

THESIS FOR THE DEGREE OF DOCTOR OF PHILOSOPHY

Blast and impact loading on reinforced concrete structures

From vapour cloud explosion assessment to energy absorption capacity of
restrained members

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Division of Structural Engineering

CHALMERS UNIVERSITY OF TECHNOLOGY

Gothenburg, Sweden 2026

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ISBN 978-91-8103-450-9

Acknowledgements, dedications, and similar personal statements in this thesis, reflect the author's own views.

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Doktorsavhandlingar vid Chalmers tekniska högskola

Ny serie nr 5907

ISSN 0346-718X

<https://doi.org/10.63959/chalmers.dt/5907>

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Cover:

Contour plot of major strains obtained through Digital Image Correlation illustrating crack development in a restrained reinforced concrete beam subjected to impact loading. The image shows the results at maximum deflection during the second impact on specimen IIIB-04, as reported in Paper VI.

Printed by Chalmers Digital Printing

Gothenburg, Sweden 2026

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Abstract

Extreme dynamic loads (such as blasts and impacts), though infrequent, pose a serious threat to public safety and critical infrastructure. Moreover, the risk associated with such loads is connected to modern society's demands, including transport of dangerous goods and densification of urban environments. Properly addressing this risk requires understanding both the development and propagation of blast loads and associated effects, as well as the response of reinforced concrete (RC) structures under impulsive actions. This research integrated these aspects by examining blast generation from vapour cloud explosions (VCEs) in traffic environments and the behaviour of RC elements subjected to blast and impact loading.

The first part examined VCEs in scenarios involving groups of vehicles. Computational fluid dynamics was employed to investigate how parameters related to the traffic configuration and the premixed fuel-air cloud influence the severity of the explosion. Based on these results, practical recommendations for applying a simplified approach—the Multi-Energy Method—in such environments were developed, providing a reliable and consistent basis for blast load estimation in vehicle-populated environments.

The second part of the thesis focused on the behaviour of RC structures under impulsive loading, with a general focus on ductile failure modes that enable higher energy absorption capacity. Initially, the risk of shear failure (brittle) occurring prior to bending failure (ductile) in slender RC one-way slabs subjected to blast loading was examined. Although flexural failure was found to be the more probable response mode for the studied slabs, a reliability-based overstrength factor was proposed to further increase the probability of achieving a flexure-dominated response. Thereafter, the impact response of RC one-way elements under different boundary conditions was investigated, including the effects of membrane action and repeated loading. The results showed that the energy dissipated under impact and subsequent static loading exceeded the energy absorption capacity under static loading only, indicating potential for more optimal structural designs regarding impulsive actions. In addition, membrane action was observed to enhance both the load-carrying capacity and deformation limit of restrained RC elements under static and impact loading. Finally, different simplified methods for predicting key aspects of the dynamic response were developed and validated, thereby supporting the analysis and assessment of impact-loaded RC structures.

Keywords: Vapour cloud explosions, urban road environments, computational fluid dynamics, premature shear failure, blast loading, impact loading, RC beams, membrane action, energy absorption capacity

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Sammanfattning

Extrema dynamiska laster (såsom explosioner och stötar), även om de förekommer sällan, utgör ett allvarligt hot mot allmän säkerhet och kritisk infrastruktur. Risken förknippad med sådana laster är kopplad till det moderna samhällets behov, bland annat genom transport av farligt gods i kombination med förtätning av befinnliga stadsmiljöer. För att på ett ändamålsenligt sätt hantera denna risk krävs förståelse för både uppkomst och utbredning av explosionslaster liksom för strukturens respons hos armerade betongkonstruktioner utsatta för impulsbelastning. I detta forskningsarbete integrerades dessa aspekter genom att undersöka explosionsförlopp vid gasmolnsexplosioner (*vapour cloud explosions*, VCEs) i trafikmiljöer samt den dynamiska responsen hos armerade betongelement utsatta för explosions- och stötlaster.

Den första delen behandlade VCE-scenarier med grupper av fordon. Beräkningsströmningsdynamik (*computational fluid dynamics*, CFD) användes för att undersöka hur parametrar kopplade till trafikkonfigurationen och det förblandade gasmolnet påverkar explosionsstyrkan. Baserat på resultaten utvecklades praktiska rekommendationer för tillämpning av en befintlig förenklad beräkningsmodell (multienergimetoden), vilket ger en tillförlitlig och konsekvent grund för uppskattning av explosionslaster i miljöer med tät trafik.

