Explosion safety evaluation for congested offshore platforms based on CFD simulations

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ABSTRACT

This thesis is to evaluate the safety of congested offshore platforms subjected to vapour cloud explosion. A new confinement specific correlation was developed to predict explosion consequence based on computational fluid dynamics (CFD). A data-dump technique was proposed to improve accuracy of CFD modelling in investigating safety gap effect on blast wave propagation. Overpressure mitigating measures such as safety gap and blast wall in a cylindrical floating liquefied natural gas (FLNG) vessel were studied. Through comprehensive CFD modelling, a quantitative explosion risk assessment method was developed to provide up-to-date techniques on safety design of large scale offshore structures.

One of the major goals of this study is to develop an efficient vapour cloud explosion overpressure prediction approach in a confined and congested environment. A confinement specific correlation (CSC) was developed based on large-scale numerical simulations carried out for offshore structures with complex components. By using the commercial software of FLACS, the CSC produced a significant improvement of the Guidance for the Application of the Multi-Energy method (GAME), especially for highly congested and confined environment. The parameters used to define the confinement, the volume blockage ratio, and the flame path distance etc. were redefined and validated by using the CSC model. The proposed CSC correlated better with the CFD simulation results along with the same calculation efficiency of the GAME correlation. The irregularity effect of the congestions was further investigated by simulating artificial and realistic platform modules.

The investigation of the effectiveness of overpressure mitigating measures is another major goal in this research. A data-dump technique, which modifies the calculation of the turbulence length scale in different safety gap simulations, was firstly proposed to improve the explosion overpressure calculation accuracy during the investigation of the cutting-edge explosion mitigating measure – safety gap. Furthermore, the safety gap effect was evaluated in the vapour cloud gas dispersion and explosion simulations on a cylindrical FLNG platform. The worst scenario studies indicated that the safety gap is active in reducing gas cloud size and mitigating gas explosion overpressures by using proper safety gap designs. Finally yet importantly, a probabilistic approach, which offers more detailed risk analysis data, was performed to assess the blast wall effect on overpressure mitigation on the cylindrical FLNG platform. By considering various uncertainties, exceedance curves were derived and the most efficient blast wall design configuration was achieved.
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Aug 2015
STATEMENT OF CANDIDATE CONTRIBUTION

This thesis is composed of my original work, and contains no material previously published or written by another person except where due reference has been made in the text.

I have obtained the permission of all other author to include the published works in this thesis. I have clearly stated the contribution by others to jointly-authored works that I have included in my thesis, including experimental assistance, professional editorial advice, and any other original research work used or reported in my thesis.

Print Name: Jingde Li    Signature:    Date:
PUBLICATIONS ARISING FROM THIS THESIS

Journal papers

1. Jingde Li, Madhat Abdel-jawad, Guowei Ma, New correlation for vapor cloud explosion overpressure calculation at congested configurations, *Journal of Loss Prevention in the Process Industries*, 31, 16-25. DOI: 10.1016/j.jlp.2014.05.013. (Chapter 4)


5. Jingde Li, Guowei Ma, Madhat Abdel-jawad, Evaluation of the safety gap subjected to gas explosions on the cylindrical FLNG platform, revision submitted to *Process Safety and Environmental Protection*. (Chapter 7)


Conference papers


2. Jingde Li, Guowei Ma, Madhat Abdel-jawad, The New correlations for the calculation of pressures at congested configurations subjected to vapour cloud explosion, *the 10th International Conference on Shock & Impact Loads on Structures*, Singapore, 2013, pp. 27-37. (Chapter 4)

3. Jingde Li, Guowei Ma, Validation of FLACS regarding the calculation of vapour cloud explosion overpressures on congested regions, *6th International Conference on Protection of Structures Against Hazards*. (Chapter 6)

4. Yimiao Huang, Guowei Ma, Jingde Li, Hong Hao, Confidence based quantitative risk analysis method for offshore accidental hydrocarbon release events, *the 3rd International Conference on Protective Structures (ICPS3)*, Newcastle, Australia.
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CHAPTER 1. INTRODUCTION

1.1 BACKGROUND

Historically, oil has been used for lighting purposes for many thousands of years. Since 19th century, oil had replaced most other fuels all over the world. For example, oil was adopted as fuel in the automobile industry, the aircraft was designed due to the invention of gasoline engines, and by replacing the coal-powered counterparts, the ships driven by oil moved up to twice speed as they were used to.

Ever since the revolution, the search for crude oil and natural gas has been showing a boom worldwide and the investments into the development of energy projects, research and scientific programs are soaring faster and faster every year. However, on the other side of the oil and gas boom, a large number of onshore and offshore accidents have been occurring over the years globally. For example, explosion accidents in North Sea (Gas explosion on Piper Alpha oil production platform, July 1988) and the Gulf of Mexico (BP Deepwater horizon well explosion, April 2010) as shown in Figure 1.1, both are on the top of the list of the most disastrous explosion accidents within the industry.

![Piper Alpha explosion](a) Piper Alpha explosion  ![BP Deepwater Horizon explosion & oil spill](b) BP Deepwater Horizon explosion & oil spill

Figure 1.1 Explosion threats

Specifically, on 6 July 1988, an explosion, which led to large oil fires, destroyed the Piper Alpha oil platform in North Sea (Drysdale & Sylvester 1998; O'Byrne 2011). The accident was triggered by a massive leakage of gas condensate, which was later ignited
causing a gas explosion with extreme heat. The gas explosion and fire took only 22 minutes, however, 167 men died with only 61 survivors (Dennis 2013; Shallcross 2013). As one of the worst offshore oil disasters in history, this accident ceased approximately 10% of North Sea oil and gas production during 1980s, the economic loss was in excess of $3 billion (U.S.) (Patecornell 1993).

On 20 April 2010 in the Gulf of Mexico, the Deepwater Horizon semi-submersible Mobile Offshore Drilling Unit (MODU) was undermined by the gas explosion and subsequent fire (Reader & O'Connor 2014). According to the BP report (CSB 2014), initially, there was a hydrocarbon release being ingested into the air intakes of the diesel generator, then the emitted hot exhaust gas from the exhaust outlets led to the vapour cloud blow out, which killed 11 people (Dadashzadeh et al. 2013; Perrons 2013). After burning for more than a day, the Deepwater Horizon sank on April 22, 2010 following with a massive offshore oil spill in the Gulf of Mexico. According to the satellite images, the spill directly affected 68,000 square miles (180,000 km²) of ocean, which is comparable to the size of Oklahoma (Elliott & John 2010). The spill area hosts 8,332 species, including more than 1,270 fish, 604 polychaetes, 218 birds, 1,456 molluscs, 1,503 crustaceans, 4 sea turtles and 29 marine mammals (Thomas et al. 2010). It was reported that the spill was "already having a 'devastating' effect on marine life in the Gulf" (Campagna et al. 2011). As of March 2012, BP estimated the company's total spill-related expenses do not exceed $37.2 billion (Jonathan & Chris 2012). However, by some estimations penalties that BP may be required to pay have reached as high as $90 billion (Tom & John 2012).

Besides the two gas explosion accidents above, the Worldwide Offshore Accident Databank (WOAD) provides a list of other historical information on major onshore/offshore accidents in the oil and gas production and process industries (OGP 2010; DNV 2014). For those historical accidents, the most direct consequences are the failure of structural components, which is due to the extreme loads generated from the accidental gas explosions. The large overpressures of gas explosion are very unpredictable and not easy to be calculated, which makes it difficult for engineers to design structural members to withstand the extreme loads. More seriously, the subsequent consequences, such as human life loss, environmental impact, and economy, etc. could be tremendous. Therefore, it is of great interest to research the gas explosions
in order to understand the explosion mechanism and to reduce its financial loss and environmental damage by using the gas explosion mitigating approaches.

The gas explosion, which is also termed as the Vapour Cloud Explosion (VCE), is defined as “an explosion resulting from an ignition of a premixed cloud of flammable vapour, gas or spray with air, in which flames accelerate to sufficiently high velocities to produce significant overpressure” (Mercx & Vandenberg 2005). One of the significant features of a vapour cloud is that it may drift some distance from the point where the leak has occurred and may thus threaten a considerable area. In order to calculate the gas explosion overpressure, it is essential to take into consideration of a large array of random variable, such as the gas property, wind conditions and congestion scenarios, etc.

So far in the oil and gas industry, a wide range of gas explosion prediction tools are available – from empirical and phenomenological, through to Computational Fluid Dynamics (CFD) based (Lea 2002; Yet-Pole et al. 2009; Bakke et al. 2010; Middha et al. 2011). For example, the empirical TNT-equivalency method had proved to be a popular overpressure calculation tool in the past as it was widely used for a long time; however, it became less acceptable as a means of determining the blast loads as the understanding of gas explosion and flame propagation mechanism increased. A multi-energy method, which was based on experimental database for approximating explosion overpressures, had become a fast and more reliable prediction solution to the shortfalls of the TNT-equivalency method by regarding the VCE not as a single entity explosion but a set of sub-explosions (Bjerketvedt et al. 1997). Though the multi-energy method was more advanced that the TNT equivalency method, it still could not accurately represent complex geometries. Later, the first tailored software analysis tool - FLACS-86 was developed by GexCon (CMR/CMI) in 1986. FLACS-86 was the first version of an advanced CFD programme; it uses the three-dimensional Cartesian Navier-Stokes flow solver to predict cloud propagation, ignition probabilities, leak probabilities and overpressures. FLACS has since become a world leader in explosion analysis (Lea 2002). By considering more fundamental physics, such as the complex congestion and confinement in the geometry, the CFD based approach provides more accurate overpressure results, but it requires increasing time to run the simulations. Therefore, from the calculation efficiency perspective, an approach with the FLACS accuracy and
the phenomenological method’s rapidness could be more applicable as a truly predictive tool in the oil and gas industry. This research was motivated by the industry’s need to better improve the prediction of the gas explosion overpressure, a new correlation based on the phenomenological and CFD coded approaches was proposed in this thesis (Li et al. 2014a; Li et al. 2014b).

Accordingly, a wide variety of approaches (as seen in Figure 1.2) can be adopted to mitigate the effects of gas explosions after the calculation of overpressures. For instance, water deluge activated upon gas detection had been shown to be very effective to limit consequences of explosions in congested areas offshore (Vanwingerden & Wilkins 1995; Vanwingerden et al. 1995; VanWingerden 2000), but it could be difficult to apply water deluge in onshore facilities for limiting the consequences of vapour cloud explosions since large amounts of water are needed (VanWingerden et al. 2013). Alternatively, the gas barriers from a soft barrier to a hard barrier (such as the blast wall) can restrict the spread of the gas cloud and thus reduce the severity of the explosion (Tam 2000). The blast wall could be located close to the target to shield the target from explosion overpressures. However, the blast wave reflection from the blast wall are very likely to be seen, which is detrimental to the target. Therefore, a detailed study should be conducted to investigate the interaction between blast waves and the wall & structural complex and optimize the size & position of blast walls (HySafe 2006). In addition, a more up-to-date explosion mitigation is the safety gap. The safety gap is the required distance between congested areas; it interrupts a positive feedback mechanism of the generation of turbulence, enhanced thermal, flame speed and thereby reducing explosion overpressure. The gas explosion consequence and fire escalation could be minimised since the safety gap significantly interrupts the turbulence during the process of flame acceleration (Ma et al. 2014). However, the safety gap is more suitable for onshore structures or offshores structure with sufficient body space, such as the Floating Liquefied Nature Gas (FLNG) Vessel. In order to investigate those gas explosion-mitigating approaches in the reality, the safety gap and blast wall were numerically modelled on the offshore structures in this thesis, and the evaluation of the mitigating measure in different design was conducted.
1.2 OBJECTIVE OF THE STUDY

The aims of the present study include:

- To develop a new correlation of VCE overpressure calculation, which possesses the calculation accuracy of CFD based software – FLACS and the rapidness of the experimental based approaches. The new correlation is efficient to approximate the overpressure for the large-scale offshore/onshore structures with complex components subjected to gas explosion.

- To undertake parametric studies and to validate the new correlation’s accuracy by using a series of artificially and irregularly arranged configurations.

- To investigate the up-to-date blast loading reduction measure – safety gap, which is a more effective and efficient explosion mitigation alternative for oil and gas
structures. In addition, to propose a Data-dump technique that improves the explosion calculation accuracy by using CFD.

- To design and apply the safety gap and blast wall on an innovative offshore structure - cylindrical FLNG, which has large-scale and complex geometry. To investigate the gas dispersion and explosion mitigation efficiency of safety gap and blast wall on the cylindrical FLNG by performing the quantitative explosion risk assessment.

1.3 THESIS ORGANISATION

This thesis comprises nine chapters. Eight chapters following the introductory chapter were arranged as follows.

Chapter 2 presented a broad literature review on the state-of-the-art overpressure calculation methods regarding vapour cloud explosion (i.e. gas explosion). The chapter covered the simplified TNT equivalent method, the multi-energy method and the CFD coded approach, etc. A series of VCE overpressure mitigating approaches including water deluge, blast wall and safety gap, were reviewed.

Chapter 3 presented the principles of the CFD based overpressure calculation approach. The gas explosion mechanism regarding the fluid flow equations, thermodynamic relationships and turbulence & combustion modelling were well described and the numerical procedure is introduced.

In Chapter 4, a phenomenological study was perform based on the FLACS simulations, a new correlation, which is a Confinement Specific Correlation (CSC) to calculate the VCE overpressures, was derived. By comparing with the GAME correlation, the new correlation possesses the same calculation efficiency and better accuracy. The parameters regarding the gas properties, congestion and confinement, etc. were redefined and over 1000 simulations cased are modelled by using FLACS.

In Chapter 5, a range of irregularly arranged configurations with different settings were numerically simulated to validate the performance of the CSC, the overall overpressure comparison results between the CSC and FLACS simulation were satisfactory.
After the calculation of gas explosion overpressures, the corresponding explosion mitigating method - safety gap was introduced in Chapter 6. The numerical models of safety gaps subjected gas explosions were validated with a series of experimental prototypes. A data-dump technique was established to increase the overpressure prediction accuracy prior to the further investigation of the safety gap effect.

In Chapter 7, the safety gap concept was applied onto a cylindrical FLNG platform to investigate its explosion mitigating efficiency. The cylindrical FLNG was numerically modelled and transformed from a traditional FLNG. By means of different arrangements of safety gaps, the gas dispersion and explosion simulations were performed to assess the explosion/fire safety on the cylindrical FLNG, which is still a challenge design in the oil and gas industry.

Chapter 8 carried out numerical simulations in investigating the blast wall effect on overpressure mitigation on the cylindrical FLNG. The numerical results indicated that blast walls are effective in overpressure mitigation in a certain direction with an appropriate arrangement. By designing four different sets of blast wall configurations and simulating over 3,000 cases, the probabilistic study presented the robust procedure of the optimization of blast wall design.

Finally, concluding remarks were made in Chapter 9, along with suggestions for future work.
CHAPTER 2. LITERATURE REVIEW

2.1 OVERVIEW

This chapter presented a literature review on the existing VCE overpressure calculation methods. Additionally, the up-to-date industrial measures and new methods currently under development to prevent, control and mitigate the explosions were reviewed.

The literature review covers: 1) the introduction of gas explosion; 2) the discussion of current explosion calculation models; 3) existing and new approaches in gas explosion overpressure prevention and mitigation.

2.2 INTRODUCTION OF GAS EXPLOSION

A gas explosion or Vapour Cloud Explosion (VCE) is the sudden generation and expansion of a premixed gas cloud, i.e. fuel-air or fuel/oxidiser causing rapid increase in temperature and pressure capable of causing structural damage (Lea 2002; Bjerketvedt et al. 1997). There is no essential difference between a VCE and gas explosion (Bjerketvedt et al. 1997), in this thesis, gas explosion is also termed as VCE.

![Diagram of gas explosion consequences](image)

Figure 2.1 Typical consequences of gas explosions due to release of combustible gas or evaporating liquid into the atmosphere (Bjerketvedt et al. 1997).
Figure 2.1 illustrated the events leading to gas explosions. It is essential to have an accidental release of gas or evaporating liquid into the atmosphere. In addition, ignition must be present to ignite the released gas, which could result in fire or an explosion (Bjerketvedt et al. 1997).

Gas explosions can take place in confined scenarios, such as offshore pipes, tanks or channels; partly confined offshore modules or buildings; and in unconfined process plants or other open areas. For gas explosions in fully confined regions with no venting and no heat loss, the turbulent combustion process causes a more dramatic increase in overpressure. For example, the overpressures and impulses in a confined chamber in the shock-dispersed-fuel explosion can enlarge 2-3 times by increasing the confinement volume (Kuhl & Reichenbach 2009), and it is of great importance to investigate the flame propagation for reliable design of structures in such confined explosions (Sauvan et al. 2012; Shi et al. 2009; Tang et al. 2014). In terms of partly confined VCEs, they can happen when a gas is accidentally leak inside an offshore module of building that are partly open. The vent area size and location play significant role in resulting overpressures in the partly confined VCEs, in order to study the complexity, increasingly more attention has been paid on the partially confined overpressures (Woolley et al. 2013; Pedersen et al. 2013). Generally, from the fully confined VCE to the partly confined explosion with un-congested condition, the venting will reduce the turbulence level hence lower the flame speed of the explosion. Therefore, for a truly unconfined and unobstructed gas cloud ignited by a weak ignition source, the generated overpressure is very low. However, if the unconfined area contains obstacles, the expansion-generated flow created by the combustion will generate turbulence as the fluid flows past the obstacles (Kim et al. 2014; Dorofeev 2007; Na’innen et al. 2013). By expanding the flame area and increasing the molecular diffusion and conduction processes, the newly generated turbulence will increase the burning velocity, which in turn increase the expansion flow and boost the turbulence. The generation of increasingly higher burning velocities and overpressures in this cycle due to the obstacles is called Schelkchkin mechanism (Lea 2002). Figure 2.3 showed the Schelkchkin mechanism of flame acceleration caused by obstructions constitutes a strong positive feedback loop (Bjerketvedt et al. 1997). Overall, the generation of VCE
overpressures will depend on the condition of confinement and obstacles that affect the flame acceleration.

In this study, different gas explosions were simply classified into two categories: chemical explosion and physical explosion, as seen in Figure 2.2.

![Figure 2.2 The classification of explosions in chemical process industry (Abbasi et al. 2010).](image)

Here, the physical explosion is mainly because the accumulated energy (e.g. the accumulation of explosive gas) is physically and rapidly released, for example, the sudden expansion of a compressed gas in containment. The causes of the accumulation of explosive gas include the gas release duration, release rate, the ignition types and the time of the ignition, etc, which are contributing to the build-up of mechanical energy in the physical explosion. Once the substance from a gas explosion is released, the chemical reaction would be induced that a flammable material may start burning due to mixing with air contributing heavily to the overall effect as with the failure of a gas tank. Whereas the term of ‘chemical explosion’ refers to the way that due to a chemical reaction, a large amount of reactants and energy is generated within an extremely short time period, the speed of overpressure build-up is rapid enough to lead to an explosion in open space. The chemical explosion is based on where in the substance the reactions are occurring at a specified time. Two distinct forms of the chemical explosion are classified: one is the homogeneous chemical explosion, which takes place throughout the mass of substance all at once; while if the explosion happens exclusively in a
propagating reaction zone, the chemical explosion can be in two well-defined but different intensities: deflection and detonation (Abbasi et al. 2010).

Figure 2.3 Positive flame acceleration feedback loop due to turbulence (Bjerketvedt et al. 1997).

The deflagration and detonation are defined according to how the combustion of the premixed gas cloud progresses and how the flow interacts with obstacles (Venugopalan 2015). The deflagration is a surface phenomenon of gas explosion, i.e. its flame propagation is by layer-to-layer burning. The rate of deflagration is lower than the sonic velocity with respect to the unburnt gas ahead of the flame, and the products of deflagration go away from/opposite to the direction of flame propagation. Depending on confinement and flame speed, the pressure of deflagration goes from zero to several bars (Bjerketvedt et al. 1997). The wrinkling of the flame front by large turbulent eddies in the turbulent deflagration contributes to the increase of flame burning rate (Kobayashi et al. 2002). Specifically, in the regime where the turbulent integral length scale is of the order of the thickness of the flame front, a thick turbulent flame brush is generated by the flame, which increases the diffusion of heat and mass and thereby a high burning rate (Sand & Arntzen 1991; Aldredge & Williams 1991; Kobayashi & Kawazoe 2000). In terms of the detonation, it is a shock-wave phenomenon, i.e. high-speed shock wave traveling through the explosive medium propagates detonation. The rate of detonation is higher than the sonic velocity in the medium. The products of detonation travel in the same direction as that of the propagation of detonation (Venugopalan 2015). Comparing to the deflagration, a detonation does not require
obstacles or confinement in order to propagate at high velocity, it is the most
overwhelming form of gas explosion (Lewis & Von Elbe 2012).

It is of great interest to study the deflagration and detonation (Lee & Moen 1980; Khokhlov et al. 1997); the slow burning deflagration starts from a weak ignition source, then the deflagration can accelerate and become a sufficiently rapid detonation, if there is the sufficient influence of the confinement and obstruction. Over the years, people have done numerous research work regarding the prediction of Deflagration to Detonation Transition (DDT) (Oran & Gamezo 2007; Oh et al. 2001; Bi et al. 2012; Gamezo et al. 2007; Kessler et al. 2010). However, so far there is no solid theory available since it is difficult to evaluate the DDT mechanism in practical situations, a great number of uncertainties related to the DDT affecting the possibilities of DDT. Those uncertainties includes:

- Fuel type
- Concentration of fuel
- Ignition source location and type
- Venting and confinement
- Obstacle blockage ratio
- Shape, size and location of obstacles etc.

The fuel properties strongly affect the flame speeds for stoichiometric fuel–air mixtures (Dorofeev 2011). The most likely fuels can lead to detonate include (Dorofeev et al. 1994)acetylene, ethylene-oxide and ethylene (Matsui & Lee 1979). For other fuels, such as butane and propane, a strong deflagration is required to initiate the detonation (Bjerketvedt et al. 1997). However, the detonation triggered by methane could be more complicated (Wolański et al. 1981; Boni et al. 1978).

The fuel concentration has significant effects on the flame region distribution and the explosion behaviours (Ma et al. 2015; Halter et al. 2005). A premixed gas cloud below the lower flammability limit (LFL) and above the upper flammability limit (UFL) will not be burnt, or if the fuel mixtures near the flammability limits will produce very low burning rate. The maximum explosion overpressure is normally generated within
stoichiometric composition or slightly rich premixed gas cloud. The term of stoichiometric composition is defined as the composition where the amounts of fuel and oxygen (air) are in balance so that there is no surplus of fuel or oxygen after the chemical reaction has been finalized (Bjerkevedt et al. 1997).

Additionally, the ignition source type critically influences the consequences of a VCE; very high explosion overpressures can be observed if the ignition sources are jet-type, or bang-box-type give rise to than a planar or point source. In addition, VCEs are very sensitive to the location of the ignition; it must be viewed in conjunction with the geometry information. The maximum overpressure can considerably jump by an order of magnitude if the location of ignition is placed at less vented or more confined areas (Babrauskas 2003; Bartknecht 2012); and edge ignition may produce greater overpressures than central ignition, since the edge ignition has longer flame propagation distance for flame acceleration (Zeldovich & Barenblatt 1959).

In terms of the effect of obstacles in gas explosion, the blockage ratio of obstacles is an important factor, which influences the flame propagation and the explosion pressure built-up (Oh et al. 2001). The blockage ratio of obstacle is used to describe the degree of obstruction, the maximum overpressure increases, generally with increasing blockage ratio but the rate of increase depends on the obstruction geometry (Ibrahim & Masri 2001). Generally, higher overpressures will be produced in small diameter objects than larger objects if the blockage ratio is given. Moreover, more tortuous route (flame in baffle-type obstacles) results in greater explosion overpressures compared to the pressure development in round obstacle, which is due to the fact that the turbulence enhancement of the burning velocity is higher in the shear layer of the sharp obstacle (Bjorkhaug 1986).

Gas explosions are very sensitive to these parameters mentioned above; therefore, it is not an easy task to evaluate the consequences of a gas explosion. So far, there are a number of research programmes and models providing some understanding of the accidently occurred gas explosions, and these explosion-prediction methodologies, which were discussed in the following section, possess both advantages and disadvantages in estimating the complexity of gas explosion.
2.3 EXPLOSION PREDICTION MODELS

Varying from simple empirical models to sophisticated CFD models, a series of models were reviewed in this thesis:

2.3.1 Empirical models

These empirical models include simplified TNT equivalency model, TNO Multi-energy model and Baker Strehlow model, etc. (Mannan 2012; Vandenberg 1985; Baker et al. 1998). Based on correlations derived from the assessment of experimental data, these models significantly simplified the physics, however, they can hardly evaluate the complex geometries. Overall, these empirical models have limited applicability, engineers tend to use them just for quick order-of-magnitude calculations.

The TNT equivalency method, which had been extensively studied (Baker et al. 2012; Mannan 2012; Stull 1977), was based on the assumption of equivalency between the flammable material and TNT, a yield factor plays a critical role in converting the energy of gas explosion into the same explosive charge of TNT. The relationship between the gas explosion and TNT explosion is seen below:

\[ W_{TNT} = 10\eta W_{HC} \]  

where \( W_{TNT} \) is the equivalent mass of TNT, \( \eta \) is an empirical explosion efficiency, \( W_{HC} \) is the mass of hydrocarbon.

Obviously, the strength of the TNT equivalency method is that it is a very simple and easy approach. There are only two parameters to be taken into consideration, then the following calculation of the equivalent overpressure is same as the TNT explosion calculation. The TNT equivalency had been widely used in the simplified models. For example, the Health and Safety Executive (HSE) evaluated the TNT equivalence method for both the near and far field range of explosives and energetic materials in a simplistic way (Formby & Wharton 1996). The TNT equivalency method was used (Rui et al. 2002) to evaluate the distributed blast of fuel-air detonation. The applicability of the TNT equivalency method for the calculations of blast wave characteristics after vessel rupture in a 1-D geometry detonation was also discussed (Skacel et al. 2013).
However in reality, unlike the TNT detonation, the local overpressure in a gas explosion is much lower, and the decay of the VCE blast wave is much slower than the overpressure from a TNT explosion. Therefore, for the near field explosion prediction, the TNT equivalency model will give very poor estimation results, for example, it was found that The TNT model over predicts overpressures at the same low distance than the TNO Multi-energy method and Baker-Strehlow method (Lobato et al. 2006). Moreover, the selection of the empirical explosion efficiency strongly depends on engineers’ experience, let alone the lack of consideration of geometry.

Another example of the empirical approach is the TNO multi-energy method (Vandenberg 1985), which is also suitable for far field explosion estimation. It was based on the assumption that only confined or congested gas cloud contribute to the overpressure built-up, and the flame velocity is assumed to be constant for the explosion where the gas cloud is ignited from its centre.

Two parameters are vital in the overpressure calculation. Firstly, the combustion-energy scaled distance $R_{ce}$, should be determined, it is defined as:
Explosion safety evaluation for congested offshore platforms based on CFD simulations

\[ R_{se} = \frac{R_0}{\sqrt[3]{\frac{E}{P_0}}} \]  

(2)

where \( E \) is the combustion energy, \( R_0 \) is the distance from the explosion centre to the target, and \( P_0 \) is the atmospheric pressure.

The second important parameter is the strength of the explosion, which is classified from a number between 1 and 10 to represent the level of explosion, as seen on the left hand side in Figure 2.4. The choice of the explosion strength level from 1 to 10 however ideally depends on a conservative assumption or other simulations, which also means it is difficult to set a sensible value for the charge strength in the multi-energy method. The simplicity and fast estimation speed of the TNO multi-energy method was proved by Alonso, et al. (Alonso et al. 2006; Alonso et al. 2008), they used this fast method to evaluate the characteristic overpressure-impulse-distance curves and to analyse the consequence of damage to humans from VCEs. The TNO multi-energy model was also applied in predicting the facility siting hazard distance of VCEs (Pitblado et al. 2014), however, an explicit implementation guidance was proposed in their research to improve the consistency in TNO MEM blast load predictions.

Additionally, the Baker-Strehlow model (Baker et al. 1994) was developed for estimating the overpressure of VCEs, the calculation process of this method includes the assessment of fuel reactivity, flame speed and confinement, etc., the overpressures are then determined by reading a range of diagrams. Baker, et al. further revised the research and extended a new set of blast curves from VCE calculations (Tang & Baker 1999; Baker et al. 1998). Overall, the Baker-Strehlow method provides conservative prediction of flame speed, and it takes into account some geometrical factors, such as the confinement, however, for the unconfined 3D flame expansion scenarios, the flame speed estimation could be not conservative. Therefore, the flame speed table in the Baker–Strehlow methodology for these exceptional cases was updated (Pierozzio et al. 2005) and a correction method to the Baker-Strehlow model for the ground effect of vapour cloud explosions was provided (Worthington & Oke 2009).
2.3.2 Phenomenological models

The phenomenological models are more sophisticated and have an extensive applicability than the empirical models. These models are somewhere between the CFD models and empirical models. On one hand, the phenomenological models are derived from the empirical observations of phenomena, which could be numerical or experimental phenomena; the mathematical expressions in the phenomenological models are indirectly relevant to fundamental theory. Therefore, the phenomenological models can provide accuracy calculation results under certain circumstances. On the other hand, the interactions between the variables cannot be well explained in the phenomenological models, since the relationships of these variables are simply based on the limited measured data. Hence, the limitation of the phenomenological models could be seen if the investigation scenario is different from the empirical observations.

The physical process involved in the VCEs can be well described by the phenomenological models, which are developed based on differential and algebraic equations. Therefore, same as the empirical models, the phenomenological methods have short running times. Moreover, these models can take some certain types of geometries into consideration, for a large number of scenarios with certain geometries, the phenomenological models are more time efficient than the CFD models. However, the actual scenario geometries are generally simple, for example, a box with regularly arranged obstacles or a one-direction chamber. For more complex and arbitrary geometries, it is normally beyond the calculation capacity of the phenomenological methodology. Examples of phenomenological models include SCOPE (Puttock et al. 2000), CLICHE (Catlin 1990) and CINDY (Molkov et al. 2004), etc..

The Shell Code for Over-pressure Prediction in gas Explosions (SCOPE) methodology has its original version (Cates & Samuels 1991). The overpressure prediction by SCOPE was based on a simple spreadsheet for an idealised geometry, it took the most obvious assumptions for each physical process in VCE (Puttock et al. 2000), Figure 2.5 summarised the overall structure of the model.
Figure 2.5 Structure of the SCOPE model (Puttock et al. 2000).

The SCOPE model is one-dimensional and it can take venting into account, which is based on the idealised geometry with single enclosures only. The single directionally vented vessel consists of a number of obstacle grids, the turbulence and rate of turbulent combustion downstream from these grids can be calculated by using the SCOPE model.

