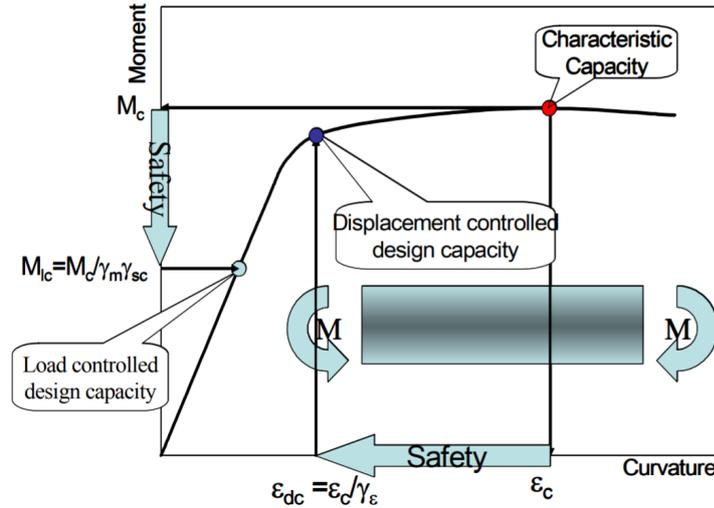


Figure 2.3: Moment-curvature design capacity [4]

2.2. PARTIAL DISPLACEMENT CONTROL

The design criteria given by DNV are for either a fully load controlled condition or a fully displacement controlled condition. An example of a fully load controlled condition would be a cantilever beam with a weight hanging from the end. A fully displacement controlled condition is a cantilever beam with a screwjack deforming the end of the beam. During pipelaying most, if not all, situations fall in between load and displacement control. And as such, strictly speaking, only the load controlled criterion should be used. Using this conservative approach will lead to increased costs and engineering challenges as the allowable utilisation of the pipe is reduced. It is therefore desirable to determine a new design capacity for situations in-between full load control and full displacement control. DNV states that: “The answer to the question on if a condition is load controlled or displacement controlled is impossible since the question is wrong, the question should be; how can one take partial benefit of that a condition is partially displacement controlled?”

2.3. LOCAL BUCKLING CHECKS – COMBINED LOADING CRITERIA

The DNV-OS-F101 Offshore Standard gives four local buckling checks for combined loading criteria. There are checks for load controlled conditions with either internal or external overpressure and checks for displacement controlled conditions with either internal or external overpressure. For a pipeline installation with an empty pipe, only the checks for external overpressure are of importance. In this section the background and derivation of the buckling checks are covered, as well as one proposed method by DNV to deal with partially displacement controlled conditions.

2.3.1. LOAD CONTROLLED CONDITION

Pipe members subjected to bending moment, effective axial force and external overpressure shall be designed to satisfy the following criterion at all cross sections:

$$\left(\gamma_m \cdot \gamma_{SC} \cdot \frac{|M_{Sd}|}{\alpha_c \cdot M_p(t_2)} + \left(\frac{\gamma_m + \gamma_{SC} \cdot S_{Sd}}{\alpha_c \cdot S_p(t_2)} \right)^2 \right)^2 + \left(\gamma_m \cdot \gamma_{SC} \cdot \frac{p_e - p_{min}}{p_c(t_2)} \right)^2 \leq 1 \quad (2.1)$$

The background and derivation of the load controlled buckling check is given by Collberg and Lev-

old [5].

Where: M_{Sd} is the design moment
 S_{Sd} is the design effective axial force
 p_e is the external pressure
 p_{min} is the minimum internal pressure that can be sustained
 p_c is the characteristic collapse pressure
 M_p is the plastic moment capacity
 S_p is the plastic effective axial force capacity
 γ_m is the material resistance factor
 γ_{SC} is the safety class resistance factor
 α_c is the flow stress parameter

The plastic capacities of the pipe are given by the two following equations:

$$M_p(t) = f_y \cdot (D - t)^2 \cdot t \quad (2.2)$$

$$S_p(t) = f_y \cdot \pi \cdot (D - t) \cdot t \quad (2.3)$$

Where: D is the nominal outer diameter
 t is the nominal wall thickness of the pipe
 f_y is the characteristic yield stress to be used in design

The characteristic yield stress to be used in design is determined as:

$$f_y = (SMYS - f_{y,temp}) \cdot \alpha_U \quad (2.4)$$

Where: $SMYS$ is the specified minimum yield stress
 $f_{y,temp}$ is the de-rating value due to the temperature of the yield stress
 α_U is the material strength factor

The values for the material strength factor α_U are given in table 2.1. The normal value must be used for materials with a normal degree of variability. Supplementary requirement U may be used when there is increased confidence in yield strength.

Table 2.1: Material strength factor

Factor	Normally	Supplementary requirement U
α_U	0.96	1.00

The flow stress parameter α_c is calculated as:

$$\alpha_c = (1 - \beta) + \beta \cdot \frac{f_u}{f_y} \quad (2.5)$$

with

$$\beta = \frac{60 - D/t}{90} \quad (2.6)$$

And

$$f_u = (SMTS - f_{u,temp}) \cdot \alpha_U \quad (2.7)$$

Where: f_u is the characteristic tensile strength to be used in design
 $SMTS$ is the specified minimum tensile strength
 $f_{u,temp}$ is the de-rating value due to temperature of the tensile strength

The characteristic collapse pressure is calculated as:

$$(p_c(t) - p_{el}(t)) \cdot (p_c(t)^2 - p_p(t)^2) = p_c(t) \cdot p_p(t) \cdot f_o \cdot \frac{D}{t} \quad (2.8)$$

With

$$p_{el}(t) = \frac{2 \cdot E \cdot \left(\frac{t}{D}\right)^3}{1 - \nu^2} \quad (2.9)$$

$$p_p(t) = f_y \cdot \alpha_{fab} \cdot \frac{2 \cdot t}{D} \quad (2.10)$$

$$f_o = \frac{D_{max} - D_{min}}{D} \quad (2.11)$$

Where: p_{el} is the elastic collapse pressure
 E is the Young's modulus
 ν is the Poisson's ratio
 p_p is the plastic collapse pressure
 α_{fab} is the fabrication factor
 f_o is the ovality
 D_{max} is the greatest measured inside or outside diameter
 D_{min} is the smallest measured inside or outside diameter

The fabrication factor depends on the fabrication process of the pipe. The fabrication factor for specific processes are given in table 2.2.

Table 2.2: Fabrication factor

Pipe	Seamless	UO & TRB & ERW	UOE
α_{fab}	1.00	0.93	0.85

The design moment and effective axial force are calculated as:

$$M_{Sd} = M_F \cdot \gamma_F \cdot \gamma_c + M_E \cdot \gamma_E + M_I \cdot \gamma_F \cdot \gamma_c + M_A \cdot \gamma_A \cdot \gamma_c \quad (2.12)$$

$$S_{Sd} = S_F \cdot \gamma_F \cdot \gamma_c + S_E \cdot \gamma_E + S_I \cdot \gamma_F \cdot \gamma_c + S_A \cdot \gamma_A \cdot \gamma_c \quad (2.13)$$

Where: Subscript *F* indicates a functional load
 Subscript *E* indicates an environmental load
 Subscript *I* indicates an interference load
 Subscript *A* indicates an accidental load
 γ_c is the condition load effect factor

For pipeline installation the Ultimate Limit State is checked, the corresponding load effect factor combinations are shown in table 2.3.

Table 2.3: Load effect factors

Limit state/ Load combination	Load effect combination	Functional loads γ_F	Environmental loads γ_E	Interference loads γ_F	Accidental loads γ_A
ULS	a: System check	1.2	0.7	-	-
	b: Local check	1.1	1.3	1.1	-

The condition load effect factor γ_c applies to the functional, interference, and accidental loads but not the environmental loads and is dependent on the condition. The factors for several conditions are given in table 2.4.

Table 2.4: Condition load effect factor

Condition	γ_c
Pipeline resting on uneven seabed	1.07
Reeling on and J-tube pull-in	0.82
System pressure test	0.93
Otherwise	1.00

Finally, the material resistance factor γ_m and the safety class factor γ_{SC} are given in tables 2.5 and 2.6 respectively. The material resistance factor is dependent on the limit state category. The safety class factor is dependent on the potential failure consequences of the pipeline and the situation.

Table 2.5: Material resistance factor

Limit state category	SLS/ULS/ALS	FLS
γ_m	1.15	1.00

Table 2.6: Safety class factor

Safety class	γ_{SC}		
	Low	Medium	High
Pressure containment	1.046	1.138	1.308
Other	1.04	1.14	1.26

2.3.2. DISPLACEMENT CONTROLLED CONDITION

Pipe members subjected to longitudinal compressive strain (bending moment and axial force) and external overpressure shall be designed to satisfy the following criterion at all cross sections:

$$\left(\frac{\epsilon_{Sd}}{\frac{\epsilon_c(t,0)}{\gamma_\epsilon}} \right)^{0.8} + \frac{p_e - p_{min}}{\gamma_m \cdot \gamma_{SC}} \leq 1 \quad (2.14)$$

Where: ϵ_{Sd} is the design compressive strain
 ϵ_c is the characteristic bending strain resistance
 γ_ϵ is the strain resistance factor

The design compressive strain is calculated as:

$$\epsilon_{Sd} = \epsilon_F \cdot \gamma_F \cdot \gamma_c + \epsilon_E \cdot \gamma_E + \epsilon_I \cdot \gamma_F \cdot \gamma_c + \epsilon_A \cdot \gamma_A \cdot \gamma_c \quad (2.15)$$

The characteristic bending strain resistance is given by

$$\epsilon_c(t, p_{min} - p_e) = 0.78 \cdot \left(\frac{t}{D} - 0.01 \right) \cdot \left(1 + 5.75 \cdot \frac{p_{min} - p_e}{p_b(t)} \right) \cdot \alpha_h^{-1.5} \cdot \alpha_{gw} \quad (2.16)$$

Which for the displacement controlled buckling check reduces to

$$\epsilon_c(t, 0) = 0.78 \cdot \left(\frac{t}{D} - 0.01 \right) \cdot \alpha_h^{-1.5} \cdot \alpha_{gw} \quad (2.17)$$

Where: p_b is the pressure containment resistance
 α_h is the strain hardening factor
 α_{gw} is the girth weld factor

The strain resistance factor γ_ϵ is dependent on the potential failure consequences and is shown in table 2.7.

Table 2.7: Strain resistance factor

	Safety class		
	Low	Medium	High
γ_ϵ	2.0	2.5	3.3

The strain hardening factor is given as

$$\alpha_h = \left(\frac{R_{t0,5}}{R_m} \right)_{max} \quad (2.18)$$

Where: $R_{t0,5}$ is the yield strength
 R_m is the tensile strength
 $\left(\frac{R_{t0,5}}{R_m} \right)$ has a maximum value of 0.93

The girth weld has a detrimental effect on the compressive strain capacity of a pipe. If no other information exists about the girth weld effect of the specific pipe, the girth weld factor in the figure 2.4 is proposed by DNV.

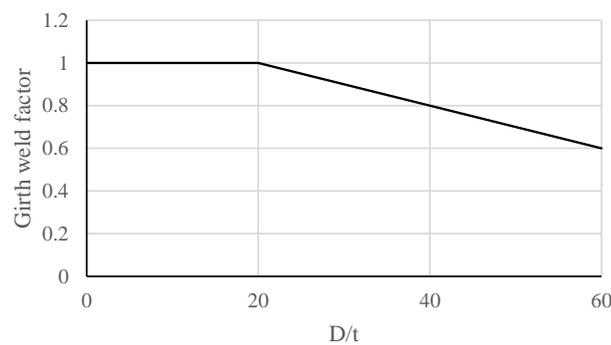


Figure 2.4: Girth weld factor

Or in equation form:

$$\alpha_{gw} = \begin{cases} 1, & \text{if } D/t \leq 20 \\ 1 - \frac{D/t - 20}{100}, & \text{if } 20 \leq D/t \leq 60 \end{cases} \quad (2.19)$$

2.3.3. LOCAL BUCKLING CHECK IN THE OVERBEND

DNVGL-ST-F101 [6] gives two alternatives to check for local buckling in the overbend, acknowledging the partially displacement controlled conditions of the situation. The first option is to use the load controlled buckling check of equation 2.1 with a modified condition load effect γ_c of 0.8, allowing for a higher utilisation. This modified condition load effect is not case-dependant or project specific, and such will not take the wide differences between stingers into account. The second option is to use the displacement controlled buckling check of equation 2.14 with modified safety factors. The modification of the safety factors is still to be determined considering the effect of point load and varying stiffness.

2.3.4. COMPARISON OF LOCAL BUCKLING CHECKS

To illustrate the difference in local buckling limits that the different local buckling checks give, the limiting moments for a 30 inch diameter pipe with diameter to wall thickness (D/t) ratios between 15 and 45 on the stinger overbend are calculated. The material used in this example is an X65 steel. As this example focusses on the stinger overbend, the pressure component in equations 2.1 and 2.14

can be taken as 0. For the sake of simplicity a static situation is considered, meaning that the dynamic environmental loads in equations 2.12, 2.13, and 2.15 are 0. For this example the ULS system check load effect combination (table 2.3) and a medium safety class are used. The top lay tension is taken as 2000 kN. All relevant input for the calculation of the limiting moments is shown in table 2.8.

Table 2.8: Input data for buckling check comparison

Description	Variable	Value	Unit	Source
Outer diameter	D	0.762	m	
Wall thickness	t	0.0169 - 0.0508	m	
Yield stress	$SMYS$	448	MPa	
Tensile strength	$SMTS$	530	MPa	
Functional axial force	S_F	2000	kN	
Material strength factor	α_U	0.96	–	table 2.1
Functional load factor	γ_F	1.2	–	table 2.3
Safety class factor	γ_{SC}	1.14	–	table 2.6
Strain resistance factor	γ_ϵ	2.5	–	table 2.7
Strain hardening factor	α_h	0.85	–	equation 2.18

The three buckling checks that are compared are a load controlled buckling check with a γ_c of 1.0, a load controlled buckling check with a γ_c of 0.8, and a displacement controlled buckling check with a γ_c of 1.0. With the pressure component removed from the buckling check and the dynamic environmental loads at 0, the limiting functional moment for the load controlled buckling checks can be calculated as follows:

$$M_F = \left(1 - \left(\frac{\gamma_m \cdot \gamma_{SC} \cdot S_F \cdot \gamma_F \cdot \gamma_c}{\alpha_c \cdot S_p} \right)^2 \right) \cdot \frac{\alpha_c \cdot M_p}{\gamma_m \cdot \gamma_{SC} \cdot \gamma_F \cdot \gamma_c} \quad (2.20)$$

Where: M_F is the functional moment

With the pressure component removed from the buckling check and the dynamic environmental loads at 0, the limiting functional strain for the displacement controlled buckling check can be calculated as follows:

$$\epsilon_F = \frac{\epsilon_c}{\gamma_\epsilon \cdot \gamma_F \cdot \gamma_c} \quad (2.21)$$

Where: ϵ_F is the functional strain

The functional strain calculated with equation 2.21 is converted to a curvature using the following relation:

$$K = \frac{\epsilon}{r} \quad (2.22)$$

Where: K is the curvature
 ϵ is the bending strain
 r is the pipe radius

The resulting functional curvatures are converted to functional moments using the moment-curvature relations of the various pipe diameter and wall thickness combinations.

The resulting limiting functional moments versus the diameter to wall thickness ratio for the two load controlled buckling checks and the displacement controlled buckling check are shown in figure 2.5. As can be seen the displacement controlled buckling check consistently gives a higher allowable utilisation of the pipeline than the two load controlled buckling checks. The load controlled buckling check with a γ_c of 0.8, to account for partially displacement controlled conditions, allows a higher utilisation of the pipeline compared to the standard load controlled buckling check.

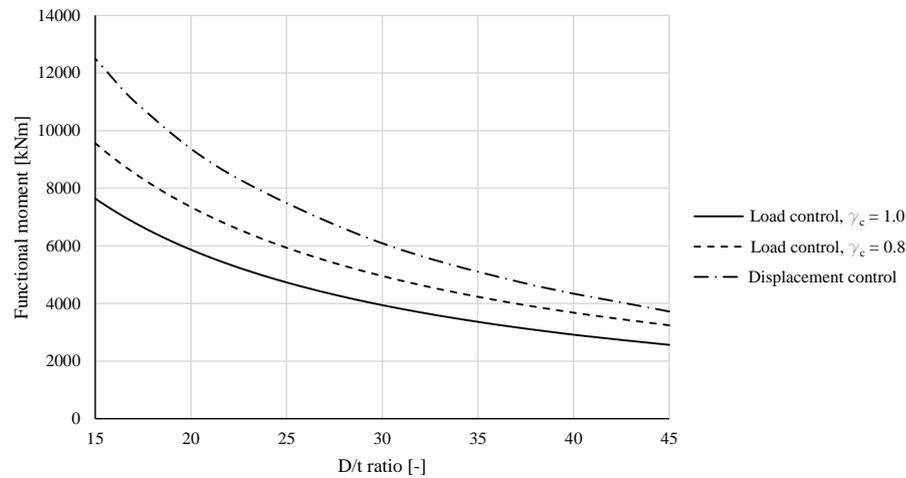


Figure 2.5: Maximum allowable moments per buckling check

3

LITERATURE REVIEW

Ever since DNV introduced the LRFD method and made a distinction between load controlled and displacement controlled situations in the local buckling check, there has been discussion on what is load controlled versus displacement controlled. The lack of significant progress in this discussion is believed by Collberg, Aronson, Palmer and Hahn [4] to be due to the focus on the difference between load and displacement controlled situations with the implicit assumption that the situation must be one or the other. In reality many conditions are neither purely load controlled nor purely displacement controlled. In the past years several authors have published papers looking into the situations in-between purely load controlled and purely displacement controlled, the so-called partially displacement controlled situations. The focus of the majority of these papers has been on the sagbend part of pipeline installation, a situation traditionally looked at as purely load controlled. The goal being to create a method to determine how partially displacement controlled a situation is and to prove that the sagbend region is not purely load controlled. Using displacement controlled limits, loads on the structure can exceed the loads associated with load controlled limits without the system failing, thus allowing further utilisation of the pipe in the design (Hahn, Palmer, and Collberg [7]).

Collberg et al have proposed a concept to analyse the level of partial displacement control in the sagbend when the pipeline deformation stays in the elastic region. This concept is based on the belief that the most likely cause of failure in the sagbend is varying properties of the pipeline, meaning that failure will occur when a weak section of pipe is located at the most exposed pipeline section. This principle is referred to as the weak link principle. Their proposed concept is based on this principle and can be considered as an extended version of limit state based design philosophy. This concept will be referred to as the weak link method and will be explained in detail in the next section. Karjadi, Walker, and El-Gebaly [8] expand on the method by Collberg by adding a way of calculating the level of partial displacement control and resulting buckling limit in the non-linear moment-curvature region. Christensen [9] applied the method of Collberg to the lateral buckling of submarine pipelines and analysed the effect that the degree of stiffness reduction in the weak joint has on the calculated level of partial displacement control.

3.1. WEAK LINK METHOD

In this section the weak link method as proposed by Collberg et al and elaborated by various authors will be explained in detail.

To determine the level of partial displacement control, two analyses are performed. First, a global analysis with nominal pipe properties will give the location of maximum moment in the sagbend configuration. A second analysis with a weak link joint located at this point of maximum moment is performed next, giving different values for the moment and curvature at the location of maximum moment than the first analysis. Figure 3.1 shows a sketch made for the DNV JIP Report [3] which gives an exaggerated example of the effect of the weak link on the analysis. According to Collberg et al the weak link joint should represent the likely maximum stiffness difference of the pipeline. For the analysis this means a weak link will have a reduced wall thickness and yield stress compared to the nominal pipeline. The difference in moment and curvature between the first and second analysis indicates the level of partial displacement control.