Den andra delen av forskningsarbetet fokuserade på responsen hos armerade betongkonstruktioner vid impulsbelastning, med särskilt fokus på seiga brottmoder som möjliggör högre energiupptagningsförmåga. Inledningsvis undersöktes risken för att skjuvbrott (sprött) inträffar före böjbrott (segt) i slanka enkelspända armerade betongplattor utsatta för explosionslast. Även om sannolikheten för böjbrott visade sig vara större hos de studerade betongplattorna, föreslogs en säkerhetsfaktor för att ytterligare öka sannolikheten för en böjdominerad respons. Därefter studerades responsen av stötblastade enkelspända armerade betongelement med olika randvillkor, inklusive effekterna av membranverkan och upprepad stötblastning. Resultaten visade att den energi som upptogs under stötblastning med efterföljande statisk belastning översteg energiupptagningsförmågan under enbart statisk belastning, vilket visar på möjligheter till mer effektiv dimensionering av konstruktioner utsatta för impulsbelastning. Vidare visades att membranverkan ökar både bärförmågan och deformationskapaciteten hos inspända armerade betongelement under såväl statisk belastning som stötblastning. Slutligen utvecklades och validerades olika förenklade metoder för att förutsäga centrala aspekter av den dynamiska responsen och därigenom stödja analysen av armerade betongkonstruktioner utsatta för stötlaster.

Nyckelord: gasmolnexplosion, stadsmiljö, beräkningsströmningsdynamik, för tidigt skjuvbrott, stötvåglast, stötblastning, armerade betongbalkar, membranverkan, energiupptagningsförmåga.

*To Linnéa, Elias
& my parents*

Preface

The research work presented in this doctoral thesis was conducted between February 2021 and September 2026 at the Division of Structural Engineering at Chalmers University of Technology. The study was supported financially by *Trafikverket* (The Swedish Transport Administration), *Fortifikationsverket* (The Swedish Fortifications Agency) and *Myndigheten för civilt försvar*, formerly *Myndigheten för samhällsskydd och beredskap* (MCF, The Swedish Civil Defense and Resilience Agency, formerly Swedish Civil Contingencies Agency). The PhD project was part of a larger collaboration project between the Division of Structural Engineering at Chalmers and the Division of Concrete Structures at KTH.

I would like to express my most sincere gratitude to my supervisors Dr. Morgan Johansson, Senior Lecturer Joosef Leppänen, and Dr. (*Docent*) Mario Plos for their continuous support and guidance throughout this work. Their expertise, dedication, and enthusiasm have been a constant source of inspiration. Our frequent discussions have been both intellectually enriching and personally memorable. This work would not have been possible without your mentorship. I am also grateful to my examiner, Professor Karin Lundgren, whose insightful feedback and constructive advice greatly contributed to the successful completion of my work.

I would like to thank our partners at KTH—Professor Anders Ansell, Dr. (*Docent*) Mikael Hallgren, and Dr. Viktor Peterson—for the great collaboration within the research project. My appreciation also extends to the members of my reference group—Rolf Dalenius and Dr. Johan Magnusson (*Fortifikationsverket*), Lars Gråbergs (MCF), Mathias Lööf (*Projektstaben*), and Henric Modig and Anna Teike (*Trafikverket*)—for their interest in my research and valuable feedback. I also want to thank my colleagues and fellow doctoral students at Chalmers for creating a good environment filled with interesting conversations and cheerful moments.

I am especially grateful to senior research engineers Sebastian Almfeldt and Anders Karlsson for their expertise and invaluable guidance in the design of the test rig and the execution of the tests conducted during the final phase of this project.

The computations and data handling for the analyses conducted in the initial phase of this project were enabled by resources provided by Chalmers e-Commons at Chalmers and the National Academic Infrastructure for Supercomputing in Sweden (NAISS)—partially funded by the Swedish Research Council through grant agreement no. 2022-06725.

I am deeply grateful to my son, Elias, who has, since entering my life, taught me valuable lessons about love, patience, and finding joy in the simplest moments. I also thank my parents and sisters who, despite the long distance, have always remained close through their unwavering support and encouragement.

Finally, Linnéa, you are the fuel that powers my heart and soul. Your love and support have undoubtedly been the most valuable source of motivation. Thank you!

¡Gracias!