Another phenomenological approach suitable for confined explosion calculation is the Confined Linked Chamber Explosion (CLICHE) model (Catlin 1990). It was originally established to study explosions in buildings involving flame propagation, now for onshore/offshore structures, the CLICHE code is also able to handle the calculations, however, the modules, such as the process plant consist of semi-confined areas would be simplified as a sequence of interlinked explosion chambers. The necessary parameters to simulate the interactions between flame and obstacles are phenomenologically determined from a system of differential equations in CLICHE. For example, based on previous work (Bray 1987), CLICHE developed a turbulent combustion model by validating against a number of explosion experiments (Abdel-Gayed et al. 1987) and TNO in a semi-circular test rig with repeated obstacles (Catlin 1990). In modelling offshore explosions with multiple venting paths, the CLICHE provided reasonable and quick calculation results due to its simplified calculation...
However, it did not take account of the venting opening at an enormous overpressure; the vents were assumed as uncovered or were covered by weak panels, which restricted the capability of CLICHE to estimate the effect of a suddenly appeared venting due to failure of a wall (Gardner et al. 1994).

Another phenomenological model – CINDY code (Molkov et al. 2004) was developed to validate a series of experiments consisting of some partially confined vessels (Hochst & Leuckel 1998; Zalosh 1979), the CINDY model provided satisfactory validation results for the modelling of vented deflagrations with inertial venting devices. Furthermore, the phenomenological model was (Hernandez et al. 2015) simplified to establish the burning rates for the combustion process in such vented explosions. Whereas for a closed vessel, a new model of gas explosion (Kobiera et al. 2007), which was based on characteristics of flame surface, was presented, however, this flame surface model was based on the assumption that no transition from deflagration to detonation (DDT) occurs within the gas and the burning velocity of a turbulent wrinkled flame can be determined from the flame surface. For all these phenomenological models mentioned above, their geometries tend to be less detailed than CFD models, so they are suitable for fast evaluating different scenarios during plant design phase. Whereas, when it comes to explosions with more geometrical detail, these models cannot provide accurate results than CFD models do.

### 2.3.3 Numerical models

The numerical approaches use the Computational Fluid Dynamic (CFD) codes. The fundamental partial differential equations, which govern the fluid flow and other explosion processes, are employed in most of the numerical models during the calculation of VCEs. When comparing the numerical models with empirical and phenomenological models, the numerical models offer greater accuracy and flexibility, and by discretising the solution domain in both space and time, a wide range of geometrical arrangements and conditions in the VCEs can be considered in the numerical simulations. So the numerical models are used in many different disciplines in the industry, such as weather forecasting, aeroplanes designing and environmental modelling. A table listing the comparison of the numerical models with other empirical and phenomenological models is provided Table 2.1. In this thesis, the numerical
models are thoroughly used to evaluate the gas dispersion and explosion in offshore structures.

Table 2.1 Comparison of numerical models (Lea 2002)

<table>
<thead>
<tr>
<th>Name</th>
<th>Type</th>
<th>Grid</th>
<th>Accuracy</th>
<th>Reaction model</th>
</tr>
</thead>
<tbody>
<tr>
<td>FLACS</td>
<td>3D CFD Finite Volume</td>
<td>Structured, Cartesian, PDR treatment of sub-grid scale objects</td>
<td>First order, Reaction progress, Variable second order</td>
<td>Empirical correlation</td>
</tr>
<tr>
<td>EXSIM</td>
<td>3D CFD Finite Volume</td>
<td>Structured, Cartesian, PDR treatment of sub-grid scale objects</td>
<td>First order temporal, Second order spatial</td>
<td>Eddy break-up</td>
</tr>
<tr>
<td>AutoReaGas</td>
<td>3D CFD Finite Volume</td>
<td>Structured, Cartesian, PDR treatment of sub-grid scale objects</td>
<td>First order temporal and spatial</td>
<td>Empirical correlation</td>
</tr>
<tr>
<td>CFX-4</td>
<td>2D and 3D CFD Finite Volume</td>
<td>Structured, body-fitted</td>
<td>Higher order temporal and spatial</td>
<td>Eddy break-up and thin flame</td>
</tr>
<tr>
<td>COBRA</td>
<td>2D and 3D CFD Finite Volume</td>
<td>Unstructured, Cartesian, Cylindrical polar or hexahedral, adaptive, PDR treatment of sub-grid scale objects</td>
<td>Second order temporal and spatial</td>
<td>Empirical correlation</td>
</tr>
<tr>
<td>Imperial College Research Code</td>
<td>2D CFD Finite Volume</td>
<td>Unstructured, Adaptive</td>
<td>Implicit temporal, Second order (TVD) spatial</td>
<td>Laminar flamelet and PDF transport</td>
</tr>
<tr>
<td>REACFLOW</td>
<td>2D and 3D CFD Finite Volume</td>
<td>Unstructured, Adaptive</td>
<td>First and second order temporal and spatial</td>
<td>Eddy break-up</td>
</tr>
<tr>
<td>NEWT</td>
<td>3D CFD Finite Volume</td>
<td>Unstructured, Adaptive</td>
<td>Higher order temporal and second order spatial</td>
<td>Eddy break-up</td>
</tr>
<tr>
<td>SCOPE</td>
<td>Phenomenological</td>
<td>N/A</td>
<td>N/A</td>
<td>Empirical Correlation</td>
</tr>
<tr>
<td>CLICHE</td>
<td>Phenomenological</td>
<td>N/A</td>
<td>N/A</td>
<td>Empirical Correlation</td>
</tr>
<tr>
<td>Congestion assessment method</td>
<td>Empirical</td>
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<td>N/A</td>
<td>None</td>
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<tr>
<td>Sedgwick loss assessment method</td>
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<td>N/A</td>
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</tr>
<tr>
<td>TNO</td>
<td>Empirical</td>
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<td>TNT equivalency</td>
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<td>None</td>
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<tr>
<td>Multi-Energy</td>
<td>Empirical</td>
<td>N/A</td>
<td>N/A</td>
<td>None</td>
</tr>
</tbody>
</table>
The numerical simulations can provide comprehensive understanding into the flow behaviours, i.e. flame velocities, gas density and overpressures, etc. For the realistic oil and gas safety issues, it is easy to use the numerical models to simulate different scenarios; therefore, performing the numerical modellings is more efficient and practical than conducting impossible or expensive experiments. However, the numerical approach also has two main shortcomings. In the first place, the accuracy of numerical estimation depends on the calibration of the numerical models with experiments, it is vitally significant to use the correct geometry, boundary conditions and mesh grids, etc. in modelling during the data validation. Secondly, accurate overpressure calculations require a great number of small grids in the numerical modelling, and the computing time of the overpressure generation depends on the mesh fineness of the geometry, therefore, the small-scale mesh grids would significantly contribute to the rise of simulation time, which means conducting those fine-scale simulations requires immense computer memory and computing speed. The numerical models reviewed here include FLACS, EXSIM, AutoReaGas and COBRA.

One of the most popular numerical model is the FLame ACceleration Simulator (FLACS) code. Over two decades, the FLACS code has been under development at Christian Michelsen Research Institute in Norway (Bjerketvedt et al. 1997). The three-dimensional Cartesian mesh is employed in FLACS, and the first order discretisation in both time and space is used in the finite volume approach of FLACS. For sub-grid scale obstacles in FLACS, the porosity resistance approach is adopted to simulate the accelerating effects of these obstacles. The commonly used $k-\varepsilon$ turbulence model is in FLACS to model the source terms through turbulent processes. And based on Arntzen’s correlations (Arntzen 1998), a $\beta$ flame model was developed in FLACS to calculate the turbulent burning velocities and other parameters (Hjertager 1986; Bakke & Hjertager 1986). FLACS has been widely used in the onshore/offshore explosion analysis, and now it has accumulated extensive validation (Bakke & Hjertager 1986a; Hjertager et al. 1988; Middha & Hansen 2009; Hansen et al. 2010a) and user experience in the oil and gas industry. For example, FLACS was applied to evaluate the consequences of the ignition of a flammable vapour cloud from an LNG spill during the LNG carrier offloading process (Gavelli et al. 2011). The safety benefits of hythane by using FLACS regarding
the flame speeds and flammability limits was analysed (Middha et al. 2011). Moreover, a study on the effect of trees on gas explosion was carried out (Bakke et al. 2010) and FLACS was employed to evaluate the possible hazards of different worst-case scenarios within a naphtha-cracking plant (Yet-Pole et al. 2009).

However, FLACS simulations still possess a range of the calculation errors in some scenarios. For example, when using FLACS to predict the VCE overpressure in far-field, the issue of numerical smearing of the blast wave shock-front was realized (Hansen et al. 2010b). For these far-field explosions, the overpressures calculated by FLACS were under-predicted, sometimes by up to a magnitude of two. In addition, the over-prediction of VCEs also exist if there is a big open space between two congestions (Ma et al. 2014). An approach was developed (Hansen & Johnson 2014) to obtain more accurate far-field blast predictions by modifying parameter settings in FLACS, and a data-dump technique (Ma et al. 2014) was proposed to assure the calculation accuracy of FLACS in the explosion scenarios with open spaces. In terms of the accuracy range, CMR states that if the FLACS code estimate VCEs overpressures within the order of ±30%, the results are acceptable (Lea 2002). In order to improve the calculation accuracy and capacity of FLACS, further developments have been conducting and they are not published in the open resource.

Another CFD coded model still under continuing development is the EXSIM model, which was initially created at Telemark Institute of Technology and Telemark Technological R&D centre in 1989 (Hjertager et al. 1992). The EXSIM code is similar to FLACS in the aspect of numerical modelling, namely, the Cartesian grid and finite volume code are used to represent small-scale objects in EXSIM (Hjertager 1997). Therefore, some previously investigated projects for FLACS had also been validated by EXSIM. For example, the Buncefield explosion was investigated by both FLACS and EXSIM for explosion simulations (Taveau 2012). In addition to the detailed flame behaviour assessment (Johnson et al. 2010) performed more recently, the modelling and simulation of gas explosion in some other complex geometries was conducted (Saeter 1998), and the EXSIM model was implemented in the investigation of Flixborough accident (Høiset et al. 2000). Similar to FLACS, the predicted overpressures are acceptable if the error compared to the measured values is within about ±40% (Hjertager et al. 1992), however, the EXSIM code is not capable of local grid refining.
The third reviewed finite volume computational code for fluid dynamics is the AutoReaGas model, which has been developed by TNO – Prins Maurits Laboratory, allows a detailed simulation of different aspects of gas explosion (VandenBerg et al. 1995). The AutoReaGas model integrates features of the REAGAS and BLAST codes as solvers to handle gas explosion and blast waves, respectively. The gas explosion solver in AutoReaGas uses the first order accurate power law scheme to conduct the discretization for all variables, which is the weakness of AutoReaGas in terms of the calculation accuracy, and the blast solver uses the Flux Corrected Transport technique to cope with the blast wave propagation by applying the 3D Euler equations (Hjertager & Solberg 1999). Although there have not been the same degree of validations of AutoReaGas compared to FLACS, a range of user experience of AutoReaGas exists in the industry. For instance, the computational simulations of vented confined explosions was performed by using AutoReaGas (Janovsky et al. 2006) and the data were compared with the CFD results for the Stramberk experiments. In addition, AutoReaGas was used to carry out numerical simulations of a series of methane–air explosion processes in a full-scale coal tunnel (Pang et al. 2014), and the flame propagation mechanism beyond the initial premixed methane–air region was analysed by comparing the numerical and experimental results. A theoretical guidance for gas explosion disaster relief and treatment in underground coal mines was provided by performing AutoReaGas simulation (Jiang et al. 2012), the propagation characteristics of VCES, and the safe distance for various initial temperatures had been investigated.

There are also some advanced CFD models with more comprehensive description of VCE process. Such as the ANSYS CFX model (CFX4.2 1997), which has more detailed representation of the geometry, and the COBRA model uses second order accurate scheme in space and time to simulate gas explosions.

For the ANSYS CFX model, it is a 3D multi-purpose code can solve the hydrodynamic equations – momentum, mass and pressure, etc. The code employs the widely used $k-\varepsilon$ turbulence model as well as a full Reynolds stress turbulence model for prediction of VCE overpressure in different complex geometries. There has been an extensive amount of studies regarding the application of ANSYS CFX. For example, the hydrogen explosion experiments by using CFX-4 was numerically modelled (Rehm & Jahn 2000), and a satisfactory agreement between the numerical and laboratory data was presented.
The CFD simulations of light gas release was conducted and mixed in the Battelle Model-Containment with CFX (Wilkening et al. 2008), a helium jet release and dispersion case were numerically validated against experimental data. Furthermore, it was proved that CFX is capable of contributing important additional information to the data that are generated by other approaches (Baraldi et al. 2007). For all these examples above, the exact geometric representation of the explosion scenario can be read by CFX comprehensively, however, it is not memory efficient as other simple CFD models, such as FLACS.

In terms of the COBRA CFD code, there is very limited published report of the numerical modelling. In some previous work (Woolley et al. 2013), the discussion regarding the performances of COBRA in explosion simulation can be found. One of the advantages of COBRA is that it uses an adaptive grid algorithm, after each calculation cycle, the mesh grid in COBRA is updated accordingly to ensure a fine grid resolution, the adaptive grid algorithm reduces the number of cells required in modelling (Catlin et al. 1995). Additionally, the COBRA code uses the second order accurate mesh-independent solution, which effectively removes numerical errors and increases the calculation accuracy. However, the visualisation of flow fields and setting up complex geometries can be slow and laborious, which requires long run times. Therefore, the code is suitable for specialist user.

Overall, the numerical simulation codes for VCE modelling can be classified into two groups based on the orders of accurate finite volume algorithm. FLACS, EXSIM and AutoReaGas are in the first group, they use first order accurate approach, the detailed gas explosion process is well described in these codes and the estimated overpressures are much more robust than that in the phenomenological models. Even though these first order accurate algorithm models are more time-consuming than the phenomenological approaches, the detailed geometries and conditions can be considered in these CFD models more accurately, especially for large-scale cases. By contrast, the second group, which includes ANSYS CFX and COBRA, provides even more accurate numerical schemes due to the fact that they use second order of accurate finite volume algorithm. However, the laborious settings of complex geometries and enormous demands on computer resources by using the ANSYS CFX and COBRA limit their applicability. In other words, ANSYS CFX and COBRA are more likely to be used by
specialists with greater understanding of VCEs, while FLACS, EXSIM and AutoReaGas are more suitable for engineers for the standard evaluation and design of onshore/offshore structures. In addition, more guidelines are available for these first order accurate approaches, such as the guidelines for FLACS (Ma et al. 2014; Hansen et al. 2010b; Hansen & Johnson 2014), to help improve and standardise VCE overpressure predictions, such as the accuracy improving guideline proposed in Chapter 6.

2.4 EXPLOSION MITIGATION

Gas explosion with great magnitude of overpressures can result in devastating consequences causing destruction of large parts of onshore/offshore facilities and severe fatalities. Following these VCE overpressure evaluation approaches mentioned above, in this chapter, a review of various explosion mitigating measures to minimize the gas explosion consequences on onshore/offshore structures was conducted below.

2.4.1 Explosion relief/venting

Venting of deflagrations is a cost-effective and widespread explosion mitigation technique for confined explosions, an example of size venting is shown in Figure 2.6. In this approach, vent devices for gas explosions are often placed to weak locations where the wall collapses during the early stage, so the explosion will be vented and the burning/unburned gas will be released into the open air to alleviate the maximum overpressures generated inside the enclosure. A comprehensive guidance on explosion vents could be found in NFPA guide 68 (NFPA68 2002), it included the vent design in low-strength and high strength enclosures regarding methane, propane, city gas and hydrogen explosions. Molkov conducted a series of studies regarding the vented gas explosion dynamics (Molkov 1995), vented explosions in buildings (Molkov 1999) and mitigation in a large-scale explosion (Molkov & Makarov 2006). In terms of the design of a venting system, it is important to make sure that personnel and surrounding facilities should be far away from the hazardous gas explosion flame and combustion products, so the discharge of gas explosion is normally ducted to a safe area by attaching ducting to the vent. However, the presence of ducting will lead to unusual increases in the explosion due to the obstacle-flow interaction and secondary explosion (Kordylewski & Wach 1986; Ponizy & Leyer 1999). The interaction between internal and ducted external explosions via explosion relief had also been investigated (Ferrara
et al. 2008), the mechanisms underlying the overpressure increase of the ducted vent was studied. To reduce the overpressure rise in such case, the explosion venting should be designed without curves or no great radius curves, and an appropriately large cross section area of the venting should be determined (HySafe 2006). However, there are limited guidelines available regarding estimating the required size of vent area and the radius of venting curves, the physical limitations on vent placement and size render the application of explosion relief/venting not always practicable.

![Image](https://example.com/image.png)

**Figure 2.6 Side explosion relief/venting from filter. (Copyright CMR Gexcon)**

### 2.4.2 Water-based protection measures

Water is safe, eco-friendly and readily available in the oil and gas process environment, therefore, it is widely applied for fire and explosion protections. Water-based measures are comprised of open sprinklers, which are designed to deliver high water-air density to the deluge area surface, the applications of water-based systems include water deluge, water mist and water curtain. Figure 2.7 showed a water deluge system and a classic sprinkler.
Water deluge

There have been some debates and studies about the advantages and drawbacks of water deluge on gas explosion mitigation (Jones & Thomas 1993).

In terms of the benefits, firstly, there is no physical limitations on application of water deluge system compared to explosion relief/venting mentioned above. Secondly, water deluge is more cost-efficient and easier to be installed, while for other large size power systems, numerous structural components may impose weight penalties in the installation of the power systems (Thomas 2000). Last but not least, compared to other triggered systems activated at the start of the explosion, water deluge can and have to be activated during gas detection, which ensures water deluge is more time-efficient in terms of protection (Pritchard 2006).

However, there have been more controversies regarding water deluge’s disadvantages. For example, according to HSE’s report (HSE 2000), water deluge may increase ignition probabilities if sufficient protection to electrical fittings is not in place. Moreover, it was mentioned that explosion enhancement can occur if the water systems are not effective in the early stage (Vanwingerden & Wilkins 1995; Vanwingerden et al. 1995; Thomas 2000), the enhancement of explosion after ignition may depend on the water droplet size and turbulence generated by spray in the deluge area. Provided the
water spray is either sufficiently fine or dense enough, it is highly likely to quench a propagating flame in the early stage of combustion. On one hand, it was stated that the water droplets should be sufficiently fine so that they could evaporate within the deluge zone (Zalosh & Bajpai 1982). The critical diameter of the water droplet was estimated as 18 µm (Sapko et al. 1977), and it was suggested that droplets with diameter less than 10 µm are also explosion-mitigating effective (Vanwingerden et al. 1995). On the other hand, according to the experimental data (Vanwingerden et al. 1995), large droplets over 200 µm may also be beneficial to overpressure mitigation in a long term as well, since the larger droplets will contribute to easier droplet break-up and thereby mitigating gas explosions. Furthermore, the explosion-mitigating effect of large droplets is subjected to another condition, which is that the flame velocity of explosion before water deluge mitigation has to be high enough so that the hydrodynamics force of flow acceleration can cause the droplets to break-up, such aerodynamic break-up had been investigated (Thomas et al. 1990). Regarding the relationship between the flame velocity and initial droplet diameter, the critical condition correlations were also provided (Pilch & Erdman 1987) along with the weber number mathematically expressed in the more recent work (VanWingerden 2000). Additionally, Pritchard concluded that the droplet break-up scenarios with high flame velocities, however, can only be seen in semi-confined/open congested geometries (Pritchard 2006), whereas the confined geometries with low level of obstruction, the initial flame velocities in the modules will be too low to break up the water droplets. For the low congested scenarios, the activation of a water deluge could result in explosion enhancement – more turbulence will be induced since the existence of water droplets contributes to higher congestion level, thereby increasing VCE overpressure. Therefore, the application of water deluge becomes complicated when it comes to the evaluation of water deluge droplets, the initial explosion severity, and the conditions of congestion/confinement.

**Water mist**

There is no major difference between water deluge and water mist systems, Kailasanath et al. (Kailasanath et al. 2002; SCHWER & Kailasanath 2006) described water deluge system (VanWingerden 2000) as water mist system in the reports. In addition, water mist system is operated on the same principle as a deluge system. However, the water
from the mist system is discharged in sufficiently smaller sized droplets, which covers wider area, whereas water deluge may have denser and larger water droplets. The relatively small droplets from water mist absorb explosion/fire energy faster than some deluge systems with higher water density (WGT 2015). Singapore Fire Safety Guidelines (SFSG 2012) also stated that water mist system discharges small water droplets to extinguishing power under high momentum or compressed gas through small orifice nozzles, which more rapidly and efficiently segregate the combustion and oxygen. However, due to the travel distance limitation of fine droplets, the application space subjected to a water mist system should be under 300 m³, while water deluge system can be used for larger offshore modules.

**Water curtain**

One of the other water-based solutions is the water curtain, which could also be classified as an active suppression barrier. Instead of providing protection to equipment or areas under explosion, water curtains are mostly used for the purpose of gas dispersion mitigation, for instance, water curtains can remove chemical and influence the gas concentration, thereby interrupting dispersion pattern. They separate congested regions from each other or protect escape routes (SFSG 2012), therefore the gas dispersion/gas cloud generation due gas leakage could be minimized and restrained in the separation region. The effectiveness of water curtains in the case of ammonia release was investigated (Bara & Dusserre 1997), the reductions of gas concentration by a factor of 10 at a distance of approximately 13 m and a factor of 3 at a distance of approximately 20 m were observed. The calculated velocities and concentration reduction due to the water curtain in these experiments were later numerically modelled (Isnard et al. 1999). A more recent investigation of water curtain application in offshore/onshore structures had been carried out (Rana et al. 2010), the researchers summarized laboratory methodology and conducted two water curtain tests regarding LNG vapour cloud. The water curtain effect on experiments, which were conducted in Mary Kay O’Connor Process Safety Center, also had been parametrically studied (Rana & Mannan 2010), it was concluded that water curtain can considerably disperse LNG vapour cloud thanks to its effects of transfer of momentum and heat, entrainment of air and dilution of vapour with entrained air. Overall, water curtains are mainly beneficial to offshore/onshore structures in toxic/flammable vapour cloud dispersion/dilution, but
if the gas cloud is ignited after gas dispersion, the escalation of explosion/fire cannot be mitigated by water curtain, so instead of an explosion mitigation, water curtain is more suitable for explosion prevention.

2.4.3 Barrier technologies

In order to control the turbulence generation in a gas explosion and to reduce the consequences, the explosion barriers are designed and widely used as explosion mitigating approaches, the concept of explosion barriers is related to the energy model developed in 1960s and 1980s (Gibson 1961; Haddon 1980), Figure 2.8 demonstrated the relationship among the explosion, safety barrier and accident prevention. Depending on the operators or technical control systems, the barrier systems may be categorized as active and passive types, the descriptions of the difference between active and passive barriers can be found in previous studies (Crowl 2001; Kjellén & Larsson 1981). In addition, a table summarizing the advantages, disadvantages and applicability of the reviewed active/passive blast barriers has been provided in Table 2.2.

Figure 2.8 Safety barrier concept in the energy model (Haddon 1980).
Table 2.2 Summary of the reviewed active/passive blast barriers

<table>
<thead>
<tr>
<th>Barriers</th>
<th>Type</th>
<th>Advantages</th>
<th>Disadvantages</th>
<th>Applicability</th>
</tr>
</thead>
<tbody>
<tr>
<td>Suppression barriers</td>
<td>Active</td>
<td>Easy to be controlled by a monitor system.</td>
<td>Expensive, Not suitable for large-scale onshore/offshore structures.</td>
<td>Small enclosures</td>
</tr>
<tr>
<td>Water-based barrier</td>
<td>Active</td>
<td>Cheap, High efficiency in flame separation and suppression for partially confined regions.</td>
<td>Not applicable for the place in water shortage.</td>
<td>Partially confined regions, Area with abundant water supply</td>
</tr>
<tr>
<td>Shutter/valve</td>
<td>Active</td>
<td>High leak-proof-ness (Block the flame front in all directions)</td>
<td>Slow-acting</td>
<td>Small regions, Fast-response system</td>
</tr>
<tr>
<td>Soft/membrane blast barrier</td>
<td>Passive</td>
<td>Cost-efficient, High-efficiency in gas cloud reduction for one-directionally vented structures</td>
<td>Non-blast resistant</td>
<td>One-directionally vented structures</td>
</tr>
<tr>
<td>Solid/hard blast wall</td>
<td>Passive</td>
<td>Easy to construct, Balance in gas cloud and explosion mitigation</td>
<td>Expensive, Blast reflection, Overpressure concentration</td>
<td>Partially confined and moderately congested regions</td>
</tr>
</tbody>
</table>

**Active barriers**

The explosion suppression system is one of the active barrier technologies, it can be activated in the situation that an incipient deflagration occurs, and the suppression agent
will be injected into the enclosure to quench the propagation of flame before damaging overpressure have generated. ISO 6184/4 (ISO6184/4 1985) and NFPA 69 (NFPA69 2002) described the active barrier systems, they consist of agent containers with compressed suppression agent, for example, mono-ammonium phosphate based and sodium bicarbonate suppression production. The detection of a deflagration and the activation of the trigger are controlled by a monitor system. However, it was mentioned that the suppression barriers are usually just suitable for small enclosures (Zalosh 2005), because for large targets, more suppression agent containers are required to be installed, which increases the operational cost, especially for those suppression barriers with chemical agent, and the refills after system discharge may be even more expensive. A replacement to reduce the cost could be the application of the water-based barrier, since water is cheaper to supply. The effectiveness of the Micro-mist water barrier had been investigated by conducting 20 experimental tests (Tam et al. 2003), it was proved that the Micro-mist devices could prevent a propagating flame and totally suppressed a developed VCE within a partially confined region. However, all these experiments (Tam et al. 2003) were in a small scale, while for large-scale modules in reality, the water suppression barrier may have physical limitations. For example, Pritchard demonstrated a water trough barrier (Pritchard 2006), which is a suppression systems discharge agent vertically, the blast wave prevention/mitigation effect became questionable due to the fact that the target was a platform leg should be protected horizontally, in such case, the vertically oriented water suppression could not provide sufficient blast prevent.

Another alternative of the active water barrier could be a solid barrier, such as shutter or valve that can vertically and horizontally prevents the passage of a flame front in a physical way. However, the effectiveness of solid barrier is dependent on the shutting or activating speed of the wall or valve. For a physically large object, such as the liquefaction modules in LNG, it requires a significant amount of construction to separate the adjacent modules by using an active solid barrier, and it is debatable whether such a solid wall could be shuttered rapidly to prevent the propagation of flame. Regardless the selection of either the high-speed wall/valve barriers or the triggered suppressant barriers, a fundamental understanding regarding the flame propagation process is required. Moore and Spring derived the algorithms governing the gas
explosion mechanism to design the active isolation barriers (Moore & Spring 2005), they found that the ignition location significantly influence the performance of barrier systems. For flame detection, the worse scenario was ignition far away from the mouth of strong explosions; while for pressure detection, the worse scenario was ignition near the mouth of the weak/soft explosion in large volumes. To summarize, the design of an active barrier system depends on the geometry size, structural orientation, budget and ignition location, etc.

**Passive barriers**

In terms of the passive systems, they lack detector and control units to trigger the preventive or protective action against an explosion; therefore, passive systems are also considered efficient approaches in terms of cost-benefit. Different types of passive systems are suitable for specific process environments to offer a proper solution in case of an explosion, such as the passive barriers, they are designed to provide physical barriers to protect process structures from heat radiation, blast waves and high velocity projectiles, etc. (Pekalski et al. 2005). The main passive barriers are categorized as soft barriers (e.g. membrane gas barriers) and hard barriers (e.g. blast walls).

The soft barriers could be a plastic or membrane sheet, which is designed to limit the size of a cloud of flammable fuel–air mixtures generating from a gas leakage with a congestion, therefore, the ensuing explosion in case of ignition will be prevented (Tam 1999). Such membrane soft barriers is more applicable in the single directionally vented chamber/module, as it is easier to subdivide the protected module into several sections in one direction, then the generation of gas cloud could be limited in only one confined section by using soft barriers. In order to investigate the behaviour of the soft barriers in terms of their ability to withstand the gas releases in the one direction module, a series of experiments had been conducted (Wilkins & Vanwingerden 2008), the performance of different soft barrier materials regarding the opening ways, opening times and duration were discussed. It was found that the soft barrier type, design and fixing method significantly influence the jet resistance, barrier opening pressure, and fragmentation characteristics, etc. All these experiments provided further insight into the development and production of soft barriers with desired functionality. Overall, the soft barriers are beneficial in limiting flammable cloud size under certain gas release
pressures; once the gas cloud is ignited to produce the explosion, the soft barriers may act as weak vent panels in the protecting direction.

In terms of the hard barriers (e.g. blast wall), they are not only capable of restricting the spread of flammable gas cloud, but also they are efficient in restraining the flame front of a gas explosion, thereby mitigating the overpressures. The guidance Technical Note 5 (TN5) (HSE 2000) issued by the Fire and Blast Information Group (FABIG) and the Steel Construction Institute (SCI) provided the design code of blast walls, and there have been an extensive amount of research regarding the design and structural analysis of blast wall (Louca et al. 2004; Langdon & Schleyer 2005; Langdon & Schleyer 2006; Schleyer et al. 2007). Regarding the application of blast walls in the oil and gas industry, blast walls were widely used in onshore or offshore facilities to separate living quarters from the process modules (Nwankwo et al. 2013), the separated units from each other could mitigate the catastrophic consequences of a possible gas explosion. A practical procedure for structural response analysis of FPSO topside blast wall under gas explosion loads was developed (Sohn et al. 2013). Besides the single degree of freedom (SDOF) dynamic model proposed in TN5 (HSE 2000), the time-domain nonlinear finite element method had also been applied in the study, which delivers the practical and useful insights into the explosion mitigating design of offshore structures by using blast walls. Boh and Louca et al. numerically modelled the blast wall and a tee-stiffened panel subjected to hydrocarbon explosions, they mentioned that the boundary restraints of blast walls contribute to significant impact on the overpressure withstanding ability of the pane (Boh et al. 2007; Louca et al. 1996), and a comparison of the FE system with the Biggs model (Biggs & Testa 1964) was also conducted. However, it was stated that the possible gas explosion overpressure can be sustained by most of the existing stainless steel blast walls is up to about 4 barg (Selby & Burgan 1998), a table of the available blast barriers and blast resistant wall products (Zalosh 2005) is listed in Table 2.3. Therefore, blast walls would not be a feasible explosion mitigation option for congested units in large size, since these units with long run-up distance can results in potential DDT (Silvestrini et al. 2008; Blanchard et al. 2011), which may readily result in overpressure over 4 barg. Moreover, the decision of the location of blast wall on the offshore platforms could be very controversial. The walls can either be placed near the protective target or located closed to the explosion source to absorb the
overpressure/energy (HySafe 2006). However, the leakage and ignition locations are unpredictable, which means it will not be practical to identify the locations of blast walls deterministically. A detailed study regarding the interaction between blast walls and surrounding objects should be conducted for the complex offshore structures, and a probabilistic study regarding the design of blast wall in overpressure mitigation is suggested. An example was discussed in Chapter 8.