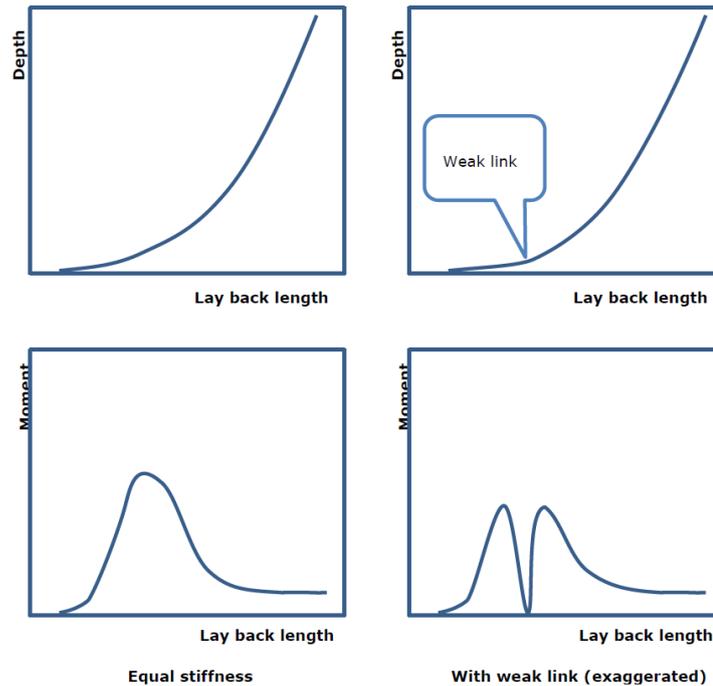


Figure 3.1: Sketch of sagbend performance with and without weak link. [3]

The maximum moments and corresponding curvatures from the first and second analysis can be plotted on the moment-curvature curves. The curve for the second analysis will differ from the first analysis due to the weaker pipe section. The moment-curvature curves for the first (named nominal joint) and second (named weak joint) analysis are shown in figure 3.2. The maximum moment and corresponding curvature of the global analysis is denoted by θ_0 and the maximum moment and curvature of the second analysis is denoted by C .

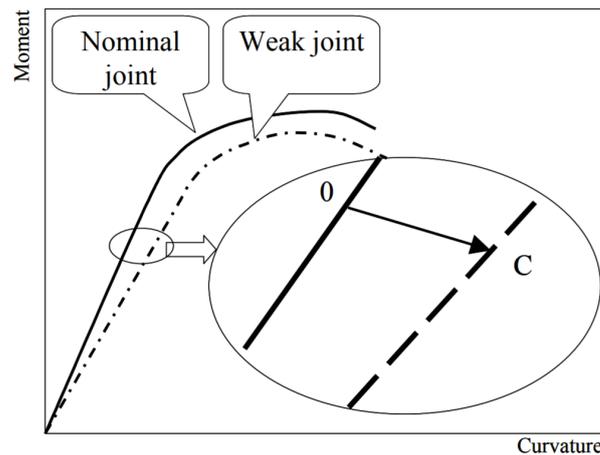


Figure 3.2: Illustration of the maximum moment for the nominal and weak link analysis [4]

Considering the definitions of load and displacement control as given in section 2.1, the expected shift in moment and curvature for fully load controlled and fully displacement controlled situations can easily be determined.

- In a totally load controlled condition the structural response is governed by the imposed loads, meaning that the moment is fixed. The only possible shift in moment and curvature in the weak link analysis is along a horizontal line to point B, as shown in figure 3.3.
- In a totally displacement controlled condition the structural response is governed by imposed geometric deformation, meaning that the curvature is fixed. The only possible shift in moment and curvature in the weak link analysis is along a vertical line to point A, as shown in figure 3.3.
- For a partially displacement controlled situation the point of moment and curvature (denoted by C) is expected to lie in-between the points for full load or displacement control. If the increase in curvature is limited, the condition can be identified as mainly DC. If the moment is mostly maintained over the weak link section, it is mainly LC.

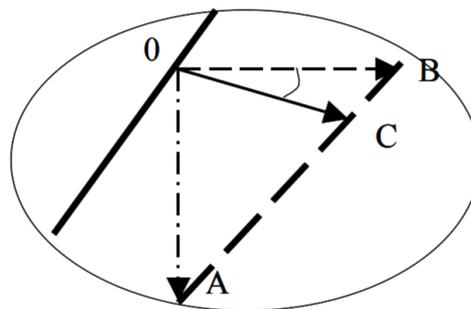


Figure 3.3: Illustration of load controlled and displacement controlled behaviour [4]

Collberg et al introduce the factor z to represent the degree of displacement control, defined as:

$$z = \frac{BC}{BA} \quad (3.1)$$

For the linear moment-curvature region, considered in Collberg, this can be calculated by taking the moments at points A, C, and 0.

$$z = \frac{M_0 - M_C}{M_0 - M_A} \quad (3.2)$$

Equation 3.2 gives $z = 0$ for a fully load controlled condition and $z = 1$ for a fully displacement controlled condition.

The new maximum allowable moment belonging to the partially displacement controlled situation is calculated by linear interpolation between the load controlled and displacement controlled buckling limit moment:

$$M_{pdc} = M_{lc} + (M_{dc} - M_{lc}) \cdot z \quad (3.3)$$

Where: M_{pdc} is the allowable moment for the partially displacement controlled situation
 M_{lc} is the allowable moment for the load controlled situation
 M_{dc} is the allowable moment for the displacement controlled situation

Calculating the z -value by comparing moments or by comparing curvatures would lead to different results in the non-linear region. To account for the non-linearity in the moment-curvature relation the method presented by Karjadi et al [8]. should be considered. The factor z is calculated as:

$$z = \frac{L_w}{L_{dc}} \quad (3.4)$$

Where: L_w is the arc length between points C and B
 L_{dc} is the arc length between points A and B

The new buckling limit for the partially displacement controlled condition is determined by Karjadi et al as a strain capacity, given as:

$$\epsilon_{pdc} = \epsilon_{lc} + z \cdot (\epsilon_{dc} - \epsilon_{lc}) \quad (3.5)$$

Where: ϵ_{pdc} is the strain capacity for the partially displacement controlled situation
 ϵ_{lc} is the strain capacity for a load controlled situation
 ϵ_{dc} is the strain capacity for a displacement controlled situation

Due to the hogging shape of the non-linear moment-curvature relation, linear interpolation along the moment axis will result in a more conservative estimate of a PDC buckling limit compared to linear interpolation along the strain axis.

Christensen [9] applied the method proposed by collberg et al to the lateral buckling of pipelines on the seabed. Christensen noted that the pipe stiffness can be reduced by any arbitrary value for the weak link analysis as long as it is close to the base stiffness. However, if the stiffness is reduced too much, the load control line (0 – B) in figure 3.3 may never intersect the reduced stiffness curve (point B would not exist). Furthermore, even though the stiffness may be reduced by any value, the chosen value has an impact on the calculated level of displacement control. Reducing the stiffness too much may result in an over prediction of the percentage displacement control. Therefore it is important to keep the variations in stiffness close to the base case.

4

METHOD PROPOSAL

In this chapter a modification and elaboration of the weak link method, as described in the previous chapter, to make it suitable for the overbend stinger region is proposed. First, the proposed method for determining the degree of displacement control is explained. Next, a way of determining the new partial displacement buckling limit is proposed. Thirdly, the method and necessary steps are summarised. Finally, the implementation of the proposed method in the software used by Allseas is covered.

4.1. DEGREE OF DISPLACEMENT CONTROL

The weak link method discussed in the previous chapter has been designed with the installation of pipe using J-lay mode in mind. Christensen has already shown that the method can be applied in a different case, namely the lateral buckling of pipes on the seabed. The original method proposed by Collberg, combined with the findings by other authors, will be adapted for the overbend region of S-lay installation. One of the obvious differences between the S-lay overbend and the J-lay sagbend (or S-lay sagbend for that matter), is that in the sagbend there is a single location where the moment and curvature is maximum. In the overbend region there are several local maxima and minima of bending and curvature along the stinger section. Figure 4.1 sketches how the moment is distributed along the stinger. During pipeline installation the pipe experiences local peaks in moment and bending on top of the rollerboxes. In the sagbend region the weak link method gives a single z-value, a single level of partial displacement control, for the entire sagbend based on the peak moment and bending. Using this method in the overbend region will consequently result in multiple z-values, one for each rollerbox, and thus multiple levels of displacement control along the stinger. The z-values are calculated for the rollerbox locations because these are the locations where the moment and bending of the pipe is the largest, and thus the pipeline is most susceptible to local buckling at these locations. To determine the z-value for each rollerbox, the weak link must span the entire stinger length.

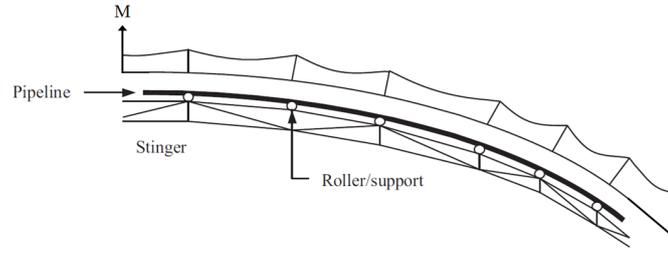


Figure 4.1: Moment distribution over the stinger [10]

To capture the effect of the magnitude of variation in stiffness, multiple variations in stiffness are used in the analysis. Two variations in pipe stiffness are modelled, one where the pipe stiffness is slightly lower than the nominal pipe, and one where the pipe stiffness is slightly higher than the nominal pipe. This will give for each rollerbox a range of the z-value, showing the sensitivity to stiffness variations. Following the findings by Christensen, it is expected that the weaker pipe variation will give a higher z-value estimation than the stronger pipe variation.

For calculating the z-value the method proposed by Karjadi (equation 3.2) will be used. The benefits of this method compared to the method proposed by Collberg (equation 3.4), is that it can be used in both the linear and non-linear region, whereas the method by Collberg will only give correct results in the linear region.

4.2. PDC BUCKLING CHECK

4.2.1. PDC BUCKLING LIMIT

The new buckling limit for the partially displacement controlled condition follows from the z-value, the load controlled buckling limit, and the displacement controlled buckling limit. For a given pipe geometry, material properties, and installation conditions a buckling limit can be calculated using the DNV equations (equation 2.1 and 2.14). For the load controlled limit the effect of pressure on the pipe buckling is assumed to be negligible due to the small water depth the pipe experiences while on the stinger. This reduces equation 2.1 to the following:

$$\left(\gamma_m \cdot \gamma_{SC} \cdot \frac{|M_{sd}|}{\alpha_c \cdot M_p(t_2)} + \left(\frac{\gamma_m + \gamma_{SC} \cdot S_{sd}}{\alpha_c \cdot S_p(t_2)} \right)^2 \right)^2 \leq 1 \quad (4.1)$$

The design effective axial force S_{sd} follows from the installation conditions. The safety factors follow from the material and installation conditions. The only unknown is the design moment M_{sd} . Solving equation 4.1 for 1 gives the maximum allowable design moment for a load controlled condition.

For the displacement controlled limit the effect of pressure is also assumed to be negligible. Equation 2.14 is thus reduced to:

$$\left(\frac{\epsilon_{sd}}{\frac{\epsilon_c(t_2,0)}{\gamma_\epsilon}} \right)^{0.8} \leq 1 \quad (4.2)$$

The only unknown in this equation is the design strain. Solving equation 4.2 for 1 gives the maximum allowable design strain for a displacement controlled condition. Dividing the design strain by

the pipe radius gives the design curvature.

$$\frac{\epsilon_{Sd}}{r} = K_{Sd} \quad (4.3)$$

Where: ϵ_{Sd} is the design strain
 r is the pipe radius
 K_{Sd} is the design curvature

When the z -value is calculated and the LC and DC buckling limits are known, the partially displacement controlled buckling limit can be found. The methods proposed by Collberg et al (equation 3.3) and Karjadi et al (equation 3.5) will give different results if the buckling limits lie in the non-linear region of the moment-curvature relation. An example is given in figure 4.2, for a hypothetical situation where a PDC buckling limit of 50% displacement control is found.

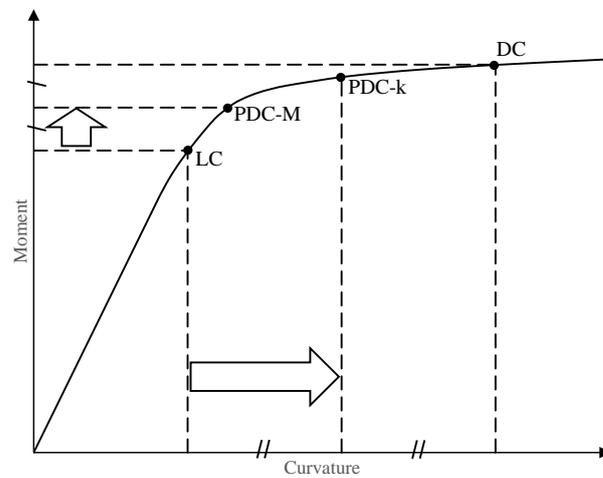


Figure 4.2: Methods of calculating partial displacement buckling limit

Where: LC is the load controlled buckling limit
 DC is the displacement controlled buckling limit
 $PDC - M$ is the partially displacement controlled buckling limit calculated along the moment axis
 $PDC - k$ is the partially displacement controlled buckling limit calculated along the curvature axis

As can be seen, due to the hogging shape of the moment-curvature relation the method of Karjadi et al, where the new limit is calculated along the curvature (or strain) axis always gives an equal or higher limit than by using Collberg et al method and calculating the limit along the moment axis. The PDC buckling limit calculated along the curvature axis (Karjadi's method) is denoted by PDC-k. The buckling limit calculated along the moment axis (Collberg's method) is denoted by PDC-M. A third method is proposed to calculate the PDC buckling limit using curve lengths, similar to the method by Karjadi to calculate the z -value (equation 3.4). An illustration of this method is shown in figure 4.3.

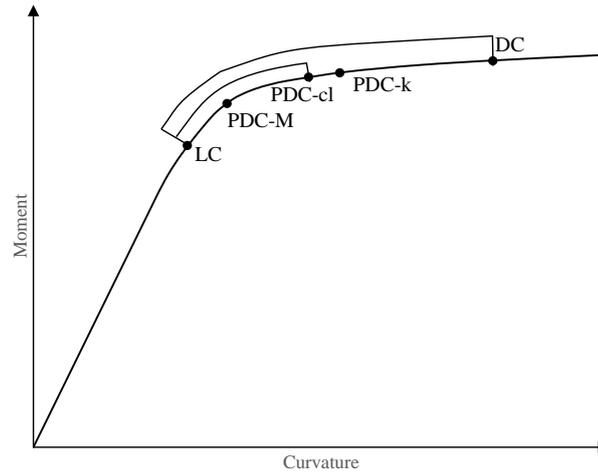


Figure 4.3: Methods of calculating partial displacement buckling limit using curve lengths

Where: $PDC - cl$ is the partially displacement controlled buckling limit calculated using curve lengths

Using the ratio between curve lengths to calculate the new PDC limit location the discussion on whether to calculate the partially displacement controlled buckling limit along the moment or curvature axis can be avoided. If all points lie in the elastic region all three methods will give the same result. Using the curve length method in the non-linear region will result in a PDC location in between the Collberg and Karjadi method. Considering that the z -value has also been calculated using curve lengths, also using curve lengths for the determination of the PDC buckling limit is a more consistent approach. In equation form this would be:

$$z = \frac{\|PDC - LC\|}{\|DC - LC\|} \quad (4.4)$$

Where: $\|PDC - LC\|$ is the curve length between the PDC and LC points
 $\|DC - LC\|$ is the curve length between the DC and LC points

With the z -value and the locations of LC and DC known, the location of PDC can be calculated.

4.2.2. PDC BUCKLING CHECK FACTOR

To implement this newly found PDC buckling limit in the design phase of pipeline installation it should be translated to a buckling check. As the assumption is that most partially displacement controlled conditions on the stinger will be closer to displacement controlled than load controlled, a modification of the displacement controlled (equation 2.14) buckling check is proposed.

From the calculated location of the PDC limit follows the maximum allowable curvature and strain. The DC location in figure 4.3 has a design strain for which the displacement controlled buckling check equals 1. To create the PDC buckling check, the DC buckling check should be modified such that it equals 1 at the design strain corresponding to the PDC-cl point in figure 4.3. To modify the buckling equation a partial displacement control factor, γ_{pdc} , is introduced. The modified DC buckling check has the following form:

$$\left(\frac{\gamma_{pdc} \cdot \epsilon_{sd}}{\frac{\epsilon_c(t_2,0)}{\gamma_\epsilon}} \right)^{0.8} + \frac{p_e - p_{min}}{\gamma_m \gamma_{sc}} \leq 1 \quad (4.5)$$

with

$$\gamma_{pdc} = \frac{\epsilon_{pdc}}{\epsilon_{dc}} \quad (4.6)$$

Where: ϵ_{pdc} is the strain belonging to the PDC limit
 ϵ_{dc} is the strain belonging to the DC limit

4.3. METHOD SUMMARY

In summary, the steps needed to determine the partial displacement control buckling limit are as follows:

- First, a finite element model composed of Euler-Bernoulli beam type elements of the given installation situation is created. Nominal pipe geometry and material properties, stinger configuration, water depth, and laying tension are used as input. Pipe stiffness variations are created for the weak link method. The output needed for the weak link method are the moments and strains at the rollerboxes, and the moment-curvature relations of the pipes.
- Using the methods described in this chapter and the output from the finite element model, the z-values per rollerbox are calculated. The moment and curvature shifts at the rollerboxes of the pipe stiffness variations are compared to the moment and curvature of the nominal pipe. The expected responses for fully load controlled and fully displacement controlled conditions are calculated (figure 3.3). From this data the level of partial displacement control per rollerbox is calculated.
- With the nominal pipe geometry and material properties in combination with the DNV guidelines, the local buckling limits for load controlled and displacement controlled situations are determined.
- With the z-value and the LC and DC limits from the two previous steps, the PDC limit is calculated.
- With the PDC limit known, the γ_{pdc} is calculated and the buckling check is modified for the PDC situation.
- Finally, the original installation situation can be checked with the newly calculated PDC buckling limit. If the model passes the PDC check the configuration is acceptable. If not, the configuration must be modified and the previous steps taken again.