Fabio José Lozano Mendoza

Gothenburg, September 2026

List of publications

This thesis is based on the work presented in the following papers, referred to by Roman numerals in the text:

- I. **Lozano F.**, Johansson M., Leppänen J., and Plos M.: *Strength of vapour cloud explosions in a traffic environment: a numerical parametric study*. Open Engineering, vol. 16, no. 1, pp. 20250162, Jan. 2026, DOI: 10.1515/eng-2025-0162.
- II. **Lozano F.**, Johansson M., Leppänen J., and Plos M.: *Guidance for estimating the blast load from vapour cloud explosions in traffic environments using the multi-energy method*, Journal of Safety Science and Resilience, vol. 7, no. 1, pp. 100222, Mar. 2026, DOI: 10.1016/j.jnlssr.2025.100222.
- III. **Lozano F.**, Johansson M., Leppänen J., and Plos M.: *Probabilistic study of premature shear failure of slender reinforced concrete one-way slabs subjected to blast loading*, Frontiers of Structural and Civil Engineering, vol. 19, no. 8, pp. 1334–1354, Aug. 2025, DOI: 10.1007/s11709-025-1205-4.
- IV. Johansson M., Leppänen J., Flansbjer M., **Lozano F.**, Jönsson J., Makdesi J., and Stenseke A.: *Experimental analysis of total energy absorption capacity in RC beams subjected to single or repeated drop-weight impacts*, International Journal of Protective Structures, vol. 17, no. 2, pp. 373-401, Jun. 2026, DOI: 10.1177/20414196251361155.
- V. **Lozano F.**, Johansson M., Leppänen J., and Plos M.: *Static membrane action in restrained RC beams: Experimental investigation, and numerical and analytical prediction*, Journal of Building Engineering, vol. 129, no. 1, pp. 116768, Jul. 2026, DOI: 10.1016/j.jobbe.2026.116768.
- VI. **Lozano F.**, Johansson M., Plos M., and Leppänen J.: *Drop-weight impact behaviour of restrained RC beams: Experimental investigation and numerical prediction*, Manuscript submitted for publication, 2026.

AUTHOR’S CONTRIBUTION TO JOINTLY PUBLISHED PAPERS

The appended papers were prepared in collaboration with the co-authors. The contribution by the author of this doctoral thesis is described below.

In **Papers I** and **II**, the author conducted the literature review, planned and carried out the numerical analyses, and performed the post-processing of the results. The author also took primary responsibility for structuring, writing, and revising the manuscripts.

In **Paper III**, the author contributed to the planning of the study, conducted the literature review, performed the analyses, and contributed to the discussion of the results. The author also assumed primary responsibility for structuring, writing, and revising the manuscripts.

In **Paper IV**, the author was responsible for conducting the tests in Series I and processing the corresponding results. The author also contributed to reviewing the manuscript.

In **Papers V and VI**, the author participated in the planning and execution of the overall study and the experiments, conducted the literature review, analysed the experimental results, and developed the proposed models. The author also took primary responsibility for structuring, writing, and revising the manuscripts.

ADDITIONAL PUBLICATIONS BY THE AUTHOR

In addition to the appended papers, the author has also contributed to the following publications (not included in this thesis).

Licentiate thesis:

- **Lozano F.:** *Explosions in urban environments: modelling of gas explosions and risk of premature shear failure in reinforced concrete structures*. Lic. Thesis, Chalmers University of Technology, Gothenburg, Sweden, 2023.

Research articles:

- **Lozano F., Johansson M., Leppänen J., Plos M.:** *CFD modelling of LPG dispersion in a road environment*, Chemical Engineering Transactions, vol. 111, pp. 313-318, 2024, DOI: 10.3303/CET24111053.

Conference papers:

- **Lozano F., Johansson M., Leppänen J., and Plos M.:** *Effect of partial confinement on vapour cloud explosions on a road: a numerical study*. In 15th International Conference on Shock & Impact, Gothenburg, Sweden, 2025.
- **Lozano F., Johansson M., Leppänen J., Plos M., Olovsson L.:** *Experimental validation of two CFD solvers for prediction of blast loading in an urban environment*. In MABS26 International Symposium, Wollongong, Australia, 2023.
- Peterson V., **Lozano F.**, Johansson M., Hallgren M., Ansell A., and Magnusson J.: *The effect of explosions in road tunnels on critical structural elements*. In ITA-AITES World Tunnel Congress 2023, Athens, Greece, 2023.
- **Lozano F., Johansson M., Leppänen J., and Plos M.:** *Explosion loads in densified urban environments: challenges and needs*. In XXIV NCR Symposium, Stockholm, Sweden, 2022.

Technical reports:

- **Lozano F.:** *Gas explosion simulations in a traffic environment: grid sensitivity analysis and choice of grid resolution*. Chalmers University of Technology, Report ACE 2024:6, Gothenburg, Sweden, 2024.