Table 2.3 Blast barrier products according to U.S. Blast Mitigation Action Group (Zalosh 2005)

<table>
<thead>
<tr>
<th>Company</th>
<th>Product</th>
<th>Web Site</th>
</tr>
</thead>
<tbody>
<tr>
<td>AIGIS Engineering Solutions Ltd.</td>
<td>Applique wall blast protection system</td>
<td><a href="http://www.aigis.co.uk">http://www.aigis.co.uk</a></td>
</tr>
<tr>
<td>Achidatex</td>
<td>Blast wall covering</td>
<td><a href="http://WWW.INFOMEDIA.CO.IL/Companies/A/ACHIDATEEXNEL.html">WWW.INFOMEDIA.CO.IL/Companies/A/ACHIDATEEXNEL.html</a></td>
</tr>
<tr>
<td>Astralloy Wear Technology</td>
<td>Blast-resistant modular fencing</td>
<td><a href="http://www.astralloy.com">http://www.astralloy.com</a></td>
</tr>
<tr>
<td>Ballistics Technology International Ltd.</td>
<td>Shock absorbing concrete blast protection wall system</td>
<td><a href="http://BallisticsTech.com">http://BallisticsTech.com</a></td>
</tr>
<tr>
<td>Battelle Memorial Institute</td>
<td>Blast mitigation using a water spray system that reduces the energy from a vehicle bomb</td>
<td><a href="http://www.battelle.org/">www.battelle.org/</a></td>
</tr>
<tr>
<td>BlastGard, Inc.</td>
<td>N/A</td>
<td><a href="http://www.blastgard.net">http://www.blastgard.net</a></td>
</tr>
<tr>
<td>CINTEC</td>
<td>Retrofit system to increase the blast-resistant capacity of masonry walls</td>
<td><a href="http://www.cintec.com">http://www.cintec.com</a></td>
</tr>
<tr>
<td>Composite Fibreglass Mouldings, Ltd.</td>
<td>Blast-resistant cladding panels</td>
<td>None listed</td>
</tr>
<tr>
<td>Corus</td>
<td>Blast-mitigation wall consisting of steel-concrete- steel sandwich construction</td>
<td><a href="http://www.bi-steel.com">http://www.bi-steel.com</a></td>
</tr>
<tr>
<td>Creative Building Products</td>
<td>Portable plastic barricades and barriers</td>
<td><a href="http://www.soacorp.com/cbp">http://www.soacorp.com/cbp</a></td>
</tr>
<tr>
<td>Cymat Corp.</td>
<td>Blast wall covering, and blast door core</td>
<td><a href="http://www.cymat.com">http://www.cymat.com</a></td>
</tr>
<tr>
<td>Federal Fabrics Fibers, Inc.</td>
<td>Blast mitigation barricades, wall coverings, and window shades</td>
<td>None listed</td>
</tr>
<tr>
<td>Firexx Corp.</td>
<td>Blast mitigation foil material made of aluminum and magnesium</td>
<td><a href="http://www.Firexx.com">http://www.Firexx.com</a></td>
</tr>
<tr>
<td>General Plastics Manufacturing Co.</td>
<td>Explosive blast-damping wall system</td>
<td><a href="http://www.generalplastics.com">http://www.generalplastics.com</a></td>
</tr>
<tr>
<td>Hesco Bastion Ltd.</td>
<td>Units are cells formed of Bezinal galvanized weldmesh, lined with polypropylene geotextile material.</td>
<td><a href="http://www.hesco.group.com">http://www.hesco.group.com</a></td>
</tr>
<tr>
<td>Inter-Block Retaining Systems</td>
<td>Interlocking, stackable modular mass (4,000 lb) concrete block units</td>
<td><a href="http://www.inter-block.com">http://www.inter-block.com</a></td>
</tr>
<tr>
<td>LINE-X Protective Coatings</td>
<td>Blast mitigation spray-able polymer coating</td>
<td><a href="http://www.linex.com/FPED/">http://www.linex.com/FPED/</a></td>
</tr>
</tbody>
</table>
2.4.4 Safety gap

In the process industry, the safety gap, which is an open space with no congestion, deliberately placed in between congested process areas, is one of the most effective and widely used safety-in-design measures (Ma et al. 2014), Figure 2.9 illustrates an example. The safety gap can isolate the flammable gas cloud in one region to the adjacent unit, and the heat and overpressure resulting from a possible ignition can be mitigated. The principle behind the operation of the safety gap is that it interrupts a positive feedback mechanism in congested areas. The positive feedback mechanism consists of the generation of turbulence, enhanced thermal and chemical mixing between combustion products and reactants, higher flame speeds and even higher pressures. The absence of obstacles in a safety gap eliminates the fluid-obstacle interaction thereby preventing the generation of turbulence.

![Figure 2.9 A safety gap between two congested modules.](image)

The research of safety gap/distance in the old days were mainly using simplified guidelines or approaches. For example, Zabetakis and Burgess provided a simplistic safety distance guidelines in 1960 to investigate the quantity-distance relationship regarding the ignition of fuel-air cloud and the vaporization of LH₂ (Zabetakis & Burgess 1960). An industrial storage code for using safety distances to protect personnel and structures aloof hydrogen, gasoline and LNG tanks had been preliminarily established (Hord 1978). In the 1990s, American Institute of Chemical Engineers (AIChE) (AIChE 1993) simply suggested the minimum separation distances for some generic chemical process units, and Center for Chemical Process Safety (CCPS) (CCPS 1994) proposed a more sophisticated method, which better suits quantitative risk assessments (QRA) to calculate the explosion overpressure as a function of separation...
distance. For all these simplified methods, it lacks detailed description of gas explosion mechanism. In order to investigate the effect of the safety gap on gas explosion more accurately, some recent experimental studies have been conducted (Gubba et al. 2008; Moen et al. 1980; Rudy et al. 2011; Na'inna et al. 2013; VandenBerg & Versloot 2003). However, most of these experimental explosion tests were carried out in highly confined chambers, which are very simple geometries with no consideration of the variation of obstacles/congestions, except that VandenBerg et al. (VandenBerg & Mos 2002; VandenBerg & Versloot 2003) developed practical guidelines by using varying congested configurations. A more practical investigation regarding the safety gap can be seen in a previous work (Berg et al. 2000), the safety gap had been designed in a FPSO. However, so far, there are very limited literatures regarding the effectiveness, limitations, design considerations and parameters of safety gap, which is the reason why the application of safety gap was thoroughly discussed in Chapter 7. The effectiveness and limitation of safety gap was mentioned in Chapter 7 that safety gap is an effective gas explosion mitigation, and for economic purposes, applying a safety gap is much cost efficient than blast wall, however, safety gap required sufficient open space distance to interrupt flame turbulence in explosion (Ma et al. 2014).

2.4.5 Inherent safety design

The inherent safety design and layout of offshore/onshore structures aim to limit the size of flammable gas cloud and reduce overpressures from a gas explosion. Both these aims can be accomplished by minimizing the confinement, in other words, the structures without wall is the optimal solution in terms of explosion safety (HySafe 2006). Some offshore structures could be good examples of the no-wall designs, such as the Floating Liquefied Natural Gas (FLNG) vessel. Even through there is still no much proven technology and operation of the FLNG system (Otsubo et al. 2012) in the industry, a FLNG model conceptually designed by Exxonmobil (Gexcon 2012) demonstrated that all process modules in the vessel are designed with large vent areas in all directions, as seen in Figure 2.10.
Additionally, the congestion level of the structure should be designed as low as possible in order to avoid DDT. For the Exxonmobil’s FLNG, each module was designed compact and congested with different units for cost efficient purpose (Lee et al. 2012), and due to the no-wall design, the gas cloud and flame can readily propagate through one module to the other. Therefore, the build-up of overpressure due to DDT in the congestion could be significantly exaggerated. In such case, the explosion mitigation systems as mentioned above, such as blast wall or safety gap can be applied to reduce the gas cloud size or suppress the turbulent flame acceleration. However, for this space-efficient ship, the available open space to allow the application of safety gap is very limited, and the installation of blast wall would undermine the original intention of no-wall design, especially when the wind blows in one direction against blast wall, the confinement of blast wall in such case may contribute to detrimental gas concentration. To optimize the gas explosion safety design, designing the FLNG as cylindrical shape (Wang et al. 2013; Kvamsdal et al. 2010; Zhao et al. 2011; Hirdaris et al. 2014) may be a solution, as seen in Figure 2.11, the cylindrical FLNG provides inherent safety advantages that it has larger available open area (but the overall area of the cylindrical platform is smaller than the size of the ship-shaped FLNG), so the safety gap can fully play its active role in explosion mitigation; and due to its symmetrical and circular shape, the uncertainty of wind direction would be minimised when blast wall is applied. The discussion regarding the gas dispersion and explosion analysis of the cylindrical FLNG by using explosion mitigating measures was in Chapter 7 and Chapter 8.
To summarize, varying from explosion relief/venting to safety gap, it is not always straightforward to choose a proper explosion mitigation. The selection concerns are dependent on the available evaluation tool, geometry size and complexity, budget, congestion and confinement conditions, etc. Detailed studies, such as CFD simulations and probabilistic analysis would be highly recommended. The basic goals of this research are to provide the fundamental insight regarding the choice of explosion mitigation systems; in addition, to propose a newly derived correlation of gas explosion calculation by evaluating different offshore/onshore structures; finally yet importantly, to deliver the practical knowledge of optimal explosion mitigation design on an innovating offshore platform – Cylindrical FLNG platform.
CHAPTER 3. CFD SIMULATION OF GAS EXPLOSION

3.1 INTRODUCTION

In this thesis, some large-scale realistic offshore structures, such as a traditional FLNG and a cylindrical FLNG, which are subjected to gas explosion, were investigated. Due to the limitations of structural scale and complexity in using empirical and phenomenological methods, the Computational Fluid Dynamics (CFD) approach – FLACS was used to conduct vapour cloud explosion analysis. FLACS agrees with experiments to a greater degree than analytical studies, and it is considered as a robust numerical tool based on finite volume solutions and the ‘physical’ models of combustion process to predict gas explosion overpressure (Li et al. 2014b). In particular, FLACS solves the Reynolds averaged mass, momentum and energy balance equations, with special schemes for supersonic flows and a database of chemical kinetics.

The CFD simulation theories regarding the flame turbulence, geometry condition and fluid-obstacle interaction, etc. were discussed in this chapter.

3.2 CFD SIMULATION MODEL

3.2.1 Fluid flow equations

The mathematical models of FLACS (Ferrara et al. 2006; Hjertager 1984; Hjertager 1993; Arntzen 1998) are given below. For a general variable, the differential equation, which is based on Reynolds averaged mass, momentum and energy balance equations, may be expressed as follows using standard symbols:

\[
\frac{\partial}{\partial t}\left(\rho \Phi\right) + \frac{\partial}{\partial x_j}\left(\rho u_j \Phi\right) - \frac{\partial}{\partial x_j}\left(\Gamma_\Phi \frac{\partial \Phi}{\partial x_j}\right) = S_\Phi; \Gamma_\Phi = \frac{\mu_{\text{eff}}}{\sigma_\Phi}
\]  (3)

where \(\Phi\) denotes a general variable, \(\rho\) is the gas mixture density, \(x_j\) is the coordinate in \(j\)-direction, \(u_j\) is the velocity component in \(j\)-direction, \(\Gamma_\Phi\) is the effective (turbulent) diffusion coefficient, \(\mu_{\text{eff}}\) is the effective turbulence viscosity and \(S_\Phi\) is a source term.
A summary of all the governing equations needed for a typical reactive gas dynamic calculation are presented below.

The state equation of an ideal gas:

\[ pW = \rho RT \]  \hspace{1cm} (4)

where \( p \) is the pressure, \( R \) is the universal gas coefficient, \( T \) is temperature and \( W \) is the molar weight of the gas mixture.

The continuity equation:

\[ \frac{\partial \rho}{\partial t} + \frac{\partial}{\partial x_j} (\rho u_j) = 0 \]  \hspace{1cm} (5)

The momentum balance equation:

\[ \frac{\partial}{\partial t} (\rho u_i) + \frac{\partial}{\partial x_j} (\rho u_i u_j) = - \frac{\partial p}{\partial x_i} + \frac{\partial}{\partial x_j} (\sigma_y) \]  \hspace{1cm} (6)

The energy balance equation:

\[ \frac{\partial}{\partial t} (\rho h) + \frac{\partial}{\partial x_j} (\rho u_j h) = \frac{\partial}{\partial x_j} \left( \Gamma_h \frac{\partial h}{\partial x_j} \right) + \frac{\partial p}{\partial t} + u_j \frac{\partial p}{\partial x_j} \]  \hspace{1cm} (7)

where \( \sigma_y \) is the flux of momentum and \( h \) is the enthalpy.

### 3.2.2 Stoichiometry

In the combustion, fuel is oxidated in the air with varying reactants and production, which is accompanied with heat and light. The fuel mixtures, which are either too lean or too rich, will suffer from oxidant dilution or incomplete reaction. For a complete combustion, the stoichiometric reaction can be expressed as:

\[ C_{n_c}H_{n_h} + \left( nc + \frac{nh}{4} \right) O_2 \rightarrow ncCO_2 + \frac{nh}{2} H_2O \]  \hspace{1cm} (8)

And for substitutive hydrocarbons, the stoichiometry is written in general (Kuchta 1985):
\[ C_{nc}H_{nh}O_nN_mZ_x + \left( nc + \frac{nh - x - 2n}{4} \right)O_2 \rightarrow ncCO_2 + \left( \frac{nh - x}{2} \right)H_2O + xHZ + \frac{m}{2} N_2 \] (9)

where \( Z \) is any halogen atom. For combustion in air, the stoichiometric concentration \( C_{sc} \) is:

\[ C_{sc} = \frac{100}{1 + 4.773 \left( nc + \frac{nh - x - 2n}{4} \right)} \text{ mol pct} \] (10)

Therefore, the weight ratio of fuel-air in stoichiometric concentration \( r_{f-a-sc} \) can be determined by:

\[ r_{f-a-sc} = \frac{28.97}{M_f} \frac{100}{100 - C_{sc}} \] (11)

where 28.97 is the molecular weight of air, \( M_f \) is the molecular weight of fuel. The air properties in different conditions are summarized in Table 3.1

<table>
<thead>
<tr>
<th>Constituent</th>
<th>Molecular weight</th>
<th>Density (0°C) g/L</th>
<th>Specific heat (20°C), cal/(g°C)</th>
<th>Vol pct</th>
</tr>
</thead>
<tbody>
<tr>
<td>Nitrogen</td>
<td>28.01</td>
<td>1.251</td>
<td>0.249</td>
<td>78.09</td>
</tr>
<tr>
<td>Oxygen</td>
<td>32.00</td>
<td>1.429</td>
<td>0.219</td>
<td>20.95</td>
</tr>
<tr>
<td>Argon</td>
<td>39.94</td>
<td>1.784</td>
<td>0.124</td>
<td>20.93</td>
</tr>
<tr>
<td>Carbon dioxide</td>
<td>44.01</td>
<td>1.977</td>
<td>0.200</td>
<td>20.03</td>
</tr>
<tr>
<td>Air</td>
<td>28.97</td>
<td>1.293</td>
<td>0.240</td>
<td>100.00</td>
</tr>
</tbody>
</table>

### 3.2.3 Thermodynamic relationships

The understanding of thermodynamic properties is vital in computing the energy balance of an explosion. As a function of temperature, the thermodynamic relationship is mainly between the formation enthalpy \( H \) and heat \( C_h \), which was described by Kee (Kee 1987). \( H \) and \( C_h \) in the Chemkin thermodynamic database are calculated in a polynomial format:
The thermodynamic relationship in terms of $H$ and $C_h$ then is:

$$C_h = \left( \frac{\partial H}{\partial T} \right)_p$$

(14)

$H$ and $C_h$ is characterized in second order polynomial as function of temperature in FLACS (Arntzen 1998):

$$C_h = a + bT$$

(15)

$$H = a(T - T_0) + 0.5b(T^2 - T_0^2) + h^0$$

(16)

where $T_0$ and $h^0$ are standard temperature and heat of formation, respectively. $h^0$ equates 0 when the combustion heat is used in enthalpy calculations. The given value of $a$, $b$ and $d$ to represent the composition of H$_2$O, H$_2$, CO, CO$_2$ and C$_2$H$_4$, etc., as seen in Table 3.2 (Arntzen 1998).

Table 3.2 Data of gas, enthalpy of combustion and enthalpy of formation in FLACS (Arntzen 1998)

<table>
<thead>
<tr>
<th>Gas</th>
<th>M g/mole</th>
<th>C</th>
<th>H</th>
<th>A</th>
<th>$\Delta h$ 298 K MJ/kg</th>
<th>h=aT+bT$^2$/2 a</th>
<th>b</th>
<th>h=f 298K MJ/kg</th>
<th>h=f=aT+bT$^2$/2-d a</th>
<th>b</th>
<th>d $10^{-6}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>Methane</td>
<td>CH$_4$</td>
<td>16</td>
<td>1</td>
<td>2</td>
<td>2</td>
<td>50.0</td>
<td>1000</td>
<td>4.11</td>
<td></td>
<td>1200</td>
<td>3.40</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td>-4.681</td>
<td></td>
<td>8.26</td>
</tr>
<tr>
<td>Acetylene</td>
<td>C$_2$H$_2$</td>
<td>26</td>
<td>2</td>
<td>1</td>
<td>2.5</td>
<td>48.2</td>
<td></td>
<td>1340</td>
<td>1.40</td>
<td>740</td>
<td>2.85</td>
</tr>
<tr>
<td>Ethylene</td>
<td>C$_2$H$_4$</td>
<td>28.1</td>
<td>2</td>
<td>2</td>
<td>3</td>
<td>47.2</td>
<td></td>
<td>1.867</td>
<td>740</td>
<td>700</td>
<td>3.70</td>
</tr>
<tr>
<td>Ethane</td>
<td>C$_2$H$_6$</td>
<td>30.1</td>
<td>2</td>
<td>3</td>
<td>3.5</td>
<td>47.4</td>
<td></td>
<td>1.867</td>
<td>740</td>
<td>700</td>
<td>3.70</td>
</tr>
<tr>
<td></td>
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<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Propylene</td>
<td>C$_3$H$_6$</td>
<td>42.1</td>
<td>3</td>
<td>3</td>
<td>4.5</td>
<td>45.8</td>
<td></td>
<td>0.486</td>
<td>690</td>
<td>660</td>
<td>3.55</td>
</tr>
<tr>
<td>Propane</td>
<td>C$_3$H$_8$</td>
<td>44.1</td>
<td>3</td>
<td>4</td>
<td>5</td>
<td>46.3</td>
<td></td>
<td>-2.355</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Component</td>
<td>Formula</td>
<td>C, H, O, S</td>
<td>( \text{a} )</td>
<td>( \text{b} )</td>
<td>( \text{c} )</td>
<td>( \text{d} )</td>
<td>( \text{e} )</td>
<td>( \text{f} )</td>
<td></td>
<td></td>
<td></td>
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<tr>
<td>--------------</td>
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<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Butane ( \text{C}<em>4\text{H}</em>{10} )</td>
<td>58.1</td>
<td>4</td>
<td>5</td>
<td>6.5</td>
<td>45.7</td>
<td>-</td>
<td>2.147</td>
<td>641</td>
<td>3.48</td>
<td>2.52</td>
<td></td>
</tr>
<tr>
<td>Hydrogen ( \text{H}_2 )</td>
<td>2</td>
<td>0</td>
<td>1</td>
<td>0.5</td>
<td>120</td>
<td>0</td>
<td>13600</td>
<td>1.719</td>
<td>4.13</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Hyd. sulfide ( \text{H}_2\text{S} )</td>
<td>34</td>
<td>1</td>
<td>1.5</td>
<td>-</td>
<td>0.606</td>
<td>925</td>
<td>0.40</td>
<td>0.90</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Sulph. dio. ( \text{SO}_2 )</td>
<td>64</td>
<td>-4.64</td>
<td>765</td>
<td>0.14</td>
<td>4.87</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Carb. m. ox. ( \text{CO} )</td>
<td>28</td>
<td>1</td>
<td>0</td>
<td>0.5</td>
<td>-</td>
<td>3.951</td>
<td>1050</td>
<td>0.115</td>
<td>4.27</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Carb. diox. ( \text{CO}_2 )</td>
<td>44</td>
<td>1</td>
<td>0</td>
<td>0</td>
<td>1002</td>
<td>0.173</td>
<td>-</td>
<td>8.957</td>
<td>1060</td>
<td>0.157</td>
<td>9.28</td>
</tr>
<tr>
<td>Water vap. ( \text{H}_2\text{O} )</td>
<td>18</td>
<td>1740</td>
<td>0.614</td>
<td>-</td>
<td>13.43</td>
<td>1780</td>
<td>0.515</td>
<td>14</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Water liq. ( \text{H}_2\text{O} )</td>
<td>18</td>
<td>-</td>
<td>4000</td>
<td>0.550</td>
<td>17.1</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Hydroxyl ( \text{OH} )</td>
<td>17</td>
<td>2.293</td>
<td>1620</td>
<td>0.200</td>
<td>-</td>
<td>1.80</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Nitr. Oxide ( \text{NO} )</td>
<td>30</td>
<td>3.010</td>
<td>1040</td>
<td>0.087</td>
<td>-2.7</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Oxygen ( \text{O}_2 )</td>
<td>32</td>
<td>888</td>
<td>0.195</td>
<td>0</td>
<td>950</td>
<td>0.112</td>
<td>0.29</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Nitrogen ( \text{N}_2 )</td>
<td>28</td>
<td>824</td>
<td>0.397</td>
<td>0</td>
<td>1036</td>
<td>0.118</td>
<td>0.31</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

The temperature in the thermodynamic system is then expressed as:

\[
T = \frac{-a + \left[ a^2 + 2b(H + d) \right]^{\frac{1}{2}}}{b} \tag{17}
\]

### 3.2.4 Ignition Process

Ignition triggers the combustion reaction with evolution of emission and heat. Ignition types include electrical ignitions, thermal ignitions and chemical ignition. In FLACS, all ignition types initially used the H-M model (Hjertager 1982; Bakke 1986) for version FLACS-86 and FLACS-89, which assumes that half of the combustible mixture in the ignition cell is altered to products at \( t = 0 \). The H-M model calculates the reaction rate for turbulent combustion as:

\[
w_r = \dot{N}_e \rho \frac{\theta}{k} \min(m_{f_i}, m_{f_o} - m_{f_i}) \text{ when } t_i > t_{ad} \tag{18}
\]
where $\varepsilon$ is the dissipation rate of turbulent energy, $k$ is turbulent kinetic energy, $w_r$ is the reaction rate, $N_c$ is a constant, $m_{fu}$ is the mass fraction of fuel, $m_{fo}$ is the initial mass fraction of fuel, $\tau$ is the turbulent time and $\tau_{id}$ is the ignition delay time. The ignition of combustion depends on the comparison between the turbulent time $\tau$ and the ignition delay time $\tau_{id}$.

According to the shock tube tests (Hjertager 1982; Bakke 1986), the ignition delay time as the chemical time scale $\tau_n$ is:

$$\tau_n = P_n e^{E_n} C_{fu}^{a_n} C_{ox}^{b_n}$$

where $C_{ox}$ and $C_{fu}$ are the concentrations of oxygen and fuel, $T$ is the gas temperature, and for fuel type $n$, the constants $E_n$, $P_n$, $a_n$ and $b_n$ can be found from the database by CHEMKIN (Kee et al. 1980). A cold front quenching criteria also controls the H-M model, then the reaction rate is expresses as:

$$w = N_c \rho \frac{u'}{l_t} \min(c, 1 - c) \text{ when } c > c_q$$

where $c$ is the mass fraction of products, $c_q$ is a function of the turbulent time $\tau$ and delay time of ignition in equation (19), $u'$ is the turbulent velocity fluctuation and $l_t$ is turbulent length scale. The cold front quenching criteria requires high mass fraction of products, which also means the time between maximum overpressure and ignition is significantly dependent on the grid dimension selected in the FLACS simulation.

To evaluate the flame area in a given flame volume, the $\beta$ flame model replaced H-M model since the version FLACS-93, and it was implemented with the ignition model in the flame area calculation:

$$A = \frac{1}{3} (6V)^{\frac{2}{3}}$$

where $V$ is the flame volume, which is related to the volume fraction of products $f$, the relationship between them is:

$$f = c \frac{1 + \tau}{1 + \tau c}$$
where $\tau$ is density ratio/expansion ratio between product and reactant.

Ignition region in FLACS is usually recommended to set as a point, a line or a plane in one control volume. However, as the new guidelines developed nowadays, the ignitions can be placed at different locations and time.

### 3.2.5 Geometry counting and porosity calculations

In FLACS, the geometry of a realistic structure with complex components is created by using a series of simple objects, such as boxes and cylinders. The cofile, which is the geometry storage file in FLACS, is used to account the amount of cylinders and boxed in the geometry. Boxes have area porosities, size and location in $x$, $y$, $z$ directions, while cylinders have a diameter, a position and the length in one direction.

Depending on the grid size, the geometry objects are represented either on-grid or sub-grid numerically, and then the area and volume porosities in the grid cells will be calculated by using Porcalc, which is one of the pre-processors in FLACS. The area porosity is defined as the mean blockage of the control volume surface area, whereas the mean blockage of the inside volume of the control volume is the volume porosity. If the control volume is completely blocked, the porosity value will be 0, and the fully open control volume will result in porosity value of 1.

Porcalc also computes drag factors and turbulence generation for end surface contributions in sub-grid. In the $i$ direction where the object walls pointing positively or negatively, the turbulence generation factor from a sub-grid object will be written as (Arntzen 1998):

$$ T_{i,k} = \gamma_i \frac{a_{ik}}{A_i} $$

(23)

where $A_i$ and $a_{ik}$ are the $i$ direction area of the grid cell and the area of the object inside the grid cell. In order to consider the differences in flows around cylinders and boxes, $\gamma_i$ is determined as 1.0 for boxes and 0.7 of cylinders.

In the control volume, the turbulence generation factors from all sub-grid items are then summed to represent the turbulence factor in a direction for a solo grid cell:
\[ T_{it}^{cell} = \sum_{cv} T_{it} \]  

In a grid cell, the sub-grid objects exist when \( (T_{it} + T_{ic}) > 0 \), and a turbulence sub-grid diameter \( D_{i}^{cell} \) is required, which is calculated as an averaged turbulence generation factor of the sub-grid diameter from all sub-grid objects:

\[ D_{i}^{cell} = \frac{\sum_{cv} D_{i} \max(T_{it}, T_{ic})}{\sum_{cv} \max(T_{it}, T_{ic})} \]  

### 3.2.6 Boundary condition

Prior to run FLACS simulations, the specific boundary conditions for the outer boundaries of the simulations domain must be chosen. Modelling of the flow conditions at the numerical boundaries in FLACS could be a problematic issue, especially if a small boundary domain is chosen, the sound speed at the boundary will be overestimated when the flame passes outside. The increase in the flow of volume over the boundary will lead to wrong overpressure calculation. Therefore, a sufficiently large boundary domain is suggested to avoid the wrong modelling of the flow at the outer boundaries. FLACS provides five boundary conditions to assure the correct calculation for different scenarios (Bjerketvedt et al. 1997).

**EULER**

The EULER formulation is the inviscid flow equations, which are discretised for a boundary element. In the case of outflow, the continuity and momentum equations are used on the outer boundary. The pressure outside the boundary is ambient pressure in simulation. While for sonic outflow and inflow, a NOZZLE boundary condition can be utilized. In unconfined scenarios, the EULER boundary condition may lead to too low overpressures, therefore, the boundary domain must be extended and the PLANE_WAVE condition can be used.

**NOZZLE**

As mentioned, the NOZZLE boundary condition is applied for both sonic outflow and sub-sonic outflow/inflow calculation. For porous objects with small sharp edged grids
or holes, such as grating and louvres, the NOZZLE formulation is the optimal choice. A discharge coefficient is computed from a drag coefficient and the area porosity. Comparing to the EULER boundary condition, the NOZZLE condition may provide rather higher explosion overpressures, however, it is a more robust formulation. Similarly, for unconfined cases, the NOZZLE boundary condition may give too low overpressures.

**PLANE_WAVE**

In order to diminish the reflection of the blast waves at open boundaries which happens when the NOZZLE or EULER formulations are used, the PLANE_WAVE boundary condition is then designed. In the PLANE_WAVE boundary condition, the reflection of outgoing pressure waves are nearly removed by extrapolating the overpressure at the boundary. However, after the expansion of the gas explosion, the overpressures in the PLANE_WAVE condition may by slightly increased and stabilized at the elevated level. Therefore, it is suggested to extend the grid to increase the total volume and to use the PLANE_WAVE formulation, which can avoid the overpressure elevation, especially in low confinement conditions. However, for the scenarios that boundaries are near the vents in the semi-confined condition, the PLANE_WAVE is not a feasible option.

**WIND**

The WIND boundary condition is used for gas dispersion simulations with wind data, such the specification of the wind direction, speed and length scale, etc. In the WIND condition, the velocity of flow is perpendicular to the outer boundary, the turbulence parameters should be given manually and calculation of wind velocity in FLACS is gradually over a given time interval in case there is a strong transient response.

**SYMMETRY**

The SYMMETRY boundary condition is applicable for the scenarios where a symmetry plane can be defined, such as the MERGE geometries. The simulation time and computational domain then can be reduced by applying the SYMMETRY boundary condition. However, for realistic geometries, the overpressures may be over-estimated since the symmetry plane will act as a computational boundary that reflects the
overpressures over the full geometry. In such case, the SYMMETRY is not recommended.

### 3.3 TURBULENCE AND COMBUSTION MODEL

#### 3.3.1 Turbulence model

The strength of a vapour cloud explosion significantly depends on the turbulent burning velocity, which is determined by using the turbulent length scale and intensity in the turbulence field. Therefore, it is critical to use a turbulence model to calculate the turbulent burning velocity; the $k$-$\varepsilon$ turbulence model given by Launder and Spalding (Launder & Spalding 1974b) is used in FLACS (Hjertager 1993; Arntzen 1998).

The equation for turbulent kinetic energy:

$$
\frac{\partial}{\partial t} (\rho k) + \frac{\partial}{\partial x_j} (\rho u_j k) = \frac{\partial}{\partial x_j} \left( \frac{\mu_{\text{eff}}}{\sigma_k} \frac{\partial k}{\partial x_j} \right) + G - \rho \varepsilon; \quad G = \sigma_y \frac{\partial u_j}{\partial x_i}
$$

(26)

The equation for dissipation of turbulent kinetic energy:

$$
\frac{\partial}{\partial t} (\rho \varepsilon) + \frac{\partial}{\partial x_j} (\rho u_j \varepsilon) = \frac{\partial}{\partial x_j} \left( \frac{\mu_{\text{eff}}}{\sigma_\varepsilon} \frac{\partial \varepsilon}{\partial x_j} \right) + C_1 \frac{\varepsilon}{k} G - C_2 \rho \frac{\varepsilon^2}{k}
$$

(27)

where $G$ is the generation rate of turbulence, the relationship between $k$ and $\varepsilon$, and $\mu_{\text{eff}}$ is the effective turbulence viscosity $\mu_{\text{eff}}$ in the Boussinesq eddy viscosity model are:

$$
\mu_{\text{t}} = C_3 \rho \frac{k^2}{\varepsilon}
$$

(28)

$$
\mu_{\text{eff}} = \mu + \mu_{\text{t}}
$$

(29)

where $\mu_{\text{t}}$ and $\mu$ are the turbulent viscosity and laminar. The constants $C_1$, $C_2$, $C_3$, $\sigma_k$ and $\sigma_\varepsilon$ are 1.44, 1.92, 0.09, 1.0 and 1.3, respectively.