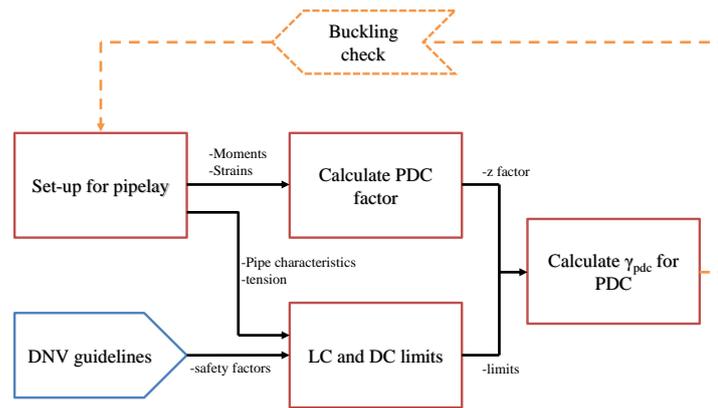


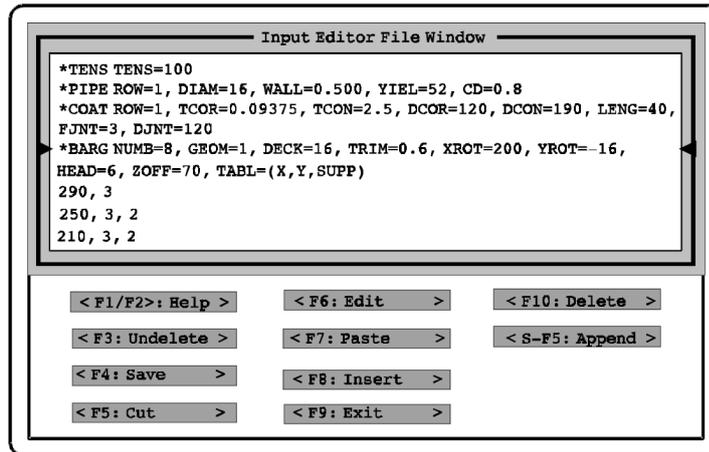
Figure 4.4: PDC assessment for specific pipeline installation

4.4. IMPLEMENTATION

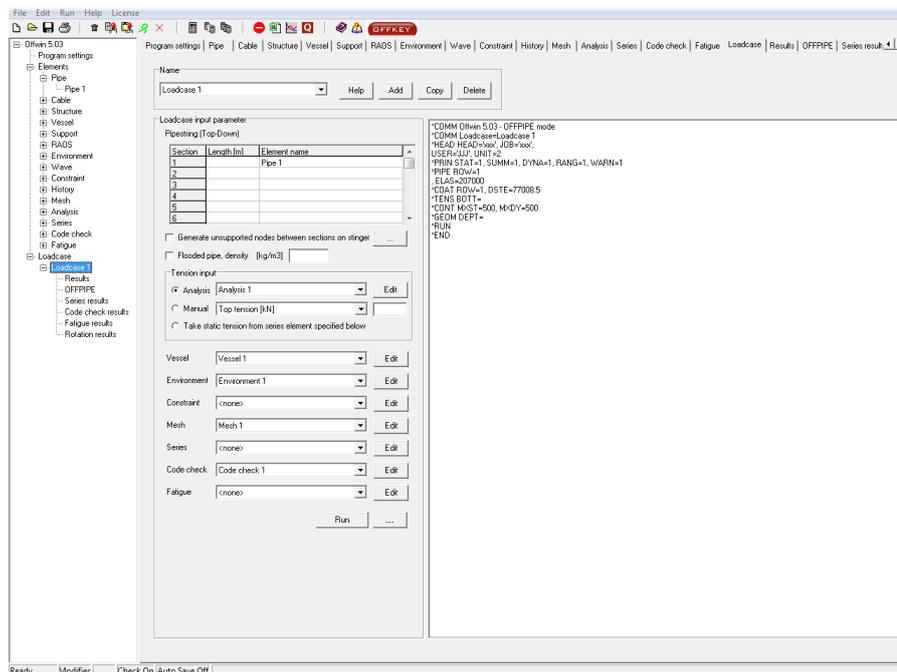
4.4.1. FE PACKAGE

At Allseas most general S-lay installation analyses are created using the fit-for-purpose program OFFPIPE. OFFPIPE is a finite element method program specifically created for the modelling and analysis of non-linear structural problems encountered in the installation of offshore pipelines. OFFPIPE uses a beam element model and can model the pipeline in both two and three dimensions. The finite element method considers both geometric (large displacement) and material (nonlinear stress-strain curve) non-linearities. In a static analysis OFFPIPE calculates the pipe stresses and deformed geometry at every point along the pipeline.

Allseas uses an in-house developed shell over the OFFPIPE program, called OFFWIN. OFFWIN is a pre- and post-processor for OFFPIPE and is created to provide a more user friendly and consistent Windows interface. Figure 4.5 illustrates the difference between a traditional OFFPIPE user interface and an OFFWIN user interface.



(a) OFFPIPE user interface [11]



(b) OFFWIN user interface

Figure 4.5: Comparison of user interfaces

4.4.2. PIPELINE MODEL

The material and geometric properties of the pipeline in OFFPIPE are modelled using a Ramberg-Osgood [12] material model. OFFPIPE defines the properties using the moment-curvature relation, which is expressed as follows:

$$\frac{K}{K_y} = \frac{M}{M_y} + A \left(\frac{M}{M_y} \right)^B \quad (4.7)$$

Where: K is the pipeline curvature
 M is the pipeline bending moment
 K_y is the yield curvature
 M_y is the yield moment
 E is the Young's modulus
 A and B are Ramberg-Osgood equation coefficients

The moment-curvature relation used in OFFPIPE is normalised by the yield moment and yield curvature. The yield moment is defined as the moment for which the outer fibres of the pipe reach the yield stress in pure bending. The yield curvature is the corresponding curvature. The stress distribution in the pipe is illustrated in figure 4.6. The yield moment and curvature are calculated as follows:

$$M_y = \frac{2 \cdot I_c \cdot \sigma_y}{D} \quad (4.8)$$

$$K_y = \frac{2 \cdot \sigma_y}{E \cdot D} \quad (4.9)$$

Where: I_c is the cross-sectional moment of inertia
 σ_y is the yield stress
 D is the pipe diameter
 E is the Young's modulus

The cross-sectional moment of inertia for a pipe is given by:

$$I_c = \frac{\pi}{64} (D^4 - (D - 2 \cdot t)^4) \quad (4.10)$$

Where: t is the pipe wall thickness

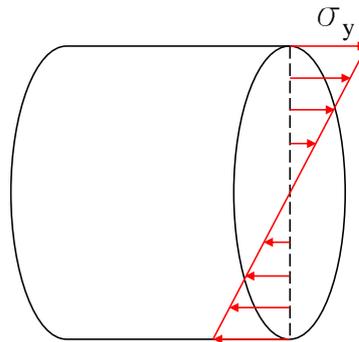


Figure 4.6: Stress distribution at yield moment

The nominal pipe properties and its variations are specified in OFFPIPE, as such the moment-curvature relation equation for each pipe is known. The curve length is calculated using the normalised data in order to not get conditioned by features with wider range of possible values when

computing distances. The curve lengths along the moment-curvature line can be calculated with equation ???. See appendix A for the derivation of this equation.

$$s = \int_a^b \sqrt{1 + \left(1 + A \cdot B \cdot \left(\frac{M}{M_y}\right)^{(B-1)}\right)^2} d\frac{M}{M_y} \quad (4.11)$$

Where: s is the curve length

a and b are the points on the curve in between which the curve length is calculated

4.4.3. ANALYSIS

To allow the weak link method to be used efficiently, a Matlab script is created to automatise the calculation steps of the analysis. The script consists of three parts:

- importing OFFWIN output
- z-value calculation
- visualising results

After running the models in OFFWIN, the output can be exported in *.OUT files, which is in text format. In the first part of the script the text files are parsed and the relevant data is placed in matrices for further manipulation.

The complete script is shown in Appendix D.

5

METHOD VALIDATION

To test and validate the method described in the previous chapter, several FE models are created in the FEM software Abaqus. Four different models are made:

- Model to determine buckling limit for fully load controlled situation. Pipe with moments applied to ends.
- Model to determine buckling limit for fully displacement controlled situation. Pipe bent over continuous support with constant curvature.
- Model to determine buckling limit for a specific partially displacement controlled situation. Pipe bent over discrete rollerboxes in a configuration of constant radius.
- Model of same specific partially displacement controlled situation with beam elements. Weak link method is applied to this model to determine the level of displacement control.

After validation of the method, the use of the OFFWIN FE package is validated. The beam element model from Abaqus is recreated as closely as possible in OFFWIN and the weak link method is applied to the model to determine the level of displacement control.

5.1. ABAQUS

Abaqus is a software package by Dassault Systèmes for finite element analysis and computer-aided engineering. The software package consists of several modules. The Abaqus/CAE (Complete Abaqus Environment) module is used for both modelling and analysis of mechanical components and assemblies. The Abaqus/Standard module is a general-purpose finite element analyzer that uses the traditional implicit integration scheme. The Abaqus/Explicit module is a special-purpose finite element analyzer that can solve highly non-linear systems by using an explicit integration scheme. Lastly, there are two additional modules for computational fluid dynamics and computational electromagnetics. Abaqus allows the analysis of highly non-linear situations with complex materials and impact and dynamic events. With the package comes a sophisticated element library of shells, beams, pipes, solids, and more. The Abaqus software suite is used by a wide range of companies in the offshore sector.

5.2. MODELS SETUP

In this section the set-up of the four models is explained.

5.2.1. PIPE PROPERTIES

The properties of the pipe used in the following models correspond to the pipeline characteristics of the pipe used in the deepest parts of the Nord Stream project by Allseas.

The nominal pipe characteristics are shown in table 5.1.

Table 5.1: Pipe properties

Characteristic	value
Outer diameter	1214.8 <i>mm</i>
Wall thickness	30.9 <i>mm</i>
Material grade	SAWL 485 FD
SMYS	485 <i>N/mm²</i>
SMTS	570 <i>N/mm²</i>
Density	7850 <i>kg/m³</i>
Young's modulus	207000 <i>MPa</i>
Poisson's ratio	0.3
Ramberg-Osgood coefficients:	
<i>N</i>	27.3484
<i>S₀</i>	0.00266

5.2.2. TRUE STRAIN - ENGINEERING STRAIN

The steel used in the Nord Stream pipe is of the SAWL 485 FD grade, which has a yield stress of 485 MPa (at 0.5% strain) and a tensile stress of 570 MPa (at 22% strain). The Ramberg-Osgood model is used to characterize the non-linear stress-strain relationship of the material. The Ramberg-Osgood equation used for stress-strain is:

$$\epsilon = \frac{\sigma}{E} + S_0 \left(\frac{\sigma}{\sigma_y} \right)^N \quad (5.1)$$

Where: ϵ is the total uniaxial strain
 σ is the uniaxial stress
 E is the Young's modulus
 σ_y is the yield stress
 S_0 and N are material constants

The stresses and corresponding strains given by equation 5.1 above are engineering stresses and engineering strains, meaning that it correlates the current state of a steel specimen with its original undeformed state (initial cross section and initial length). Abaqus requires the stress-strain data to be true stress and true strain, meaning that it correlates the current deformed state of a steel specimen with the history of previously deformed states. The engineering stress/strain can be converted to true stress/strain with the following equations:

$$\sigma_{true} = \sigma_{engineering} \cdot (1 + \epsilon_{engineering}) \quad (5.2)$$

$$\epsilon_{true} = \ln(1 + \epsilon_{engineering}) \quad (5.3)$$

Where: σ_{true} is the true stress
 $\sigma_{engineering}$ is the engineering stress
 ϵ_{true} is the true strain
 $\epsilon_{engineering}$ is the engineering strain

A comparison between the true and engineering stress-strain curves is shown in figure 5.1.

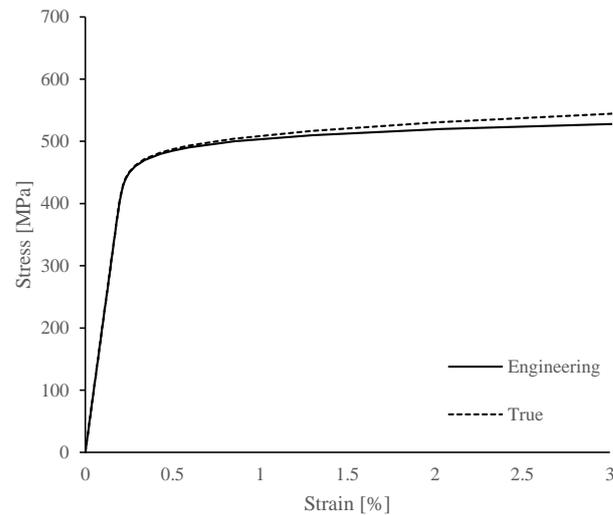


Figure 5.1: Comparison of true and engineering stress-strain curves

5.2.3. AXES

The labelling convention for displacement and rotational degrees of freedom used in Abaqus is shown in figure 5.2. For an axis-system with the labels x,y, and z, $U1$ would correspond with the displacement along the x-axis, $U2$ with displacement along the y-axis, and $U3$ with displacement along the z-axis.

- 1 Translation in the 1-direction ($U1$).
- 2 Translation in the 2-direction ($U2$).
- 3 Translation in the 3-direction ($U3$).
- 4 Rotation about the 1-direction ($UR1$).
- 5 Rotation about the 2-direction ($UR2$).
- 6 Rotation about the 3-direction ($UR3$).

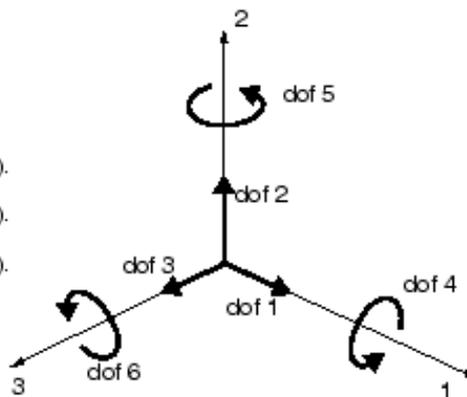


Figure 5.2: degrees of freedom labelling conventions [13]

5.3. LOAD CONTROLLED SITUATION

To determine the buckling point for a load controlled condition, a finite element model of a pipe section subjected to bending moments applied to the ends is developed. The length of the modelled pipe section is 10 outer diameters long, which according to literature ([14–20]) is more than sufficient to negate any influence of end-effects on the buckling region. To reduce the model size and make efficient use of computing time only a quarter of the pipe is modelled. Symmetry conditions are applied on the bending plane and the mid span cross section.

The mesh consists of the shell type element S4, which is a 4-node, quadrilateral, stress/displacement shell element. Shell elements are preferred when the structure has one dimension much smaller than the other two dimensions, in this case the pipe wall thickness versus the area. Analyses by Mohareb, Elwi, Kulak, and Murray [21] have shown that this element agrees well with experimental tests on pipe buckling. Along half the pipe circumference 24 elements are placed and 80 elements are placed along the pipe length. A total of 1920 elements are used.

5.3.1. BOUNDARY CONDITIONS

- Kinematic coupling. The edge of the free end is coupled to a reference node placed at the center of the pipe end. The constrained degrees of freedom relative to the reference node are:
 $U1 = U2 = U3 = UR1 = UR2 = UR3 = 0$
- Roller support. The reference node has a displacement/rotation boundary condition. The constrained degrees of freedom are:
 $U1 = U2 = UR2 = UR3 = 0$
- Bending plane symmetry. The symmetry constraints are that displacements in the $U1$ direction and rotation about the $U2$ and $U3$ axis are fixed.
 $U1 = UR2 = UR3 = 0$
- Mid-section symmetry. The symmetry constraints are that displacements in the $U3$ direction and rotation about the $U1$ and $U2$ axis are fixed.
 $U3 = UR1 = UR2 = 0$

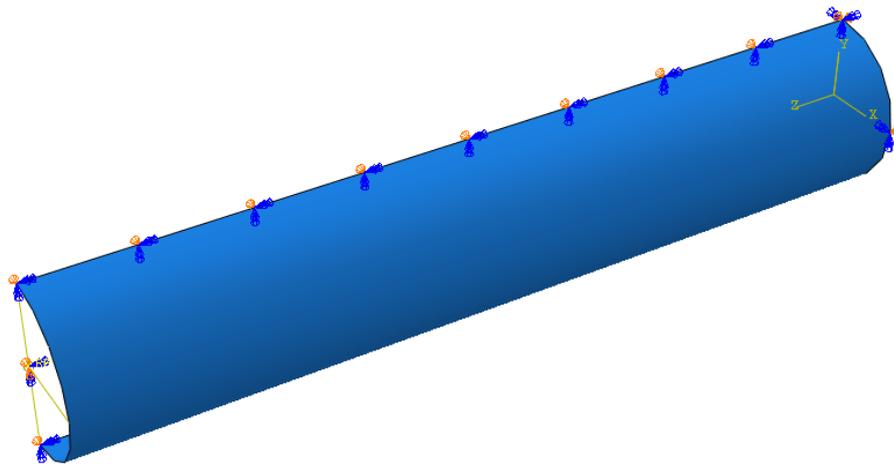


Figure 5.3: Model setup for load controlled situation

5.3.2. LOADING

At the free end a bending moment along the U1 axis is placed at the reference node. This moment is then transferred through the coupling condition to the pipe shell elements. The total applied reference moment for a whole pipe is put at 10000 kNm, which means that for a half pipe circumference the applied reference moment input must be 5000 kNm.

5.3.3. ANALYSIS PROCEDURE

The analysis procedure type is a static Riks analysis with accounting for geometric nonlinearity. Abaqus offers an analysis method called the “modified Riks method”, this method can be used to find the static equilibrium states during the unstable phase of a response. For simple cases a linear eigenvalue analysis might be sufficient to predict the buckling behaviour, but if there is concern about material non-linearity, geometric non-linearity prior to buckling, or unstable postbuckling response, a load-deflection analysis (Riks) must be performed. The Riks method takes the load magnitude as an additional unknown. It solves simultaneously for loads and displacements. During the Riks analysis the load proportionality factor is gradually increased, i.e. the load magnitude increases, as long as an equilibrium can be found. If no equilibrium can be found with an increase in load magnitude, the load proportionality factor decreases gradually to analyse the post-buckling response of the structure. To measure the progress of the solution Abaqus uses the arc length l along the static equilibrium path in load-displacement space. Any loads defined in a step before the Riks step are treated as “dead” loads with constant magnitude and do not change during the Riks step. Loads defined in the Riks step are treated as “reference” loads with varying magnitudes. The current load magnitude in a certain increment step is defined by:

$$P_{total} = P_0 + \lambda (P_{ref} - P_0) \quad (5.4)$$

Where: P_0 is the dead load
 P_{ref} is the reference load
 λ is the load proportionality factor

The load proportionality factor (LPF) is found as part of the solution. To determine the maximum moment the pipe can handle before buckling, the maximum LPF found in the solution and the reference moment are used in equation 5.4. As there is no loading prior to the Riks step in this model, the equation reduces to:

$$M_{buckling} = \lambda \cdot M_{ref} \quad (5.5)$$

Where: $M_{buckling}$ is the buckling moment
 M_{ref} is the reference moment

The resulting buckling moment is for the modelled half pipe circumference, to determine the buckling moment for a complete pipe this value must be doubled.

5.3.4. RESULTS

The result of the analysis shows the deformation the pipe experiences during the moment increase and subsequent decrease. Figures 5.4 and 5.5 show the deformation of the pipe at maximum moment before buckling and the post-buckling shape respectively.

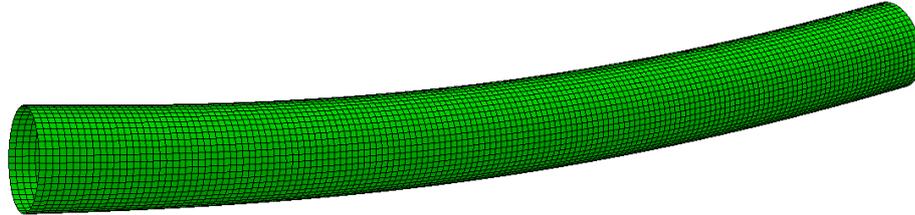


Figure 5.4: Pipe deformation at maximum moment

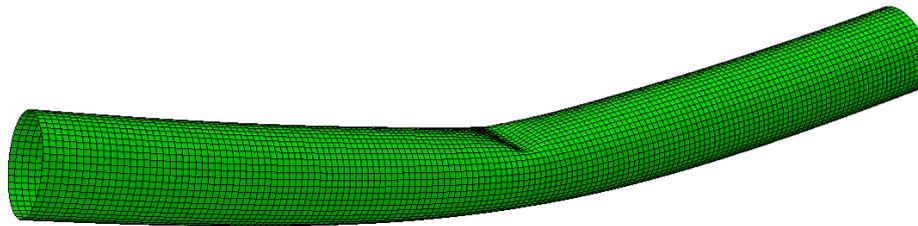


Figure 5.5: Pipe deformation after buckling

The load proportionality factor versus the arc length is plotted in figure 5.6. To determine the moment at point of buckling, the maximum load proportionality factor is of interest.