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Chapter 1

Introduction

1.1 Background

The risk of explosion-related incidents in urban environments has increased markedly in recent years worldwide, as reflected in current news reports and the evolving geopolitical tensions. While deliberate attacks continue to highlight the vulnerability of the built environment, accidental explosions can be equally destructive. Moreover, these risks are closely tied to the development of the modern society, including urban densification and the growing use of alternative fuels. As cities expand, buildings and structures may be situated near petrochemical, chemical, and even explosive storage facilities, increasing exposure to accidental events. The explosion at the Port of Beirut in 2020 [1], which caused widespread damage and loss of life several kilometres from the source, exemplifies the scale of devastation that accidental explosions may cause.

The risk for accidental urban explosions is also largely associated with the transport of hazardous materials (hazmat) by road, particularly flammable gases—such as liquified petroleum gas (LPG), liquified natural gas (LNG), and hydrogen—and explosive materials, including ammunition, fireworks, and explosive compounds for excavation works. However, in general, transport of flammable gases occurs far more frequently than that of explosive materials [2]. A traffic accident involving a flammable gas may result in the unintended release of the gas, which in turn can escalate into a vapour cloud explosion (VCE). VCEs can cause severe harm to people and extensive damage to infrastructure. In the near field, the high overpressures caused by a VCE are accompanied by significant thermal hazards, including intense radiation and fire. In addition, the resulting blast wave can extend the damage zone far beyond the immediate vicinity of the explosion. Recent incidents, such as the LPG tanker accident in Wenzhou (China) in 2020 [3], demonstrate the destructive potential of such events, potentially resulting in numerous fatalities, extensive injuries and large-scaled damage to infrastructure and vehicles.

Beyond direct blast pressures, urban structures may also be vulnerable to impact loads. Such loads may arise from secondary effects of an explosion, including the collapse of overlying or nearby structures, and flying fragments. Impact loads may also be induced by unrelated events, such as falling debris, rockfall, or vehicular collisions. These impact actions impose concentrated and impulsive forces on the affected structure, which may lead to the failure of individual members and, in severe cases, the progressive collapse of the entire structure. Indeed,

numerous vehicle collision incidents involving bridge structures have been documented in recent decades, highlighting the potentially severe consequences of impact loading in the transport infrastructure [4], [5].

An important factor contributing to the increased risk of accidental explosions and impacts is the ongoing trend towards densifying existing urban environments [6]. Indeed, several municipalities in Sweden have adopted densification strategies as a means of accommodating growing populations with additional residential and office spaces [7], [8], [9]. Densification often entails permitting reduced separation distances between roads and adjacent buildings, and the construction of street overbuilds. As a result, structures are situated closer to potential sources of VCEs and impacts along transport routes, thereby aggravating the consequences in the event of an accident.

Traditionally, blast-resistant design has been reserved for special structures, such as government buildings and petrochemical facilities. However, partly as a result of the aforementioned developments, the management of explosion and impact hazards is emerging as a critical concern in urban development and the wider construction industry [10], [11]. This trend places growing demands on practising structural engineers, who may be required to design structures capable of withstanding extreme dynamic loading scenarios. Achieving this objective typically involves ensuring that key structural elements can resist the intense, short-duration loads. Additionally, structures should possess sufficient robustness and redundancy to prevent a disproportionate collapse of the entire system due to the failure of one or a limited number of components [12]. Moreover, the inherent uncertainties associated with explosion and impact events require evaluation of multiple scenarios. For detonations involving high explosives, this may entail varying the charge mass and stand-off distance to assess the resulting blast loads [13]. VCEs present a more complex challenge, as their consequences are influenced by several factors, including the location of the release, atmospheric dispersion, degree of confinement, ignition conditions, and the interaction between the flame and surrounding obstacles [14].

According to Johansson et al. [8], three principal areas must be considered to adequately assess and manage explosion-related risks in urban environments: risk analysis, explosion loading, and structural response. If the conclusions drawn within any one of these domains are based on incorrect assumptions, the resulting design solutions are likely to be compromised—either by insufficient safety margins or by unnecessarily high construction costs. However, broadly speaking, blast and impact engineering remains a specialised field, and the wider structural engineering community does not normally possess the specialised knowledge, experience, and analytical capabilities required to reliably design and assess structures subjected to extreme dynamic loads [8], [15]. Likewise, although risk analysts are well equipped to evaluate hazards and probabilities, they may not possess the technical knowledge needed to link a specific accidental event to the resulting dynamic load and the structural response it may trigger. These gaps in expertise contribute to a range of challenges in development projects, including inadequate communication, insufficient data exchange, and suboptimal design solutions [8].