#### 3.3.2 Combustion model

The combustion process consists of two models, namely the flame model and burning velocity model.
Flame model

For the flame modelling, the relation between the mass fraction of products and reaction rate from the unburned reactants to fully burned products is:

\[
\frac{\Gamma \rho c}{\Gamma_t} = \nabla \rho \Gamma \nabla c + w
\]  

(30)

where \(w\) is the reaction rate, \(c\) is mass fraction of products, \(\Gamma\) is the diffusion coefficient.

In terms of mass fraction of fuel \(m_{fu}\) in FLACS, the conservation equation in the combustion modelling is:

\[
\frac{\Gamma \rho m_{fu}}{\Gamma_t} = \nabla \rho \Gamma \nabla m_{fu} + m_{fu} w
\]  

(31)

Initially, the H-M model, which calculates the reaction rate for turbulent combustion as seen in equation (18), was used earlier FLACS. The \(\beta\) flame model later replaced it in the combustion modelling, the reaction rate after a modification of the probability density function is then expressed as:

\[
w_{\beta} = W \rho \min\left[\delta\left(c - c_q\right), c, 9 - 9c\right]
\]  

(32)

where \(c_q\) is the minimum mass fraction of products when the reaction rate is larger than 0, \(\delta\) is flame thickness, \(W\) is dimensionless reaction rate.

Corresponding with the burning eigenvalue, the diffusion coefficient and dimensionless reaction rate must satisfy the following relation to calculate the burning velocity \(S\):

\[
\pi c_q = 0.325
\]  

(33)

\[
WT = 1.37S^2
\]  

(34)

Burning velocity model

The burning velocity as the input in flame propagation during the explosion varies from the laminar burning velocity to quasi-laminar burning velocity, and eventually it reaches congested region to become turbulent.
In the beginning of the combustion, the laminar burning velocity is initiated depending on the fuel type, pressure and fuel-air mixture. Below a standard atmospheric pressure, the pressure dependency on the laminar burning velocity is written as (Kuo 1986):

$$S_L = S_{LO} \left( \frac{P}{P_o} \right)^\beta$$

(35)

where $S_{LO}$ is the initial laminar burning velocity, $P_o$ is the initial pressure, $\beta$ is the pressure exponent, which is around 0 for hydrocarbons with burning velocity in the range of 0.5 – 1.0m/s. For stoichiometric methane and propane mixtures, $\beta$ are -0.18 and -0.05, respectively. And for compressed gas used in FLACS, $\beta$ is 0.07 for methane, and 0.44 for propane and ethylene.

Whereas in the quasi-laminar regime, the burning velocity increases with the flame propagation distance from the ignition point to the end of flame radius. The correlation between the laminar and the quasi-laminar burning velocity is described as :

$$S_{QL} = S_L \left( 1 + \chi_a \min \left[ \frac{R}{3} \right]^{1/2} \right)$$

(36)

Where $R$ is the flame radius and $\chi_a$ is a fuel dependent constant, which is between 2 and 8 depending on the fuel-air mixture and the condition of ignition.

Lastly, in the turbulent regime, the turbulent burning velocity correlation in FLACS is expressed as below (Bray 1990):

$$S_T = 0.875 u' K_f^{-0.392}$$

(37)

where $u'$ is the turbulent velocity fluctuation, $K_f$ is the ratio of flow strain rate to flame gradient, which is termed as the Karlovitz stretch factor (Abdel-Gayed et al. 1987).

The Karlovitz stretch factor is then described by using the turbulent Reynolds number $R_n$ and the integral length scale $l_I$ :

$$K_f = 0.157 \left( \frac{u'}{S_L} \right)^2 R_n^{-0.5}$$

(38)
where $v$ is the kinematic viscosity.

With the insertion of the turbulent Reynolds number $R_n$ and the strain rate/ Karlovitz stretch factor $K_f$ into equation (37), the turbulent burning velocity in FLACS then becomes:

$$S_T = 15S_L^{0.784}u^{0.412}t_f^{0.196}$$  \hspace{1cm} (40)

Eventually, the burning velocity in FLACS will be chosen as below:

$$S_u = \max(S_{QL}, S_T)$$  \hspace{1cm} (41)

FLACS solves the equations above such that the overpressures from previous time step, the momentum equation gives a velocity field, which will be corrected along with the updated pressure and density field by implementing a pressure correction algorithm (Patankar 1980).

The factors of the fuel density, the flame radius, the initial laminar flame speed of fuel play important roles in the combustion of an explosion, thereby resulting in the development of the overpressure.

Overall, influence of all parameters on the formation of explosion pressures including the mechanism of turbulent reactive gas dynamics, combustion processes and the geometry of the configurations are taken into account in the methodology of the CFD-based solver – FLACS.

### 3.4 COMPUTATIONAL TECHNIQUES

#### 3.4.1 Conservation equations in finite-domain

For a typical gas explosion, the reactive dynamic calculation is determined by using all the distinct terms in Equation (3). Figure 3.1 illustrates the finite domain where all the conservation equations should be applied.
A discrete number of cells in all directions (N, S, W and E directions as shown in Figure 3.1) unite a grid point (such as the central point P), which constitutes the finite domain. The value of different velocities will be stored midway between two grid points, while other quantities are stored at the grid line intersections.

The conservation equations of convection, transient, diffusion and source terms are shown below, and a summary of all these equations to calculate mass, velocity, kinetic energy of turbulence, dissipation rate and enthalpy, etc., can be found in the work done by Hjertager (Hjertager 1984).

\[
\frac{\partial}{\partial t} (\rho \Phi) \leftrightarrow \text{transient} \tag{42}
\]

\[
\frac{\partial}{\partial x_j} (\rho u_j \Phi) \leftrightarrow \text{convection} \tag{43}
\]
3.4.2 Finite domain calculations

The governing equations in the calculation domain are integrated over a control grid volume, in the $y$ direction, the net convective and diffusive flux will be stored as:

$$ A^\phi_N (\Phi_P - \Phi_N) + A^\phi_S (\Phi_P - \Phi_S) $$

where the notations of $N$, $S$, $P$ are directions and locations, $\Phi$ denotes a general variable $\Phi$ and $\Phi$ are coefficients expresses by:

$$ A^\phi_N = \left( \frac{\Gamma_{\Phi,N}}{\Delta y_N} + 0.5(e + |e|)(-\rho_N v_N) \right) \Delta x \Delta z $$

$$ A^\phi_S = \left( \frac{\Gamma_{\Phi,S}}{\Delta y_S} + 0.5(e + |e|)(\rho_S v_P) \right) \Delta x \Delta z $$

where $v$ is velocity, $0.5(e+|e|)$ is the differencing of upwind for the convection terms and $\Delta$ is the grid cell size.

The time derivative in the numerical approximations is:

$$ t_\delta = \frac{\rho_P \Delta x \Delta y \Delta z}{\Delta t} (\Phi_P - \Phi_P^o) $$

where $\Phi_P^o$ is the general variable value $\Phi$ at previous time level for point $P$.

The source terms after the volume integral then can be expressed as:

$$ S_\phi = S_\phi^o + S_\phi^p \Phi_P $$

where $S_\phi^o$ is the source term - $S_\phi^p$ at previous time level for point $P$.

3.4.3 The continuity and momentum equations in finite domain

Similarly, by integrating the momentum equations over a control volume in all directions, the momentum equations in the finite domain estimation can be expressed as:
\[ A_p^U U_p = \sum_i A_i^U U_i + b^U U_p^o - \Delta z \Delta y (p_p - p_w) + S_w^o \]  
(51)

\[ A_p^V V_p = \sum_i A_i^V V_i + b^V U_p^o - \Delta z \Delta x (p_p - p_s) + S_s^o \]  
(52)

\[ A_p^W U_p = \sum_i A_i^W U_i + b^W U_p^o - \Delta x \Delta y (p_p - p_l) + S_l^o \]  
(53)

where \( b^U, b^V \) and \( b^W \) are expressed as \( b^\Phi \) equal to one part of the time derivative equation (49), \( p \) is pressure.

For the continuity equation, it is summarized around the adjacent points in all directions to point \( P \), overall, the continuity equation in the finite domain calculation is written as:

\[
\left( \frac{\rho_p - \rho_w}{\Delta z} \right) \Delta x \Delta y + \left[ (\rho U)_x - (\rho U)_w \right] \Delta z \Delta y + \left[ (\rho V)_y - (\rho V)_s \right] \Delta z \Delta x - \left[ (\rho W)_z - (\rho U)_y \right] \Delta z \Delta y = 0
\]  
(54)

where \( H \) and \( L \) denotes the directions normal to the \( xy \)-plane (\( E, W, N, S \) directions are in the \( xy \)-plane) in Figure 3.1.

### 3.4.4 Calculation procedure

In order to obtain the density and velocity fields in conformity to continuity, the equations below are utilized:

\[ U_p = U_p^* + \frac{\Delta y \Delta z}{A_p^U} (p_w - p'_p) \]  
(55)

\[ V_p = V_p^* + \frac{\Delta x \Delta z}{A_p^V} (p_s - p'_p) \]  
(56)

\[ W_p = W_p^* + \frac{\Delta x \Delta y}{A_p^W} (p_l - p'_p) \]  
(57)

\[ p_p = p'_p + p'_p \]  
(58)

where \( p'_p \) is the pressure correction.

The source term in the continuity equation is then written as:

---

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\[ S_p = \left[ (\rho U^*)_y - (\rho U^*)_e \right] \Delta z \Delta y + \left[ (\rho V^*)_y - (\rho V^*)_e \right] \Delta z \Delta x + \left[ (\rho W^*)_y - (\rho W^*)_e \right] \Delta y \Delta x - \frac{\left( \rho^* - \rho^o \right)}{\Delta t} \Delta x \Delta y \Delta z \] (59)

A tri-diagonal matrix algorithm (Patankar 1981) is then used to solve the pressure correction and momentum equations, the trip sweep technique consist of the three phases:

\[ A_p^0 \Phi_p^I = A_p^0 \Phi_p^I + A_p^0 \Phi_p^I + F_I \left( \Phi^o, S_p^o \right) \text{ Phase I} \] (60)

\[ A_p^0 \Phi_p^II = A_p^0 \Phi_p^II + A_p^0 \Phi_p^II + F_{II} \left( \Phi^o, \Phi^I, S_p^o \right) \text{ Phase II} \] (61)

\[ A_p^0 \Phi_p^III = A_p^0 \Phi_p^III + A_p^0 \Phi_p^III + F_{III} \left( \Phi^o, \Phi^I, \Phi^II S_p^o \right) \text{ Phase III} \] (62)

By integrating all the conservation, continuity and momentum equations, etc. into the tri-diagonal matrix algorithm, the velocity, turbulence kinetic energy and pressure, etc. in finite domain then can be calculated. However, the stability, convergence and accuracy are not discussed in this thesis, these computational techniques of FLACS CFD simulation are mainly depending on the selections of grid cell size, initial environmental condition and boundary condition etc. The discretisation and convergence of the simulations are achieved by conducting sensitivity analysis, and a case study on the accuracy of FLACS simulation would be conducted in Chapter 6.

### 3.5 SUMMARY

In this chapter, the methodology of the CFD simulation by using FLACS was discussed. In the first place, the general FLACS mathematical models, which are the Reynold’s averaged mass, continuity, momentum, energy balance equations that constitute the differential equations in the fluid flow mechanism of an explosion, had been discussed. Secondly, the stoichiometry condition relating to the balance of reactants and production, the thermodynamic relationships regarding the energy balance in explosion, and ignition process that triggers the flammable cloud into combustion, had been mathematically expressed. Thirdly, the geometry counting and boundary conditions that critically influence the three-dimensional simulation accuracy had been described. Moreover, the fundamental mechanism of gas dispersion and explosion, which is the modelling of turbulence and combustion, also had been thoroughly demonstrated.
Finally yet importantly, the inherent numerical procedure of FLACS had been summarized, the reactive gas dynamic approximations, which use the discrete grid volumes, finite domain equations and tri-diagonal matrix algorithm, etc., had been algebraically expressed.
CHAPTER 4. GAS EXPLOSION OVERPRESSURE
CALCULATION FOR CONGESTED OIL & GAS
FACILITIES

4.1 INTRODUCTION

After the introduction of the inherent algorithm of the CFD simulation by using FLACS, a newly developed correlation, which is based on FLACS modelling and the Guidance for the Application of the Multi-Energy method (GAME) (Eggen 1998), was presented in this chapter for the estimation of boundary overpressures in and around congested regions subjected to vapor gas explosions.

As reviewed in Chapter 2, the empirical methods for the calculation of overpressures arising from accidental inventory releases and subsequent delayed ignition of resulting gas clouds leading to explosions have long been in use, these methods hold significant uncertainty because they do not adequately account for several important parameters, particularly the role that congestion and confinement play in flame acceleration and hence the overpressures arising. In particular, methods such as the multi-energy method (MEM) can be in error by more than an order of magnitude because they do not take into account the geometry detail of most industrial layouts and also rely on estimates of explosion strength and congestion input by the engineer. Where such methods are applied conservatively, the estimated overpressure can be much higher than in a real event, leading to significant financial overspends. Conversely, where these estimates are under-conservative (which is a possibility with these methods even when thought to be applied conservatively), the results can be catastrophic.

Computational fluid dynamics (CFD) is by far the most detailed methodology for quantifying the risk posed by this class of catastrophic events. However, despite significant advances deployed in CFD, it remains computationally and labor intensive. There is, therefore, a need for the development of faster analytical models that can be applied with far less effort yet still capture the dominant mechanisms for gas dispersion and flame propagation and flame acceleration. Here, a new correlation that better accounts for important details of complex geometries had been presented in this chapter,
the new correlation is more accurate than existing analytical methods while offering greater implementation speed compared to existing CFD methods.

4.2 THE GAME CORRELATIONS & CFD CASE STUDIES

4.2.1 GAME correlations

This study was conducted and inspired by the Guidance for the Application of the Multi-Energy method (GAME) (Eggen 1998). GAME was designed to provide additional guidance and to extend its applicability to cases where MEM was designed to address. The phenomenological approach, which is effective for qualitative research projects (Gurwitsch & Garcia-Gomez 2009; Edmund 1989; Alfred 1976), was used to derive the GAME correlation based on the experimental research programs performed during the MERGE and EMERGE projects (Mercx et al. 1995; EMEG 1997; Schumann et al. 1993; Vanwingerden 1988; Vanwingerden 1989; Harris & Wickens 1989) at the Dutch research institute TNO.

As seen in the report (Eggen 1998), satisfactory correlation with limited experiments were obtained by using GAME correlation, and the it is a safe approach in the determination of the overpressure in most situations characterized by artificially homogenous congestion and confinement.

To setup such experimental tests is a very expensive task and there is a significant limit on the quality of possible tests in that it is very difficult to create realistic fields of congestion and confinement at the appropriate scale. Further, the reliability and repeatability of the tests are often very difficult to achieve because some factors such as initial turbulence, the stability of the wind direction and speed as well as the flexibility of some structural components is very difficult to characterize or account for. Hence, it is chosen to compare the results from the new correlation as well as results from the GAME correlation against the highly validated well-established CFD software FLACS. This allows us to examine hundreds of cases including those for realistic geometries at realistic scales which would be impossible to setup without tens of years of significant spend.
As originally derived from experiments, two variants of the GAME correlation were given in the GAME project to determine the vapor cloud explosion overpressure.

For low ignition energy and no confinement in 3-D flame expansion conditions:

\[
\Delta P_o = 0.84 \cdot \left(\frac{VBR \cdot L_f}{D}\right)^{2.75} \cdot S_l^{2.7} \cdot D^{0.7}
\]  

(63)

For low ignition energy and confinement between parallel plates (2-D expansion)

\[
\Delta P_o = 3.3B \cdot \left(\frac{VBR \cdot L_f}{D}\right)^{2.25} \cdot S_l^{2.7} \cdot D^{0.7}
\]  

(64)

where:

\[\Delta P_o\] = the overpressure [barg],

\[VBR\] = the volume blockage ratio, which is defined as the ratio of the total volume of the obstacles inside an obstructed region,

\[L_f\] = the maximum distance of flame propagation obtained by assuming \(L_f\) equal to the radius of a hemisphere with a volume equal to the volume of the configuration [m],

\[D\] = the average obstacle diameter, which give a single average value for the whole obstructed region by assuming a homogeneous distribution of obstacle types and obstacle diameters [m],

\[S_l\] = the laminar flame speed of the flammable gas by assuming a homogenous stoichiometric flammable cloud in all assessment [m/s].

### 4.2.2 Modules tested in CFD simulations

CFD simulations were carried out to validate the results from both the GAME correlation and the newly developed correlation. The overpressures arising from the CFD coded FLACS were extracted for the purpose of comparison with results from both correlations. Referring to the CFD simulation methodology in Chapter 3, the specific explosion modelling in FLACS was conducted as following.

The CFD simulations were performed for three artificial cases with homogenous congestion along with five realistic and inhomogeneous configurations as shown in
Figure 4.1 to Figure 4.2. For the artificial modules 1-3 (Figure 4.1), all module sizes are 80x80x80 (m), and the obstacles in the configurations were arranged orthogonally by filling the pipes of diameter of 0.5m. The five realistic modules were from a Liquefied Natural Gas (LNG) train, all complex structural components were modelled by using a series of boxes and cylinders. In addition, the pre-processor–Porcalc was utilized to calculate the on-grid and sub-grid geometry objects, referring to the geometry counting and porosity calculations in Section 3.2.5. The Volume Blockage Ratio (VBR) in Table 4.1 was read in the cofiles stored in FLACS.

Propane and methane were applied in the explosion simulations, the properties of propane and methane were referred to Table 3.1 and Table 3.2. Equivalent stoichiometric gas clouds were used to perform the explosion modelling; the stoichiometric reaction during the combustion was expressed from equation (8) to (11) in Section 3.2.2, Chapter 3.

After modelling the geometry and assigning gas properties, the EULER formulation (Section 3.2.6), which is composed of the inviscid flow equations, was chosen as the boundary condition. Subsequently, by applying the equation from (18) to (22) in Section 3.2.4, the ignition process was conducted in the fractionation area, the pipe rack area and the combination areas of the pipe racks and the mercury removal and dehydration areas, respectively (Figure 4.2).

During the explosion overpressure calculation in the FLACS simulator – Run-manager, the turbulence model (equation (26) to (29)) and combustion model (equation (30) to (41)) in Section 3.3 were utilized to determine the flame and velocity field, which would result in the development of explosion overpressure by introducing the numerical algorithm (equation (55) to (62)) in Section 3.4.4.

Overall, three artificial modules subjected to propane vapor explosions, five methane vapor explosions of realistic modules and another five propane vapor explosion of the same realistic modules composed the 13 module cases as seen in Table 4.1. And for all the modules, the values of volume blockage ratio, the laminar flame velocity, the characteristic average obstacle diameter and the gas composition, as shown in Table 4.1, were extracted to calculate the overpressure in the following section.
Figure 4.1 Artificial modules 1-3

Realistic Module 4

Realistic Module 5
Realistic Module 6

Realistic Module 7

Realistic Module 8

Table 4.1 Parameters in difference modules

<table>
<thead>
<tr>
<th>Case No.</th>
<th>Gas composition</th>
<th>D(m)</th>
<th>VBR</th>
<th>S$_l$ (m/s)</th>
<th>Gas density (kg/m$^3$)</th>
<th>Cm</th>
</tr>
</thead>
<tbody>
<tr>
<td>1. Module 1</td>
<td>Pure Propane</td>
<td>0.50</td>
<td>0.070</td>
<td>0.46</td>
<td>1.8</td>
<td>1.000</td>
</tr>
<tr>
<td>2. Module 2</td>
<td>Pure Propane</td>
<td>0.50</td>
<td>0.070</td>
<td>0.46</td>
<td>1.8</td>
<td>0.925</td>
</tr>
<tr>
<td>3. Module 3</td>
<td>Pure Propane</td>
<td>0.50</td>
<td>0.070</td>
<td>0.46</td>
<td>1.8</td>
<td>0.888</td>
</tr>
<tr>
<td>4. Module 4</td>
<td>Pure Methane</td>
<td>0.37</td>
<td>0.040</td>
<td>0.4</td>
<td>0.65</td>
<td>0.716</td>
</tr>
<tr>
<td>5. Module 5</td>
<td>Pure Methane</td>
<td>0.45</td>
<td>0.058</td>
<td>0.4</td>
<td>0.65</td>
<td>0.707</td>
</tr>
<tr>
<td>6. Module 6</td>
<td>Pure Propane</td>
<td>0.37</td>
<td>0.040</td>
<td>0.46</td>
<td>1.8</td>
<td>0.716</td>
</tr>
<tr>
<td>7. Module 5</td>
<td>Pure Propane</td>
<td>0.45</td>
<td>0.058</td>
<td>0.46</td>
<td>1.8</td>
<td>0.707</td>
</tr>
<tr>
<td>8. Module 6</td>
<td>Pure Methane</td>
<td>0.12</td>
<td>0.080</td>
<td>0.4</td>
<td>0.65</td>
<td>0.917</td>
</tr>
<tr>
<td>9. Module 7</td>
<td>Pure Methane</td>
<td>0.34</td>
<td>0.103</td>
<td>0.4</td>
<td>0.65</td>
<td>0.980</td>
</tr>
<tr>
<td>10. Module 8</td>
<td>Pure Methane</td>
<td>0.31</td>
<td>0.096</td>
<td>0.4</td>
<td>0.65</td>
<td>0.903</td>
</tr>
<tr>
<td>11. Module 6</td>
<td>Pure Propane</td>
<td>0.12</td>
<td>0.080</td>
<td>0.46</td>
<td>1.8</td>
<td>0.917</td>
</tr>
<tr>
<td>12. Module 7</td>
<td>Pure Propane</td>
<td>0.34</td>
<td>0.103</td>
<td>0.46</td>
<td>1.8</td>
<td>0.980</td>
</tr>
<tr>
<td>13. Module 8</td>
<td>Pure Propane</td>
<td>0.31</td>
<td>0.096</td>
<td>0.46</td>
<td>1.8</td>
<td>0.903</td>
</tr>
</tbody>
</table>

Figure 4.2 Realistic modules 4-8
4.2.3 Validation results of GAME correlation in case studies

By using the CFD configurations listed above, the applicability and accuracy of GAME correlation were investigated in this section. Three models of modules with artificial configurations were initially created with a uniform distribution of cylinders similar to those in the experiments upon which the GAME correlation is defined. Due to the homogeneity of the obstacle arrangement and meshing grid, the CPU time for each calculation of the artificial configuration was relatively short (i.e. within one hour).

The confinement of the modules was controlled with the insertion of parallel plates, and hence equation 2 of the GAME correlations was employed. It was seen in Figure 4.3 that the values of the correlation R-squared factor, (which indicates how well data points fit a line or curve), for the first two homogenous cases are 0.78 and 0.51 respectively when applying the linear least square method. These values showed that the GAME correlation was valid for congestion configurations that were filled with the regular-patterned pipes within a certain range of the confinement. However, as the area of the top plate decreases, negative R-squared values (-0.32) were seen in the homogenous case 3 with partially confined roof, Figure 4.3. The main reason is that the confinement effect was not accounted for in the GAME correlation. By varying the confinement (Figure 4.1) with the other parameters kept constant, the overpressures of those three homogenous scenarios obtained in the GAME correlation remained the same, while the pressures were reduced due to the decrease of confinement in the results of FLACS simulations, which means the overpressures were overestimated by the GAME correlation in the low confinement case.

The results given by the GAME correlation described above demonstrated that the lack of appropriate definition of confinement within the GAME equation resulted in an increasing error when this parameter becomes important. They also showed that GAME can only give satisfactory results when the confinement is within a certain range.
Explosion safety evaluation for congested offshore platforms based on CFD simulations

In addition to these three artificial configurations, another 5 realistic configurations (Figure 4.2) were also investigated, for each of those realistic cases; the CPU time was increased to the range of one hour to three hours each due to the complexity of the geometries and longer calculation time of the flame turbulence development within the irregularly congested regions. As those realistic cases (case 4-13) were included in the comparison, the overall correlation between the GAME results and FLACS data gave a poor value as seen in Figure 4.4, which could be attributed to the geometric inhomogeneity of the realistic geometric configurations in addition to the lack of...
appropriate modeling of confinement. The GAME correlations were derived from MERGE experiments which have a highly regular pattern of obstacles, all of which were idealized as cylinders and homogeneously distributed in the obstructed region.

When the repeatability of obstacles, equal obstacle spacing and the obstacle diameter were carefully chosen the GAME correlation produced results moderately close to those predicted by CFD. However as seen in Figure 4.4, for realistic modules with inhomogeneous congested volumes, the GAME correlation had poor prediction of overpressures and the GAME correlation often over-predicted but sometimes under-predicted the overpressures significantly.

In summary the GAME equations showed a very poor correlation to numerically simulated results when all realistic modules were included, it only gave a moderate \( R^2 \) squared value for the idealized case created with a homogenous distribution of congestion. The lack of consideration of congestion inhomogeneity and the definition criteria of confinement hindered the applicability of GAME correlation in practical problems. Those issues were improved and developed in the following section by introducing a new correlation.

### 4.3 Parametric Studies and Development of a New Correlation

Confinement was introduced and defined in a confinement specific correlation (CSC); other critical parameters were chosen to model other factors as was done for the GAME correlation describing the physical phenomenon of gas explosion. The derivation of the CSC was based on the linear least square method with a subset of the simulations. In order to appropriately isolate the important set of parameters, all the CFD cases in this subset (approximately 400 cases) were simulated as homogeneous models; the distributions of pipes were arranged in regular patterns by hand.

#### 4.3.1 Conceptual definition of confinement and congestion

Both confinement and congestion, which are parameters that affect turbulence induced flame acceleration, can significant affect the development of overpressures (Harrison & Eyre 1987; Bradley et al. 2008; Moen et al. 1980; VandenBerg & Mos 2005).
For the GAME correlation, the congestion was defined using the volume blockage ratio (VBR) divided by the average pipe diameter. This is a very useful parameter; however the manner in which it is applied does not take into account the fact that changing the congestion inherently changes the confinement, which was demonstrated in this section below. In addition, the manner in which the congestion parameter was applied for the GAME correlation appears to weight VBR and the characteristic pipe diameter equally. Here, the isolation of a unique confinement parameter and different weighting of VBR in relation to the characteristic pipe diameter as part of the overall congestion parameter were investigated.

The conceptual confinement ratio was defined as the total blocked edge area of a space divided by the total volume of the space, i.e. $A_{Blocked}/A_{Total}$. Then for a cubic volume (of dimension $1m \times 1m \times 1m$) with six open sides, the conceptual confinement ratio $A_{Blocked}/A_{Total} = 0/6$ (m$^2$/m$^3$), while the fully confined cube has the conceptual confinement ratio $A_{Blocked}/A_{Total} = 6/6 = 1$ which means the more the surface area being blocked the greater the confinement of the cube. It follows that for a partially confined volume with 2 sides fully blocked; the ratio is $1/3$.

![Figure 4.5 Conceptual definition of confinement and congestion](image_url)

For the same cube (dimension $1m \times 1m \times 1m$) with six open sides and with conceptual confinement ratio $A_{Blocked}/A_{Total} = 0$, by placing a pipe with dimension of $1m$ length and $0.4m$ diameter in the centre of the cube, as seen in Figure 4.5, the congestion volume in the cube became $0.126$ m$^3$ (the volume of the pipe) whereas it was $0$ m$^3$ in the empty space. Commensurately, the volume blockage ratio ($VBR = V_{blockage}/V_{total}$) increased from $0/1$ (m$^3$/m$^3$) to $0.126/1$ (m$^3$/m$^3$), meanwhile the conceptual confinement ratio of the cube increases from $0/6$ (m$^2$/m$^2$) to $0.25/6$ (m$^2$/m$^2$), $0.25$ m$^2$ is the total area of the
top and bottom cross section of the pipe which reach the surfaces of the cube on two size. It is clear that a change in congestion influences the confinement of the configuration simultaneously, the confinement and congestions should be considered together as two interactional factors to determine the explosion pressure.

4.3.2 Definition of Parameters

Six parameters were taken into account in determining the overpressure in a vapor cloud explosion event. They include the confinement ratio, the volume blockage ratio, the characteristic obstacle diameter, the flame propagation path, the laminar flame speed and the gas density. In order to investigate their relative importance in the determination of the overpressures from explosions, parametric studies were conducted and compared to the output from CFD simulations using the software FLACS.

4.3.2.1 Confinement effect

In accordance with the 2D expansion of the GAME correlation, simulations were conducted using a geometric configuration that had parallel plates, the confinement was then defined as

\[ C_m = \frac{A_B}{A_T} \]  

(65)

The blocked area \( A_B \) is the sum of obstructed areas on the top and bottom of the domain simulated; \( A_T \) is the total area of the top and bottom surfaces. As seen in Figure 4.1, the confinement parameter was regulated by reducing the blocked surface on the top of the geometries modeled using FLACS, while the averaged diameter and volume blockage ratio as well as other parameters were fixed at certain values. 24 CFD simulations with 6 different confinement levels were performed to investigate the effect of the confinement parameter on overpressure. It was seen that the pressure varied with confinement according to:

\[ P_o \sim \exp(8.5 \cdot C_m) \]  

(66)

Where \( P_o \) is the overpressure calculated at different monitor points in along the explosion flame path.
4.3.2.2. Effects of VBR and the average obstacle diameter D

The GAME correlation used the equally weighted volume blockage ratio (VBR) and the characteristic obstacle diameter (D) as the basic predictors of congestion. However, in order to address the issue of irregular congestion, the VBR was differently weighted in the correlation developed here, and the averaged obstacle diameter was investigated separately.

The volume blockage ratio here was defined as the ratio of obstruction volume within the domain from the ignition point to the target point to the total configuration volume, so for each specific target of interest, there was a unique VBR to calculate the overpressure.

Applying the results from 8 cases with different VBR resulting in a total of 32 CFD cases, the parameter of VBR was varied while other parameters kept constant (e.g. constant $C_m=1$) to determine the effect of VBR in correlation with overpressure. Similarly, 25 CFD simulations with 5 different averaged obstacle diameters (D) were conducted to investigate the relationship between overpressure and averaged obstacle diameter while the VBR and the other parameters are fixed. The similar slopes were
Explosion safety evaluation for congested offshore platforms based on CFD simulations

The correlation among overpressure, $VBR$ and $D$ are:

$$P_o \sim 1.6 \ln(VBR) + 6$$  \hspace{1cm} (67)$$

$$P_o \sim \left(\frac{D}{H}\right)^{-1.5}$$  \hspace{1cm} (68)$$

where $H$ is the height of the configuration.