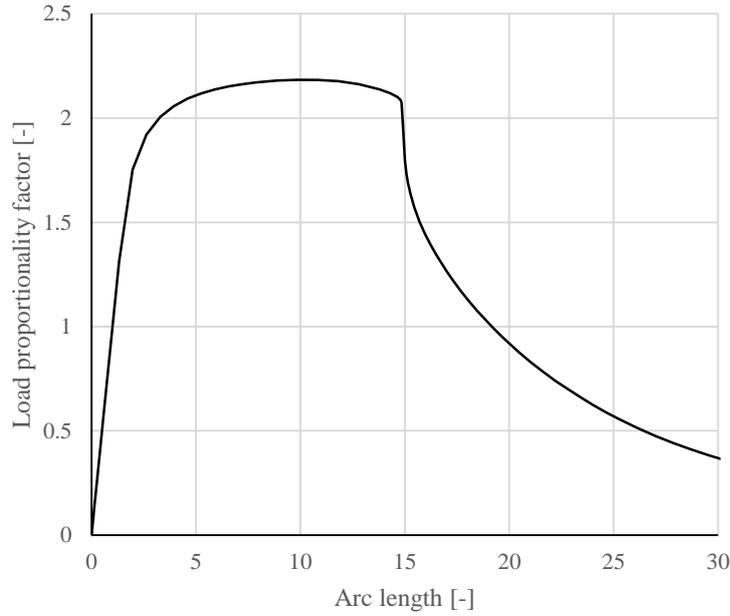


Figure 5.6: Load proportionality factor vs. arc length

The maximum LPF found in the analysis is 2.1828. Placing this value and the reference moment of 5000 kNm in equation 5.5 gives a buckling moment for half the pipe circumference of 10914 kNm and thus a buckling moment of 21828 kNm for the whole pipe circumference.

The moment-curvature relationship for this pipe is determined from the pipe deformation prior to buckling. The moment follows from the reference moment and LPF. The global curvature of the pipe is calculated by the coordinates of the nodes along the centreline of the pipe as shown in figure 5.7.

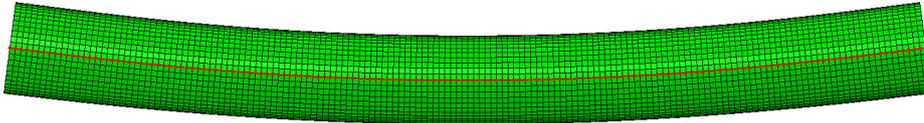


Figure 5.7: Nodes on the neutral axis of the pipe

The global curvature is determined by calculating all the Menger curvatures of three subsequent nodes and taking the average value of this. The Menger curvature is defined by:

$$c(x, y, z) = \frac{1}{R} = \frac{4A}{|x-y||y-z||z-x|} \quad (5.6)$$

Where: c is the curvature belonging to points x , y , and z
 R is the radius of curvature
 A is the area of the triangle spanned by x , y , and z

The area of a triangle spanned by three points is given by:

$$A(x, y, z) = \frac{1}{2} |(y_1 - x_1)(z_2 - x_2) - (z_1 - x_1)(y_2 - x_2)| \quad (5.7)$$

Where: Subscript 1 denotes coordinates along axis 1
 Subscript 2 denotes coordinates along axis 2

With the moment and curvature calculated the resulting moment-curvature relation is plotted in figure 5.8. The point of buckling corresponding to the fully load controlled situation is shown in the figure and denoted by LC.

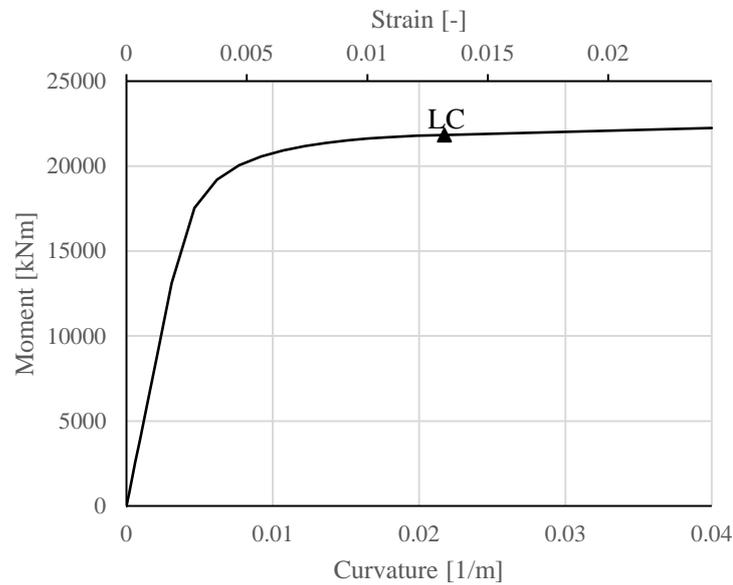


Figure 5.8: Moment-curvature relation

5.4. DISPLACEMENT CONTROLLED SITUATION

To determine the buckling point for a displacement controlled condition, a finite element model of a pipe section bent over a fixed shape with constant curvature is developed. The length of the modelled pipe section is 60m with the fixed shape placed in the middle. The mesh consists of the same S4 shell type elements as the load control model. To reduce the number of elements in the analysis, a coarser mesh is used in the regions not in contact with the fixed shape and a finer mesh is used where the pipe contacts the fixed shape.

An initial imperfection is created in a 0.5m long section in the middle of the pipe by placing an upward vertical line load of 40 kN in this section. It is necessary to create a small imperfection in the model to facilitate the buckling of the pipe. Without any imperfections or discontinuities in the pipe, the pipe will not 'know' where to buckle once the conditions for buckling have been met and will thus not buckle. This will wrongly give a much higher buckling limit.

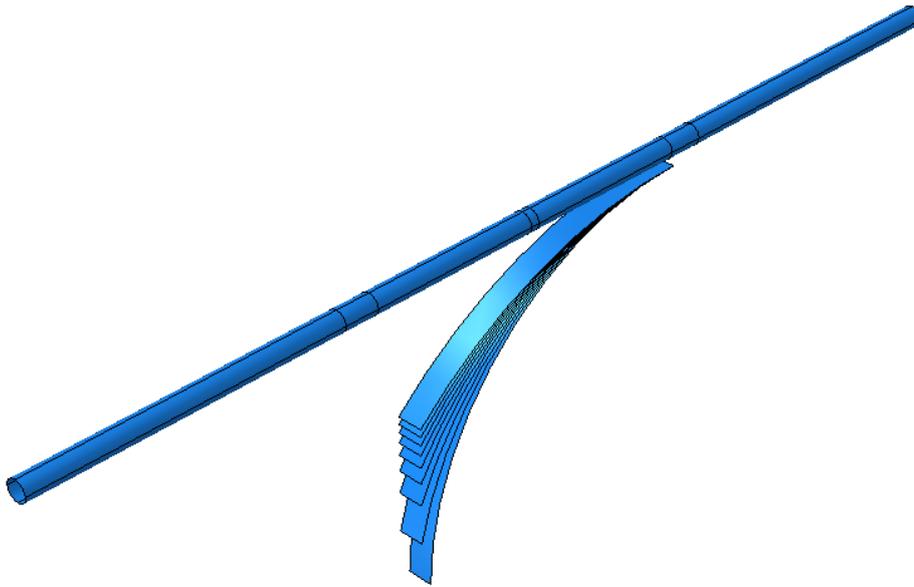


Figure 5.9: Model setup for displacement controlled situation

5.4.1. BOUNDARY CONDITIONS

- Kinematic coupling. At both pipe ends a reference node is placed at the center of the pipe end. The edges of the free ends are coupled to the reference nodes placed at that pipe end. The constrained degrees of freedom relative to the reference node are:
 $U_1 = U_2 = U_3 = UR_1 = UR_2 = UR_3 = 0$
- Fixed support. The reference node at the right end of the pipe has a displacement/rotation boundary condition. The constrained degrees of freedom are:
 $U_1 = U_2 = U_3 = UR_1 = UR_2 = UR_3 = 0$
- Free end. The reference node at the left end of the pipe has a displacement/rotation boundary condition. The constrained degrees of freedom are:
 $U_1 = UR_2 = UR_3 = 0$

5.4.2. LOADING

The loading of the pipeline is split into two steps. In the first step an upward (U_2) line load of 40kN is applied to a 0.5m long section in the middle of the pipe. This loading is shown in figure 5.10. In the second step a downward (U_2) displacement is imposed at the reference node at the free (left) end of the pipe. This applied displacement causes the pipeline to drape over the fixed rigid curvature.

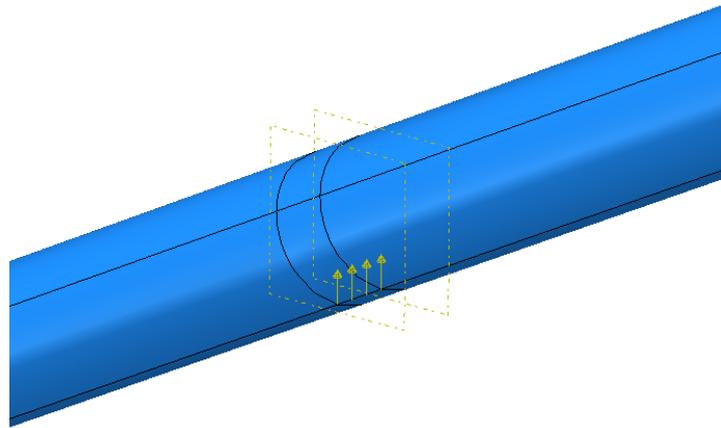


Figure 5.10: Line load applied to pipe section

5.4.3. ANALYSIS PROCEDURE

To determine the exact curvature at which the pipe shows the onset of local buckling, a range of curvatures is modelled. The pipeline is draped over curves with an increasing curvature until the first symptoms of buckling appear. A selection of curves can be seen in figure 5.9. The onset of local buckling in this case is defined as the moment where the pipe loses local contact with the rigid curve, i.e. the pipeline does not follow the shape of the curve completely. The buckling curvature is defined as the reciprocal of the largest radius for which onset of buckling happens:

$$k_{buckling} = \frac{1}{R_{buckling,max}} \quad (5.8)$$

5.4.4. RESULTS

The result of the analysis shows that the onset of buckling first happens at a curve radius of 32.5m, corresponding to a curvature of 0.03077. Figure 5.11 shows the initial small onset of local buckling where the pipeline first loses contact with the rigid curve and figure 5.12 shows the buckled pipeline after further bending. The buckling limit for the fully displacement controlled condition is plotted in the moment-curvature relation in figure 5.13 and is denoted by DC.

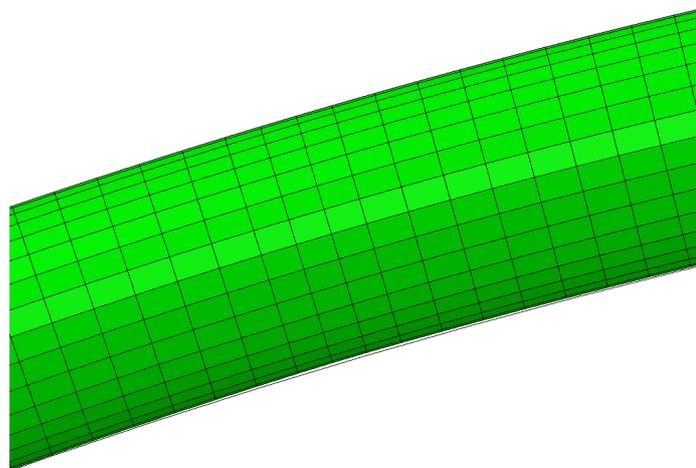


Figure 5.11: Onset of buckling

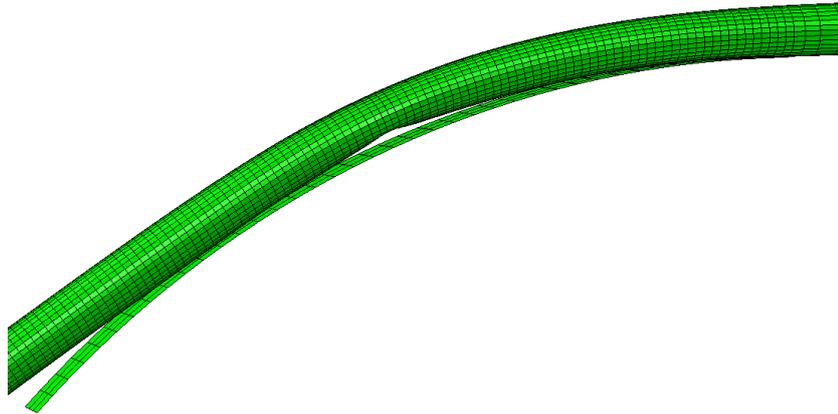


Figure 5.12: Buckled shape in a displacement controlled condition

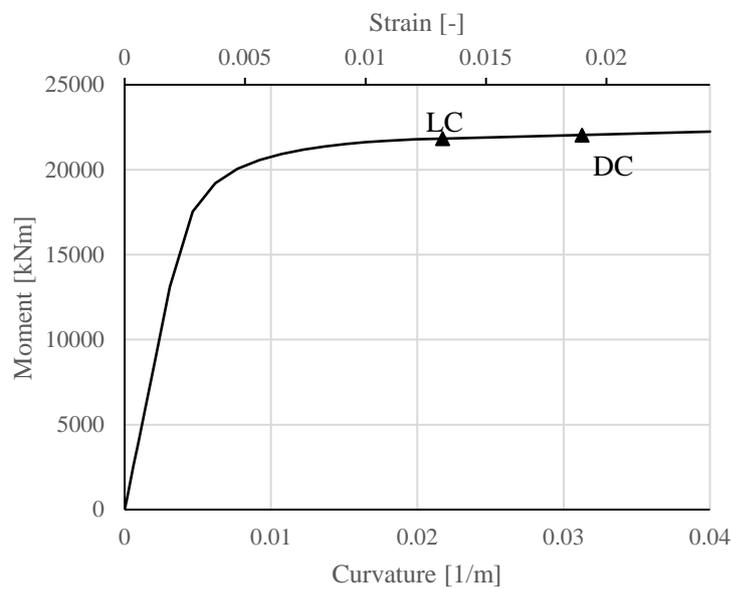


Figure 5.13: Moment-curvature relation

5.5. PARTIALLY DISPLACEMENT CONTROLLED SITUATION

To determine the buckling point for a specific partially displacement controlled condition, a finite element model of a pipe section bent over a stinger section with constant curvature is developed. The length of the modelled pipe section is 80m with the fixed shape placed in the middle. The mesh consists of the same S4 shell type elements as the two previous models. To reduce the number of elements in the analysis, a coarser mesh is used in the regions not in contact with the fixed shape and a finer mesh is used where the pipe contacts the stinger section. An overview of the assembly of the model is shown in figure 5.14.

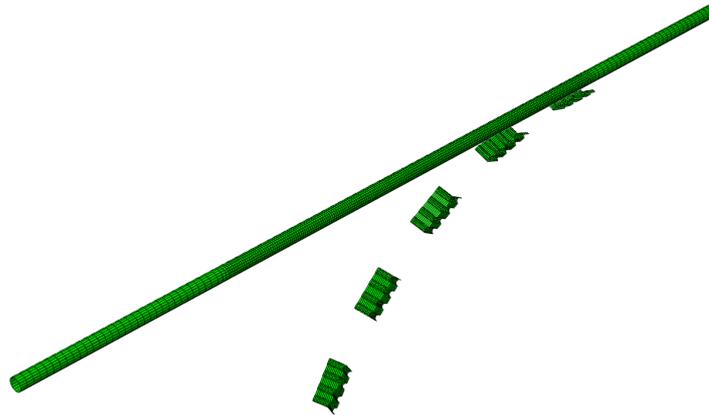


Figure 5.14: Overview of partially displacement controlled model

The stinger section is modelled to have a constant radius. The rollerboxes are modelled to the likeness of the rollerboxes aboard of the Solitaire vessel. The dimensions of the rollerbox is shown in figure 5.15. The center to center spacing of the rollerboxes is 8.5m, combined with a rollerbox length of close to 4m this results in a stinger section where approximately 50% of the pipe is supported.

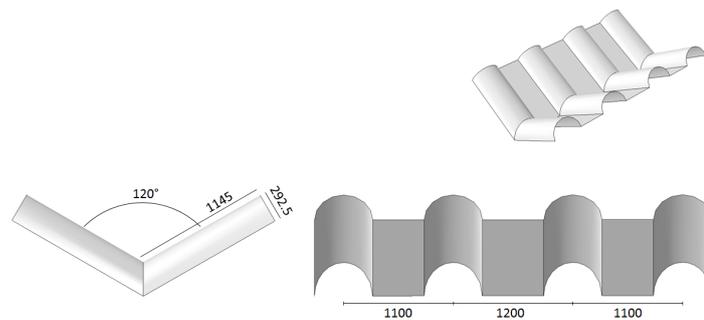


Figure 5.15: Dimensions of rollerbox

5.5.1. BOUNDARY CONDITIONS

- Kinematic coupling. At both pipe ends a reference node is placed at the center of the pipe end. The edges of the free ends are coupled to the reference nodes placed at that pipe end. The constrained degrees of freedom relative to the reference node are:

$$U1 = U2 = U3 = UR1 = UR2 = UR3 = 0$$

- Fixed support. The reference node at the right end of the pipe has a displacement/rotation boundary condition. The constrained degrees of freedom are:

$$U1 = U2 = U3 = UR1 = UR2 = UR3 = 0$$

- Free end. The reference node at the left end of the pipe has a displacement/rotation boundary condition. The constrained degrees of freedom are:

$$U1 = UR2 = UR3 = 0$$

5.5.2. LOADING

In the analysis step a downward ($U2$) displacement is imposed at the reference node at the free (left) end of the pipe. This applied displacement causes the pipeline to drape over the stinger sec-

tion.

5.5.3. ANALYSIS PROCEDURE

To determine the exact curvature at which the pipe shows the onset of local buckling, a range of stinger sections is modelled. The pipeline is draped over stingers with a decreasing radius until local buckling appears. The local buckling is characterised by a sudden loss of stiffness and a gross deformation of the pipeline. The buckling curvature is defined as the reciprocal of the largest radius for which local buckling happens.

5.5.4. RESULTS

The result of the analysis shows that for this partially displacement controlled situation local buckling first happens at a slightly smaller curvature compared to the fully displacement controlled condition. Local buckling of the pipeline first happens at a stinger section with a radius of 33m, corresponding to a curvature of 0.0303. Figure 5.16 shows the location and shape of the local buckle. As expected, the buckling occurs on top of one of the rollerboxes. The buckling limit for this partially displacement controlled condition is plotted in the moment-curvature relation in figure 5.17 and is denoted by PDC.

At the timestep where the local buckling first appears, the coordinates of the displaced pipe end are noted. These coordinates will be used as an input in the beam models of the next section.

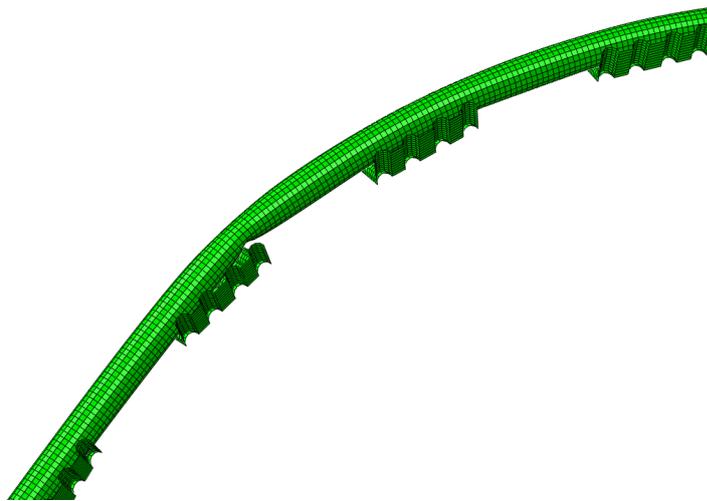


Figure 5.16: Buckled shape in a partially displacement controlled condition

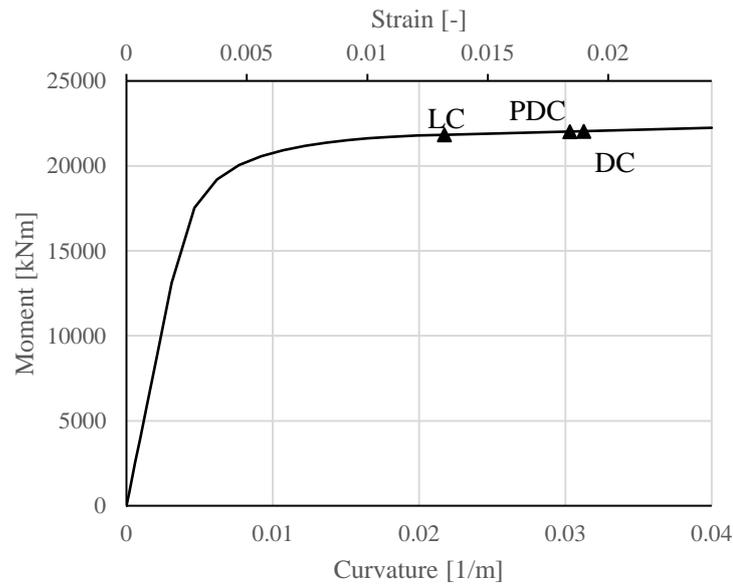


Figure 5.17: Moment-curvature relation

The resulting moments, curvatures, and strains for the onset of local buckling for the three shell-type element models are summarised in table 5.2.