The work presented in this thesis aims to support the effective management of explosion and impact risks on the built environment, guided by the broader purpose of contributing to safer and more efficient design of RC structures exposed to explosion and impact hazards in traffic and urban environments. Equally important is the development and validation of simplified engineering approaches and practical design and analysis methods for blast-resistant and

impact-resistant RC structures, which can be readily integrated into existing design practices to assist engineers and risk analysts in assessing explosion- and impact-related hazards.

1.2 Aim and objectives

The aim of this doctoral thesis was to combine numerical, analytical, and experimental methods to advance the understanding of extreme dynamic loading and its effects on reinforced concrete structures, and to develop practically applicable methods for prediction and assessment.

The research was focused on two key research areas:

- I. Characterisation of the blast load generated by vapour cloud explosions in traffic environments.
- II. Investigation of the dynamic response, failure mechanisms, and energy absorption capacity of reinforced concrete elements subjected to blast and impact loading.

The following objectives and research questions (RQ) were defined for each research area:

Research Area I:

Objective 1: To improve the prediction of blast loads from vapour cloud explosions in traffic environments by identifying significant parameters and developing practical guidance for simplified blast-load estimation. This objective was divided into two research questions:

- RQ-1: *Which geometric parameters most strongly influence the severity of VCEs in traffic environments, such as roads and carparks?*
- RQ-2: *What simplified approach, supported by advanced numerical calculations, can be employed to achieve reliable and conservative estimates of the blast load generated by VCEs occurring on roads and carparks?*

Research Area II:

Objective 2: To analytically study the probability of brittle shear failure in blast-loaded slender RC one-way slabs considering the uncertainties related to the material properties, geometry, and resistance models, and propose a simplified approach to decrease the likelihood of shear failure. Based on this, the following research question was formulated:

- RQ-3: *How can the probability of premature shear failure in blast-loaded slender RC one-way slabs—accounting for material, resistance, geometry, and load uncertainties—be quantified and reduced through simplified methods to ensure a high confidence regarding ductile failure?*

Objective 3: To enhance the understanding and prediction of the response, failure mechanisms, and energy-absorption capacity in reinforced concrete elements subjected to single or repeated impact loading and different boundary conditions. This objective was divided into two research questions:

- RQ-4: *How are the response and total energy absorption capacity of RC beams influenced by impact loading, including repeated impacts, and different boundary conditions?*
- RQ-5: *What simplified analytical or numerical methods can be utilised to describe the response—including load-carrying capacity, deflection, reinforcement fracture, and membrane force—of restrained RC beams subjected to static and impact loading?*

1.3 Methodology and scientific approach

Figure 1.1 illustrates the overall structure of the research and the methods employed to address the research questions. The study integrated advanced numerical calculations, analytical methods, and experimental work.