![Figure 4.7 Simulation results and trendlines for the CFD cases](image)

4.3.2.3. **Maximum distance of flame propagation**

The maximum distance of flame propagation ($L_f$) in the CSC was defined as the direct distance from the ignition location to the target point of overpressure in contradistinction to the assumption in the GAME project that $L_f$ is equal to the radius of a hemisphere with a volume equal to the volume of the configuration, which makes the CSC easier and more convenient to use. About 300 CFD simulation cases were included in the investigation. The trendlines for all cases showed similar slopes in Figure 4.8 with the power of 2.2 of the maximum distance of flame propagation, namely:

$$P_o \sim \left(\frac{L_f}{H}\right)^{2.2}$$  \hspace{1cm} (69)$$
4.3.2.4. Mass density and laminar flame speed of gas

For these two parameters, 13 explosion scenarios consist of approximately 1100 simulation cases were conducted by using two different gases which are methane and propane with two different mass densities and two different laminar flame speeds. The approach taken here was to use the phenomenological method to analyze all the available data. The last correlation for mass density and laminar flame speed with power of 0.5 and 2 was subsequently found by minimizing the total variance of the complete correlation against CFD results.

4.3.3 Newly proposed correlation

With the parameters of confinement, volume blockage ratio, the average obstacle, laminar flame velocity and gas density derived in the manner described above, the new dimensionless correlation (CSC) was given by:

$$\frac{\Delta P_o}{P_{air}} = 0.037 \cdot e^{8.5 c_m} \cdot [1.6 \ln(VBR_t) + 6] \cdot \left(\frac{L}{H}\right)^{2.2} \cdot \left(\frac{V}{H}\right)^{-1.5} \cdot \left(\frac{p_{gas}}{p_{air}}\right)^{0.5} \cdot \left(\frac{S}{S_0}\right)^{0.2}$$  \hspace{1cm} (70)

where:

$\Delta P_o$ = the escalation overpressure [barg],
\[ P_{\text{air}} = 1 \text{ standard atmospheric pressure } 101.325\text{kPa [1 barg]}, \]

\[ D = \text{the average obstacle diameter [m]}, \]

\[ L_{t} = \text{the direct distance from the ignition location to the target point[m]}, \]

\[ S_{l} = \text{the laminar flame speed of the flammable gas [m/s]}, \]

\[ S_{s} = \text{the speed of sound [m/s]}, \]

\[ C_{m} = \text{the confinement ratio}, \]

\[ VBR_{t} = \text{the volume blockage ratio of configuration region from the ignition point to the target}, \]

\[ \rho_{\text{gas}} = \text{mass density of gas (kg/m}^{3}) \text{ (the gas density is assumed ideally under one standard atmosphere pressure at normal temperature 26 degrees in this study)}, \]

\[ \rho_{\text{air}} = \text{mass density of air (kg/m}^{3}), \]

\[ H = \text{the height of the configuration (m)}. \]

### 4.3.4 Validation of the new correlation

In the validation of CSC, about 1100 realistic and idealized vapor cloud explosion simulations from the modules in Figure 4.1 to Figure 4.2 were conducted to compare the results of CSC with FLACS data. The obstacle configurations were filled with equivalent stoichiometric flammable gas cloud in the simulations; methane and propane were used as fuels in this study. The parameters are shown in Table 4.1.
Figure 4.9 Overall R-squared values of CSC vs. FLACS results for 13 simulation cases subject to two types of gas vapor explosions
As seen in Figure 4.9, the comparison of the overall results between the pressures yielded by the CSC and the simulation results from FLACS were remarkable; the R-squared value yielded by this comparison is 0.8395. For individual cases as shown in Figure 4.10, the correlation factor R-squared is within the moderate range of 0.44 to 0.90, which means the CSC applies to all realistic and idealized modules very well. In terms of the homogeneous case 1-3 with different confinement, the factors of R-squared in CSC are 0.886, 0.608, 0.464 respectively, which are closer to the CFD results than for the GAME correlation. Overall, the CSC equation corresponding to the confinement criteria can be effectively utilized to the practical problems with different congestion and confinement conditions.

4.3.5 Discussion

Although the GAME correlation includes the volume blockage ratio and the characteristic pipe diameter together in an attempt to account for congestion, the confinement of the combusting gas was not thoroughly examined in the experimental programs. It was shown in section 4.3 that a change in congestion necessarily affected
the confinement, and the change can be significant. Confinement plays a major role in the evolution of combustion and the overpressure magnitudes generated. In real explosion situations, the two parameters of confinement and congestion should be both explicitly accounted for in the calculation of explosion overpressure.

Secondly, the acceptable results can only be obtained from the GAME correlation when it was used in the situations where obstacles were homogeneously distributed; indeed the hydraulic average obstacle diameter was taken into account to investigate the congestion effect on the overpressure calculation. However, in the practical scenarios where there was inhomogeneous distribution of obstructions, the explosion overpressure would vary significantly and this would result in overpressure being overestimated as a result of using GAME correlation based on the averaged parameters of obstacle diameter.

Another critical parameter to determine the vapor explosion pressure is the flame path length. In the GAME correlation, the flame path length had been assumed to be equal to the radius of a hemisphere with a volume equal to the volume of the obstructed region (Mercx et al. 1998). Hence, for cases that ignition locations at the edge/corner of the configurations and the aspect ratio of configurations larger than 1, it creates the question that how to convert or determine the flame path length appropriately. This uncertainty resulted in another uncertainty in the overpressure through the use of the GAME correlation, which also made the GAME correlation difficult to be applied in realistic scenarios.

Finally, and interestingly, one notes that the GAME correlation is dimensionally unbalanced, i.e. the dimensions of the right hand side of the equation do not match those for the left hand side. This indicated that there were either too few or too many parameters. In addition, there might be cases where this equation is not appropriately applicable.

On the other hand, the applicability of the newly developed correlation was greatly improved, and the issues about confinement and congestion were addressed.

The greatest difference between the two correlations is that the confinement $C_m$ which was not accounted for separately in the GAME correlation was introduced in CSC. In
order to invoke two different GAME equations to calculate the overpressure, the gas explosions were classified into three categories, namely 1D, 2D and 3D expansions depends on the degree of confinement. However, the way in which the GAME distinguishes between the 2D and 3D flame expansion lacks detail; for instance, configurations with explosion charge confined by parallel planes were deemed to be 2D expansion to enable Eq. (2) applying to all such cases, which resulted in very large errors for those cases with partially confined top. Therefore, a criterion was established in the CSC, the confinement was well defined by considering the ratio of blocked area to total surface area of the configuration, the improved correlation of the CSC equation’s results and the FLACS data were seen in Figure 4.10.

Secondly, in the CSC, the volume blockage ratio and the average diameter were investigated separately with unequal weightings to quantify the congestion. Unlike the parameters $D$ and $VBR$ in GAME correlation, the volume blockage ratio of configuration was defined as it is the calculation volume from the ignition point to the target, the overpressure calculation in the configuration with inhomogeneous congestion therefore can be accurate since for each specific target of interest, it has individual $VBR$ to determine the overpressure.

Moreover, the CSC is relatively easy to use by redefining the maximum distance of flame propagation ($L_f$) as the direct distance from the ignition location to the target point of overpressure, whereas in the GAME project that $L_f$ was assumed to be equal to the radius of a hemisphere with a volume equal to the volume of the configuration, where for edge/corner ignition cases and the configurations with aspect ratio of larger than 1, the GAME correlation may not be applicable. And thanks to the balanced inputs’ dimension to represent different fuels, the gas mass density was introduce as well as the laminar flame speed of gas ($S_l$) to improve the accuracy of the new correlation.

However, all the simulations conducted so far had used methane and propane as fuels for explosions. Further tests using different flammable gases and mixed gases were required to test and validate the CSC model for those cases.
It is noteworthy that for CFD, the advent of use of mixed gases and multiple species is a recent development and only 10 years ago, the use of pure propane or pure methane is the industry standard.

The effect of Carbon Dioxide in these reactions is predominantly as a thermal sink which can slow down the combustion rate and thereby reduce overpressures. However a notable effect requires large amounts of the gas to be present. The same is true for humidity (i.e. water in vapor form).

Hence the impact of air humidity was not thoroughly considered in the simulations. The cases modeled here, and the current common practice with CFD in industry had been to date, to ignore humidity in the overwhelming majority of cases as its effect on overpressure was considered to be relatively small. The exception is for mitigating measures involving deluge, or events involving rain where the evaporation of the water can have a significant effect. The inclusion of this effect would make an interesting expansion to the current work. But this requires detailed studies dedicated to this effect.

Similarly the effect of Carbon Dioxide mixed in with the reactants had not been considered here. CO₂ is also often ignored in CFD modeling of explosions unless it exists in significant quantities mixed with the reactants which is possible but not frequent.

### 4.4 SUMMARY

In this chapter, a new correlation to quantify the overpressure was developed based on the linear least square method by using 400 CFD simulations of homogeneous geometries. The method is applicable only to propane and methane and represents a first step in developing robust rapid correlations.

The newly proposed correlation termed CSC had satisfactory results when it was applied to all scenarios consisting of realistic and idealized homogenous modules in two different explosion blast sources.

CSC consists of a relation between parameters describing the obstructed region (the average obstacle diameter, volume blockage ratio and confinement) and describing the fuel properties. Data from numerical simulations using the CFD software FLACS
served as a reference for comparison with the results of the CSC. The numerical results for approximately an additional 700 simulations on more realistic geometries were compared with both GAME and CSC.

The difference between the two approaches relates to one significant parameter, confinement. Confinement was introduced in CSC to set up a reference in different confined scenarios. The concept to quantify congestion in an obstructed configuration as well as the volume blockage ratio was redefined. Additionally, the gas mass density was taken into account in the calculation and the CSC was derived as dimensionless.

Because the new correlation had been tested against over 1100 simulation monitor points carried out using CFD, it appears to have less restriction on its applicability than does the GAME correlation. In any case, this correlation as well as GAME has applicability as a benchmarking tool only. However based on the discussion above also it is not recommended using the GAME correlation outside the confines of the experiments from which it was derived.

Indeed it would be advantageous to test and develop this newly proposed correlation by comparison to further experiments.
CHAPTER 5. OVERPRESSURE EVALUATION FOR IRREGULARLY STRUCTURED MODULES

5.1 INTRODUCTION

In this chapter, instead of using the homogenously arranged modules in previous chapter to evaluate the large-scale oil and gas structures subjected to gas explosion, the irregularly structured modules with deliberately varied geometrical parameters were investigated by using the newly developed correlation –Confinement Specific Correlation (CSC), which was derived based on CFD simulation in Chapter 4.

The main purpose of modelling these irregularly structured modules is to mimic the large-scale realistic oil and gas structures and conduct the explosion simulation on such complex geometries. These realistic onshore/offshore facilities typically display a high degree of inhomogeneity in confinement and congestion. Using experiments to evaluate risk for each industrial facility is impractically expensive due to the numerous variations of geometry detail, size and inventory composition and size in industrial explosion scenarios. Cost constraints mean that experiments performed so far have been scaled down in size and simplifications were applied. The scaling factor may result in inherent uncertainties for experimental results and it is sometimes difficult to even quantify the impact of the simplifications used in these experiments.

Consequently, at the present time, many of the vapour cloud explosion analyses are increasingly being carried out using the Computational Fluid Dynamics (CFD) tools. Because it agrees with experiments to a greater degree than analytical studies, the CFD approach is considered a robust numerical tool based on finite volume solutions and the ‘physical’ models of combustion process to predict gas explosion overpressure for large-scale geometries. In particular, some CFD solvers can capture the flame acceleration and venting of the overpressure build-up for gas clouds in irregularly patterned obstacles which have significant effects on overpressures.

However CFD is time consuming and expensive and in addition requires a degree of expertise in its application for meaningful results and there is still significant need for rapid approximate methods for benchmarking such events that can be later targeted, if
necessary with detailed CFD analysis. In this Chapter, the detailed CFD methodology was employed as a benchmark, and the previously suggested rapid solution – CSC was utilized to predict overpressures for cases with variation of a few fundamental parameters including confinement and congestion driven flame propagation.

A range of practical modules (400 scenarios) with irregularly arranged obstacles were assessed and compared with the results from the correlation of the Guidance for the Application of the Multi-Energy method (GAME) as well. It was found that the overpressure predictions obtained using the correlation still better agrees with the CFD modelling results compared with the GAME correlation suggesting. To show the importance of increased accuracy in these cases, a structural damage level evaluation process was used to place the damage levels for 4 monitor points on a $p-i$ curve and the results showed that often these damage levels are near critical, demonstrating the need for improved accuracy.

### 5.2 GEOMETRY MODEL

The cases examined in this chapter were analysed using CSC and modelled using FLACS. These were cases of large-scale geometries at scales encountered in industrial scenarios in process safety. Examples are artificial and realistic models in Figure 5.1 with sizes of 90x45x15(m) and 80x50x50(m), respectively. The artificial geometries in this study were modelled with mixed obstacle arrangement patterns, obstacle diameters and confinement ratios and one realistic module truncated from a LNG (Liquefied Natural Gas) train (Figure 5.1 (b)) was also investigated.
Both propane and methane VCEs were modelled in this chapter. The ambient temperature and pressure were set as 26°C and 101 kPa, respectively. Eulerian boundary conditions of the domain were used and the BC pressure was set to be equal to the ambient pressure.

Walls and decks were assumed to be unyielding during the entire explosion, i.e. rigid walls remained in place even for the largest explosion loads. FLACS was based on several sub-grid models that require careful observation of some best practice guidelines.
These include the use of cubical grid cells in the combustion region were applied in order to diminish the deviations of flame propagation and pressures; the aspect ratio of the grid was controlled to within 20% and grid cells smaller than 5cm were avoided to ensure the accurate results.

For purpose of extracting the pressures, monitor points were defined at specific locations in the simulation domain where variables including volume blockage ratio (VBR), the distance of flame propagation, the characteristic average obstacle diameter were to be monitored. For instance, as shown in Figure 5.1 (a), the gas cloud was ignited at the edge centre of the configuration; the monitor points were then placed along the direction of flame propagation to obtain the pressures at the increasing of the flame propagation distance. And for each simulation in this chapter, more than 30 monitor points were assigned according to the grid arrangement.

5.3 EVALUATION OF THE IRREGULAR-STRUCTURED CONFIGURATIONS SUBJECTED TO GAS EXPLOSION

The newly developed correlation - CSC in previous work (Li et al. 2014a) was used to independently predict the overpressures for similar cases with irregular arrangement of obstacles. The parameters of confinement, volume blockage ratio, the average obstacle, laminar flame velocity and gas density were discussed in last Chapter.

5.3.1 Definition of regularity and irregularity of Confinement and Congestion

This subsection described two types of geometries - regular and irregular arrangements of congestion and confinement.

In terms of the congestion, the artificial module in Figure 5.1 (a) featured uniform obstacle diameter and a regular pattern of obstacles. By contrast, the module 1 and module 4 in Figure 5.3 were modelled with irregularities. And more importantly, unlike the previous study (Li et al. 2014a) where the simulations were modules extracted from an existing LNG (Liquefied Natural Gas) train; they were composed of realistic layouts of structural components with random irregularities. The geometry displayed in Figure 5.3 of this chapter were artificial modules with controllable irregularities, for example, from module 1 to 4, they were intentionally organized with increasing obstacle
diameters, equidistant separation distances and mixed intersecting obstacle arrangements, etc. Additionally, those artificially arranged irregular modules in this chapter were large-scale modules whilst those artificial ones in the previous study (Li et al. 2014a) were in small-scale.

Using the definition of confinement in the previously proposed paper (Li et al. 2014a), all simulations were conducted under the configurations with the parallel plates in semi-3D overpressure expansion; the confinement ratio was characterized as the ratio of the blocked area on the top and bottom plates over the total area of the top and bottom surfaces. Therefore, a configuration covered with two solid top and bottom plates, such as the module in Figure 5.2 (a), was considered to be fully confined in the z-direction; and the one without top plate was defined as open in the +z-direction, as seen Figure 5.2 (c). In this study, the partial confinement between the open air and the full confinement was used to test the correlations under conditions of irregular confinement.

(a) Fully confined module  (b) partially confined module  (c) Open in +z-direction

Figure 5.2 Artificial modules with varying confinement
5.3.2 Application of the CSC to the irregular-arranged modules

By using the CSC, overpressures were estimated for configurations with congestion of an irregular arrangement subjected to vapour cloud explosions and the results were described in this section. As seen in Figure 5.3, four modules with inhomogeneous obstacles plus one realistic module were modelled here to simulate 400 new explosions for this study. Four of the modules were of highly confined configurations. In the explosion models, a stoichiometric flammable gas cloud was used to fill the obstacle configurations; methane and propane are both used as fuels in this study. The parameters were shown in Table 5.1.
**Module 5 – Realistic Module**

**Figure 5.3** Modules with irregularities 1-5

Table 5.1 Parameters in modules 1-5 with irregularities.

<table>
<thead>
<tr>
<th>Case No.</th>
<th>Gas composition</th>
<th>D (m)</th>
<th>VBR *</th>
<th>S_1 (m/s)</th>
<th>Gas density (kg/m³)</th>
<th>C_m</th>
</tr>
</thead>
<tbody>
<tr>
<td>1. Module 1</td>
<td>Pure Methane</td>
<td>0.37</td>
<td>0.11</td>
<td>0.40</td>
<td>0.65</td>
<td>1.00</td>
</tr>
<tr>
<td>2. Module 1</td>
<td>Pure Propane</td>
<td>0.37</td>
<td>0.11</td>
<td>0.46</td>
<td>1.80</td>
<td>1.00</td>
</tr>
<tr>
<td>3. Module 2</td>
<td>Pure Methane</td>
<td>0.31</td>
<td>0.14</td>
<td>0.40</td>
<td>0.65</td>
<td>0.96</td>
</tr>
<tr>
<td>4. Module 2</td>
<td>Pure Propane</td>
<td>0.31</td>
<td>0.14</td>
<td>0.46</td>
<td>1.80</td>
<td>0.96</td>
</tr>
<tr>
<td>5. Module 3</td>
<td>Pure Methane</td>
<td>0.33</td>
<td>0.13</td>
<td>0.40</td>
<td>0.65</td>
<td>0.90</td>
</tr>
<tr>
<td>6. Module 3</td>
<td>Pure Propane</td>
<td>0.33</td>
<td>0.13</td>
<td>0.46</td>
<td>1.80</td>
<td>0.90</td>
</tr>
<tr>
<td>7. Module 4</td>
<td>Pure Methane</td>
<td>0.21</td>
<td>0.04</td>
<td>0.40</td>
<td>0.65</td>
<td>0.90</td>
</tr>
<tr>
<td>8. Module 4</td>
<td>Pure Propane</td>
<td>0.21</td>
<td>0.04</td>
<td>0.46</td>
<td>1.80</td>
<td>0.90</td>
</tr>
<tr>
<td>9. Module 5</td>
<td>Pure Methane</td>
<td>0.59</td>
<td>0.12</td>
<td>0.40</td>
<td>0.65</td>
<td>0.76</td>
</tr>
<tr>
<td>10. Module 5</td>
<td>Pure Propane</td>
<td>0.59</td>
<td>0.12</td>
<td>0.46</td>
<td>1.80</td>
<td>0.76</td>
</tr>
</tbody>
</table>

* VBR here is the volume blockage ratio of the entire obstructed region for Module 1 to 5.
Figure 5.4 showed the correlation pressure predictions on the x-axis against the pressures calculated with FLACS on the y-axis. The R-squared ($R^2$) value was extracted for each of these cases. As seen in Figure 5.4, the R-squared value for each simulation model is between 0.66 and 0.90, which shows the CSC correlation applies to practical geometries of greatly varying confinement ratios as well as irregular pattern of VBR and varying obstacle diameters in the configurations. The results from the CSC correlation were also compared to results from the Guidance for the Application of the Multi-Energy method (GAME) correlations (Eggen 1998).

The GAME correlations (Equation (63) and (64)) as mentioned in last chapter were used to determine the gas explosion overpressure for the modules in Figure 5.3 with confinement between parallel plates. The GAME correlations were seen to be generally, but not always conservative in the determination of the overpressure for cases with artificially homogenous congestion. When applied to geometries (Figure 5.3) with irregularities of confinement and congestion, the overall comparison results, seen in Figure 5.5, gave a poor agreement with the FLACS results, specifically, the data obtained by means of the GAME correlations tended to overestimate the overpressure significantly whereas the CSC correlation result agreed well with FLACS simulations, Figure 5.5.

The GAME correlations were derived from MERGE experiments (Mercx et al. 1995; EMEG 1997; Schumann et al. 1993; Vanwingerden 1988; Vanwingerden 1989) which possesses the idealized obstacles with average diameter and homogeneously distributed
in the configuration, the volume blockage ratio and confinement ratio are regularly patterned. In this study, the performance of the GAME correlation for cases where the irregularities of the obstacles as well as high degrees of confinement are characteristics of geometry was examined. This had not been adequately tested using GAME correlation up till this point. The CSC correlation was derived based on the CFD coded software – FLACS, the parameters regarding the geometrical detail and the turbulent reactive gas dynamics mechanism were accounted for, hence this approach better modelled the inhomogeneous configurations where the turbulence generation/degeneration and the burning velocity acceleration/ deceleration are key factors in the variation of the congestion and confinement.

![Figure 5.5 The comparison of the new correlation and the GAME overpressure data vs. FLACS results for the irregular-patterned configurations subject to methane and propane vapour explosions](image)

5.3.3 Rapid prediction of structural damage

The CSC correlation had undergone validation (Li et al. 2014a) with very good agreement with pressures predicted using CFD modelling. In this study a rapid structural damage level prediction process was also added; two different simulation configurations with 8 well-located monitor points were numerically modelled using
using FLACS (GexCon 2011) as the case studies shown below, the pressure vs. time history data was obtained for the specific structure members at those monitor points.

As seen in Figure 5.7, the overpressure figures were observed from the fully congested configuration Figure 5.6 (a) and the configuration with a sufficient separation distance Figure 5.6 (b). For both configurations, the explosion occurs from the centre of the left module as illustrated in Figure 5.1 (a), the flame propagated through the fuel away from the ignition point till the fuel exhausted, the monitor points 1 to 4 were placed in the centre along the flame propagation direction from left to right. It was noted in Figure 5.7 (a) that the magnitude of the maximum overpressure increased from 125 kPa to 230
kPa as the flame path from the ignition through congestion increased, the maximum overpressure was seen at monitor point 4.

The phenomenon observed above was attributed to flame acceleration which was described in (Eggen 1998; Li et al. 2014a; Bjerketvedt et al. 1997), the geometry of the gas explosion scenario and flame propagation distance both contributed the development of the flame acceleration and overpressure. In a gas explosion scenario, turbulence is generated when the flame interacts with the obstacles, which results in the flame acceleration and the generation of more turbulence as the flame propagates further in the congested area: a self-feeding mechanism increasing flame speed and thereby increasing the overpressure. This is in contrast to an explosion pressure field from a scenario using explosives where the maximum blast load is seen at the minimum stand-off distance decreasing with distance from ignition point.

However, if a flame propagated in a premixed air-fuel cloud in an uncongested open space, as seen in Figure 5.7 (b), the phenomenon of flame acceleration did not continue in the open uncongested space. The separation space in Figure 5.6 (b) reduced the congestion and intensity of turbulence which resulted in the decrease of the overpressure. An explosion generated with explosives is not affected by a separation space in the same manner and hence the determination of TNT explosion overpressure is only a function of stand-off distance in the space.

For gas explosions, the pressure time history is typically a triangular shaped wave with an extremely short time period, (Figure 5.7). For each monitor point, the impulse vs. time data was obtained by means of integration of the pressure time history and this was seen in Figure 5.8. The maximum impulse was observed after the peak of the overpressure and the steady state of the impulse was seen after the pressure attenuates to 0kPa.
By applying the data above to the structural members, the calculation of the final states of damage, which is of major concern can be assessed. Specifically, a structural member
in an offshore module subjected to gas explosion is simplified as a Single Degree Of Freedom (SDOF) equivalent structural model to assess its structural response behaviour. The maximum deflection rather than the detailed deflection-time history of the structure determines the failure criterion of the structure.

In order to evaluate the structural damage level, a pressure-impulse ($p-i$) diagram of the equivalent SDOF structural model (Smith & Hetherington 1994; Mays & Smith 1995) was developed as shown in Figure 5.9. Once the critical deflection (maximum allowable deflection) $y_c$ of the structure was specified, a curve was obtained, as the dashed line shown in Figure 5.9, which indicated various combinations of the non-dimensional initial peak overpressure $p$ and the impulse $i$ of the external load that would cause the same deflection of the structure. The non-dimensional pressure and impulse were defined as $p = P_o A / (k y_c / 2)$ and $i = I_o / y_c \sqrt{km_{se}}$.

The impulsive asymptote of the curve is $i = 1.0$ and the quasi-static asymptote is $p = 1.0$. $P_o$ is the initial peak pressure of the blast load and $I_o$ is the impulse of the blast load as shown in Figure 5.8. $A$ is the cross-sectional area of the SDOF structural, $m_{se}$ is the equivalent mass of the equivalent SDOF structure and $k$ is its stiffness. In this study, taking the gas explosion scenarios at the four monitor points in the congested configuration as examples, the steel material was used to simulate the offshore
structural members which were modelled as simply supported beams, the cross-sectional area, the equivalent mass and the stiffness were set as 1m\(^2\), 1kg and 3\times10^6 N/m.

Therefore, the \( p-i \) combinations of the gas explosion blast load were determined; the four points indicated in Figure 5.9 represented the blast load results obtained in Figure 5.8. For the four monitor points, any data below the dashed curve (overpressure and impulse at point 1 and point 2) would not result in any damage of the structure while those above the curve (overpressure and impulse at point 3 and point 4) would induce failure of the structure.

### 5.4 SUMMARY

This chapter examined 400 scenarios in irregularly structured modules similar to the MERGE experiments on which the GAME correlation was based, with one important distinction: The confinement and congestion were deliberately varied such that some of the geometries possessed inhomogeneity of both parameters. Little experimental data exists for such irregularly structured configurations and hence the cases were modelled here with the commercial CFD software FLACS. A realistic model, which typically display a high degree of inhomogeneity in confinement and congestion, was also examined and modelled by using the commercial code.

The overpressure predictions using FLACS at various target locations were compared with the results from the newly derived correlation - CSC and the GAME correlation. It was found that the CSC correlation better agreed with the overpressure predictions obtained using CFD when compared with the GAME correlation. The results further demonstrated that the correlation by the CSC is suitable for the modelling of realistic geometries.

The numerically calculated pressure and impulse vs. time results were related to damage level by simplifying the offshore structural component as an SDOF equivalent model, the structural damage level was determined within the \( p-i \) diagram. The results showed that the cases examined are ones that require an increased level of accuracy as they are very close to cases that may cause permanent damage to structural members.

Having developed the new VCE approximation correlation – CSC in Chapter 4 and investigated the irregularly structured and realistic oil and gas modules in Chapter 5, the
corresponding up-to-date design of the gas explosion mitigating measures would be
discussed in the following chapters. Specifically, in Chapter 6, a Data-dump technique
to improve the overpressure calculation accuracy in the safety gap modelling was
proposed, and safety gap’s gas explosion mitigating effect on a cylindrical FLNG
platform was investigated in Chapter 7. Moreover, the blast wall design on the
cylindrical FLNG platform was also conducted along with a probabilistic study in
Chapter 8.
CHAPTER 6. DATA-DUMP TECHNIQUE IN CFD SIMULATION FOR MULTI-MODULES

6.1 INTRODUCTION

Following the discussion of the VCE overpressure calculations in previous chapters, the explosion mitigating measures to protect offshore structures were investigated in this and next two chapters. Amongst these reviewed overpressure mitigating approaches in Chapter 2, safety gap is one of the most efficient and state-of-the-art measures. Here, a Data-dump technique to assure the CFD accuracy of safety gap modelling in FLACS was developed prior to the application of safety gap in the realistic offshore structure in Chapter 7.

In this chapter, a comparison of simulations and published data from experiments carried out by TNO Prins Maurits Laboratory on geometric configurations that involved safety gaps of various separation distances was performed. The safety gaps were placed between the donor and acceptor in a series of multi-modules.

In the comparison, the majority of the CFD simulation results satisfactorily agreed with those obtained by TNO Prins Maurits experiments. However, when the size of safety gap reaches one or two times of the multi-modules dimension, a great difference between the numerical and laboratory data in the explosion overpressures in the acceptor module was seen.

The main purpose of this chapter is to propose a Data-dump technique to reset the turbulence length scale for these cases with different separation distances before the evaluation of the safety gap in FLACS. Five sets of multi-modules containing obstructed regions with different separation spaces as described in the TNO Prins Maurits experimental program were numerically modelled. The overall results indicated that the software with the Data-dump technique is still an extremely effective tool when it comes to the evaluation of gas explosion overpressures in areas with large separation gaps. In addition, the further investigation of safety gap’s application in the process industrial structures was carried out in Chapter 7.
6.2 NUMERICAL MODELS OF SEPARATED CONGESTIONS

6.2.1 Experimental set-up

In this study, the scenarios of the available tests extracted from the Research to Improve Guidance on Separation Distance for the Multi-energy Method (RIGOS)-research program (VandenBerg & Versloot 2003) had been modelled. The configuration set-up parameters were indicated in Table 6.1.

As seen in Figure 6.1, the test modules consist of a number of tubes in two separated modules, a plastic sheet was used to cover the two obstacle configurations, which were placed on a concrete pad and filled with a flammable fuel-air mixture. The gas clouds were ignited at the centre of the congestion and at the ground level in one module. This module was termed the donor module. The flame propagated through the donor module, reached the safety gap, and propagated through the safety gap to the second module, which was termed the ‘acceptor’.

In the experiments (Figure 6.2.), nine overpressure sensors were positioned in at regular distances along the axis of the donor–acceptor configurations. Here, the entire setup, including the location of the sensors, which were represented in the simulations by using monitor points, was numerically simulated. In addition, the pressures from sensors at the edge of each module obtained from experiments were compared with the results from the numerical simulations.