Table 5.2: Onset of local buckling for shell FE models

	moment kNm	curvature m^{-1}	strain –
Load controlled	21828	0.0217	0.0132
Partially displacement controlled	22023	0.0303	0.0184
Displacement controlled	22033	0.0313	0.0190

5.6. BEAM MODELS

To validate the weak link method, the specific partially displacement controlled situation of the model in section 5.5 is recreated using a beam model such that the weak link method can be applied to it. The model setup largely corresponds to the setup of the shell model. The configuration of the rollerboxes is kept the same at a constant radius of 33m. The shell element pipeline part is replaced by a beam element pipeline part. The length of the modelled pipe section is 80m. The mesh consists of the PIPE31 beam type element. The PIPE31 element is a 2-node linear pipe in space with 6 degrees of freedom. Thin-walled pipe formulations are selected in the section properties. The pipe is equally divided into 400 elements, resulting in an element length of 200mm. An overview of the assembly of the model is shown in figure 5.18.

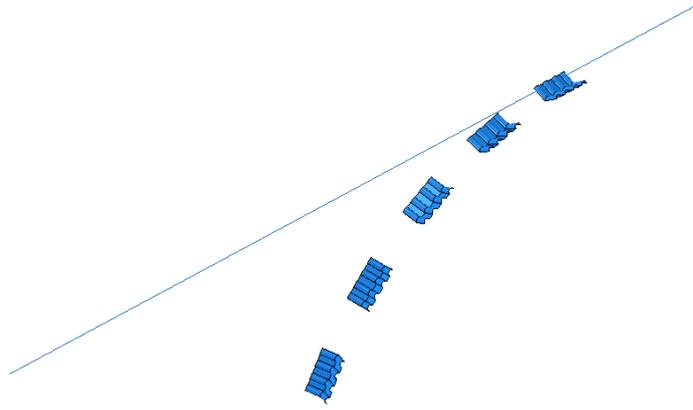


Figure 5.18: Overview of partially displacement controlled beam model

Three variations of the beam model are made, each with different pipe properties. The pipe property variations are as follows:

- Nominal. The pipe properties match those from table 5.1 and those used in the shell element models.
- Weaker. The yield stress, tensile strength, and wall thickness are both reduced by 2%.
- Stronger. The yield stress, tensile strength, and wall thickness are both increased by 2%.

The resulting changes in pipe properties are summarised in table 5.3.

Table 5.3: Variations in pipe properties

	Yield stress [MPa]	Tensile strength [MPa]	Wall thickness [mm]
Nominal	485	570	30.9
Weaker	475.3	558.6	30.282
Stronger	494.7	581.4	31.518

5.6.1. BOUNDARY CONDITIONS

- Fixed support. The end node at the right end of the pipe has a displacement/rotation boundary condition. The constrained degrees of freedom are:

$$U1 = U2 = U3 = UR1 = UR2 = UR3 = 0$$

- Free end. The end node at the left end of the pipe has a displacement/rotation boundary condition. The constrained degrees of freedom are:

$$U1 = UR2 = UR3 = 0$$

5.6.2. LOADING

In the analysis step a downward ($U2$) displacement is imposed at the reference node at the free (left) end of the pipe. This applied displacement causes the pipeline to drape over the stinger section. The displacement is chosen such that it matches the coordinates of the free pipe end in the partially displacement controlled shell element model at the first point of buckling. For this configuration a required vertical displacement of 33.94m is found. By forcing the end displacements to be the

same, the general shape of the pipeline over the stinger is also the same between the shell and beam element models.

5.6.3. ANALYSIS PROCEDURE

Once the end of the displacement step has been reached and the end displacement matches the pipe end coordinates of the shell model, the local moment and curvature of the beam element at the location of buckling is noted. Figure 5.19 shows the exact location where the moment and curvature is measured. Beam elements are incapable of modelling the local buckling because the pipe profile is fixed and cannot deform. As such, there will not appear any signs of local buckling in this model.

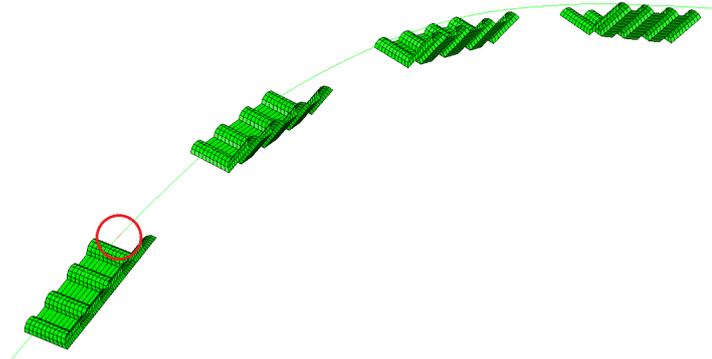


Figure 5.19: Measurement location in beam model

5.6.4. RESULTS

Once the moments and curvatures at the location of buckling have been noted for each of the three variations, they can be plotted in their respective moment-curvature relations. The moment-curvature relations for the three pipe variations are produced by measuring the changes in moments and curvatures during the analysis. This relation will differ slightly from the moment-curvature line shown in figures 5.8, 5.13, and 5.17. This is due to the fact that beam elements cannot take the ovalisation of the pipe during bending into account. The three moment-curvature relations and the corresponding buckling points are shown in figure 5.20, normalised by the yield moment (equation 4.8) and yield curvature (equation 4.9).

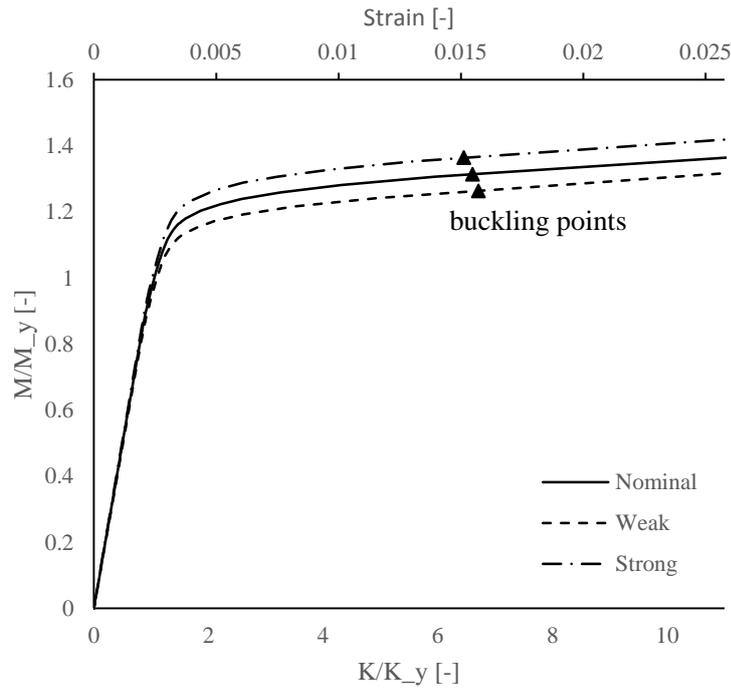


Figure 5.20: Moment-curvature relation

5.7. COMPARISON OF MODELS

To validate the weak link method, a level of partial displacement control is calculated for the shell element models and is then compared to the z -factor calculated with the weak link method on the beam models. If these separately calculated levels of partial displacement control agree, the weak link method is considered validated.

First, the level of partial displacement control in the shell element models is calculated. The curvatures at which buckling begins for the load controlled, displacement controlled, and partially displacement controlled situation are shown in table 5.4. As can be seen in figure 5.17, the three buckling points all lie in an almost linear region of the moment-curvature curve. For simplicity's sake the level of partial displacement control is calculated by comparing the linear distance between the LC and DC points with the linear distance between the PDC and DC points.

Table 5.4: Buckling curvatures of shell element models

Condition	Buckling curvature [m^{-1}]
Load control	0.02173
Partial displacement control	0.03030
Displacement control	0.03077

In equation form the calculation of the level of displacement control is:

$$z = \frac{DC - PDC}{DC - LC} = \frac{0.03077 - 0.03030}{0.03077 - 0.02173} = 0.95 \quad (5.9)$$

Which indicates that this particular partially displacement controlled situation is 95% displacement controlled and 5% load controlled.

Next, the level of partial displacement control is calculated for the beam models using the weak link method. Using the method as described in chapter 4, the expected responses for fully load controlled and fully displacement controlled conditions can be plotted in the graph of figure 5.20. Figure 5.21 shows a zoomed in part of figure 5.20 with the LC and DC responses shown by the dotted line. The measured buckling points are denoted by the triangles.

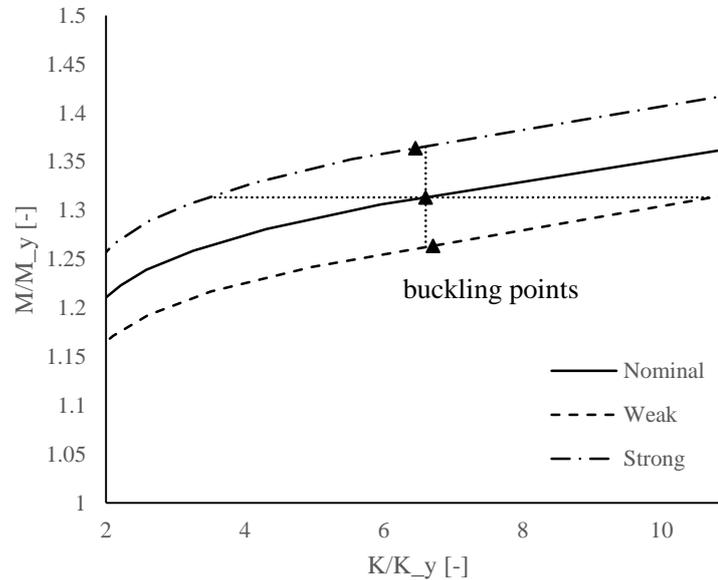


Figure 5.21: LC and DC responses for beam models

As the buckling points are in the non-linear part of the curve, a linear calculation of the z-factor is not exact. Instead the z-factor is calculated by comparing the curve lengths (see equation 3.4). The calculated curve lengths and corresponding z-factor for the weaker and stronger variations are shown in table 5.5. As noted by Christensen [9], a weaker variation gives a slightly higher prediction of the level of displacement control compared to a stronger variation. Using the weak link method a level of partial displacement control in the range of 95% to 97% is found.

Table 5.5: Curve lengths for weak link method

Variation	L_w	L_{dc}	z
Weaker	4.02	4.13	0.97
Stronger	2.97	3.11	0.95

As the z-factor values calculated with the weak link method are within 2% of the z-factor calculated with the more detailed shell element models, the first step towards validation of the proposed method has been taken with satisfactory results.

5.8. COMPARISON OF FEM PROGRAMS

To validate the use of OFFWIN in combination with the weak link method, the beam models as described in section 5.6 are recreated as closely as possible in OFFWIN. OFFWIN is a FE package developed specifically for situations that occur in the offshore pipelaying industry, as such its modelling capabilities are a lot more limited than a general FE package such as Abaqus. Therefore creating a 1:1 copy of the Abaqus model in OFFWIN is impossible. However, by identifying the important

parameters of the pipeline configuration on the stinger and keeping those similar, it is possible to create a close approximation of the Abaqus model in OFFWIN.

5.8.1. MODEL SET-UP

The stinger model created in OFFWIN corresponds with the rollerbox configuration in the Abaqus models. As the rollerboxes in OFFWIN are not modelled explicitly, only the coordinates and angles of the rollerboxes are used.

In OFFWIN the pipe supports are modelled as frictionless point supports. Each pipe support consists of two sets of independent rollers, as shown in figure 5.22. One pair of rollers, referred to as the bottom rollers, is approximately horizontal and used primarily to support the weight of the pipeline. The second set of rollers, referred to as the side rollers, are approximately vertical and are primarily used to restrain the lateral or transverse displacements of the pipeline.

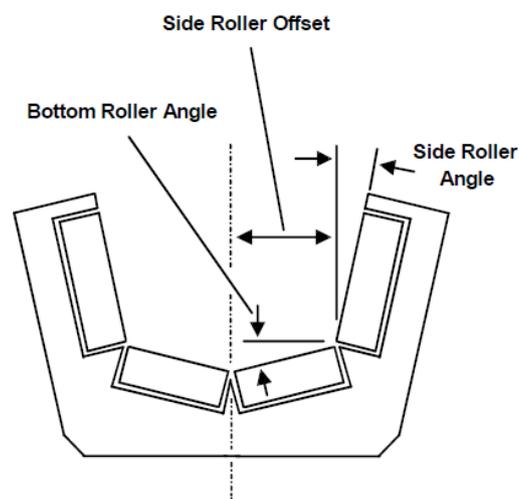


Figure 5.22: The standard pipe support model used by OFFPIPE [11]

To recreate the rollerbox model of figure 5.15 the pipe support properties shown in table 5.6 are used. The side roller offset is sufficiently large that the pipe is not in contact with the side roller. The rollerboxes in the Abaqus model are completely rigid. In OFFWIN the pipe supports are standard modelled as spring supports. By using a very large stiffness the rollerboxes can be considered as rigids.

Table 5.6: Pipe support properties

Property	value	units
Vertical stiffness	1000000	kN/m
Lateral stiffness	1000000	kN/m
Bottom roller angle	30	deg
Side roller angle	0	deg
Side roller offset	0.992	m
Roller bed length	3.985	m

The moment-curvature relation of the pipeline is modelled using the Ramberg-Osgood material model (equation 4.7) in OFFWIN. The moment-curvature relations found during the analyses of the

beam models in Abaqus (see figure 5.20) thus need to be converted to Ramberg-Osgood parameters. The method for converting the moment-curvature relations to Ramberg-Osgood parameters is explained in detail in appendix B. The resulting Ramberg-Osgood parameters for the pipelines in table 5.3 are summarised in table 5.7.

Table 5.7: Ramberg-Osgood parameters for variations in pipe properties

	Yield stress [MPa]	Tensile strength [MPa]	Wall thickness [mm]	Ramberg-Osgood	
				A [-]	B [-]
Nominal	485	570	30.9	0.008461	23.61311
Weaker	475.3	558.6	30.282	0.008782	23.61946
Stronger	494.7	581.4	31.518	0.007971	23.68074

To recreate the same shape of the pipeline on the stinger model, the departure angle of the pipeline is kept the same between Abaqus and OFFWIN. The departure angle is determined by pipe characteristics, water depth and lay tension. The focus of this model is the stinger section, thus what happens in the sagbend or bottom touchdown is not of interest and is ignored. The water depth needs to be specified to run the model and is taken as 200m, but is not of further interest. The departure angle of the pipeline in the Abaqus model is approximately 47 degrees. A lay top tension of 3700kN is used in the OFFWIN model, resulting in a departure angle of 47.07 degrees. The model configuration is shown in figure 5.23. The end of section point indicates where the edge of the Abaqus model would be.

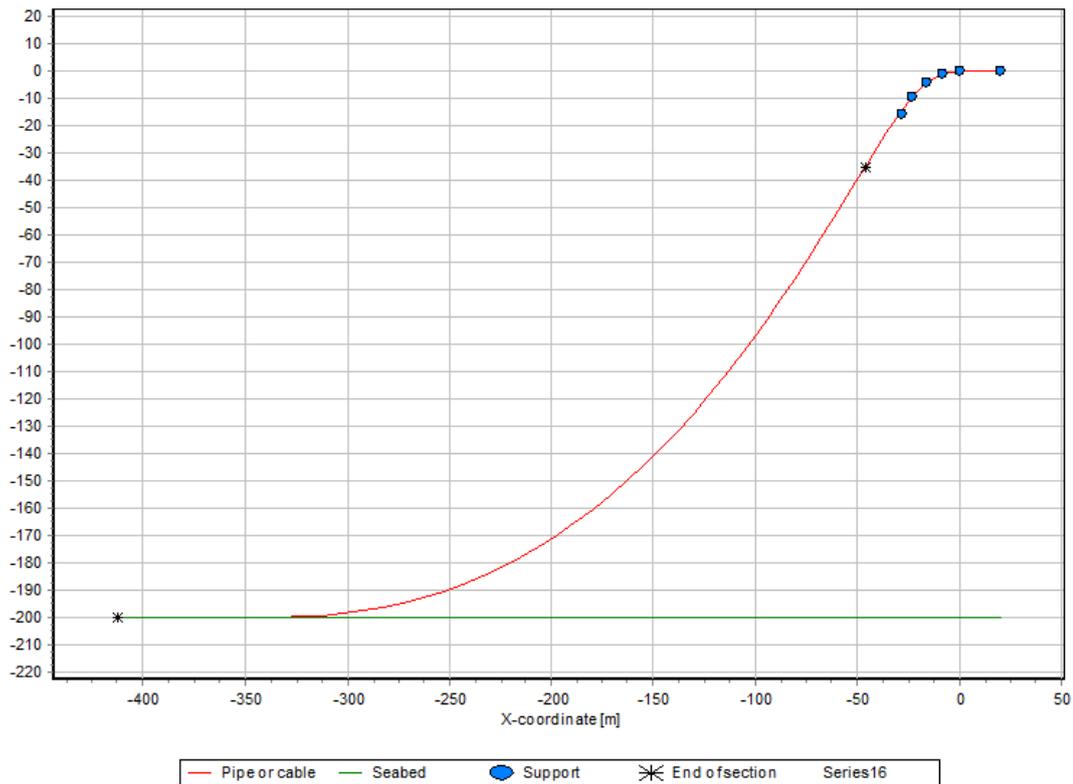


Figure 5.23: Configuration of the OFFWIN model

5.8.2. RESULTS

The weak link method is applied to the OFFWIN results using the Matlab script of appendix D. The three normalised moment-curvature relations for the nominal, weaker, and stronger pipe are shown in figure 5.24. The three buckling points calculated by Abaqus and OFFWIN are indicated in the figure.

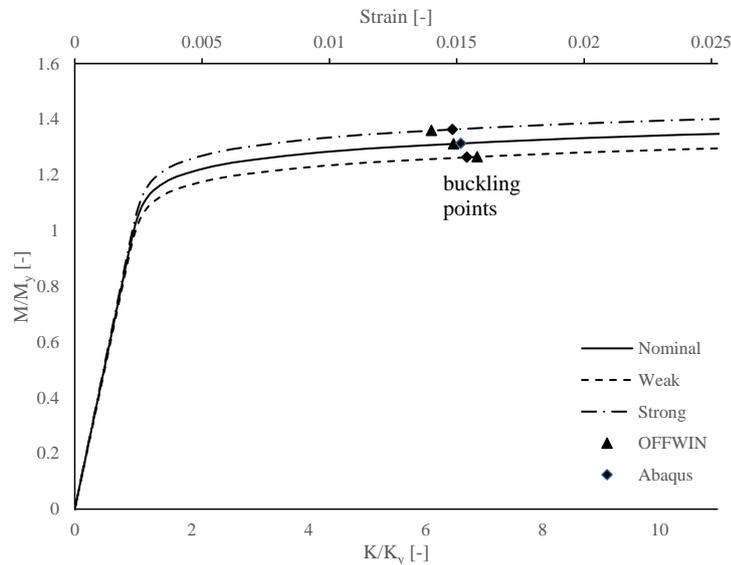


Figure 5.24: Moment-curvature relation

Figure 5.25 shows a zoomed in part of figure 5.24. Only the buckling points calculated by OFFWIN are shown and are denoted by the triangles. The LC and DC responses are shown by respectively the horizontal and vertical dotted lines.