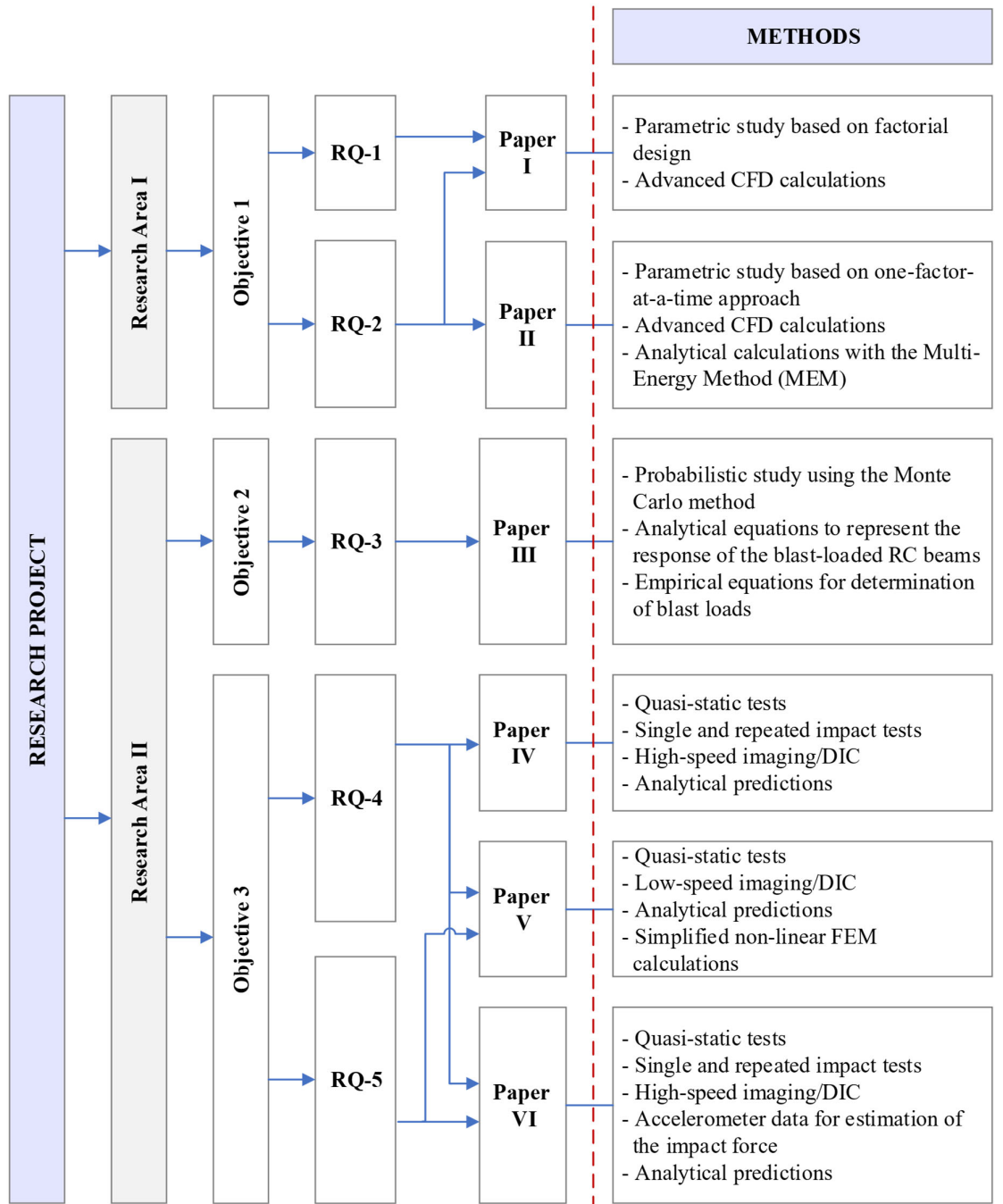


Figure 1.1. Schematic overview of the research framework, showing the relationships between the research questions, the resulting papers, and the methods employed.

To conduct the work concerning Research Area I and Objective 1, numerical simulations based on *Computational Fluid Dynamics* (CFD) were performed. The numerical models provided an approximate representation of a complex physical phenomenon. The primary motivation for

this approach was the lower cost of numerical modelling compared to experimental campaigns. Moreover, numerical modelling enables the investigation of multiple scenarios, allowing for an extensive parametric study. In addition, such models also facilitate more extended sampling of results compared with what is typically feasible in experiments. The numerical work was supported by preliminary sensitivity studies, including grid sensitivity analysis and validation against experimental data to support the choice of grid cell size. The geometry and setup of the studied cases were designed with the intention to allow for potential future experimental studies. A two-stage parametric study was conducted using the principles of *factorial design*, a structured approach that enables the efficient evaluation of multiple influencing factors while limiting the number of required simulations. This is particularly beneficial given the high computational demands associated with CFD analyses. Based on the CFD results, a simplified predictive method—the Multi-Energy Method—was adapted for the scenarios of interest. The findings from Research Area I are presented in **Papers I and II**.

Paper III addressed Objective 2 within Research Area II, which is concerned with the uncertainties involved with the failure mode of blast-loaded slender RC one-way slabs. The strategy adopted combined simplified analytical models for estimating the resistance of the RC slabs with the conventional *Monte Carlo* method to evaluate the effects of the relevant uncertainties. The use of simplified analytical models enabled a large number of iterations within the probabilistic framework. Additionally, the study employed the Monte Carlo method to determine overstrength factors aimed at increasing the confidence level regarding the preferred response mode.

Experimental research was the primary method used to address Objective 3, which concerns the response and energy absorption capacity of impact-loaded RC beams. The experimental programme included both impact and quasi-static tests. Different methods were used to record the response of the specimens, including load cells, displacement transducers, accelerometers and high-speed imaging combined with Digital Image Correlation (DIC). **Paper IV** focused on simply supported beams. The study examined the influence of varying impact conditions (including drop-weight mass, drop height, and number of strikes). In **Papers V and VI**, restrained RC beams were investigated. The study examined the influence of restrained boundary conditions, slenderness, reinforcement ratio, drop-weight mass, and repeated impacts. In addition to the experimental work, the study developed simplified models—based on empirical, analytical, and numerical approaches—to predict the structural response of restrained RC elements.

1.4 Limitations

A summary of the main limitations of the presented work, including the appended papers, is presented below:

Research Area I:

- Due to the high computational demands of the CFD simulations of gas explosions, only one type of gas was considered. Furthermore, some important parameters, such as the geometry and ground clearance of the vehicles, were kept constant during the calculations.
- The study considered only gas explosion scenarios in open areas. Explosions in confined environments, such as tunnels or street overbuilds, are outside of the scope of this thesis.
- The only source of congestion within the gas cloud that was considered in the CFD calculations was the vehicles on the road. Other potential obstacles, such as noise barriers or nearby structures, were not included.