![Figure 6.1 Obstacle configurations in experiments](image)
Table 6.1 Definition of the obstacle configurations

<table>
<thead>
<tr>
<th>Case no.</th>
<th>Fuel type</th>
<th>VBR (%)</th>
<th>Separation Distance (m)</th>
<th>Cylinder Diameter (m)</th>
<th>Pitch (m)</th>
<th>Dimension of the donor (m)</th>
<th>No. of tubes in a row</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>Ethylene</td>
<td>10.1</td>
<td>2.11</td>
<td>0.0191</td>
<td>0.089</td>
<td>1.408</td>
<td>16</td>
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<td>16</td>
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<td>16</td>
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<td>16</td>
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<tr>
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<td>0.0191</td>
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<td>12</td>
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<td>0.0191</td>
<td>0.134</td>
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<td>10</td>
</tr>
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<td>0.40</td>
<td>0.0191</td>
<td>0.134</td>
<td>1.596</td>
<td>12</td>
</tr>
<tr>
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<td>Ethylene</td>
<td>4.6</td>
<td>1.33</td>
<td>0.0191</td>
<td>0.134</td>
<td>1.33</td>
<td>10</td>
</tr>
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<td>Ethylene</td>
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<td>1.60</td>
<td>0.0191</td>
<td>0.134</td>
<td>1.596</td>
<td>12</td>
</tr>
<tr>
<td>19</td>
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<td>4.6</td>
<td>0.40</td>
<td>0.0191</td>
<td>0.134</td>
<td>1.596</td>
<td>12</td>
</tr>
</tbody>
</table>

* VBR is the volume blockage ratio, which is the ratio of the summed volume of the obstacles in an obstructed region and the volume of that region.

6.2.2 CFD modelling using FLACS

In this study, three different volume blockage ratios (VBR) were utilized, as seen in Figure 6.3, the donor modules in FLACS were numerically modelled with varying obstacle diameters and arrangements of obstacles. Specifically, all the cylinders, were of the same diameter (\(D=19.1\) mm), and were orientated orthogonally and regularly in the
FLACS simulations. By varying the pitches of $P=4.65D$ and $P=7D$, two different volume blockage ratios of $VBR = 10.1\%$ and $VBR = 4.6\%$ modules were created (see Figure 6.3 (a) and (b)). A third type of configuration ($VBR = 14\%$) was modelled by adding 24 regularly patterned vertical tubes of 114 mm diameter, Figure 6.3 (c). The three obstacle modules were named as type 1, type 2 and 3, respectively. The acceptors in the configurations were identical in all simulations with the volume blockage ratios of $VBR = 10.1\%$ and pitch $P=4.65D$, and all the simulations in FLACS were conducted by using the grid cell size of 0.03m, which equates to 33\% of the smallest pitch length ($P=4.65D=0.089m$). The grid cell size of 0.03m was determined based on a calibration using a series of different grid sizes.

(a) VBR = 10.1\%  
(b) VBR = 4.6\%  
(c) VBR = 14\%  

Figure 6.3  Obstacle configurations in FLACS
Each simulation model in FLACS consists of two separate configurations of obstacles as seen in Figure 6.4. The separation distances between the configurations were in the range of 0.20 – 2.11m, and the width and length of the donor were set equally from
1.06m to 1.76m by changing the number of the cylinders in a row as seen in Table 6.1, while the height of the configuration was half of the donor width.

In the numerical simulations, the configurations were fully enveloped by a stoichiometric gas cloud, as seen in Figure 6.5, and the fuel types in the simulations were pure ethylene, and pure methane, in different tests. All explosions were simulated by igniting the cloud in the centre at ground level of the donors. In FLACS, the overpressures were monitored along the central axis within the configurations corresponding to location of the pressure sensors in the RIGOS test layout, as seen in Figure 6.4.

6.3 RESULTS AND DISCUSSIONS

6.3.1 Comparison of the gas explosion overpressures with a small separation distance

A small separation distance in this chapter was defined as the open space with the separation distance to donor dimension ratio within the range of 0.125 to 1. Larger gaps had a greater separation distance to donor dimension ratio. Simulations of explosions in these congested configurations with the small separation distances were conducted using FLACS. The development of the overpressure after ignition of the gas cloud was presented in Figure 6.6. High pressures were observed at the boundaries of the obstacles of the donor region. This overpressure reduced significantly in the separation gap due to flame deceleration brought on by the lack of obstacles in the gap. A comparison of simulation and experimental results for the measurement sensor at the edge of the donor module was shown in Figure 6.7. The maximum pressures and development of the pressure agreed very well with the results from the experiments. However, the simulated arrival time was somewhat sooner than the experimental observations, the reason is that in FLACS the gas cloud was ignited immediately after the specified time in the FLACS simulation, whereas in the experiments, the combustion occurred only when the fuel reached the flammable limits, therefore, the generation of stoichiometric gas cloud delayed the ignition in experiments. As seen in Figure 6.7 (b), the overpressure observed in the acceptor at point 9 was significantly greater than the pressure at the donor module point 1 (see Figure 6.7 (a)). A very small
time span was seen between the development of blast in the donor and the acceptor due to the fact that the separation distance between the modules was too small for significant turbulence decay; the development of overpressures was illustrated well in both numerical simulation and experiment observations. By taking the maximum overpressure at the measurement sensor at the edge of the obstacles, the overall comparison of the experimental and FLACS overpressure data was presented in Figure 6.8 where a reasonably good agreement was seen.
Figure 6.6 Overpressure development during simulation

Figure 6.7 Experimental records of Overpressure-time paths and FLACS simulations results at sensors
6.3.2 Comparison of the gas explosion overpressures with a large separation distance

Despite the good agreement found for the overwhelming majority of the cases, it was seen that for one example wherein the separation distance ($SD$) to donor dimension ($DD$) ratio was equal to 1.5, the simulations over-predicted overpressures in the acceptor module. This one anomaly - test AE08, was taken from the RIGOS program (VandenBerg & Mos 2002) and compared with the FLACS simulation for the same case here. The donor has the same dimension as the acceptor in test AE08 of 1.4x1.4x0.7 m$^3$, and the entire configuration as shown in Figure 6.9 was set up with the $SD/DD$ ratio of 1.5, 10.1% $VBR$, and obstacle diameter of 0.019m, the fuel type was stoichiometric ethylene-air in FLACS.
Figure 6.9 Simulation model with a separate distance of SD/DD=1.5

(a) Overpressures-time data monitored in FLACS
As seen in Figure 6.10, the overpressures observed at the monitors within and near the donor from FLACS, which were approximately 80kPa, coincide to the experimental data. However, the remarkably different developments of the overpressures were seen after the flame propagates to the acceptor module.

Initially, the pressure values in both experiment and numerical simulation dropped to near zero since an important driver for turbulence generation, namely fluid-obstacle interaction were lacking in the open space. At a later point in time, when the flame reached the acceptor module, the flame re-accelerated and the overpressures measured increased. It is interesting to note that the new pressures monitored in acceptor in Figure 6.10(b) of the RIGOS test were lower than the overpressures in the donor due to the effect of sufficient separation distance. By contrast a significant jump of overpressures was obtained by using FLACS as seen in Figure 6.10(a).
The reason for this is postulated as related to turbulence length scale. In the combustion process, the rise of pressure is mainly due to the increase of turbulent burning velocity, and an important parameter used in the calculation of this velocity in the turbulence model, is the turbulence length scale $l_{LT}$, which is a typical length scale on the boundary. The turbulence length scale is used to calculate an initial value for dissipation of turbulent kinetic energy, as seen in Equation (71) and (72).

The equation for dissipation of turbulent kinetic energy (Hjertager 1984):

$$\frac{\partial}{\partial t}(\rho \varepsilon) + \frac{\partial}{\partial x_j}(\rho u_j \varepsilon) = \frac{\partial}{\partial x_j}\left(\frac{\mu_{\text{eff}}}{\sigma_{\varepsilon}} \frac{\partial \varepsilon}{\partial x_j}\right) + 1.44 \frac{\varepsilon}{k} G - 1.79 \rho \frac{\varepsilon^2}{k}$$ \hspace{1cm} (71)

$$\varepsilon = \frac{C_{\mu} k^{3/2}}{l_{LT}}$$ \hspace{1cm} (72)

where $\varepsilon$ is the rate of dissipation, $k$ is the kinetic energy of turbulence, $C_{\mu}$ is the constant in the $k$-$\varepsilon$ equation (typically $C_{\mu} = 0.09$), $\rho$ is the density, $x$ is the length coordinate in $j$-direction, $u$ is the velocity (ith component), $\mu_{\text{eff}}$ is the effective viscosity, $\sigma_{\varepsilon}$ is the Prandtl-Schmidt number which is given the value of 1.3 (Launder & Spalding 1974a), $G$ is the generation rate of turbulence.

For the numerical calculation process, the critical parameters, such as $\varepsilon$, $k$, $u$, $\mu_{\text{eff}}$ and $G$ are variables and will be updated according the inherent turbulent and combustion equations in FLACS. However, the turbulence length scale - $l_{LT}$ plays a role in the overpressure calculation in a different manner. $l_{LT}$ is applied as the length from the ignition point to the edge boundary of the congestion throughout the entire explosion simulation without update. Specifically, for a configuration with a large separation distance, such as the case in Figure 6.9, the turbulence length scale $l_{LT}$ equals to $3DD$, which was the distance from the ignition to the donor boundary (0.5$DD$) plus the separation distance (1.5$DD$), and the dimension of acceptor ($DD$).

While this is appropriate for a continuously congested region, in the large separation distance simulation case, the turbulence length scale may not be appropriately measured from the initial ignition point in the donor module, as the flow would have gone through a relaminarisation process over the length of the large safety gap. Possibly a more
appropriate datum for the calculation of the turbulence intensity in the acceptor module is the distance from the upstream end of the acceptor boundary where the flame begins to accelerate again.

The exclusive turbulence length scale value of 3DD in the default overpressure calculation resulted in smaller dissipation of turbulent kinetic energy for cases with large separation gaps, and further leaded to the significant over-prediction of the overpressure at the boundary of the acceptor.

To investigate this further, a technique was employed whereby two different turbulence length scales were used: One of the donor modules was referenced to the ignition point and the other for the acceptor module was referenced to the most upstream location of that module. This can be easily implemented by data-dumping the results just before the flame reaches the beginning of the acceptor module. This process was termed the Data-dump technique in reference to the process of extracting the data before the flame enters the acceptor module.

### 6.3.3 Data-dump technique

The Data-dump technique was therefore introduced here, and the same configuration AE08 was utilized as the example to investigate the effect of running the simulations in this manner.

![Image](image.png)

(a) Case AE08 before the Data-dump command, ignition in the centre of donor
As best practice for FLACS simulations require a minimum of 13 grid cells across the partially confined gas cloud for simulations using FLACS (GexCon 2011), the simulation grids were supposed to be meshed in the range of 0.03-0.06 m uniformly in all directions. A calibration using a series of different grid sizes had been conducted in section 6.3.1, the grid sizes had been independently tested in the comparison of the numerical overpressures and the experimental results, the satisfactory agreement (as seen in Figure 6.8) proved the accuracy of 0.03m grid utilized in this study, therefore, 0.03m grid was also applied throughout this section. By creating a cc-file in FLACS, the explosion results of scenario AE08 were dumped at time 0.026s when the flame exited the edge of the first congested region (the donor), as seen in Figure 6.11, and a new explosion file (AE08’) with the same setup as AE08 was created. However, the explosion AE08’ started from 0.026s by loading the previous data dumped from AE08. And in order to restart the flame acceleration process in the acceptor module with a new turbulence length scale datum at the upstream edge of the acceptor module, the ignition was relocated to the edge of the second congested region (the acceptor), as seen in Figure 6.11(b).
By using the Data-dump command, it was noted in the comparison in Figure 6.12 that the overpressures obtained in the second congested region were significantly reduced from 700kPa to 85kPa which was very close to the experimental results (60kPa in Figure 6.10 (b)). The Data-dump technique appeared to work for the one case from the RIGOS experiments calculation with an updated turbulent length scale.

In order to access the overall performance of the Data-dump technique, a total of 5 available sets of experimental tests in RIGOS research program (VandenBerg & Versloot 2003) were numerically simulated by using FLACS. As shown in Table 6.2, the separation distances were varying from 0.5DD to 1.5DD and the ethylene and
methane as fuel were both tested. For all cases, the grid cell size in FLACS was uniformly kept as 0.03×0.03×0.03m, and the identical Data-dump procedure as demonstrated above was applied to all the simulations. The cases were modelled with Ethylene and Methane as fuels.

Table 6.2 Configurations simulated by using FLACS

<table>
<thead>
<tr>
<th>Case No.</th>
<th>Exp. No.</th>
<th>Fuel type</th>
<th>VBR (%)</th>
<th>Separation Distance (m)</th>
<th>Cylinder Diameter (m)</th>
<th>Dimension of the donor (m)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>AE07</td>
<td>Ethylene</td>
<td>10.1</td>
<td>0.70(0.5DD*)</td>
<td>0.019</td>
<td>1.4×1.4×0.7</td>
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<td>AM04</td>
<td>Methane</td>
<td>10.1</td>
<td>0.70(0.5DD)</td>
<td>0.019</td>
<td>1.4×1.4×0.7</td>
</tr>
<tr>
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<td>BE05</td>
<td>Ethylene</td>
<td>4.6</td>
<td>1.33(1.0DD)</td>
<td>0.019</td>
<td>1.33×1.33×0.7</td>
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<td>0.019</td>
<td>1.4×1.4×0.7</td>
</tr>
</tbody>
</table>

*AE denotes the type A donor with the volume blockage ratio of 10.1% and ethylene as fuel.
*AM denotes the type A donor with the volume blockage ratio of 10.1% and methane as fuel.
*BE denotes the type B donor with the volume blockage ratio of 4.6% and ethylene as fuel.
*DD denotes the maximum length of the donor dimensions.

![Figure 6.13 The peak donor explosion overpressures in different cases](image-url)
Corresponding to the explosion cases for a wide separation gap from Table 6.2, the peak gas explosion overpressures at the both of the donor and acceptor modules for cases with a wide separation gap had been graphically represented in Figure 6.13 and Figure 6.14 respectively together with the overpressures calculated by using FLACS.

The overpressures in the donor module from the FLACS simulations were the same regardless of whether the Data-dump technique was used. As seen in Figure 6.13, the donor explosion overpressures calculated by FLACS were in good agreement with results from the experimental tests.

For the acceptor module, there were 2 sets of data. The first was obtained by allowing the flame to propagate straight through into the acceptor module with no interruption of the simulations while the second set was obtained using the Data-dump technique described in section 6.3.3.

The results in the acceptor module for FLACS simulations without using the Data-dump technique were seen to be significantly higher than the experimental results. In other words, the software predicted a detonation in the acceptor module as the flame propagated into the acceptor module while no such phenomenon was seen in the experiments. However when the Data-dump technique was used, the magnitude of the
overpressures from the simulations for the wide gap configuration was found to be in good agreement with data from the RIGOS experiments.

Therefore, the overall results in the assessment of FLACS manifested that the calculation accuracy of FLACS exists in the congested regions without separation distance interruption. However, when it comes to the evaluation of gas explosion overpressures in area with large separation gaps, the over-prediction of overpressures should be addressed by using the Data-dump technique. As it is the intrinsic issue of inappropriate calculation of the turbulence length scale in FLACS, using the Data-dump technique is only a suggestion/guideline for others working in this area, while the modification of the software will not be covered in this chapter.

6.4 SUMMARY

The CFD software FLACS is a strongly validated software and generally gives good agreement with experimental data. This chapter reported a comparison of simulations and published data from experiments from TNO Prins Maurits Laboratory for cases that involved safety gaps of various sizes. In the overwhelming majority of cases, good agreement between the simulated results and those obtained by experiment was found in both the donor and acceptor modules.

However, when the separation distance increased to a very large size, numerically over-predicted overpressures in the acceptor module were seen. Specifically, a large discrepancy in the overpressure between the numerical and experimental results was seen. As the flame propagates into the acceptor, a detonation in the acceptor was predicted by using FLACS while only the deflagrations were seen in the experiments. It is postulated that the over-estimation of overpressures in FLACS was due to the turbulence length scale, which plays a major role in the calculation of flame acceleration. The inappropriate application of turbulence length scale across the separation gap leaded to exaggerated flame speeds, thereby contributing to ultimately higher than expected overpressures.

A technique termed the Data-dump was tested in this study. The mechanism of the Data-dump is that the simulation is interrupted before the flame propagates into the acceptor module, and then it would be restarted with the ignition point reset to the
upstream end of the acceptor module, therefore the turbulence length scale could be reset.

By using the Data-dump technique on five sets of explosion scenarios with two types of fuel and three different separation distances, numerical data using this technique was obtained and compared with experimentally observed explosion overpressures. The overall results indicated that FLACS can still give remarkably good agreement with experimental data even for large-scale safety gaps when this technique was used. As an alternative tool to improve the accuracy of FLACS rather than modifying the software, it is suggested that evaluate the gas explosion overpressures in the region with large separation gaps by employing the Data-dump technique.

In the next chapter, the accuracy improvement guideline –the Date-dump technique was utilized in the investigation of the explosion mitigation effect of safety gap in the realistic offshore structure, a cylindrical FLNG was numerically modelled based on a traditionally ship-shaped FLNG.
Explosion safety evaluation for congested offshore platforms based on CFD simulations

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CHAPTER 7. EVALUATION OF SAFETY GAP EFFECT ON OVERPRESSURE MITIGATION

7.1 INTRODUCTION

This chapter evaluated the effect of safety gap on gas dispersion and explosion for a cylindrical Floating Liquefied Natural Gas (FLNG) vessel. The conventional ship-shaped and an innovative circular FLNG platforms had been established and used for the detailed CFD based analysis. Rather than the structural and hydrodynamics advantages of mobility, stability and cost efficiency, etc., this study aims to investigate the safety of gas explosion on the cylindrical FLNG and compare the safety gap effects on different configurations.

In the process industry, the safety gap, which is an open space, with no congestion, deliberately placed in between congested process areas, is one of the most effective and widely used safety-in-design measures. The principle behind the operation of the safety gap is that it interrupts a positive feedback mechanism in congested areas. The positive feedback mechanism consists of the generation of turbulence, enhanced thermal and chemical mixing between combustion products and reactants, higher flame speeds and even higher pressures. The absence of obstacles in a safety gap eliminates the fluid-obstacle interaction thereby preventing the generation of turbulence. It can be very effective in reducing pressures prior to the onset of detonation. Investigations of flame acceleration and overpressure in gas explosions, in most studies so far, had focussed on setups involving multi-obstacle groupings with successive, periodically spaced obstacles (Alekseev et al. 2001; Molkov et al. 2006; Wen et al. 2013; Chan et al. 1983; Kindracki et al. 2007; EMEG 1997), and a limited number of experimental studies had examined the effect of the safety gap on gas explosions (Gubba et al. 2008; Moen et al. 1980; Rudy et al. 2011; Na'inna et al. 2013; VandenBerg & Versloot 2003).

In most experimental explosion programs, investigation of the safety gap was conducted in highly confined chambers whereby tubes were arranged such that cylindrical flames propagate in one direction only, except for (VandenBerg & Mos 2002; VandenBerg & Versloot 2003) who carried out gas explosion experiments in vapour clouds containing two separate configurations of obstacles to develop practical guidelines with regard to
critical safety gap. The experimental configuration generally consists of orthogonally arranged obstacles enclosed in plastic sheeting. Therefore, the flames propagate three-dimensionally in the tests. Several configurations of (VandenBerg & Versloot 2003) hemispherical flame propagation and multiple separation distances had been modelled in this study. The separation distance was defined as the distance between the boundaries of the congested regions, i.e. the distance between the downstream end of the first module, where ignition is initiated and the upstream end of the second module to which the flame propagates after passing the separation.

This chapter consists of two major parts – gas dispersion and explosion analysis. A series of different safety gap configurations were firstly evaluated for gas dispersions, and simulations of gas explosions later were conducted to evaluate the overpressure mitigation effect of safety gap, the Data-dump guideline proposed in previous chapter was utilized.

7.2 FLNG CONCEPT

Today, increasingly more oil and gas projects are being constructed in less accessible fields with more challenging weather conditions, such as the area north of Darwin, Australia which experiences cyclonic conditions. Floating LNG vessels are relatively new concepts that are increasingly being considered as cost effective alternatives to fixed platforms. The main advantage of the “ship-shaped” vessel is cost but also importantly, these vessels have the mobility to be towed out and anchored at many locations in the ocean (Shimamura 2002; Suardin et al. 2009).

However, most current ship shaped vessels can only drift in one direction in the water and cannot provide a stable platform due to hull deflection (sag/hog) caused by the large aspect ratio, hence, under the extreme weather conditions such as typhoons, these vessels have to cease operation. This cease of operations for more than 5% of the year can make the difference between a profitable operation and an unprofitable one.

In order to improve stability, cylindrical FLNG’s are currently under consideration. These are symmetrically designed and this not only eliminates the wave induced fatigue loads, but also eliminates the large bending moments experienced in rough seas. The improvement in hydrodynamic stability allows for less cease of operations and hence is
presents a significant advantage particularly in areas where cyclonic conditions are a factor.

Very little has been investigated for cylindrical FLNGs and even for other cylindrical vessels the work done so far has been focused on construction, operation studies, hydraulic and the hydrodynamic analysis (Wang et al. 2013; Kvamsdal et al. 2010; Zhao et al. 2011; Hirdaris et al. 2014). Topsides safety evaluation for circular platforms subjected to gas dispersion and explosion have never been published in the open literature. On-duty cylindrical-hulled FPSOs in the world are mainly designed for cruel oil drilling and production, whereas the design and construction of FLNG with circular hull are still conceptual or under development. Here, a cylindrical FLNG model was investigated and compared with traditional vessel FLNG. The cylindrical FLNG model, which is composed of 6 liquefaction unit modules and 6 other natural gas processing modules with detailed equipment and piping design, was utilized in this study, and the main purpose here is to conduct the gas dispersion and explosion safety analysis on the obstructed configurations with a series of different safety gaps.

7.3 NUMERICAL MODELS

The ship-shaped and cylindrical FLNG cases in this study were both modeled by using the pre-processor CASD of FLACS, all main structural components of the FLNG were converted into boxes and cylinders as congestion blocks in FLACS, and anticipated congestions, which were walls and decks were always assumed to be unyielding within CFD simulations, i.e. rigid walls remain in place even for the extreme loads. In terms of the grid models in the dispersion simulations, the refined and cubical grid cells in the near region of the leak were applied, while the grid outside the main area of interest was further stretched to the boundaries. In addition, the aspect ratio of the grid was controlled to within 20%.

7.3.1 Ship-shaped FLNG

The PRICO (Sultan 2011; Xu et al. 2013; Xu et al. 2014) liquefaction, power generation, compressor and fuel gas system units etc. were used in this study. A realistic geometry model of a ship-shaped FLNG was modeled in detail as shown in Figure 7.1. And Figure 7.1(b) illustrated the arrangement of topside modules including module 1 to 12:
1. Power generation Module 1

2. 3 Trent gas turbines and 2 essential diesel generators Module 2

3. Nitrogen package, hot oil, Mono-Ethylene-Glycol (MEG) processing and inlet facilities Module 3

4. Boil off gas compressor and fuel gas system Module 4

5. Acid gas removal unit & end flash gas compressor Module 5

6. Dehydration and mercury removal Module 6

7. Liquefaction modules from Module 7 to Module 12

Figure 7.1 FLACS geometry of the ship-shaped FLNG
7.3.2 Cylindrical FLNG

Figure 7.2 below showed a three dimensional view of the cylindrical FLNG platform. As seen in Figure 7.3, the order of the 12 modules were kept the same as they are in Figure 7.1, except that a U shape arrangement of those modules was organized to fit the cylindrical deck. Therefore, the cylindrical FLNG had the same process order, but modules were in a more compact area and the turret area was eliminated. This resulted in the cylindrical FLNG having a smaller topsides area. Figure 7.4 displayed a closer view of the block of the liquefaction train - module 7 and the power generation and control function module – module 1 located in top and bottom rows of the platform, respectively.

![Figure 7.2 FLACS geometry of the cylindrical FLNG floater](image)
Figure 7.3 Topside arrangement of the modules
7.4 SAFETY GAP EFFECT ON GAS DISPERSION

The numerical models of different gas dispersion scenarios were then performed on the above FLNGs with the ship-shaped and cylindrical layouts, respectively. The effects of different safety gaps on the platform were discussed below.

7.4.1 Gas dispersion simulation in the ship-shaped FLNG

Initially, the gas dispersion simulations were conducted between adjacent modules on one side of the platform in the ship-shaped FLNG. Two different safety gap configurations, as shown in Figure 7.5, were used for the dispersion simulations. The first configuration allowed a 12.5-meter gap between two modules. The second configuration had a 20-meter gap.
Dispersion simulations for large and small leaks with two wind directions (-15° and 0°) at a speed of 5m/s were conducted to analyze the effects of gap configuration on gas dispersion. These effects were interpreted both qualitatively and quantitatively. The gas composition used in the risk analysis was 27% Methane, 33% Ethane, 15% propane, 19% Pentane and 6% Nitrogen.

Figure 7.6 and Figure 7.7 illustrated the gas dispersion of a 96 kg/s release impinging on the power generation unit in the Module 7 (M7). As seen in those figures, in the monitor region of M7, the reduction in gas cloud size due to the increase in the safety gap was more pronounced for the -15° E-W wind direction as compared to 0° E-W wind direction, Q9 is volume of the equivalent stoichiometric gas cloud. This reduction however was limited for these two cases because a large portion of the gas cloud remained within the module, and nearly zero equivalent stoichiometric gas cloud size was monitored in the Module 9 (M9).
Figure 7.6 Effects of safety gaps on the dispersion of an impinging 96 kg/s leak rate with $0^\circ$ E-W wind direction. The Q9 volume within module M7 is displayed for both gap configurations.

Figure 7.7 Effects of safety gaps on the dispersion of an impinging 96 kg/s leak rate with $-15^\circ$ E-W wind direction. The Q9 volume within module M7 is displayed for both gap configurations.

In contrast, when the specific release resulted in the gas spreading across the safety gap to adjacent modules, the gap size could have a significant effect on the resulting cloud size in the neighboring module. Figure 7.8 and Figure 7.9 illustrated a free-jet leak originating in module M7 that spread towards the adjacent module M9. The results clearly showed that as the safety gap increased the cloud size in the module where leak source originates was minimally affected, while the resulting gas cloud in the
neighboring module was significantly reduced. More specifically, the current gas dispersion simulations (96 kg/s free jet) showed a 62% and 44% reduction in the gas cloud size for the 0° and -15° E-W wind cases, respectively.

Figure 7.8 Effects of safety gaps on the dispersion of a free-jet 96 kg/s leak rate with 0° E-W wind direction. The Q9 volumes within modules M7 and M9 are displayed for both gap configurations.

Figure 7.9 Effects of safety gaps on the dispersion of a free-jet 96 kg/s leak rate with -15° E-W wind direction. The Q9 volumes within modules M7 and M9 are displayed for both gap configurations.

7.4.2 Gas dispersion simulation in the cylindrical FLNG

In the assessment of gas dispersion on the ship-shaped FLNG, the safety gaps between modules were exclusively arranged in W-E direction due to the space limit, as seen in
Figure 5.1. And when all gas dispersion scenarios described in Section 7.4.1 reached the steady state, the gas clouds only spread towards the adjacent modules. For instance in Figure 7.8 and Figure 7.9, the leak originating in module M7 had the major influence in module M9, while the modules further away were still unaffected. In other words, the safe gap only affected the gas dispersion in the near field in the ship-shaped FLNG.

However, in the cylindrical FLNG, due to the circular shape of the vessel and the U-shape module arrangement, as seen in Figure 7.3, the gas dispersion would more likely influence the far field modules. For example, if leakage release occurs in module M9, the gas clouds would expand to all other modules in the cylindrical FLNG which depends on the wind directions. Therefore, both near field and far field gas dispersion simulations were conducted on the cylindrical FLNG platform.

7.4.2.1. **Gas dispersion simulation in near field**

Similar to the arrangement of the safety gap in a vessel borne FLNG, for adjacent modules in the cylindrical FLNG, the safety gaps were allocated between different liquefaction modules in \( W-E \) direction, as shown in Figure 7.10. The configuration on the left had 12.5m gaps between each two modules, while in the second configuration, 20m safety gaps were used in the analysis, as seen in Figure 7.10 (b). The release was situated in Module 7 at the gas release rate of 96 kg/s, two wind directions (-15° and 0°) at a speed of 5m/s from west to east were both analyzed, and the gas composition was also 27% Methane, 33% Ethane, 15% propane, 19% Pentane and 6% Nitrogen. Due to the wind direction, the equivalent stoichiometric gas clouds were monitored in two regions allocated to the two adjacent modules (M7 and M9), as shown in Figure 7.11.
Explosion safety evaluation for congested offshore platforms based on CFD simulations

Figure 7.10 Test layout of the cylindrical FLNG

(a) Safety gap of 12.5m  (b) Safety gap of 20m

Figure 7.11 Effects of safety gaps on the dispersion of 96 kg/s leak rate in the liquefaction units in the cylindrical FLNG

(a) Gas release impinges on the power generation unit at 12.5m gap
(b) Gas release impinges on the power generation unit at 20m gap

(c) Gas release spreads across the safety gap of 12.5m
(d) Gas release spreads across the safety gap of 20m
In correspondence with the two types of gas release scenarios in the ship-shaped FLNG, the cases of gas release impinged on the power generation unit (Figure 7.11 (a) and Figure 7.11 (b)) and gas cloud spread across the safety gap without large blockage (Figure 7.11 (c) and Figure 7.11 (d)), were assessed in the adjacent modules for the cylindrical FLNG.

**Gas release impinges on the power generation unit**

Figure 7.12 illustrated the data of the equivalent stoichiometric gas cloud volume Q9 in two monitor regions when the gas dispersion reached the steady status. For the 0° W-E wind direction case, it was clearly seen that the reduction of Q9 in the monitor 1 due to safety gap is negligible (i.e. specifically from $5.23 \times 10^3$ m$^3$ to $5.22 \times 10^3$ m$^3$). It is due to the fact that power generation unit, which is a relatively large obstruction, hindered the fluid- during the gas dispersion. This resulted in a significantly large portion of the gas cloud remaining within the monitor region 1.

However, unlike the gas dispersion in the ship-shaped FLNG, when the wind direction comes to -15° W-E wind direction with a deviation of the impinging upon the power generation unit, more gas cloud spread across the safety gap. For example, about $3.11 \times 10^3$ m$^3$ gas cloud dispersed from the monitor region 1 (with $5.87 \times 10^3$ m$^3$ gas cloud) to the monitor region 2 at the safety gap of 12.5m, while it was only $0.6 \times 10^3$ m$^3$ in the 0° W-E wind direction scenario.

Furthermore, a great decrease in gas cloud size as a result of the increase in the safety gap was seen in the monitor region 2. Specifically, in the -15° W-E wind direction case, the gas cloud size of $3.11 \times 10^3$ m$^3$ dropped to $1.45 \times 10^3$ m$^3$ (about 53% reduction) when the safety gap increased from 12.5m to 20m, and the reduction percentage for the 0° W-E wind direction was also 35%.

Overall, for the scenario that the gas release impinges on the power generation unit, the same tendency that the main portion of gas cloud gathers in the gas-leaking module was seen in both ship-shaped and cylindrical FLNGs. It was however seen that the influence of safety gaps in the cylindrical FLNG was greater than they were in the ship-shaped FLNG, which was reflected by the gas cloud size reduction percentage of 53% and 35%.
for the -15° and 0° W-E wind direction in the monitor region 2, respectively. And it is also interesting to note that significantly more gas cloud spread across the safety gap to the monitor region 2 in the cylindrical FLNG, which was due to the fact that the boundary in such FLNG platform was larger than the ship-shaped one, though the FLNG platform has a smaller body shape area. More precisely, for the case that the wind blew the gas cloud with an angle deviating W-E direction, the extensional space in the S-N direction in the cylindrical FLNG platform allowed the gas cloud to expend further, whereas the ship-shaped has a narrow body width in S-N direction, the limitation of such boundary confined the development of the gas dispersion.