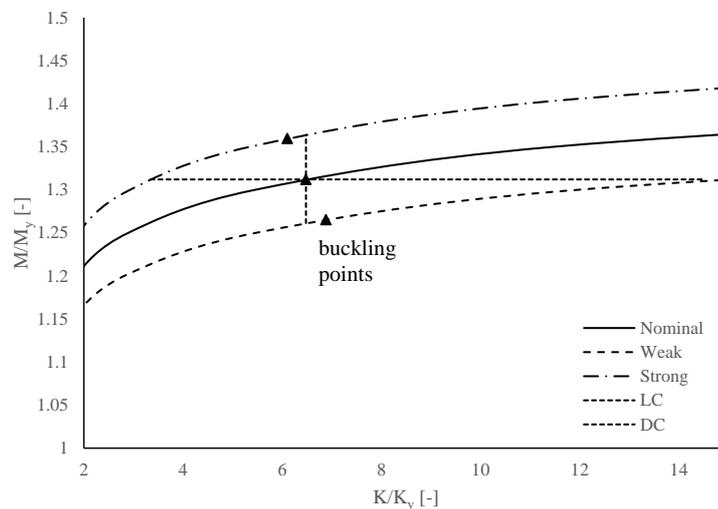


Figure 5.25: LC and DC responses for OFFWIN beam models

The z-values calculated for the weaker and stronger pipe variation are shown in table 5.8. As expected, the weaker variation predicts a higher level of partial displacement control compared to the stronger pipe variation.

Table 5.8: Curve lengths for weak link method in OFFWIN and Abaqus

Program	Variation	L_w	L_{dc}	z
OFFWIN	Weaker	7.80	8.21	0.95
	Stronger	2.71	3.08	0.88
Abaqus	Weaker	4.02	4.13	0.97
	Stronger	2.97	3.11	0.95

Comparing the z -values calculated in OFFWIN to the z -values calculated with Abaqus, it can be seen that the OFFWIN model gives a slightly lower estimate of the level of displacement control than the Abaqus model. However, it should be kept in mind that the two models are not exact copies of each other and that some difference in results is to be expected. Considering the slight differences between the two models the 9% difference between the z -values calculated in OFFWIN and Abaqus are not considered too large to invalidate the use of OFFWIN in combination with the weak link method. To validate the use of OFFWIN in combination with the weak link method further studies with more similar models is recommended.

5.8.3. COMPUTATIONAL TIME COMPARISON

The models described in this chapter all have different levels of detail. The shell element models have a fully modelled pipeline which allows for local deformation of the pipe and the PDC model has fully modelled rollerboxes. The Abaqus beam model has a lower level of detail as the pipeline shell model is replaced by a beam model, removing the ability to model the local deformation of the pipe. Finally the OFFWIN beam model has the lowest level of detail by replacing the modelled rollerboxes with point supports. The level of detail has a large impact on the computational time of the models and this is summarised in table 5.9. Considering that these models only compute a single static situation and that for large pipelay projects it is typical to run thousands of these situations, it is clear that the more detailed Abaqus models are not feasible.

Table 5.9: Runtimes for FE partial displacement control models

Model	section	computational time [seconds]
Abaqus shell element	5.5	668
Abaqus beam element	5.6	576
OFFWIN beam element	5.8	2

5.9. CHAPTER CONCLUSIONS

The detailed shell element models are capable of modelling the onset of local buckling and the results agree with the expected difference in buckling limit for load, displacement, and partially displacement controlled conditions. It is found that for the large diameter pipeline tested, the onset of buckling in a partially displacement controlled condition is very close to the buckling limit for a fully displacement controlled condition.

Modelling the same PDC situation with beam elements in Abaqus and using the weak link method to compute the level of displacement control gives results that agree with the results from the shell element models. Considering the closeness of the results in the investigated case, the weak link method shows potential as a reliable tool to easily and efficiently determine the PDC conditions of a situation without the need to create very detailed models. Further testing with different cases is

recommended to substantiate this claim.

The comparison of the Abaqus beam model with the OFFWIN beam model is clouded by the fact that the modelled situations are not exact copies of each other. Despite the difference in modelling capabilities of the two programs, an approximation of the Abaqus model is made in OFFWIN. The computed level of displacement control in the OFFWIN model lies lower than the computed level in the Abaqus model. A possible explanation could be the introduction of a tension component in the OFFWIN model, which has the effect of reducing the level of displacement control.

To obtain more confidence in the validation of the weak link method, it is recommended to perform the same method of validation for more pipe properties. The currently tested pipe is a very large and stiff pipe, so at least a pipe with a smaller diameter should be tested. For now, with the currently available model results, the weak link method shows potential and will be used for the upcoming parametric study.

6

PARAMETRIC STUDY

In this chapter the effect of several parameters in the pipeline installation configuration on the calculated z-value are investigated. To efficiently design an installation configuration, it is essential to understand how changes in the situation (e.g. stinger radius, rollerbox spacing, laying tension, etc.) affect the level of partial displacement control.

The OFFWIN program is used to model an extensive collection of parametric variations. The parameters investigated are as follows:

- Stinger radius
- Rollerbox spacing
- Rollerbox load
- Tension
- Pipe diameter
- Pipe D/t ratio

Lastly, the effect of the level of displacement control on the local buckling limits is investigated.

6.1. OFFWIN MODEL SET-UP

The basis of the parametric studies will be S-lay situations with a "perfect" stinger. This means that the stinger shape follows the arc of a circle and thus has constant radius. By keeping the influence of an irregular stinger shape to a minimum, the analyses will provide more reliable results. For the parametric studies a static analysis will be used. In the following section, the model set-up in OFFWIN is explained.

6.1.1. VESSEL MODEL

The pipelaying vessel is modelled as a rigid body. In the vessel model the coordinates of the pipe supports and tensioners are defined. These elements have no degrees of freedom to the vessel model. In static analyses, the position of the vessel and the coordinates of the pipe supports and tensioners on the vessel are fixed. For this model, the horizontal and vertical positions (as calculated according to Appendix C) of each pipe support and tensioner are given explicitly in the input

data. Figure 6.1 illustrates the vessel model used by OFFPIPE.

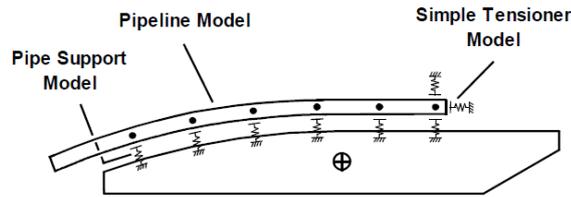


Figure 6.1: Vessel model as used by OFFPIPE [11]

6.1.2. STINGER MODEL

The stinger is modelled as a rigid extension of the vessel, meaning its curvature and position is fixed relative to the vessel. Its position or geometry does not change in response to pipe loads or other forces acting on the stinger. The positions of the pipe supports are explicitly defined similar to how the positions of the pipe supports and tensioners on the vessel are defined. In a static analysis its position is completely determined by the position of the vessel. Figure 6.2 illustrates how the vessel and stinger are modelled.

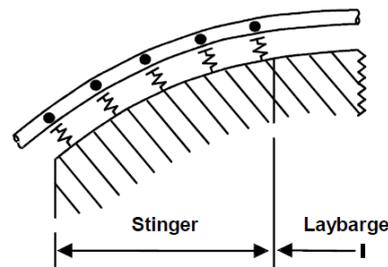


Figure 6.2: Modelling of stinger as rigid extension of the vessel [11]

6.1.3. PIPE SUPPORT MODEL

The pipe supports on the vessel and stinger are modelled as frictionless point supports. Each pipe support consists of two sets of independent rollers, as shown in figure 5.22. One pair of rollers, referred to as the bottom rollers, is approximately horizontal and used primarily to support the weight of the pipeline. The second set of rollers, referred to as the side rollers, are approximately vertical and are primarily used to restrain the lateral or transverse displacements of the pipeline.

The angles of the rollers, the stiffness of the pipe support and the roller bed length are specified in table 6.1. The height of the side rollers is assumed to be infinite and the pipeline is not permitted to climb above or over the top of the side rollers.

Table 6.1: Pipe support properties

Property	value	units
Vertical stiffness	70000	kN/m
Lateral stiffness	70000	kN/m
Bottom roller angle	30	deg
Side roller angle	0	deg
Side roller offset	1.35	m
Roller bed length	4.8	m

Each of the four rollers is modelled independently by OFFPIPE. The pipe support rollers are assumed to be cylindrical. The pipe support reactions act in the direction that is mutually perpendicular to the longitudinal axes of the pipeline and each support roller. The pipeline is free to lift off the support when appropriate. The pipe support element is illustrated in figure 6.3. The calculated support reactions are assumed to be proportional to the deformation of the rollers and is given by:

$$R_s = \delta_s \cdot K_s \quad (6.1)$$

Where

R_s is the support reaction

δ_s is the roller deformation

K_s is the roller stiffness

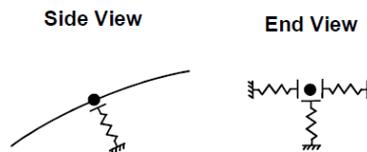


Figure 6.3: Pipe support model stiffness [11]

6.1.4. SEABED MODEL

In OFFPIPE the seabed is modelled as a continuous elastic-frictional foundation using a specially constructed soil element. In pipelaying analyses, the seabed is assumed to be linearly elastic respect to deformations in the vertical direction. In the parametric analyses, the seabed is taken as a flat surface with a depth of 1000m. As the focus of these analyses is the overbend region, no further detail is placed in the seabed modelling.

6.1.5. PIPE PROPERTIES

In the parametric analyses, the X65 material will be used unless noted otherwise. The X65 material is frequently used for pipelines laid offshore. The characteristics of the X65 material are specified in table 6.2. OFFPIPE defines the constant bending stiffness of the pipe elements with a non-linear moment-curvature relation given by a Ramberg-Osgood equation, as explained in detail in section 4.4.2. This non-linear moment-curvature relation is dependent on the material properties and the pipe geometry; the diameter and wall thickness.

Table 6.2: Properties of X65 material

Property	value	units
SMYS	448	MPa
SMTS	530	MPa
Young's modulus	207	GPa
Yield strain	0.005	-
Ultimate strain	0.220	-
Density	7850	kg/m ³

6.2. ANALYSES

In this section the sensitivity of the z-value to certain parameters is analysed. Each subsection will cover the model set-up for that specific analysis and the results that follow from it.

6.2.1. PIPE DIAMETER AND PIPE DIAMETER TO THICKNESS RATIO

MODEL SET-UP

The pipe diameter and its pipe diameter to thickness (D/t) ratio are one of the governing properties for the pipelaying configuration. These properties are often specified by the client and depend on, for example, the operational capacity of the pipe and the installation depth. As such, in most design there is not a lot of freedom in modifying these variables to increase the level of displacement control. Nevertheless it is important to investigate the effect the properties of the pipeline have on the level of displacement control.

As the local buckling checks given by DNV (equations 2.1 and 2.14) are only validated for D/t ratios between 15 and 45, the pipelines modelled will also have D/t ratios in this range. Three D/t ratios are selected for this analysis, D/t ratios of 15, 30, and 45. The pipe diameters chosen for this analysis are 15 inch, 30 inch, and 45 inch. These diameters lie within the range of regularly used dimensions for offshore pipelaying. In total 9 combinations of diameter and D/t ratio are modelled for the nominal pipe. For the weak link method, two additional variations in pipeline stiffness are created, bringing the total number of pipeline models to 27. Table 6.3 shows the characteristics of all the pipeline models.

The stinger model used in this analysis is a stinger with a stinger radius of 150m and a center-to-center rollerbox spacing of 15m. With a rollerbox length of 4.8m, this means that globally one third of the pipeline is supported on the stinger.

The naming of the pipeline models uses the following naming convention $D_{xx}D_{tyy}_{zzz}$. Where xx indicates the pipeline diameter in inches, yy indicates the D/t ratio, and zzz indicates the pipeline variation (098 for minus 2% stiffness, 102 for plus 2% stiffness).

The lay top tension is chosen such that the lift-off angle, and thus the global configuration on the stinger, of the pipeline is kept similar between the various diameters and D/t ratios. The lift-off angle is kept close to 65 degrees. The top tensions used are summarised in table 6.4.

Table 6.3: Pipeline model properties

Name	Diameter	Wall thickness	SMYS	SMTS	Ramberg-Osgood	
	<i>in</i>	<i>mm</i>	<i>MPa</i>	<i>MPa</i>	A	B
D15_Dt15_nominal	15	25.400	448	530	0.0975	10.9413
D15_Dt15_098	15	24.892	439.04	519.4	0.1060	10.8414
D15_Dt15_102	15	25.908	456.96	540.6	0.0906	11.0015
D15_Dt30_nominal	15	12.700	448	530	0.5215	7.3355
D15_Dt30_098	15	12.446	439.04	519.4	0.5900	7.1677
D15_Dt30_102	15	12.954	456.96	540.6	0.4629	7.5034
D15_Dt45_nominal	15	8.470	448	530	1.8735	6.0126
D15_Dt45_098	15	8.297	439.04	519.4	2.2550	5.9323
D15_Dt45_102	15	8.636	456.96	540.6	1.5763	6.0826
D30_Dt15_nominal	30	50.800	448	530	0.0453	12.7228
D30_Dt15_098	30	49.784	439.04	519.4	0.0465	12.7760
D30_Dt15_102	30	51.816	456.96	540.6	0.0439	12.6858
D30_Dt30_nominal	30	25.400	448	530	0.0720	12.5870
D30_Dt30_098	30	24.892	439.04	519.4	0.0747	12.5894
D30_Dt30_102	30	25.908	456.96	540.6	0.0693	12.6022
D30_Dt45_nominal	30	16.930	448	530	0.1007	12.0873
D30_Dt45_098	30	16.595	439.04	519.4	0.1053	12.0824
D30_Dt45_102	30	17.272	456.96	540.6	0.0954	12.1522
D45_Dt15_nominal	45	76.200	448	530	0.0432	12.8242
D45_Dt15_098	45	74.676	439.04	519.4	0.0443	12.8703
D45_Dt15_102	45	77.724	456.96	540.6	0.0420	12.7820
D45_Dt30_nominal	45	38.100	448	530	0.0619	12.9371
D45_Dt30_098	45	37.338	439.04	519.4	0.0633	12.9734
D45_Dt30_102	45	38.862	456.96	540.6	0.0603	12.9061
D45_Dt45_nominal	45	25.400	448	530	0.0727	12.9006
D45_Dt45_098	45	24.892	439.04	519.4	0.0749	12.9051
D45_Dt45_102	45	25.908	456.96	540.6	0.0713	12.8321

Table 6.4: Top tensions for diameter and D/t ratio analyses

Diameter <i>in</i>	D/t ratio –	Top tension <i>kN</i>
15	15	3860
15	30	2000
15	45	1350
30	15	15450
30	30	8000
30	45	5390
45	15	32850
45	30	17000
45	45	11460

RESULTS

The z-values for each pipe support on the stinger are calculated using the weak link method as described in chapter 4. The average z-values of the stinger are presented in figure 6.4. As can be seen, for the larger diameter pipelines, 30 inch and 45 inch, the D/t ratio of the pipeline only has a marginal effect on the level of displacement control in a specific stinger configuration. For the smaller diameter pipe of 15 inch the D/t ratio has larger influence on the level of displacement control. No clear correlation between the increase of D/t ratio and the increase or decrease of z-value follows from this analysis. What is clear however, is the link between pipe diameter and z-value. The larger the diameter is, the greater the level of partial displacement control. And thus, generally speaking, a greater stiffness of the pipe leads to a greater degree of displacement control, as also described by Williams, Williams, and Walker [22].

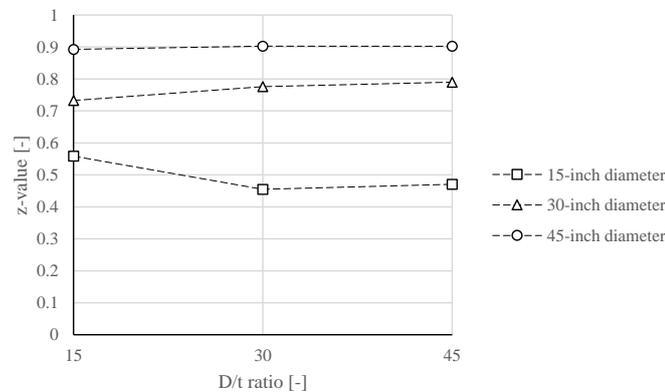


Figure 6.4: Effect of pipe diameter and D/t ratio on z-value

6.2.2. STINGER RADIUS AND ROLLERBOX SPACING

MODEL SET-UP

The stinger shape limits the maximum bending the pipeline can experience in the overbend section. The maximum global bending follows from the stinger radius. During pipelaying the stinger radius is often kept close to constant if possible, but may vary along the stinger due to the stinger shape or limits in configuration of the rollerboxes. For these analyses the stinger radius is constant. The local maximum bending follows from the combination of stinger radius and rollerbox spacing. If the pipeline is continuously supported, meaning a rollerbox spacing of 0, the local maximum bending

will be equal to the global maximum bending. If the rollerboxes are spaced further apart, the local maximum bending will be greater than the global maximum bending.

To study the effect of the stinger radius and the rollerbox spacing on the degree of displacement control, several variations in stinger models are created. Stinger radii of 100m, 150m, and 250m are combined with rollerbox center-to-center spacing of 10m, 15m, and 20m. The stinger models are created using the method described in appendix C. These 9 combinations are then used in combination with three different pipe diameters of 15 inch, 30 inch, and 45 inch, all with a d/t ratio of 30. The pipeline properties can be found in table 6.3.

RESULTS

The z-values for each pipe support are calculated using the weak link method. The average z-values of the various stingers are calculated. The results of the total of 21 models are ordered by pipe diameter and shown in figures 6.5, 6.6, and 6.7. As can be seen in the figures, the effect of rollerbox spacing on the degree of displacement control is obvious. The entire discussion whether a stinger should be considered displacement or load controlled stems from the fact that the stinger does not continuously support the pipeline. The effect of the spacing on the z-value completely agrees with the notion that less support of the stinger leads to less displacement control. What should be noted is that even with a very large rollerbox spacing of 20m, larger than you would typically find on real stingers, the situation is still not fully load controlled. For the larger diameter pipelines of 30 inch and 45 inch the situation is still closer to displacement control than load control in this case. An increase in stinger radius has a negative effect on the level of displacement control. With a larger stinger radius the pipe will experience less bending and lower moments. A possible cause could be that other parameters, such as lay tension, have a greater effect on the bending along the pipeline compared to the stinger radius for larger stinger radii than for smaller radii, and thus the pipeline bending conforms less to the global maximum bending. The results also show that the larger diameter, and thus larger stiffness, pipes are less influenced by changes in stinger radius or rollerbox spacing. This indicates that stiffer pipelines are less susceptible to behave in a load controlled fashion, regardless of the stinger configuration.