- Due to the lack of published experimental results concerning gas explosions in conditions comparable to the scenarios of interest, the validation of the CFD models and the choice of grid cell size were based on comparisons with experimental results from deflagrations in obstructed two-dimensional setups, which were considered representative of the flow beneath a group of vehicles.

Research Area II:

- The RC elements examined in **Papers III** and **IV** had relatively low structural complexity and are therefore more representative of isolated elements rather than of complete structural systems.
- In **Paper III**, the design shear force in the blast-loaded RC slabs was determined using the concept of *equivalent static load*. This simplified approach enabled the analysis of a very large number of cases with limited computational resources. However, the actual distribution of shear forces in blast-loaded elements may differ from this assumption, particularly for highly impulsive loads.
- In **Paper IV**, the impact energy dissipated by the simply supported beams was not measured directly. Instead, it was inferred based on classical impact theory assuming an ideal inelastic collision. This assumption was considered reasonable, as the velocity-time histories of the drop-weight and the beams were observed to converge and evolve together up to peak deflection, indicating a behaviour similar to that expected in ideal inelastic collisions.
- The experimental campaign presented in **Paper IV** was conducted on small-scale beams, allowing for testing of multiple specimens. Consequently, due to potential size effects, the results may not be directly transferable to larger-scale elements. Therefore, the findings should be validated through tests on larger elements. The specimens in **Papers V** and **VI**, while still relatively limited in scale, were larger and more complex, which restricted the number of tests conducted within the framework of this project. Consequently, the observations need to be confirmed, either through additional experimental studies or numerical simulations.
- The study examined exclusively beams with symmetric and continuous reinforcement. Further research is required to extend the findings to specimens with curtailed or lapped reinforcement, as well as to specimens with asymmetric reinforcement layouts. Similarly, the simplified models developed in this work should be further expanded and validated for RC members with varying reinforcement amounts and discontinuous reinforcement detailing.
- The study examined primarily beams subjected to a concentrated load at midspan, either static or dynamic. Alternative loading conditions may activate different failure modes and should therefore be explored in future work.

1.5 Outline of the thesis

This thesis consists of an extended summary and six appended journal papers. The introductory chapters provide background information and complementary material relevant to the work presented in the appended papers.

Chapter 1 outlines the background, aim and research questions of the thesis. It also presents the scope and limitations and a general description of the research methodology.

Chapter 2 introduces fundamental concepts related to vapour cloud explosions, blast and impact loading, and structural response, which constitute the theoretical background relevant for this study.

Chapter 3 summarises the research work addressing Objective 1 within Research Area I, concerning gas explosions in traffic environments. This topic is explored in detail in **Papers I and II**.

Chapter 4 presents the work related to Objective 2 within Research Area II, focusing on the uncertainties associated with the failure modes of slender blast-loaded RC one-way slabs. This work is further detailed in **Paper III**.

Chapter 5 covers the research work regarding the influence of single or repeated impacts and varying boundary conditions on the response of RC elements, corresponding to Objective 3 within Research Area II. This work is discussed in detail in **Papers IV, V and VI**.

Chapter 6 compiles the simplified engineering approaches and practical analysis methods developed or validated in this thesis.

Chapter 7 summarises the main conclusions of the thesis and provides recommendations for future research.

Chapter 2

Theoretical background

2.1 Overview

Structural protection against extreme dynamic actions, such as blast and impact loading, has received increased attention in recent years due to the growing number of accidental explosions and deliberate attacks affecting both the built environment and public safety. These extreme loading events are characterised by high load intensities acting over a very short time, resulting in a structural response that differs substantially from those associated with static loading or less intense dynamic actions (e.g. wind or vehicular traffic). Consequently, design and analysis methods developed for statically loaded structures cannot, in many cases, be directly employed for designing structures subjected to blast or impact loading. Indeed, special consideration must be given to the characteristics of the applied load and the associated dynamic response. Moreover, achieving a reliable design requires a good understanding of different phenomena, such as explosions of fuel-air mixtures, blast wave propagation, and the behaviour of materials under high strain rates, areas that are not typically covered within the traditional expertise of structural engineers.