![Figure 7.12 The Q9 volume within the monitor regions when the gas release impinges on the power generation unit](image)

**Gas release spreads across the safety gap**

In terms of the scenario of gas release spread across the safety gap without disturbance of large objects, it was seen in Figure 7.11 (c) and Figure 7.11(d) that the gas dispersion mainly went to the second monitor region along with the wind direction.

Similar to the observation in the ship-shaped FLNG, in the monitor region 1 where the leakage originates in the cylindrical FLNG, the gas cloud size was significantly smaller
by orders of magnitude compared to the neighbouring monitor region. In addition, the variation of gas cloud size in the monitor region 1 due to safety gap increase was minimally affected regardless of the wind directions.

However, in the monitor region 2, Figure 7.13 indicated the significant effect of the safety gaps on decreasing cloud size. The reduction percentage of gas cloud size due to the increase of the safety gap from 12.5m to 20m was about 33% (from $4.83 \times 10^3$ m$^3$ to $3.23 \times 10^3$ m$^3$) for the $-15^\circ$ W-E wind direction scenario, and it was also about 36% for the $0^\circ$ wind.

Therefore, for the gas release spread across the safety gap case, very similar gas dispersion development in the adjacent modules were seen in both ship-shaped and cylindrical FLNG. The safety gap played a critical role in reducing the gas cloud size in the gas dispersion.

![Figure 7.13 The Q9 volume within the monitor regions when the gas release spreads across the safety gap](image)

7.4.2.2. Gas dispersion simulation in far field

In the far field safety gap assessment in the cylindrical FLNG, the platform was split into two monitor regions as seen in Figure 7.14. Two configurations were used in the
investigation of the safety gaps, as seen in Figure 7.15 (a). The configuration without gap is consisted of 12 modules organized in a $U$-shape along the pipe rack, while the one in Figure 7.15 (b) has 6 modules in the internal circle against the pipe rack with the same safety gaps of 10m in N-S direction.

A series of gas dispersion at 6 different leakage locations were conducted. As seen in Figure 7.14, for gas leak initiates in the monitor region 1, the equivalent stoichiometric gas cloud monitored in region 2 was the termed as the far field data to investigate, vice versa.

For all the gas dispersion simulations, the releases were simulated on the deck level, the leakage area size, release rate and release duration were kept constant, while wind directions were varied along with the different gas leakage locations. Table 7.1 showed the input data of the gas dispersion simulations and the output of the equivalent stoichiometric gas cloud volume monitored in two regions.

![Figure 7.14 Release locations and monitor regions in the cylindrical FLNG platform](image-url)
Explosion safety evaluation for congested offshore platforms based on CFD simulations

(a) Modules in the cylindrical FLNG without gap

(b) Modules in the cylindrical FLNG with 10m gap

Figure 7.15 Test layout of the cylindrical FLNG for gas leakage at monitor point 6

Table 7.1 Input and output of the far field gas dispersion simulations

<table>
<thead>
<tr>
<th>Leak point</th>
<th>Area Description</th>
<th>Wind direction</th>
<th>Hole size ($m^2$)</th>
<th>Leak height (m)</th>
<th>Release rate (kg/s)</th>
<th>Release duration (s)</th>
<th>$Q_9_1$</th>
<th>$Q_9_2$</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>Liq train</td>
<td>NS -25°</td>
<td>0.9</td>
<td>100</td>
<td>403</td>
<td>236</td>
<td>5.26</td>
<td>9.23</td>
</tr>
<tr>
<td>2</td>
<td>Pipe rack</td>
<td>NS 0°</td>
<td>0.9</td>
<td>100</td>
<td>403</td>
<td>236</td>
<td>2.96</td>
<td>7.77</td>
</tr>
<tr>
<td>3</td>
<td>Liq train</td>
<td>NS +25°</td>
<td>0.9</td>
<td>100</td>
<td>403</td>
<td>236</td>
<td>6.81</td>
<td>6.49</td>
</tr>
<tr>
<td>4</td>
<td>Acid gas removal unit</td>
<td>WE -45°</td>
<td>0.9</td>
<td>100</td>
<td>403</td>
<td>236</td>
<td>9.29</td>
<td>8.79</td>
</tr>
<tr>
<td>5</td>
<td>Boil off gas compr module</td>
<td>NS 0°</td>
<td>0.9</td>
<td>100</td>
<td>403</td>
<td>236</td>
<td>5.05</td>
<td>7.60</td>
</tr>
<tr>
<td>6</td>
<td>Power gen module</td>
<td>EW +45°</td>
<td>0.9</td>
<td>100</td>
<td>403</td>
<td>236</td>
<td>12.06</td>
<td>11.73</td>
</tr>
</tbody>
</table>

$Q_9_1$ is the equivalent stoichiometric gas cloud volume ($10^3 m^3$) in the monitor region 1 and $Q_9_2$ is the equivalent stoichiometric gas cloud volume ($10^3 m^3$) in the monitor region 2.
Figure 7.15 demonstrated one of the gas dispersion cases for gas leakage at the monitor point 6; the wind direction was from east to west at a degree of 45 for both configurations with and without a safety gap. The gas cloud spread from the power generation module to other adjacent modules in two directions, and the concentration of the gas cloud was seen between the gap of the 6 liquefaction trains on the top (monitor region 1 as indicated in Figure 7.14) and the other processing modules on the bottom (monitor region 2).

In the comparison, the equivalent stoichiometric gas cloud $Q_9$ (10853 m$^3$) in the monitor region 2 of the configuration with 10m safety gap was smaller than that (11729m$^3$) in the configuration without safety gap. By contrast, in the monitor region 1, $Q_9$ for 10m safety gap configuration increases to 13559m$^3$ which was 11% greater than the equivalent $Q_9$ (12059m$^3$) for the scenario without safety gap. In other words, due to the increase of 10m safety gap, more gas cloud expanded to the far field area of the configuration, as shown in Figure 7.15(b).

For all other 5 different leak location cases, the far field $Q_9$ data was recorded in Figure 7.16. It was seen that the safety gap of 10 between the modules in $N$-$S$ direction
increased the gas cloud size in the far field for the cylindrical FLNG. The main reason was that the addition of 10m safety gap against the pipe rack actually narrowed and congested the space between the liquefaction train area (monitor region 1) and the other processing area (i.e. the monitor region 2 including the acid gas removal unit, boil off gas compressor and power generation module etc.). It is known that the congestion plays a critical role in the gas dispersion and explosion (Li et al. 2014a; Bakke et al. 2010; Li et al. 2014b). The more congested space where the gas leakage concentrates the more contribution of the turbulence of the gas flow, therefore in such case, the more congested region due to the 10m safety gap increased the generation of the equivalent stoichiometric gas cloud in the far field.

In order to further investigate the gas dispersion in the far field, the diffusion ratio was defined as the equivalent stoichiometric gas cloud volume \( Q_9 \) in the far field monitor region divided by the volume \( Q_9 \) in the region where the release originates. As seen in Figure 7.17, for all the gas dispersion occurs in the configuration with 10m safety gap, the gas diffusion ratios were determined for the 6 different leakage locations. It is interesting to see that all the gas dispersion formed from the monitor region 1 to monitor region 2 had greater diffusion ratios compared to the corresponding cases where the gas leaked in the monitor region 2. In other words, gas leakage in the liquefaction train would more likely to transmit more gas cloud to the modules in the far field, which could cause a bigger concern in the cylindrical FLNG. Further investigations to the layout and geometry details of the modules are required and therefore the gas explosion simulations in the future.
7.4.3 Discussion

In this section, the gas dispersion simulations were conducted on both ship-shaped and cylindrical FLNG vessels, the commercial CFD software FLACS was used to evaluate the effect of the safety gap on the gas dispersion.

In the investigation of the gas dispersion in close to the release in both ship-shaped and cylindrical FLNG configurations, the overall results in this study indicated that the safety gap played a critical role in reducing the gas cloud size regardless of the gas leakage manners, and the same tendency of gas cloud size decrease was observed. However, the complexity was seen for the cylindrical FLNG. Specifically, in the scenario of the gas release impinges in the large blockage during the gas dispersion, the effect of the safety gap was more obvious in the cylindrical FLNG, more gas cloud spread through the safety gap into the neighboring modules and the reduction percentage of gas cloud in those modules was greater than that in the ship-shaped FLNG.

Unlike the ship-shaped FLNG which has no concern of the gas dispersion transmits to the direction perpendicular to the ship body length direction, the U-shape layout of the modules in the cylindrical FLNG however requires to investigate the far field gas
dispersion in both directions. The complexity was also seen in the further investigation of the safety gap effect on the modules in the far field, instead of reducing the equivalency stoichiometric gas cloud size, the safety gap increased the gas cloud volume. And it is also interesting to note that if the gas leakage originates in the liquefaction trains, more gas cloud can spread through the safety gap to treat the far field modules.

In conclusion, the safety gap is an effective way to reduce the gas cloud size in the near field in both traditional and cylindrical FLNGs, whereas it increases the gas cloud size in the further away from the point of release in the cylindrical FLNG. The layout of the modules in the cylindrical FLNG contributes to complexity of the congestion in the gas dispersion; therefore, it is suggested to investigate gas dispersion in the cylindrical FLNG both adjacent to the release and further away, separately.

7.5 SAFETY GAP EFFECT ON GAS EXPLOSION

In the sub-section, a series of gas explosion scenarios were then simulated on the cylindrical FLNG platform. The effects of different safety gaps on the platform were investigated in both near field and far field explosion regions. The Data-dump technique was utilized to improve the calculation accuracy of FLAC.

7.5.1 Near field gas explosion simulation on the cylindrical FLNG platform

On the cylindrical FLNG platform, the near field gas explosion region was defined as the scenario where flame propagates through two adjacent congestions with one safety gap. Two different safety gaps of 12.5m and 20m were modeled as seen in Figure 7.18. And Figure 7.19 showed the major equipment in the liquefaction module which includes the turbine air intake, turbine bundle removal equipment, scrubber and cold box on the lower level and two heat exchangers on the top levels; and the detectors were placed on the lower level to monitor overpressures, the gas composition was 27% Methane, 33% Ethane, 15% propane, 19% Pentane and 6% Nitrogen.
Figure 7.18 Two configurations with different safety gaps in near field

(a) 3D view of the liquefaction lower level
7.5.1.1. **Application of Data-dump in gas explosion simulations on the cylindrical FLNG platform**

Before the comparison of different sets of the safety gaps, the Data-dump technique (Ma et al. 2014) was firstly applied in this study. The Data-dump is an effective tool to reset the turbulence length scale for gas explosion scenario with a safety gap, thereby increasing FLACS’s calculation accuracy. In this study, the 25%, 50% and 100% filled gas cloud cases were all investigated, the overpressures before and after Data-dump were monitored under or on the surfaces of the turbine air intake, turbine bundle removal equipment, scrubber and cold box as seen in Table 7.2.
For each gas explosion simulation, the explosion results were firstly dumped at the time when the flame exits the edge of the donor which was the module where gas cloud was ignited. Then by creating a cc-file and executing a duplication command, a new explosion file for the receiving module was created along with the data loaded from overpressure results in the donor. In order to reset the turbulence length scale and restart the flame acceleration in the receiving module, the ignition was relocated to the upstream edge of the accepting module which is opposite to the donor.

The comparison data of the overpressure modification percentages due to Data-dump technique were depicted in Figure 7.20 and Figure 7.21. Overall, the Data-dump technique decreased the overpressures recorded in the acceptor module. Specifically, for the 12.5m safety gap scenario, the overpressure modification percentages tend to be
below 12% except the overpressures observed in the scrubber area; whereas the overpressures were modified to larger extents in the 20m safety gap case, the percentage for each case in Figure 7.21 was greater than the corresponding case in Figure 7.20. In other words, the greater the safety gap size is, the more over-prediction of overpressure in FLACS should be amended by Data-dump technique. Therefore, in order to assure the overpressure calculation accuracy in safety gap modeling, the Data-dump was applied for all the following simulations in this study.

Figure 7.20 Overpressure reduction percentages after Data-dump for 12.5m safety gap scenario with different gas cloud coverage
7.5.1.2. Different safety gaps subjected to gas explosion under different gas cloud coverage

To demonstrate the explosion overpressure distribution on the two sets of safety gap configurations, Figure 7.22 gave an overview of the 100% gas filled simulation case with the maximum overpressure up to 9 barg. The results indicated that nearly identical pressures were observed in the donor module (the modules where ignition occurs on the left hand side of the safety gap) and the pressures were lower in the receiving module (modules on the other side) for the 20m gap as compared to the 12.5m gap.
Explosion safety evaluation for congested offshore platforms based on CFD simulations

The University of Western Australia

Figure 7.22  Maximum overpressures for 100% filled gas cloud spanning the modules separated by different safety gaps

In addition, the comparison of all the 25%, 50% and 100% gas cloud filled cases was conducted; the coverage percentage was controlled by manipulating the volume height of the gas cloud. Figure 7.23 to Figure 7.25 illustrated three similar scenarios that a reduction in the overpressure was observed in the receiving modules if the safety gap increased from 12.5m to 20m.

Figure 7.23  Maximum overpressures for cloud (100% filling) configurations with 12.5m and 20m safety gaps
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Figure 7.24 Maximum overpressures for cloud (50% filling) configurations with 12.5m and 20m safety gaps

Figure 7.25 Maximum overpressures for cloud (25% filling) configurations with 12.5m and 20m safety gaps

It was interesting to note that overpressures near the turbine air intake and scrubber were higher than the observed overpressures in the cold box and turbine bundle removal areas regardless of the safety gap size. The main reason is that the flame propagation distances from the ignition in the donor to the targets of the turbine air intake and scrubber were longer than the flame propagation distances to the cold box and turbine bundle removal areas (as seen in Figure 7.19(b), the turbine air intake and scrubber...
were placed on the right hand side even further away from the ignition coming from left), the longer the flame path length was, the longer time the flame turbulence developed within the congestion, which further induced greater overpressures (Li et al. 2014a; Li et al. 2014b). Moreover, the turbine air intake and scrubber region was more congested with small dimension objects; therefore, the smaller average diameter of the obstacles and greater congestion ratio contributed to more turbulence induced flame acceleration, which built up greater overpressures as well (Bradley et al. 2008; VandenBerg & Mos 2005).

In terms of the safety gap effect, it was seen that the overpressure difference between the 12.5m and 20m safety gap cases was more apparent in the turbine air intake and scrubber areas, which indicated that the safety gap reduced more overpressures where the flame path was longer and the average obstacle diameter was smaller and the congestion ratio was greater. Besides the analysis of the safety gap effect on overpressure difference in near field, the investigation to the safety gap effect in far field was further conducted below.

### 7.5.2 Far field gas explosion simulation on the cylindrical FLNG platform

In order to investigate the consequence associated with the ignition of gas cloud from one module to the far away modules on the cylindrical platform, explosion simulations were performed by using varying parameters such as different gas cloud locations, size and ignition locations. The far field gas explosion scenario was defined as the region where flame propagates through more than one safety gap. 4 gas clouds with same size of $140 \times 140 \times 10m$ were placed at different locations covering all the modules, for each gas cloud, 6 ignition scenarios occurring in the ground center of each module were simulated, as seen in Figure 7.26. Overall, for each cylindrical FLNG platform, 24 simulations were carried out in the far field gas explosion investigation.

And two different cylindrical FLNG platforms (as seen in Figure 7.27) were modeled in order to compare the effect of safety gaps with different safety gaps on the gas explosion mitigation. One configuration is the platform with all modules move 10m inwards within the pipe rack circle to form the safety gap of 10m in $N-S$ direction.
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(Figure 7.27(b)); while the other configuration has no gap between the modules and pipe rack, as seen in Figure 7.27 (a).

Figure 7.27 showed one explosion scenario that the gas cloud was ignited in Module 10 and the flame propagated further to all other surrounding modules and far filed ones such as Module 3 and Module 4. The gas explosion path went through two gaps in N-S direction was defined as Path 1, e.g. Path 1 in Figure 7.27 was distance from Module 10 to Module 3 or from Module 9 to Module 4; whereas Path 2 was defined as the flame path after three gaps. For each module, about 10 monitor points were uniformly allocated on the ground level to detect the overall overpressures; and the recorded overpressures tabulated in Table 7.3 were averaged from the modules in the far end of the flame path. Table 7.3 and Figure 7.28 illustrated the comparison results observed on the cylindrical FLNG platform with two different safety gap setups.
Explosion safety evaluation for congested offshore platforms based on CFD simulations

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(a) Modules in the cylindrical FLNG without gap

(b) Modules in the cylindrical FLNG with 10m gap

Figure 7.27 Test layout of the cylindrical FLNG for gas cloud ignited in Module 10

Figure 7.28 Overpressures recorded in the far end of the flame
Table 7.3 Results of the far field gas explosion simulations

<table>
<thead>
<tr>
<th>Case no.</th>
<th>Ignition location</th>
<th>Gas cloud no.</th>
<th>Overpressure without safety gap</th>
<th>Overpressure with 10m safety gap</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td></td>
<td>Flame propagation path 1</td>
<td>Flame propagation path 2</td>
</tr>
<tr>
<td>1</td>
<td>Module 1</td>
<td>4</td>
<td>4.11</td>
<td>3.84</td>
</tr>
<tr>
<td>2</td>
<td>Module 3</td>
<td>4</td>
<td>3.96</td>
<td>3.72</td>
</tr>
<tr>
<td>3</td>
<td>Module 3</td>
<td>3</td>
<td>5.00</td>
<td>4.71</td>
</tr>
<tr>
<td>4</td>
<td>Module 5</td>
<td>3</td>
<td>5.77</td>
<td>5.48</td>
</tr>
<tr>
<td>5</td>
<td>Module 7</td>
<td>2</td>
<td>6.55</td>
<td>6.47</td>
</tr>
<tr>
<td>6</td>
<td>Module 9</td>
<td>2</td>
<td>8.04</td>
<td>7.68</td>
</tr>
<tr>
<td>7</td>
<td>Module 9</td>
<td>1</td>
<td>5.17</td>
<td>4.79</td>
</tr>
<tr>
<td>8</td>
<td>Module 11</td>
<td>1</td>
<td>3.90</td>
<td>3.68</td>
</tr>
<tr>
<td>9</td>
<td>Module 12</td>
<td>4</td>
<td>1.47</td>
<td>1.89</td>
</tr>
<tr>
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<td>Module 10</td>
<td>4</td>
<td>1.47</td>
<td>2.04</td>
</tr>
<tr>
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<td>Module 10</td>
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<td>3.52</td>
<td>4.48</td>
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<td>3.80</td>
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<td>Module 6</td>
<td>2</td>
<td>3.51</td>
<td>4.43</td>
</tr>
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<td>Module 4</td>
<td>2</td>
<td>3.14</td>
<td>4.01</td>
</tr>
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<td>Module 4</td>
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<td>2.15</td>
<td>2.97</td>
</tr>
<tr>
<td>16</td>
<td>Module 2</td>
<td>1</td>
<td>1.98</td>
<td>2.66</td>
</tr>
</tbody>
</table>

For gas explosion flame propagating through path 1, it was seen in Figure 7.28 that the overpressures between the two FLNG layouts had two distinct group results. Precisely, for simulation group case 1 to 8, the gas explosion in the configuration without safety gap would produce greater overpressures in the far field than that in the 10m safety gap configuration. While the opposite observation was seen when it comes to the simulation group case 9 to 16, the overpressures in the 10m safety gap configuration became comparatively larger. It can be explained that the gaps between three adjacent modules (e.g. Module 3, Module 9 and Module 10) in N-S direction had different distance, which resulted in different flame turbulence interruption effects. For instance, it was seen in Figure 7.27(a) that the flame propagated from ignition point in Module 10 towards to
Module 3 firstly experienced a small safety gap, then it was followed by a greater distance safety gap between Module 9 and Module 3. While if the flame started oppositely from ignition in Module 3 towards to Module 10, different turbulence path order would be seen.

Unlike the flame path 1 simulations, the flame path 2 experienced the same safety gaps arrangement order regardless of the flame propagation direction in N-S, which was due to the geometrical symmetry of the cylindrical layout. Therefore, from case 17 to 24, the same overwhelming tendency was observed, namely, the far field overpressures without safety gap configuration exceeded the overpressures in the 10m safety gap counterpart.

Overall, the arrangement order and the distance of the safety gap between the modules played critical roles. In order to quantify the safety gap distance and investigate its effect systematically, the following two sets of artificial configurations were modelled.

### 7.5.2.1. Safety gaps between three congested regions

The artificial configurations with three congestions were firstly modelled to investigate the corresponding explosion scenario in Section 7.5.2 where the flame path 1 goes through two safety gaps (Figure 7.29). The FLACS models were mimicking tests extracted from the Research to Improve Guidance on Separation Distance for the Multi-energy Method (RIGOS)-research program (VandenBerg & Versloot 2003), the overpressure calculations for those artificial models were validated in previous work (Ma et al. 2014).

In FLACS, those three modules in Figure 7.29 were modelled with the same obstacle diameter of 19.1 mm and volume blockage ratios of 10.1%, and all obstacles were orientated orthogonally and regularly. The ignition locations in this study were in the centre of the donor on the left hand side of the configuration.

8 simulation scenarios with 8 different safety gaps were set up, for all those simulations, the overall distance from the donor to Acceptor 2 was fixed, while Acceptor 1 was at varying location in between the donor and Acceptor 2. Here, the safety gap distance ratio was defined as the ratio of the Safety gap 1 distance divided by the distance of Safety gap 2. Overpressure monitors were positioned at regular distances along the axis.
of the donor–acceptor configurations, the safety gap distance ratio increased from 0.11 in case (1) to 4.00 in case (8), as seen in Figure 7.30.

![Figure 7.29 Congested configurations with two safety gaps](image1)

![Figure 7.30 Overpressures in different configurations with two safety gaps](image2)

It was seen in Figure 7.30 that the overpressures in the farthest module – Acceptor 2 increased when Acceptor 1 was placing increasingly closer to Acceptor 2, the maximum
overpressures was observed in simulation case 8 which had the greatest safety gap distance ratio, the main reason is that the congestion volume in case 8 was increased once Acceptor 1 connects closer to Acceptor 2, the larger congestion volume leaded to longer flame turbulence path, thereby increasing the overpressure generation in Acceptor 2. Therefore, if the target of protection was Acceptor 2 in the farthest end, the solution in such case was to maximize the distance of Safety gap 2 which could tremendously decelerate the flame turbulence in the open space, such as the example case 1 shown in Figure 7.30.

For each accepting module, the overpressures in the monitor points were averaged and recorded in Figure 7.31. Unlike the overpressure increased in Acceptor 2, it was interesting to note that the averaged overpressure in Acceptor 1 decreased from case 1 to 7, which was due to the fact that the increasing Safety gap 1 between the donor and Acceptor 1 amplified the flame turbulence interruption effect.

Overall, depending on the overpressure safeguarding targets, different approaches are available in explosion mitigating. In the first place, in order to minimize the overpressure in Acceptor 2, the Safety gap 2 should have the greatest distance to discharge the flame turbulence and overpressure generation. Secondly, if the task is to safeguard Acceptor 1, sufficient distance of Safety gap 1 should be applied. Thirdly, if it is to balance the overpressures in Acceptor 1 and Acceptor 2, the optimal solution is to make the safety gap distance ratio equals to 1, namely Safety gap 1 and Safety gap 2 have the same distance, as seen the intersection point in Figure 7.31.
7.5.2.2. Safety gaps between four congested regions

Furthermore, the second set of artificial configurations with four congestions was modeled to investigate the corresponding explosion scenario in Section 7.5.2 where the flame path 2 propagated through three safety gaps (Figure 7.32). The obstacle diameter, arrangement and volume blockage ratio were same as they were in Section 7.5.2.1. The distance from the donor to Acceptor 2 was fixed, while the two congested regions in the middle moved oppositely so that Safety gap 1 equals to Safety gap 3. As seen in Figure 7.33, 8 simulation scenarios with 8 different safety gap distance ratios were modeled, the safety gap distance ratio was defined as the distance of Safety gap 1 divided by the distance of Safety gap 2.
As the simulation cases being conducted from (1) to (8) in Figure 7.33, the overpressures in the three safety gap configuration had obvious decreasing tendency in Acceptor 2 compared to the overpressure varying tendencies in Figure 7.30.

Figure 7.33 Overpressures in different configurations with three safety gaps ( barg)

However, it was seen in Figure 7.34 that the averaged overpressures in Acceptor 1 significantly increased along with the increase of the safety gap distance ratio from case 1 to 7, which means that although the safety gaps 1 and 3 effectively reduced the overpressures in the farthest field (Acceptor 2) by manipulating the middle congestions, Acceptor 1 on the other hand was subjected to greater explosion overpressures. Therefore, depending on the protecting target under such circumstances, the arrangement of safety gaps provided different overpressure mitigation solutions, the larger Safety gap 2 benefited Acceptor 1, such as case 1, whereas the greater distance of Safety gap 1 and 3 significantly mitigated the explosion overpressures in Acceptor 2. Amongst, the balanced overpressures in both accepting modules would exist at the intersection point in Figure 7.34 where Safety gap 2 was about 1.5 times greater than Safety gap 1 or 3, in such scenario the overpressures were alleviated in both Acceptor 1 and 2, as shown in the example - case 5.
7.5.3 Discussion

The investigation to the two sets of artificial configurations well illustrated the overpressure distribution phenomenon on the cylindrical FLNG platform in Section 7.5.2.

For the simulations of the artificial configurations with two safety gaps, in case 1 to 8 of Table 7.3, the cylindrical FLNG platform without safety gap was the equivalent scenario of case 8 in Figure 7.30, while the 10m safety gap cylindrical FLNG platform was the equivalent scenario of case 7. In those scenarios, the gas cloud was ignited in the donor (i.e. equivalent Module 3 on the cylindrical FLNG platform); Acceptor 1 and 2 were equivalent Module 9 and 10. It was seen in Figure 7.30 that if Acceptor 1 closely connected to Acceptor 2 in case 8, the overpressures in Acceptor 2 became greater than that in case 7. In other words, more safety gap space against Acceptor 2 in case 7 reduced overpressures in the accepting modules, which reflected the phenomenon that the far field overpressures were smaller on the cylindrical FLNG platform with 10m safety gap.

However, for simulations from case 9 to 16 in Table 7.3, the overpressure distribution phenomenon on the cylindrical FLNG platforms could be explained by corresponding scenario 1 and 2 in Figure 7.30 Overpressures in different configurations with two
safety gaps (barg). Comparing to scenario 2 in Figure 7.30, the closer distance between the donor and Acceptor 1 in scenario 1 generated smaller overpressures in the far end Acceptor 2 due to the greater distance of Safety gap 2. It was the equivalent scenario on the cylindrical FLNG platform where the ignition relocated to Module 10; the far field overpressures on the cylindrical FLNG platform with 10m safety gap were however greater.

In terms of simulation cases from 17 to 24 in Table 7.3, the flame propagated from the edge module through three safety gaps to the farthest module, which was the equivalent artificial model in Section 3.2.2. The cylindrical FLNG with safety gap of 10m equals to the simulation case 8 in Figure 7.33 where the safety gaps against the donor and Acceptor 2 effectively interrupted the flame turbulence and further mitigated overpressures in the far field.

In summary, 16 different artificial configurations were numerically simulated with different ignition locations and gas cloud on the cylindrical FLNG platform. It was concluded that the 10m safety gaps on the cylindrical FLNG platform effectively mitigated overpressures in the far field modules in most cases. However, the exception was seen in some scenarios that the overpressures were increased in the far field due to the flame turbulence interaction with the adjacent modules. Therefore, the solution to balance overpressures in all far field modules is to achieve the balancing safety gap distance ratio that safety gap plays the most efficient role in overpressure mitigation.

7.6 SUMMARY

In this Chapter, the safety gap had been applied on the cylindrical FLNG platform. The gas dispersion and explosion simulations had been both conducted to investigate the gas cloud size and overpressure mitigation effect of safety gap. The Data-dump guideline proposed in previous chapter had been utilized to ensure the overpressure calculation accuracy in gas explosion simulations.

In the gas dispersion analysis, different gas leakage manners had been investigated, the safety gap showed its efficiency in reducing gas cloud size. Especially in the gas dispersion scenario where the gas release impinges the nearby large object, the gas cloud size decreasing efficiency and ratio were higher in the cylindrical FLNG vessel.
compared to the traditional FLNG. However, in the far field, the stoichiometric gas cloud sizes could be slightly increased by applying the safety gap, which was due to the fact that far-field regions have more complicated congestion and confinement condition. Further investigation regarding the gas explosion therefore had been carried out to evaluate the gas dispersion consequences due to safety gap.

In the assessment of the gas explosion in the cylindrical FLNG with safety gaps, the Data-dump technique which improves the FLACS overpressure calculation had been applied. The overall results indicated that the larger of the safety gap, the more efficient of Data-dump technique to correct overpressure prediction.

In the near field gas explosion simulations, overpressure mitigation phenomenon had been observed by applying the safety gap into the congested regions. The increase of safety gap size resulted in greater overpressure reduction in the accepting modules, and the safety gap reduced more overpressures where the average obstacle diameter was smaller, the flame path was longer and the congestion ratio was greater.

In terms of the far field gas explosion, the simulations had been conducted on two cylindrical FLNG configurations with and without 10m safety gap in North-South direction. For each FLNG configuration, 24 simulations with 4 different gas clouds and 6 different ignition locations were carried out. The corresponding artificial models well demonstrated that depending on the ignition and the overpressure mitigation target locations, the safety gaps played different roles in reducing overpressures. Overall, in order to optimize the gas explosion alleviation effect of safety gap on the cylindrical FLNG platform, the effective way is to balance overpressures in all far field modules and to achieve the balancing safety gap distance ratio accordingly.

However, this chapter only demonstrated limited cases of gas dispersion and explosion simulations, even though safety gap tended to effectively mitigate both gas cloud size and explosion overpressures in most cases. Some exceptions still could be seen that the safety gap actually increased explosion overpressures. Therefore, in order to conduct a more robust gas explosion mitigation design, a probabilistic study is recommended, the overall exceedance curve in the probabilistic study can provide the most convincing results. In Chapter 8, an example of the probabilistic study regarding the optimization of the gas explosion mitigating measure was carried out.
CHAPTER 8. EVALUATION OF BLAST WALL EFFECT ON OVERPRESSURE MITIGATION

8.1 INTRODUCTION

In this chapter, the investigation of overpressure mitigation due to blast wall on the cylindrical FLNG platform was conducted. Three major purposes for this study are 1) firstly, in the previous chapter, the safety gaps were designed to separate the congested modules in the N-S direction since there was sufficient space in such direction on the cylindrical FLNG platform, whereas the available open space in the E-W direction was limited, which confined the applicability of safety gap. Therefore, blast wall turns out to be more feasible in the N-S direction to mitigate gas explosion. 2) Secondly, in order to consider more variabilities, such as different leakage rates and leakage locations, a probabilistic study was performed in this chapter. 3) Finally, this study was carried out to demonstrate the general procedure of the optimization of overpressure mitigating measures by comparing a series of different blast wall designs in the explosion risk analysis.