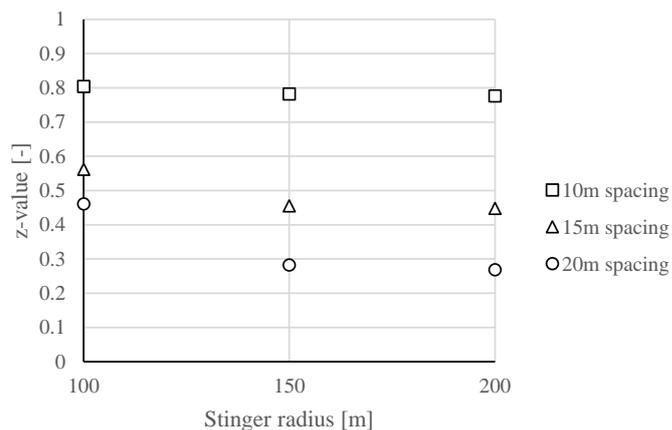


Figure 6.5: Effect of stinger radius and rollerbox spacing on z-value. Pipe diameter: 15 inch, D/t ratio of 30

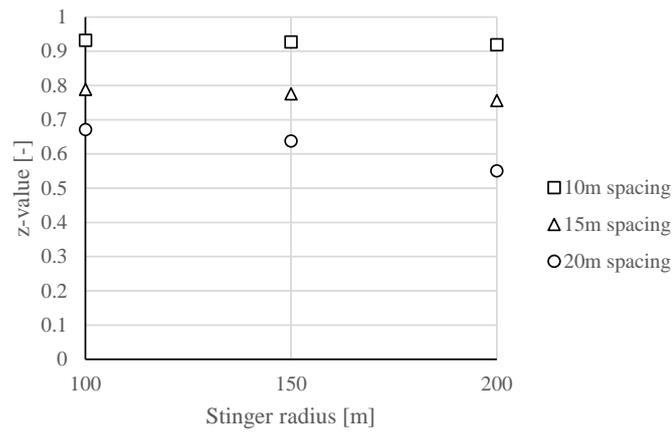


Figure 6.6: Effect of stinger radius and rollerbox spacing on z-value. Pipe diameter: 30 inch, D/t ratio of 30

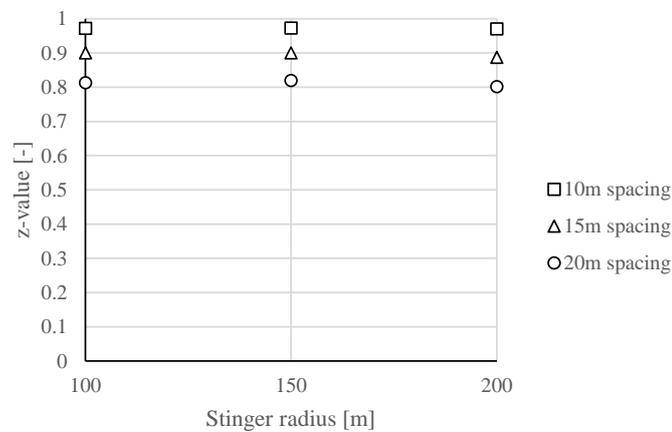


Figure 6.7: Effect of stinger radius and rollerbox spacing on z-value. Pipe diameter: 45 inch, D/t ratio of 30

6.2.3. TENSION

MODEL SET-UP

The effect of lay tension on the level of displacement control for three different pipe diameters is investigated in this section. The stinger used in these analyses has a 150m radius and 15m rollerbox spacing. The pipes used in these models have diameters of 15 inch, 30 inch, and 45 inch, and all have a D/t ratio of 30. The properties of these pipes can be found in table 6.3. The basecase top tensions used are listed in table 6.4. For the 15 inch pipe a basecase tension of 2000kN is used. For the 30 inch pipe a basecase tension of 8000kN, and for the 45 inch pipe a basecase tension of 17000kN is used. To investigate the effect of tension on the z-value, three additional analyses are done for each pipe diameter where the basecase tension is increased with 1000kN for each analysis.

RESULTS

The z-values per node are computed with the weak link method. Next, the average z-value for the stinger is calculated. Figures 6.8, 6.9, and 6.10 show the resulting average z-values versus the lay tension. As can clearly be seen from figures 6.8 and 6.9 an increase in lay tension in the pipelaying situation has a negative effect on the level of displacement control of the situation. However, the effect of the lay tension diminishes with increasing pipe diameter. For the largest diameter of 45 inch, the effect of lay tension is almost negligible. This corresponds with the findings in the two previous

parametric analyses that the larger the diameter of the pipe is, the less the level of displacement control is influenced by the laying configuration. The consequence of the tension effect on the level of displacement control is that when determining the level of displacement control of the stinger in a dynamic pipelay analysis the highest top tension found should be considered governing.

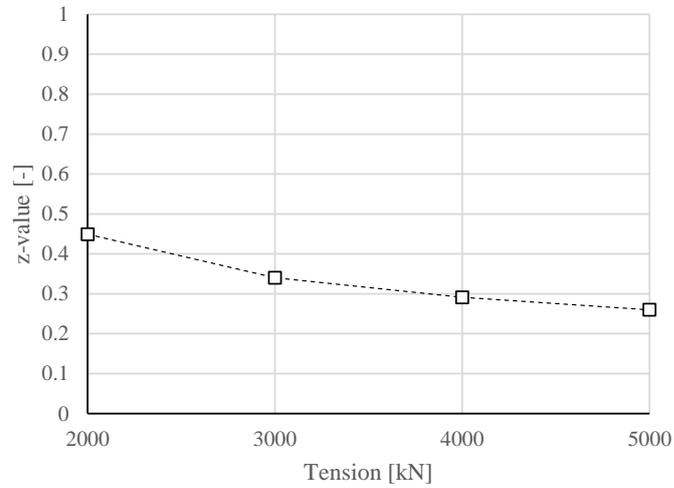


Figure 6.8: Effect of tension on z-value. Pipe diameter of 15 inch

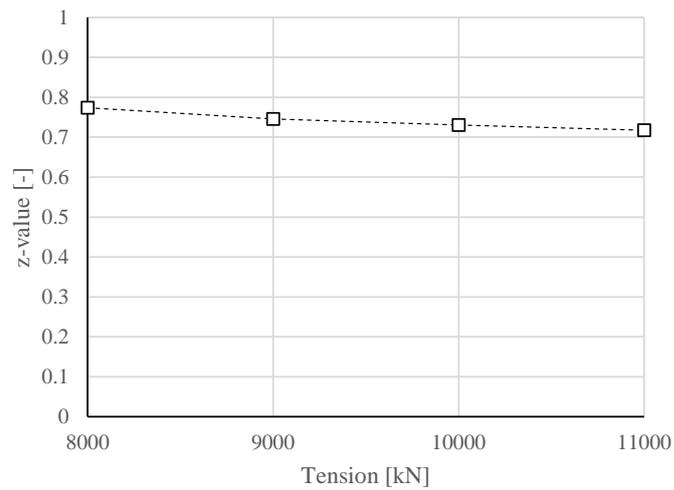


Figure 6.9: Effect of tension on z-value. Pipe diameter of 30 inch

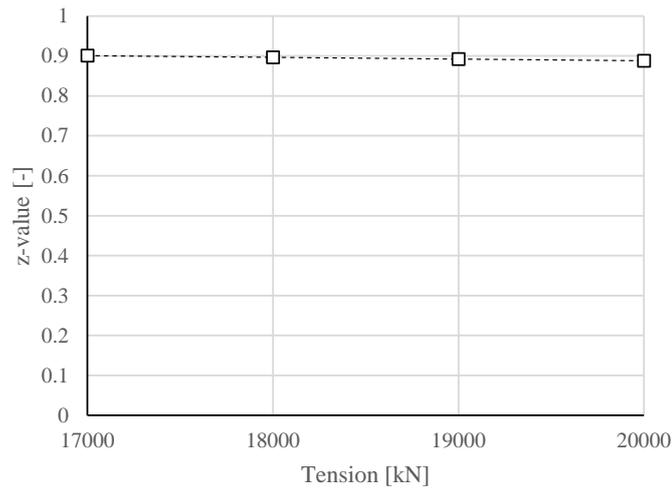


Figure 6.10: Effect of tension on z-value. Pipe diameter of 45 inch

6.2.4. ROLLERBOX LOAD

MODEL SET-UP

Finally, the effect of changes in the rollerbox load on the level of displacement control are studied. A stinger with a radius of 150m and a rollerbox spacing of 15m is taken as the basis for this analysis. The pipe used has a diameter of 15 inch and a D/t ratio of 30. The top tension used is 2000kN. To increase the load on a single rollerbox, the location of a rollerbox in the middle of the stinger is adjusted. The rollerbox is adjusted by moving it higher, normal to the stinger curve, as illustrated in figure 6.11. This effectively decreases the local radius at the adjusted rollerbox while simultaneously increasing the local radii of the two neighbouring rollerboxes. For the basecase no rollerbox is adjusted, for the two following analyses the middle rollerbox is moved by 200mm and 400mm, leading to respectively a local radius of 120m and 100m.

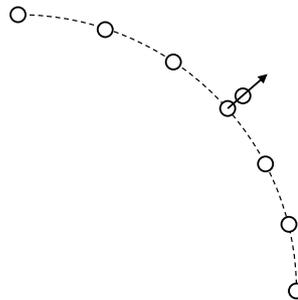


Figure 6.11: Adjustment of rollerbox node on stinger

RESULTS

The resulting rollerbox loads along the stinger are shown in figure 6.12. As can be seen, for the unadjusted basecase the rollerbox load is constant for the entire stinger. In the two following analyses the height of rollerbox number 5 is increased. Adjusting one rollerbox height leads to increased loads at this rollerbox and reduced loads at the neighbouring rollerboxes. The local bending of the pipe at the adjusted rollerbox also increases with increasing rollerbox height. The calculated z-value per rollerbox node is shown in figure 6.13. As can clearly be seen when comparing figure 6.13 to figure 6.12 the increase and decrease in z-value follows the same relation as the rollerbox loads. The in-

crease of level of displacement control at a higher rollerbox, and thus higher strain, agrees with the findings in section 6.2.2. There the level of displacement control is higher for smaller stinger radii, and thus for higher strain levels.

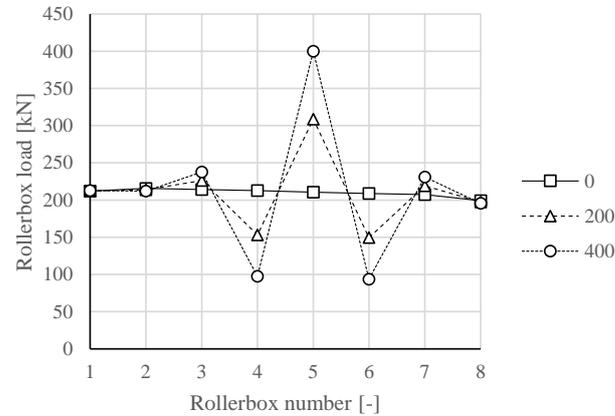


Figure 6.12: Effect of rollerbox height on rollerbox load

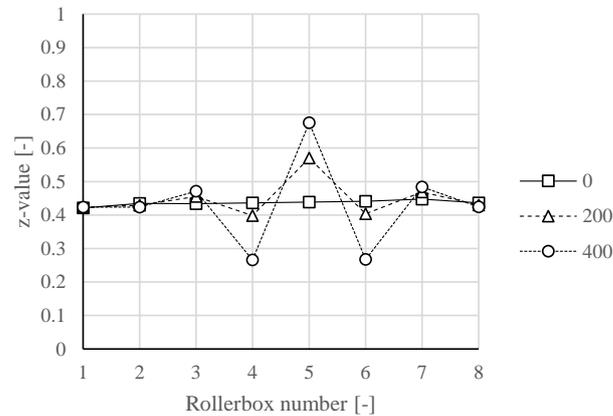


Figure 6.13: Effect of rollerbox load on z-value

6.3. EFFECT ON LOCAL BUCKLING LIMITS

To illustrate the effect of the level of partial displacement control on the maximum allowable moment and strain values in a pipeline, the local buckling limits are calculated for a 30 inch pipeline of X65 steel. As can be seen in figure 6.4, the z-values for a 30 inch pipeline in that specific situation fall in a range between 0.70 and 0.80. For this example an average z-value of 0.75 is chosen. All safety factors and installation conditions are taken as the same as in section 2.3.4 and can be found in table 2.8.

The load controlled and displacement controlled buckling limits are calculated using equations 2.20, 2.21, and 2.22. The partially displacement controlled limit is then calculated using equation 4.4 and the method described in section 4.2.1. The resulting limiting functional moments (M_F), curvatures (K_F), and strains (ϵ_F) are summarised in table 6.5. The positions of the limits belonging to the load controlled, displacement controlled, and partially displacement controlled buckling checks on the normalised moment-curvature relation are shown in figure 6.14.

Table 6.5: Local buckling limits for 30 inch pipe

D/t ratio		M_F	K_F	ϵ_F
–		kNm	m^{-1}	–
15	load control	7641	0.005	0.0020
	displacement control	12496	0.045	0.0171
	partial displacement control	12197	0.035	0.0132
22	load control	5375	0.005	0.0019
	displacement control	8536	0.028	0.0107
	partial displacement control	8327	0.022	0.0085
30	load control	4013	0.005	0.0018
	displacement control	6214	0.019	0.0072
	partial displacement control	6033	0.015	0.0058
45	load control	2563	0.005	0.0017
	displacement control	3721	0.010	0.0037
	partial displacement control	3580	0.008	0.0032

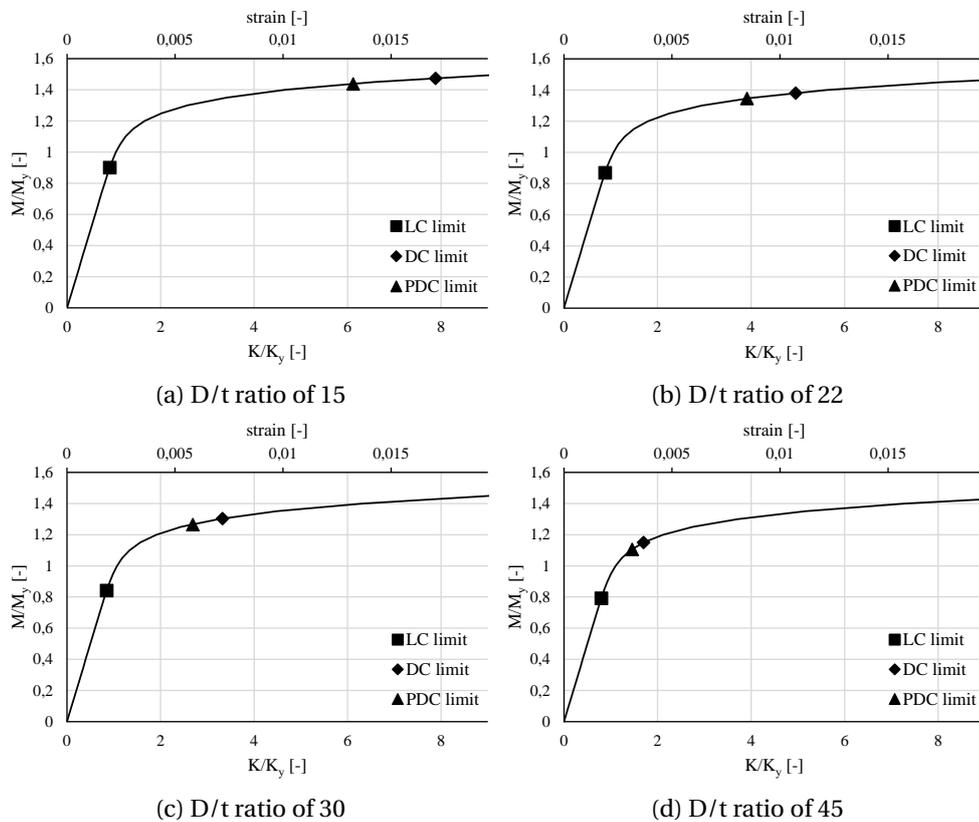


Figure 6.14: Local buckling limits for various D/t ratios

The limiting functional moments for the partially displacement controlled condition with a z-value of 0.75 are combined with the local buckling limits of figure 2.5 and are shown in figure 6.15 to illustrate the shift in allowable functional moment from a displacement controlled condition to a partially displacement controlled condition.

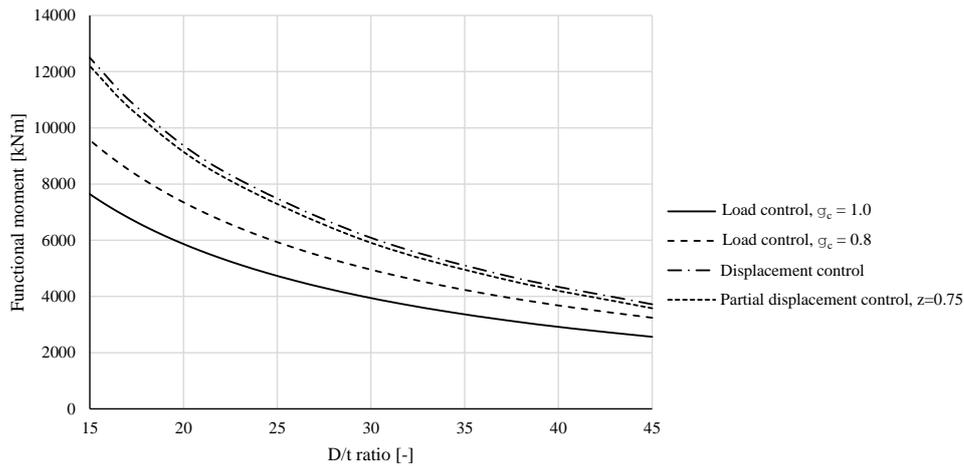


Figure 6.15: Maximum allowable moments per buckling check

Finally, the partial displacement control factor γ_{pdc} per D/t ratio is calculated using equation 4.6. The resulting partial displacement factors are shown in figure 6.16.

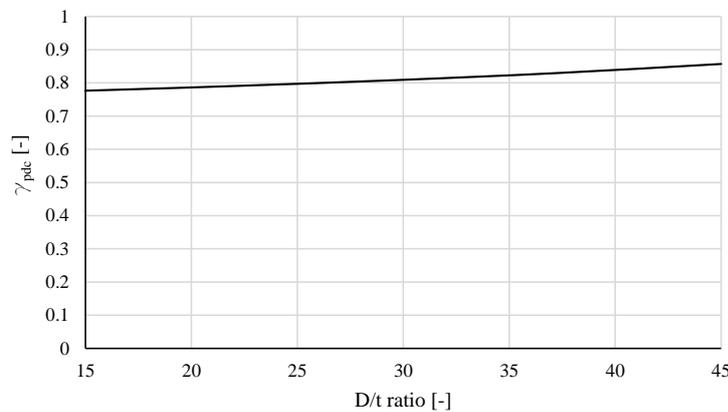


Figure 6.16: Partial displacement control factor per D/t ratio

As can be seen in figure 6.15, even with a level of displacement control of 75%, the allowable functional moment is only slightly lower than the limiting moments for a fully displacement controlled condition. Furthermore, the use of the weak link method and its case specific determination of the level of displacement control allows for a higher utilisation of the pipeline than the non-case specific method of dealing with partially displacement controlled conditions using the load controlled buckling check with a γ_c of 0.8.

6.4. CHAPTER CONCLUSIONS

The effect of several pipelaying parameters on the level of displacement control have been investigated. The findings in this chapter will allow an engineer in the design phase of a S-lay project to influence the level of displacement control when necessary. The effect of the pipe diameter on the z-value is clear. The larger the diameter of the pipe, the greater the level of displacement control of the situation is. Unfortunately no clear correlation between the D/t ratio and the z-value has been found, further study with more diameters and D/t ratios is needed to get a clear picture of their relations to the z-value. Variations in the stinger configuration have been investigated. As expected

the spacing of the rollerboxes, and thus how often the stinger supports the pipe, is of great impact on the level of displacement control. With longer free spanning pipe sections in between the rollerboxes, the condition tends to move towards a more load controlled situation. The global radius of the stinger, and thus also the global strain levels, appear to have an influence on the z -value. The situation becomes more displacement controlled when the global strains are higher. This finding is also confirmed by the effect of the rollerbox loads on the z -value. A local increase in strain also increases the level of displacement control locally. This is positive, because a situation with high strains is often also the situation where the buckling checks tend to become more critical. Finally, the effect of tension on the z -value and its implications for dynamic analyses is studied. Higher tensions lead to lower degrees of displacement control.

7

CONCLUSIONS AND RECOMMENDATIONS

At the beginning of this project the following goal was set:

To develop a method to determine the project specific level of partial displacement control, and to apply this percentage to the current DNV load controlled and displacement controlled formulations such that it can be used for future projects.

To achieve this goal, a method for determining the level of displacement control and modification of the displacement controlled buckling check formulation is proposed. Subsequently the method is validated and a parametric study is performed. In this chapter will be reflected on the goal set by providing the conclusions in section 7.1 and recommendations in section 7.2.

7.1. CONCLUSIONS

At the start of this project three research questions were defined to achieve the project goal. These three questions will be revisited in this section along with the findings of this research.

The first question is how the level of displacement control in the stinger overbend situation can be determined. In this report a method for determining the level of displacement control using pipe stiffness variations in the stinger overbend is proposed. This method is based on a method for determining the level of displacement control in the sagbend region and is modified to allow its application in the overbend region. A first step towards validation of the proposed method through detailed FEM analyses has been taken with satisfactory results. Three detailed shell element models are created for a load controlled, displacement controlled, and partially displacement controlled situation. The onset of buckling for all three situations is determined. The partially displacement controlled situation is recreated using a beam element model and the weak link method is applied. The level of displacement control calculated with the weak link method is within a 2% difference to the ratio of the onset of buckling in the partially displacement controlled shell element model to the onsets of buckling in the load controlled and displacement controlled shell element models. In conclusion, the use of the proposed weak link method shows potential and allows the calculation of the level of displacement control using less detailed beam element models and with reduced computational time.

The second research question is how the calculated level of displacement control can be implemented in a local buckling check. In this thesis an approach to translate the calculated level of displacement control to a usable buckling check equation is given. Using this approach the z-value is converted to a new factor, γ_{pdc} , to be implemented in the displacement control buckling check.

By using the original displacement control buckling check as the basis for the new partial displacement control buckling check, the LRFD philosophy of DNV and the matching safety factors are kept constant. Example calculations show the resulting γ_{pdc} for a partially displacement controlled condition. Furthermore, the calculations show the increase in utilisation this method provides compared to the non-case specific method for accounting the partially displacement controlled condition using a load controlled local buckling check with a γ_c of 0.8, illustrating that calculating the case specific partially displacement controlled conditions can allow for a more optimised pipeline design.

The final research question of the thesis is how the level of displacement control is influenced by the pipe and stinger configuration. Different modifications were made to the pipe properties and the stinger configuration and their effect on the level of displacement control was studied using the proposed approach. The studied variants consist of change in stinger radius; change in rollerbox spacing; change in pipe diameter; change in pipe diameter to wall thickness ratio; change in tension and change in rollerbox load. The variants were studied by analysing the average level of displacement control on the stinger for each situation. Of greatest influence on the level of displacement control are the pipe diameter and the spacing of the rollerboxes. Larger diameter pipes behave in a more displacement controlled fashion compared to pipes with a smaller diameter. The degree of displacement control of pipes with a large diameter is also less sensitive to changes in the pipelaying configuration compared to smaller diameter pipes. The spacing of the rollerboxes greatly affects the amount of displacement control in the situation. As expected, increasing the free span of the pipe in between rollerboxes leads to a reduction in displacement control. The amount of strain the pipeline experiences also appears to influence the degree of partial displacement control, albeit to a lesser extent. The increase of tension has a negative effect on the level of displacement control. The implication of this is that for dynamic analyses the highest expected tension should be considered governing in the computation of the degree of partial displacement control.

In conclusion, the research done for this thesis provides an engineering approach to determine the partial displacement conditions in the S-lay configuration as well as an understanding of the influence of various pipelaying variables on the partial displacement condition.

7.2. RECOMMENDATIONS

Although this project gives more insight on the partially displacement controlled conditions in the overbend, further work could increase its understanding. The scope of the research done in this project was limited. The following topics are considered as interesting and valuable for further investigation.

Validation of weak link method in combination with smaller diameter pipelines: First steps have been taken to validate the proposed method in combination with the large diameter, thin walled Nord Stream pipeline. To further validate and prove the robustness of the proposed method a validation with smaller diameter, thicker walled pipeline is suggested. The pipeline used in the Rota 3 project, which is 24 inch diameter with a wall thickness of 28.2mm, would be a good candidate for this analysis. Care has to be taken in defining the onset of local buckling in these analyses. In contrast to the local buckling in thin walled pipes where denting of the pipeline can be observed, the local buckling in thick walled pipelines happens more gradually over a longer section of pipeline. Strict criteria to determine the start of local buckling must be defined to correctly determine the local buckling limits in these analyses.

Partially displacement controlled behaviour of concrete coated pipe: Large diameter pipelines are often concrete coated to increase the weight of the pipe to combat the buoyancy and add stability on the seabed. Due to the difference in bending stiffness between the concrete coated pipe and the field joint coated pipe, stress and strain concentrations will occur at the location of the field joint. It is recommended to investigate the effect of these stress and strain concentrations on the level of displacement control. It is likely that for such a pipe the location of the field joint relative to the rollerbox is of importance, and thus resulting in fluctuating z-values as the pipeline travels over the stinger. It is suggested to start this research by creating detailed FE models of the concrete coated and field joint coated pipe sections over a detailed stinger model in a software like Abaqus. When the effect of concrete coating on the partially displacement controlled condition is understood, steps can be taken to implement these findings in a beam element FE package such as OFFWIN.

Effect of local disturbances on level of displacement control: In most pipelaying projects not only regular pipelines are installed, but also inline structures. Some of these structures can be installed with the S-lay method. Often the structures are too complex to allow the use of the relatively simple DNV buckling checks and require more detailed analysis. However, the inline structures have an effect on the connected pipe sections. As the inline structures are much stiffer than a regular pipe section, the pipeline configuration when the inline structure passes the stinger is changed. To understand the impact this has on the degree of partial displacement control of the pipeline further study on this topic is recommended. It is expected that each time the inline structure passes over a rollerbox the level of displacement control of the neighbouring pipeline sections is drastically changed. Similar to the study on the effect of concrete coated pipe on the level of displacement control, it is suggested to first create detailed FE models in a software package such as Abaqus before attempting to translate the findings to a simpler software such as OFFPIPE.

Further parametric studies: The parametric studies performed for this thesis research have found several relations between parameters and computed levels of displacement control. Unfortunately the exact relation of some parameters, such as the D/t ratio, to the calculated z-values have not been found. For full understanding of the partial displacement conditions, further studies are recommended.

Applying the proposed method to an existing project: Now that the first steps to validate the proposed method have been taken with satisfactory results and the effect of various pipelaying parameters on the level of displacement control have been studied, it would be interesting, especially for Allseas, to apply the proposed method to one of the existing projects to study how the design of the pipeline would have changed. Considering that the first steps of validation of the method have been performed on Nord Stream pipeline, the Nord Stream project would make a logical first candidate for this study. The Nord Stream project has been designed with displacement controlled local buckling checks, as such applying the proposed method to this project will lead to a loss in workability. Therefore it would be interesting to compare this loss in workability to the loss in workability that would occur if the project was forced to account for the partially displacement controlled conditions using the load controlled buckling check with a γ_c of 0.8. With this study the time and financial impact of the proposed method can also be investigated.

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A

CURVE LENGTH

If a curve in \mathbb{R}^2 is defined by the equation $y = f(x)$, and f is continuously differentiable the curve length is given by:

$$s = \int_a^b \sqrt{1 + \left(\frac{dy}{dx}\right)^2} dx \quad (\text{A.1})$$

The Ramberg-Osgood equation for the moment-curvature relationship is given by:

$$\frac{K}{K_y} = \frac{M}{M_y} + A \cdot \left(\frac{M}{M_y}\right)^B \quad (\text{A.2})$$

If $\frac{K}{K_y} = y$ and $\frac{M}{M_y} = x$ then the equation looks as follows:

$$y = x + A \cdot x^B \quad (\text{A.3})$$

leading to:

$$\frac{dy}{dx} = 1 + A \cdot B \cdot x^{(B-1)} \quad (\text{A.4})$$

Substituting equation A.4 in equation A.1 we get:

$$s = \int_a^b \sqrt{1 + (1 + A \cdot B \cdot x^{(B-1)})^2} dx \quad (\text{A.5})$$

Finally, substituting $x = \frac{M}{M_y}$ in equation A.5 gives:

$$s = \int_a^b \sqrt{1 + \left(1 + A \cdot B \cdot \left(\frac{M}{M_y}\right)^{(B-1)}\right)^2} d \frac{M}{M_y} \quad (\text{A.6})$$

B

RAMBERG-OSGOOD CURVE FIT OF MOMENT-CURVATURE

If the moment-curvature relation is known but needs to be expressed in a Ramberg-Osgood relation for OFFWIN the following method can be used. OFFWIN uses the normalised moment-curvature relation in its calculations. Thus the known moment-curvature relation must first be normalised. The general form of the Ramberg-Osgood moment-curvature relation used in OFFWIN is:

$$K^* = M^* + A(M^*)^B \quad (\text{B.1})$$

with:

$$K^* = \frac{K}{K_y} \quad (\text{B.2})$$

$$M^* = \frac{M}{M_y} \quad (\text{B.3})$$

$$M_y = \frac{2 \cdot \sigma_y \cdot I_c}{D} \quad (\text{B.4})$$

$$K_y = \frac{2 \cdot \sigma_y}{E \cdot D} \quad (\text{B.5})$$

$$I_c = \frac{\pi}{64} (D^4 - (D - 2 \cdot t)^4) \quad (\text{B.6})$$

Where: K^* is the normalised curvature
 K_y is the yield curvature of the pipe
 M^* is the normalised bending moment
 M_y is the yield moment of the main pipe
 I_c is the pipe inertia
 A is the Ramberg-Osgood coefficient
 B is the Ramberg-Osgood exponent
 D is the pipe steel outside diameter
 t is the pipe steel wall thickness

The Ramberg-Osgood coefficient and exponent are calculated using curve-fitting with the following equations [23]:

$$B = \frac{-\log\left(\frac{K_2^* - M_2^*}{K_1^* - M_1^*}\right)}{\log\left(\frac{M_1^*}{M_2^*}\right)} \quad (\text{B.7})$$

$$A = (K_1^* - M_1^*) (M_1^*)^{-B} \quad (\text{B.8})$$

(K_1^*, M_1^*) and (K_2^*, M_2^*) are two points on the normalised moment-curvature curve. Figure B.1 shows two possible places for the sample locations.

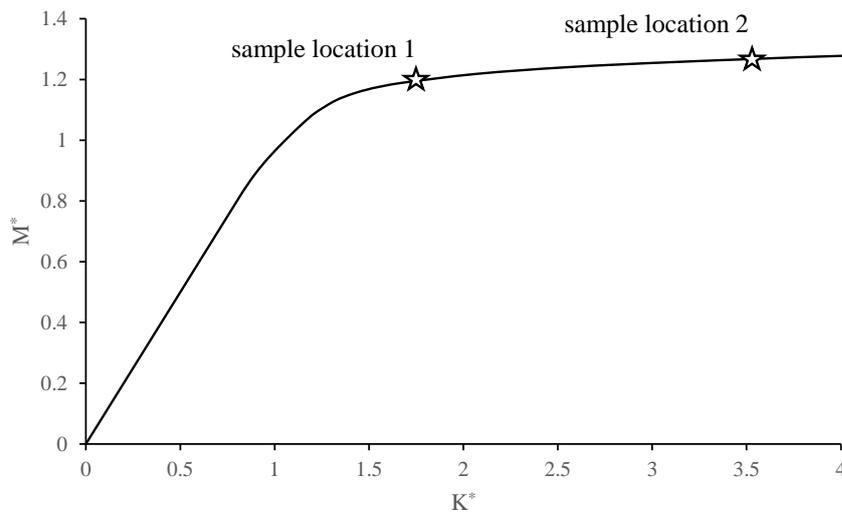


Figure B.1: Locations for the Ramberg-Osgood curve fit

C

STINGER COMPUTATION

In OFFPIPE the locations of the pipe supports on the vessel and stinger are explicitly defined by their coordinates and angle. For the analyses in the parametric study, “perfect” stingers are used. Meaning that they have a constant radius and consistent spacing between the rollerboxes. This section will explain how the coordinates and angles of the rollerboxes are computed.

All coordinates of the pipe supports are defined in the local vessel coordinate system. In OFFPIPE it is standard practice to place the origin of the vessel coordinate system at the stern of the vessel, as shown in figure C.1.

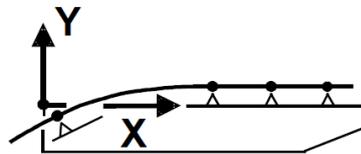


Figure C.1: Vessel coordinate system [11]

The input for the stinger coordinates calculation are the global stinger radius, the rollerbox spacing, and the coordinates for the start of the stinger. Before the pipeline moves over the stinger, it moves over the firing line. This is modelled as a straight and horizontal region with pipe supports and a tensioner at the beginning. The last pipe support of the firing line is taken as the start of the stinger. The x-coordinates, y-coordinates, and angle of the pipe support are computed with the following equations:

$$x_n = -\sin\left(n \cdot \frac{d}{R}\right) \cdot R + x_0 \quad (\text{C.1})$$

$$y_n = \cos\left(n \cdot \frac{d}{R}\right) \cdot R + y_0 - R \quad (\text{C.2})$$

$$\alpha_n = \frac{1}{2}\pi - n \cdot \frac{d}{R} \quad (\text{C.3})$$

Where: R is the global stinger radius
 d is the distance between two rollerboxes
 x_0 and y_0 are the coordinates of the start of the stinger
 n indicates the rollerbox number after the start of the stinger
 α_n is the rollerbox rotation at the n^{th} rollerbox
 x_n and y_n are the coordinates at the n^{th} rollerbox

OFFPIPE requires the rollerbox angle input to be in degrees, so α_n should be translated from radians to degrees.

D

MATLAB CODE

```
clear;
clc;
dbstop if error;

dinfo = dir('*.out');

%% Load Offpipe input

for N = 1 : length(dinfo)
    thisfilename = dinfo(N).name;
    fid = fopen(thisfilename);
    tline = fgetl(fid);
    [k1,k2,k3,k4,k5,k6,k7] = deal(1);

    while ischar(tline)
        colonLocation = strfind(tline, 'STEEL MODULUS OF ELASTICITY
            .....');
        if ~isempty(colonLocation)
            subString = tline(40:50);
            modulus(N,k1) = str2double(subString);
            k1 = k1 + 1;
        end
        colonLocation = strfind(tline, 'STEEL OUTSIDE DIAMETER
            .....');
        if ~isempty(colonLocation)
            subString = tline(40:50);
            OD(N,k2) = str2double(subString);
            k2 = k2 + 1;
        end
        colonLocation = strfind(tline, 'STEEL WALL THICKNESS
            .....');
        if ~isempty(colonLocation)
            subString = tline(40:50);
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WT(N,k3) = str2double(subString);
k3 = k3 + 1;
end
colonLocation = strfind(tline, 'YIELD STRESS
.....');
if ~isempty(colonLocation)
subString = tline(40:50);
YS(N,k4) = str2double(subString);
k4 = k4 + 1;
end
colonLocation = strfind(tline, 'RAMBERG-OSGOOD COEFFICIENT
.....');
if ~isempty(colonLocation)
subString = tline(40:49);
RC(N,k5) = str2double(subString);
k5 = k5 + 1;
end
colonLocation = strfind(tline, 'RAMBERG-OSGOOD EXPONENT
.....');
if ~isempty(colonLocation)
subString = tline(40:49);
ROE(N,k6) = str2double(subString);
k6 = k6 + 1;
end
colonLocation = strfind(tline, 'PIPE SECTION LENGTH
.....');
if ~isempty(colonLocation)
subString = tline(40:50);
SectionL(N,k7) = str2double(subString);
k7 = k7 + 1;
end
tline = fgetl(fid);
end
fclose(fid);
end

%% Load Offpipe results

for N = 1 : length(dinfo)
thisfilename = dinfo(N).name;
fid = fopen(thisfilename);
linenumber = 1;
k = 1;
outputtog = [];
while ~feof(fid)
tline = fgetl(fid);
compare = strcmp(tline, ' NODE      PIPE      X      Y
      VERT      PIPE      SUPPORT  SEPARA  AXIAL  BENDING

```

```

    TENSILE    BENDING    TOTAL    PERCNT');
if compare
linecmppos(N,k) = linenumber;
k = k + 1;
end
linenumber = linenumber + 1;
end
fclose(fid);
for n = 1:k-1;
fid = fopen(thisfilename);
outputpart = cell2mat(textscan(fid,'%f %*s %f %f %f %f %f %f %f
    %f %f %f %f %f','headerlines',linecmppos(N,n)+3));
outputtlog = vertcat(outputtlog,outputpart);
fclose(fid);
end
output{N} = outputtlog;
end
clear k

%% Analysis part

Node = 32;    %Node number for analysis
WL = 1;      %Weak link section number

stinger_radius = 90;
stinger_K = 1/stinger_radius;
stinger_eps = (0.4572/2)/stinger_radius;

Ic = pi/4*((OD/2).^4-(OD/2-WT).^4) /10^8;
Ky = 2*YS./(modulus.*OD) *100;
My = 2*Ic.*YS./OD *10^5;
%%
MMy = 0:0.00001:1.6;
for k = 1:size(RC,1);
KKy(k,:) = MMy + RC(k,WL)*MMy.^ROE(k,WL);
end
%%
K = bsxfun(@times,KKy,Ky(:,WL));
M = bsxfun(@times,MMy,My(:,WL));

% pre-allocating for speed
BM = zeros(length(output{1}),length(output));
BK = zeros(length(output{1}),length(output));
Kalc = zeros(length(output{1}),length(output));
Madc = zeros(length(output{1}),length(output));
Lw = zeros(length(output{1}),length(output));
Ldc = zeros(length(output{1}),length(output));

for n = 1:length(output);

```

```

for N = 1:length(output{n});
BM(N,n) = abs(output{n}(N,9));
BK(N,n) = (BM(N,n)'./My(n,WL) + RC(n,WL).*(BM(N,n)'./My(n,WL)).^
    ROE(n,WL)).*Ky(n,WL);
Kalc(N,n) = (BM(N,1)'./My(n,WL) + RC(n,WL).*(BM(N,1)'./My(n,WL))
    .^ROE(n,WL)).*Ky(n,WL);

error = 1;
error_prev = 1;
M_find = 0;
step = 100;
%steps = 0;
while abs(error) > 0.000000001
K_find = (M_find/My(n,WL) + RC(n,WL)*(M_find/My(n,WL))^ROE(n,WL)
    )*Ky(n,WL);
error = BK(N,1) - K_find;
Madc(N,n) = M_find;
if sign(error)*sign(error_prev)<0
step = step/2;
end
if error >0
M_find = M_find + step;
else
M_find = M_find - step;
end
error_prev = error;
end

ROc{n,1} = @(MMyc) sqrt(1+(1+RC(n,WL).*ROE(n,WL).*MMyc.^(ROE(n,
    WL)-1)).^2);
Lw(N,n) = integral(ROc{n},BM(N,n)./My(n,WL),BM(N,1)./My(n,WL));
Ldc(N,n) = integral(ROc{n},Madc(N,n)./My(n,WL),BM(N,1)./My(n,WL)
    );
end
end

z = Lw./Ldc;

%% Visualising results

sections = [-2 2]; %Percentage variations in pipe stiffness
stinger_start_node = 21;
stinger_end_node = 82;

for n = 1:length(output);
stinger_start = find(output{n}(:,1)==stinger_start_node);
stinger_end = find(output{n}(:,1)==stinger_end_node);
end

```

```
n = 1;
for k = stinger_start:stinger_end;
if ~any(isnan(z(k,:))) && output{1}(k,6) > 0
z_nominal(n) = interp1(sections,z(k,2:end),0,'spline');
nodes(n) = output{1}(k,1);
n = n+1;
end
end

figure('Name','z-value per node')
hold on
plot(nodes,z_nominal);
title('z-value per node')
xlabel('Node number [-]')
ylabel('z-value [-]')
ylim([0 1])
hold off
```