In order to restrict the spread of flammable fuel-air cloud and control the generation of turbulence in a gas explosion, the blast wall has been designed and widely used as one of the efficient explosion mitigating approaches in the oil and gas industry. For example, the living quarters on an offshore platform can be separated from the process modules by using a blast wall, thereby mitigating the consequences of a possible gas explosion.

There have been an extensive amount of studies regarding the structural analysis and design of blast walls (Louca et al. 2004; Langdon & Schleyer 2005; Langdon & Schleyer 2006; Schleyer et al. 2007). However, most studies were about the blast wall material and structural response analysis, while there is very little concern about the overpressure consequences of the arrangement design of blast wall. Moreover, the existing offshore applications of blast walls were tend to be in some traditional structures (Boh et al. 2007; Louca et al. 1996; Sohn et al. 2013), such as FPSO (Sohn et al. 2013), but very limited research has been done on the innovating offshore structure - FLNG, let alone the cylindrical-shaped FLNG developed in this thesis.
Here, the same cylindrical FLNG platform evaluated previously had also been investigated in this chapter. Overall, 4 varying leakage rates, 2 opposite leakage directions, 3 different gas release locations, 4 sets of blast wall configuration designs plus the original platform configuration without blast walls constituted 120 gas dispersion simulation scenarios in this study. For each module on the cylindrical FLNG platform, more than 50 monitor points had been assigned to record the overpressures in the gas explosion analysis. Therefore, the probabilistic study in this chapter had simulated over 3000 gas explosion cases in the overpressure mitigating design.

8.2 INPUT AND ASSUMPTIONS

The major variations in gas dispersion and explosion study included leak rate, leak direction, leak location, gas composition and wind condition. In this study, the investigation into blast wall’s explosion mitigating effect was only in \( E-W \) direction, and the protection target - living quarter was in the very east side (Figure 8.1). Therefore, the input of wind speed and wind direction were fixed as +4 m/s from west to east direction (+ x coordinate in CASD of FLACS) to examine the worse gas dispersion scenarios with such wind condition, and leak directions were modelled in both east and west directions.

In terms of leak locations, three critical locations in Figure 8.1 were numerically modelled. Accordingly, the leak rates from 0.25kg/s to 96kg/s were chosen in the gas dispersion simulations, as seen in Figure 8.2.
In the simulations of dispersion leaks and explosion gas clouds, the inventory of gas composition inside the cylindrical FLNG were summarize in Table 8.1
Table 8.1 Gas composition for dispersion and explosion study

<table>
<thead>
<tr>
<th>Component</th>
<th>Export gas</th>
</tr>
</thead>
<tbody>
<tr>
<td>Methane</td>
<td>27%</td>
</tr>
<tr>
<td>Ethane</td>
<td>33%</td>
</tr>
<tr>
<td>Propane</td>
<td>15%</td>
</tr>
<tr>
<td>Hexane</td>
<td>19%</td>
</tr>
<tr>
<td>CO₂</td>
<td>6%</td>
</tr>
</tbody>
</table>

8.3 GEOMETRY

Geometries for the blast wall investigation in this chapter were based from the previous study in Chapter 7. Figure 8.3 provided the general overview of the geometry with blast wall installation in front of the congested modules.

As seen in Figure 8.4, various blast wall configurations were inserted next to the congested modules. Since the wind was blowing from west to east and the aim in this study is to block the flammable gas cloud propagating to the living quarter in the east end, therefore, blast walls were not installed on the left hand side of the congestion in the west. Four essential blast wall configurations were designed, the red colour
represents blast wall in the plane view of the cylindrical FLNG platform, as shown in Figure 8.4.

![Figure 8.4 Blast wall arrangement designs](image)

### 8.4 DISPERSION AND EXPLOSION ANALYSIS

#### 8.4.1 Dispersion analysis

In this study, the 8 categories leak rate ranges in Figure 8.2 had been included, however, the leak rates below 12kg/s for all the blast wall configurations had little difference of flammable gas cloud size due to the fact that the low leak rates resulted in too lean gas cloud, which even could not reach the stoichiometric gas concentration. Therefore, only 4 leak rates above 12 kg/s were defined to study the possible gas volume buildup in the comparison and design of the blast wall configurations. Besides the variations of 3 leak locations and 2 leak orientations, Table 8.2 presented the overall leak cases used in this chapter. The gas monitor region covered all the modules on the cylindrical FLNG platform, as seen in Figure 8.5.
Table 8.2 Various leak cases determined for dispersion study

<table>
<thead>
<tr>
<th>Case</th>
<th>Wind direction</th>
<th>Wind speed (m/s)</th>
<th>Leak rate (kg/s)</th>
<th>Leak position</th>
<th>Leak orientation</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>West to East</td>
<td>4</td>
<td>12, 24, 48, 96</td>
<td>West end</td>
<td>Along and opposite wind</td>
</tr>
<tr>
<td>2</td>
<td>West to East</td>
<td>4</td>
<td>12, 24, 48, 96</td>
<td>Middle</td>
<td>Along and opposite wind</td>
</tr>
<tr>
<td>3</td>
<td>West to East</td>
<td>4</td>
<td>12, 24, 48, 96</td>
<td>East end</td>
<td>Along and opposite wind</td>
</tr>
</tbody>
</table>

8.4.2 Results and discussion from dispersion simulations

Figure 8.6 demonstrated four gas dispersion simulation outputs for gas releases in different blast wall configurations. For these scenarios, the leak rates were set as 48kg/s, leak directions were assigned along with the wind direction from west to east, and leaks were placed on the ground in the middle module.
The comparison of the flammable gas cloud was conducted within the Equivalence Ratio (ER) range of Lower Flammability Limit (LFL) 0.5 to Upper Flammability Limit (UFL) 2.5. The time was terminated at 210s when the stoichiometric gas cloud reached the steady status. It was seen in Figure 8.6 (a) and (b) that the blast walls in the east end effectively restrained the propagation of the gas cloud. In terms of the other two blast wall configurations in Figure 8.6 (c) and (d), the additional blast walls on the left hand side of the east end further reduced the cloud size near the living quarter. However, the gas cloud was more condensed in the middle module of the FLNG platform. The rich gas concentrations in these two configurations resulted in the ER going beyond the upper flammability limit, which on the other hand reduced the overall stoichiometric gas cloud size. Therefore, according to the comparison, the extra blast walls benefited the mitigation of the total gas cloud size in gas dispersion simulations. Nevertheless, the comparison here was only carried out for 4 typical cases of the overall 24 scenarios at
48kg/s in Table 8.2. In order to investigate all leak rate cases thoroughly, the overall exceedance curve of gas cloud sizes within the gas monitor region was summarized in Figure 8.7. The gas dispersion simulations for the original cylindrical FLNG without blast wall were also performed, the exceedance curve was obtained by sorting the gas cloud size from small to large, and equal leak frequencies were assigned to all leaks.

**Figure 8.7 Exceedance curve of gas cloud sizes for all leak rate scenarios**

**Figure 8.8 Exceedance curve of gas cloud sizes over 3×10^4 m^3 for different leak rate scenarios**
As seen in Figure 8.7, for stoichiometric gas cloud size smaller than $3 \times 10^4 \text{ m}^3$, all blast wall designs would have greater frequencies compared to the original cylindrical FLNG without blast wall. In other words, the blast walls magnified the gas cloud size, which was detrimental. However, it has to be pointed out that all these enlarged gas clouds were for the leakages at leak rates of 12kg/s and 24kg/s only; they were much smaller than the clouds in 48 kg/s and 96 kg/s gas release cases, which might not generate high overpressures in gas explosion to threaten structures and people. By contrast, for the gas dispersions at leak rate 48kg/s and 96kg/s, a distinct tendency of cloud size restriction was seen in Figure 8.8. It was shown that most of the blast wall configurations possessed lower frequencies at the same stoichiometric gas cloud size. For instance, at gas cloud size of $4.7 \times 10^4 \text{ m}^3$, the exceedance probability for blast wall design 4 was only 12% while it was 24% for the original configuration without blast wall. That is, if the exceedance probability was read at 24%, the gas cloud size of $4.7 \times 10^4 \text{ m}^3$ could be reduced to $4.1 \times 10^4 \text{ m}^3$, which was 12.8% reduction, by using the blast wall design 4.

![Figure 8.9 Averaged gas cloud size for different leak rate scenarios](image)

In order to have a straightforward understanding of the gas cloud mitigating effect of blast walls at different leak rates, the stoichiometric gas clouds at all locations were
averaged and displayed in Figure 8.9. It was seen in the figure that the blast walls only played active roles in gas cloud reducing when leakage rate went over 48kg/s. In such condition, blast wall design 2 and 4 had the smallest stoichiometric gas cloud sizes. However, blast wall design 2 was the most disadvantageous configuration if the leak rate was 24kg/s. Therefore, blast wall design 4 was generally the optimal design amongst all the configurations in terms of reduce the overall gas cloud size.

The gas dispersion simulations above provided the blast wall design comparison of stoichiometric gas cloud sizes. So far, according to the exceedance curve and the chart of the averaged gas could sizes for these four leak rate scenarios, the conclusion was that blast walls were meaningless and even detrimental under low leak rate (<24kg/s), whilst the appropriate blast wall designs could effectively mitigate the gas cloud size if the leak rate reached 48kg/s. However, for these low leak rate gas dispersions, the gas cloud sizes were all below $3 \times 10^4$ m$^3$, which highly likely had very little effect on gas explosion overpressures. Whereas for high leak rate cases, the reduction of gas cloud size in large volume would probably contribute to significant different in gas explosion overpressures. Therefore, the further investigation into gas explosion was required and carried out in the following section based on the gas dispersion data.

### 8.4.3 Explosion analysis

Explosion simulations were performed using gas cloud data resulted from dispersion simulations with leak rates 12 kg/s to 96 kg/s. The gas clouds were situated in 4 different locations covering the entire platform so that the overall gas explosion consequences for all modules could be analyzed (Figure 8.10). For all gas clouds, the plan view sizes were all fixed as $100 \times 80$ m$^2$, while the heights of clouds were varying, which was according to the gas dispersion results obtained previously. For each gas explosion simulation, the gas cloud was ignited in the ground center of each module.

It was seen in Figure 8.10 that each gas cloud covered 4 modules, about 200 monitor points were homogeneously assigned on the ground to record the overpressures in a gas explosion simulation. By taking all different gas leak rate scenarios, gas cloud sizes and locations into account, for each blast wall configuration investigation, more than 3000
VCE overpressures were monitored in this probabilistic study regarding the gas explosion simulations.

8.4.4 Results and discussion from explosion simulations

8.4.4.1. The integral analysis of gas explosion overpressures

Figure 8.11 showed the overpressure output of one of the gas explosion simulations for the original FLNG platform without blast walls. The gas cloud size utilized in this explosion simulation was corresponding to the dispersion simulation with the leak rates of 48kg/s in Section 8.4.2. The ignition was in the center of the gas cloud located in the east end of the platform. The overpressures were displayed in range of 0.1 to 2 bar, the red color represented the maximum overpressure. It was seen that the gas explosion blast was spreading from the ignition center to all surrounding objects, the maximum overpressures were observed in congested region near the edge of the gas cloud, and the living quarter was subjected to about 1.0 bar overpressure.

The comparison between the original platform and the blast wall configurations was then conducted and shown in Figure 8.12. The blast wall configuration 1 in Figure 8.12
(a) effectively reduced the overpressure from 1.0 bar to 0.6 bar near the living quarter, while an extra blast wall installed in the Design 2 in Figure 8.12 (b) restricted more overpressures in the neighboring area of the living quarter. However, the overpressure concentrations were seen at the blast walls, over 2 bar overpressure could be reflected to the confined modules from the blast walls unavoidably.

![Figure 8.11 Gas explosion simulation of the original platform without blast wall configurations](image)

Job=200008, Var=PMAX barg. Time= 1.795 (s).
XY plane, Z=101 m

(a) Design 1. Blast walls on 1 sides  
(b) Design 2. Blast walls on 2 sides
The issue of overpressure concentration was considerably addressed by adding additionally blast walls, as seen in Figure 8.12 (c) and (d). More importantly, the living quarter was more efficiently protected that the overpressures were eliminated to below 0.1 bar by using the blast wall configuration 4 in Figure 8.12 (d). However, the above comparison of these design were deterministic analysis for one of the worst scenarios.

In order to consider all gas dispersion output as input in the gas explosion simulations, more than 120 explosion cases were numerical modelled and the overall exceedance curve of gas explosion simulations was summarized in Figure 8.13. The equal frequencies were allocated to all monitored overpressures, which were sorted from small to large.
Figure 8.13 Exceedance curve of overpressures for all leak rate scenarios

Figure 8.13 showed the integral overpressure data for all blast wall designs. Regardless of the varying leak rate consequences, all blast wall configurations were negative in explosion mitigation if the resulting overpressures were below 0.3 bar. Whereas if the gas explosions result in overpressures over 0.3 bar, there were more than 50% possibilities that blast wall design 2, 3, 4 were beneficial to the integral structures, namely, all of the recorded overpressures could be mitigated by using blast walls. It was noted that the overpressures over 0.3 bar had already reach the detonation status of a gas explosion, which was more destructive to structures and personals compared to the deflagration. Therefore, blast wall configurations of 2, 3, 4 here were meaningful in gas explosion mitigation for gas explosions with detonation overpressures over 0.3 bar. Amongst the designs, the blast wall configuration 4 with lowest exceedance frequencies in Figure 8.13 was the optimum.

The individual exceedance curves of VCE overpressure resulted from all different leak rate scenarios were shown from Figure 8.14 to Figure 8.17. For leak rate of 12 kg/s, the gas dispersion simulations resulted in around $0.5 \times 10^4$ m$^3$ averaged gas cloud size
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(Figure 8.9), which was less than one-tenth of that \(6 \times 10^4 \text{ m}^3\) from the gas dispersion of leak rate 96kg/s (Figure 8.9). The comparably much smaller gas cloud size from the leak rate of 12 kg/s generated VCE overpressures smaller than 0.2 bar for all blast wall configurations as seen in Figure 8.14, and the overpressures obtained from the original cylindrical FLNG platform without blast walls were even too small to display in the figure. Moreover, the exceedance frequencies for all blast wall configurations were virtually below 15% for overpressures between 0.08 and 0.2 bar. In such case, although these blast wall configurations had higher exceedance frequencies than the original platform, the overpressures produced from these small gas clouds would have too little impact on the structures. Therefore, these negligible overpressures with low exceedance probabilities should not be major interest to further study in this Chapter.

![Figure 8.14 Exceedance curve of overpressures for leak rate at 12 kg/s](image)

**Figure 8.14 Exceedance curve of overpressures for leak rate at 12 kg/s**
Figure 8.15 Exceedance curve of overpressures for leak rate at 24 kg/s

By contrast, the benefits of blast walls were seen in Figure 8.16 and Figure 8.17 where the gas clouds used in the gas explosion simulations were from leak rates of 48 kg/s and 96 kg/s. It was shown in Figure 8.16 that blast wall design 2 and 3 had slightly lower exceedance frequencies than the original configuration without blast wall, while blast wall design 4 showed its distinct advantage of overpressure mitigation that most of the overpressures in this design had much lower frequencies than others. Figure 8.17 further indicated the VCE overpressure alleviation benefits of blast walls in the 96 kg/s leak case. The overpressure mitigation efficiency was especially obvious for blast wall configuration 2 and 4. For instance, as seen in Figure 8.17, the exceedance probabilities of overpressures over 0.4 bar in the originally no-blast-wall platform could be significantly dropped from 83% to 63% by using blast wall configurations 2 and 4.
Figure 8.16 Exceedance curve of overpressures for leak rate at 48 kg/s

Figure 8.17 Exceedance curve of overpressures for leak rate at 96 kg/s
Therefore, according to the exceedance curves above, it could be concluded that blast wall configurations were only effective in VCE overpressure mitigation for overpressure over the detonation status, blast wall configuration 2 and 4 were particularly beneficial in overpressure mitigation for large leak rate scenarios. Additionally, Figure 8.18 compared all blast wall designs by averaging all monitored overpressures in all different leak rate scenarios. Although it was mentioned that blast wall configuration 2 and 4 were the most effective blast wall design in large leak rate gas releases, blast wall configuration 2 surprisingly increased the averaged overpressures for the integral structure at leak rate of 24 kg/s, by contrast, blast wall configuration 4 was consistent in overpressure mitigation under the same circumstance. Consequently, blast wall design 4 was again approved to be the optimal integral design.

### 8.4.4.2. Gas explosion overpressures to the living quarter

Besides the integral analysis of the explosion overpressure distribution in the entire structure, another major interest of this study is to investigate the protection target –
living quarter. For each gas explosion scenario, 10 monitor points were assigned near the living quarter to record the overpressures.

Figure 8.19 showed the averaged overpressures from all monitors around the living quarter in different leak rate scenarios. The similar overpressure mitigation effect of blast wall designs in large leak rate (48 kg/s and 96 kg/s) was seen. However, comparing to the overpressure mitigation in the integral structure in Figure 8.18, the blast wall configuration 4 more considerably reduced the averaged overpressure around the living quarter at 96 kg/s leakage case.

Furthermore, the detailed explosion frequency calculation was performed in this section. The exceedance frequency of overpressure at the living quarter were calculated by using the monitored overpressures over 1000 scenarios along with the leak frequencies and ignition probability determined in the previous report (Gexcon 2012).

Table 8.3 indicated the ignition intensity for all the releases from 12 kg/s to 96 kg/s in this study. The exposed area in the CFD simulations was corresponding to the area on
the vessel exposed to Equivalent Stoichiometric gas Cloud (ESC) from a release in the liquefaction module, according to the Guidelines for quantitative risk assessment Purple Book (Uijt & Ale 2005), the leak frequency was taken as $3.3 \times 10^{-1}$ per year. Moreover, based on the ignition intensities and the previously performed dispersion simulations, the ignition probability was determined as 0.36%. The explosion frequency was calculated by multiplying the leak frequency and ignition probability, therefore, the total explosion frequency was approximated as $1.2 \times 10^{-3}$ per year.

Table 8.3 Discrete and continuous ignition intensities for releases (> 6 kg/s)

<table>
<thead>
<tr>
<th>Discrete</th>
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<th># items or sq. meter</th>
<th>Adjust</th>
<th>Discrete</th>
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<td>Age</td>
<td>Maintenance Manning Technology Overall</td>
<td>Module</td>
<td>Total</td>
</tr>
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<td>Pump</td>
<td>2.10E-07</td>
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<td>0.46</td>
<td>1.54E-08</td>
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<td>Compressor</td>
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<td>0.00E+00</td>
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<tr>
<td>Electrical eq. *</td>
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<td></td>
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<td>2.16E-04</td>
</tr>
<tr>
<td>Other equipment *</td>
<td>2.10E-08</td>
<td></td>
<td>0.49</td>
<td>1.68E-05</td>
</tr>
<tr>
<td>Other **</td>
<td>1.70E-08</td>
<td></td>
<td>0.49</td>
<td>1.17E-04</td>
</tr>
<tr>
<td>Personnel *</td>
<td>4.00E-08</td>
<td></td>
<td>0.49</td>
<td>3.76E-04</td>
</tr>
<tr>
<td>* per m² exposed to gas</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Continuous</th>
<th>Adjustment Factors for Ignition Source Categories</th>
<th># items or sq. meter</th>
<th>Adjust Continuous</th>
</tr>
</thead>
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<tr>
<td>Gas</td>
<td>Age</td>
<td>Maintenance Manning Technology Overall</td>
<td>Module</td>
</tr>
<tr>
<td>Hot work (# hours per 365*24h)</td>
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<td></td>
<td>-</td>
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<td>Electrical equipment *</td>
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<tr>
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<tr>
<td>Personnel *</td>
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<td>0.49</td>
</tr>
<tr>
<td>* per m² exposed to gas</td>
<td></td>
<td></td>
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</tbody>
</table>

Figure 8.20 Exceedance curve of overpressures around the living quarter for all leak rate scenarios
Consequently, the explosion risk regarding the living quarter subjected to overpressures of VCE from the liquefaction modules was conducted, the probability of exceedance curves with a frequency of $10^{-4}$/year was shown in Figure 8.20. Unlike the previously analyzed integral overpressure distribution on the entire FLNG platform, the leak frequency and ignition probability were considered in the explosion risk analysis for the specific target - the living quarter. For each group of investigations (blast wall configurations), the overpressure occurring frequencies could be used as the index for engineers to choose the appropriate blast wall design more confidently. For example, for 0.1 bar explosion design, it was seen in Figure 8.20 that the overpressure exceedance frequency from the original platform could greatly fall from $4.5 \times 10^{-5}$ per year to only $0.7 \times 10^{-5}$ per year by using the optimal blast wall design – configuration 4. Overall, the explosion risk analysis in this chapter provided a solid explosion design process, and the optimization of blast walls was completed by comparing four groups of critical blast wall arrangements according to the exceedance curves.

8.5 SUMMARY

Unlike the worst scenario analysis of VCE overpressure mitigation design in Chapter 7. A probabilistic study regarding the gas dispersion and explosion risk on the blast wall configurations had been conducted in this chapter.

FLACS had been utilized to model the blast walls on the cylindrical FLNG platform; four sets of different blast wall configurations had been established for gas dispersion and explosion comparisons. The explosion risk assessment in this study was carried out as a follow-up overpressure mitigation analysis on the cylindrical FLNG platform after Chapter 7. Since the $N$-$S$ direction with safety gaps had been investigated previously, blast walls here were installed in the $E$-$W$ direction to address the issue of space limitation. Therefore, some critical assumptions had been made in this chapter that wind direction and speed were fixed at certain values, leak directions were also assigned in the $E$-$W$ direction, correspondingly.

Over 120 gas dispersion simulations had been carried out, the results indicate that all blast wall designs were not beneficial for stoichiometric gas cloud size restriction if the leakage rate was below 24 kg/s, and only slight reduction of cloud size could be seen for
blast wall configuration 2 and 4 in large rate scenarios. The investigation of gas explosions with over 3000 scenarios had later been performed; the gas dispersion data had been used as the input in the explosion simulations.

Globally, the integral analysis of gas explosion overpressures in the blast configurations showed that additional blast walls could mitigate more overpressures on the entire FLNG platform. Even though some overpressure enhancement and exceedance frequency increase could be seen in low leak rate scenarios (12 kg/s and 24 kg/s), the overpressures in these scenarios were mostly under detonation status with low possibilities (below 15%), which were nearly negligible in structural analysis. By contrast, in the large gas releases (48 kg/s and 96 kg/s), blast wall played significantly positive role in VCE overpressure mitigating, in some cases, No. 4 blast wall design could considerably decrease the overpressure exceedance frequency by 20%.

Locally, the optimization design of blast wall was further conducted by using the detailed explosion frequency calculation for the living quarter, which was of the greatest interest to be protected. The leak frequency and ignition probability had been taken into account, the exceedance curve of overpressures for the living quarter had further validated the effectiveness of overpressure mitigation by using blast walls, and blast wall configuration 4 had been again approved to be the optimum.

To sum up, the optimization of the blast wall design on the cylindrical FLNG platform had been carried out globally and locally for different objects. The results from the gas dispersion analysis indicated that blast walls are detrimental to the structures in the gas release scenarios with low leak rate. Whereas, in the gas explosion analysis of large leak rate scenarios, blast walls played active role in explosion overpressure mitigation. Comparing to the worst case/deterministic study, the probabilistic study in this research had considered more uncertainties and provided more convincing and reliable data in the general process of explosion design. Overall, this chapter provided the practical information of up-to-date safety evaluation procedure for engineers to optimize the safety design in gas explosion mitigation, however, the subsequent studies on the material testing, structural behaviour of the blast wall and the structural response of the integrated structure would be conducted in future work.
CHAPTER 9. CONCLUDING REMARKS

9.1 MAIN FINDINGS

This thesis had conducted the gas dispersion and explosion analysis by using the CFD simulations. Comparing to most of previous work done by others, which were empirically conducted for traditional offshore structures, such as fix platforms and FPSOs. The main significance of this study is that the quantitative explosion risk assessment by using CFD was carried out for the innovative offshore structures (e.g. the cylindrical FLNG transformed from the large-scale FLNG in ship shape). A newly developed explosion calculation correlation - CSC, which has the rapidness of the phenomenological methods and the numerical simulation accuracy of the CFD software – FLACS, had been proposed in this study. The application of CSC had been validated in the explosion simulations in a series of realistic offshore structures. Additionally, an innovating FLNG vessel with a cylindrical platform had been numerically modelled as the gas explosion/fire safety-investigating object, the explosion mitigations, such as safety gap and blast wall had been designed on the cylindrical FLNG platform. The gas dispersion and explosion simulations had been carried out to investigate the overpressure mitigation effectiveness of safety gap and blast wall. The major contributions and finding made in this research are summarized below.

(1) A newly developed model (confinement specific correlation), which consists parameters of volume blockage ration, the density of the gas, the flame path distance, the confinement ratio and the laminar flame speed of the flammable gas had been proposed as a non-dimensional alternative and it showed a closer correlation with detailed CFD simulation in general particularly for realistic geometries. A linear least square method had been used to achieve the best fitting parameters by applying the validated commercial software FLACS. About 400 CFD cases with homogeneous congestions had been modeled using FLACS for testing both the GAME correlation and the confinement specific correlation (CSC). In addition to those 400 CFD homogeneous cases, around 700 realistic cases in ten different module scenarios of a Liquefied Natural Gas (LNG) train along with three simplified models had been simulated to validate the CSC; it was found that the
CSC was applicable to both realistic modules with irregular obstacles and homogeneous artificial modules.

(2) The CSC correlation had been utilized to investigate the irregularly structured modules with a high degree of inhomogeneity in confinement and congestion. Little experiments have been done for such configurations with large-scale inhomogeneity; the numerical simulations with over 400 scenarios had been performed by using CFD. By comparing the CSC results with FLACS and GAME data, it was found that the overpressure calculations by CSC in these deliberately varied geometries still better agreed with CFD simulations rather than GAME suggesting. A structural damage level approximation process, which requires an increased level of overpressure calculation accuracy, had also been proposed. The importance of CSC in providing accurate gas explosion approximation had been further validated.

(3) Prior to the investigation of safety gap, which is one of the most efficient gas explosion mitigating approaches, a Data-dump technique that assures the CFD simulation accuracy was proposed. This study also reported a comparison of simulations and published data from experiments carried out by TNO Prins Maurits Laboratory on geometric configurations that involved safety gaps of various separation distances. In the majority of cases, good agreement had been found between the simulated results and those obtained by experiment in both the donor and acceptor modules. However, a large discrepancy in the overpressures in the acceptor module had been seen when the size of the separation gap approached one or two times of the module size. A Data-dump technique had been used in this study to reset the turbulence length scale for these cases with different separation distances, five sets of explosion scenarios had then been numerically simulated and the overpressures had been compared with experimentally measured explosion overpressures. The overall results indicated that the software with the Data-dump technique was still an extremely effective tool when it comes to the evaluation of gas explosion overpressures in areas with large separation gaps.

(4) Safety gap had been utilized in the gas explosion analysis on a cylindrical FLNG platform. The traditional ship-shaped and a circular FLNG platforms had been numerically modelled and used for the detailed CFD analysis. While others had
investigated the structural and hydrodynamics advantages of mobility, stability and cost efficiency, etc. on the traditional ship-shaped FLNG, this study was carried out to analyse the safety of gas dispersion and explosion on the cylindrical FLNG. For gas dispersion simulations, the overall results indicated that the safety gap was effective in reducing the gas cloud size in both FLNG configurations, however, when it comes to the gas dispersion in the far field against the leakage point, the safety gap increased the gas cloud size in the cylindrical FLNG vessel on the contrary. In terms of gas explosion simulations, it was concluded that the safety gap was effective in reducing overpressures in two adjacent congestions; while for the explosion scenario where the flame propagated through several safety gaps to the far field congestion, the safety gap only effectively mitigated overpressures in the certain explosion protecting targets. Two series of configurations with different gas cloud locations and ignitions had been modelled, and the optimal explosion alleviation way by using safety gap on the cylindrical FLNG platform had been achieved by balancing the safety gap distance ratio in the congested regions.

(5) A probabilistic study to perform the comparison of another explosion mitigation design – blast wall was conducted. Blast wall has been commonly using as one of the effective overpressure reduction measures in the offshore industry; it restricts the propagation of flammable gas mixture and blocks the turbulence and combustion in gas explosion. In order to evaluate these characteristics of blast wall, the gas dispersion and explosion simulations had been conducted; blast walls had been designed in 4 different ways and installed in \(E-W\) direction on the cylindrical FLNG platform. The variations including leakage rate, leakage location, gas composition, leak frequency and ignition probability, etc. had been taken into account in the gas dispersion and explosion risk analysis. The exceedance curves had been summarized both globally and locally for all congested modules, the optimization of blast wall designs had been obtained by reading the exceedance curves that blast wall configuration 4 was the optimal design in gas explosion mitigation. Overall, the gas dispersion and explosion analysis in this study had provided a general risk assessment procedure, which delivers reliable results to design the proper blast walls in a realistic and cutting-edge offshore structure – cylindrical FLNG.
9.2 RECOMMENDATIONS FOR FUTURE WORK

Further investigation could be made in the future study as follow.

(1) The new correlation– CSC was only applicable to the explosion approximation for propane and methane explosions. This study could be followed by testing and checking more different explosion sources. In addition, one significant parameter - confinement in CSC was investigated based on the realistic offshore structures with high confinement ratio over 0.7; further study about the confinement can be conducted in lesser-confined structures. Finally yet importantly, it would be beneficial to develop the CSC by comparing to further experiments.

(2) The data-dump technique corrects the calculation of turbulence length scale in explosion simulations in large safety gaps, which was thoroughly validated by experiments in this research. However, the quantitative relationship between the safety gap and the congestion condition, such as the scale of the congestion, flame propagation distance and the coverage of gas cloud within the congestion, has not been discussed yet. Future work regarding these variations in safety gap analysis can be performed.

(3) The cylindrical FLNG platform was used for the studies regarding the design of safety gap and blast wall. The detailed deterministic analysis of safety gap and the probabilistic study of blast wall configurations both showed that these two explosion mitigating measures were effective in controlling gas cloud size and mitigating explosion overpressures by using appropriate designs. Even though the risk analysis and explosion design conducted in this research were the commonly used safety analysis procedures that can provide dependable results for offshore safety engineers in engineering judging, the cylindrical FLNG platform however is still a conceptually designed structure, which requires further industrial validation. Moreover, the safety gap and blast wall were investigated separately due to the limitation of the geometries in this research, future work regarding the explosion mitigation assessment with the combination of safety gap and blast wall could be performed. Finally yet importantly, the structure damage analysis based on the predicted overpressure also would be conducted in the future.
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