In engineering practice, the assessment of blast-loaded structures is often based on single-degree-of-freedom (SDOF) idealisations, which provide reasonable estimates of the main structural response and form the basis of many design guidelines. Similar simplified approaches may also be applied to impact-loaded members, normally by extending the SDOF approach to a two-degree-of-freedom (2DOF) system. During the conceptual design phase, such rapid engineering assessment methods are particularly valuable, as they enable the evaluation of multiple design alternatives and threat scenarios with relatively limited computational effort. These methods can support decision-making regarding structural layouts, protective measures, and required levels of robustness. However, as the design progresses, more advanced numerical analyses or experimental investigations may be necessary to achieve a more reliable prediction of the structural response. Nevertheless, simplified methods remain useful even during detailed design, both as an independent means of verification and as a practical option for the final design of less complex structures.

In addition to accurately predicting the structural response, it is equally important to properly characterise the explosion scenario in order to determine the resulting blast load. This aspect is particularly challenging for explosions involving fuel-air mixtures, where the severity of the event is strongly influenced by the surrounding environment, including geometric conditions

(e.g. degree of confinement and the presence of obstacles) and weather conditions (e.g. wind and rain). As a result, predicting the consequences of such explosions is often associated with significant uncertainty. Several simplified methods have been developed to estimate the strength of gas explosions and the associated blast loading, offering varying levels of accuracy. However, many of these approaches were originally developed for applications in the process industry and may not be directly applicable to other environments. More reliable predictions may be obtained through advanced numerical simulations, but their application is often constrained by the associated computational costs, modelling complexity, and requirement for specialised expertise.

This chapter summarises the relevant theoretical background concerning these topics to support the understanding of the research work presented in this thesis.

2.2 Explosions and blast loading

In general terms, an explosion can be defined as the release of energy over a very short time. The source of this energy could be chemical (such as high-explosive charges or fuel-air mixtures), physical (such as a rupture of vessels containing a pressurised liquid) or nuclear [16]. The released energy is dissipated through various mechanisms, including the generation of air-blast waves, acceleration of casing fragments [17], and cratering of the surrounding ground.

In the context of protective design, explosion hazards are commonly associated with blast overpressures generated by the detonation of chemical high explosives (HEs). However, blast effects may also arise from accidental industrial explosions occurring either within industrial facilities or during the transport of hazardous materials. Examples of such incidents include vapor cloud explosions (VCEs), BLEVEs¹, and dust explosions. This section provides essential theoretical background on explosions and blast loading associated with both HEs and VCEs. It also outlines different methods for estimating the blast effects produced by these two types of explosion source.

2.2.1 High explosives

Chemical explosives are reactive compounds or mixtures with a significant amount of stored chemical energy that can be rapidly released upon initiation, generating gaseous products (commonly known as *detonation products*) at high pressure and temperature [18]. Once the reaction has been initiated at a particular point within the explosive charge, it continues to propagate by the transfer of energy from the reacted material to the surrounding unreacted explosive. This energy transfer can proceed through two fundamentally different mechanisms, namely, detonation and deflagration.

In a *detonation*, the reaction propagates by means of a shock front that rapidly compresses and triggers the unreacted material ahead of it [19]. Chemical explosives that react through this mechanism are classified as *high explosives*. Common examples include TNT (trinitrotoluene), RDX (hexogen) and PETN (pentaerythritol tetranitrate) [18]. The reaction advances extremely rapidly due to the highly efficient transmission of the mechanical energy through the shock front, with detonation velocities exceeding the speed of sound within the material [20].

In contrast, in a *deflagration* the explosive material decomposes at a rate below the speed of sound. In this process, the liberated heat of reaction is transferred to the unreacted material,

¹ BLEVE stands for Boiling Liquid Expanding Vapour Explosion

raising its temperature above its ignition threshold and thereby sustaining the reaction [19]. Explosives that react through this explosion mechanism are classified as *low explosives*. Typical examples include propellants and gunpowder. The study of low explosives is outside the scope of this work.

Detonations produce greater overpressure and are generally more destructive than deflagrations for a given quantity of explosive.

2.2.2 Vapour cloud explosions

A vapour cloud explosion is defined as the explosion of a fuel-air mixture. This type of explosion has commonly been associated with hazards in the petrochemical industry, although recent incidents have demonstrated that it can also occur in urban and road environments [3].

Similarly to the mechanisms governing the explosion of HEs, the flame front (i.e. the interface between burned and unburned material) in VCEs can progress through deflagration or detonation. In a deflagration, the flame front is propagated by conduction and molecular diffusion of heat [21]. Laminar flames in a deflagration are characterised by a reaction zone—where heat is generated by chemical reaction—and a preheating zone—where the unburned material is heated and preconditioned for ignition—as illustrated in Figure 2.1. In deflagrations, the flame front always travels at subsonic speeds relative to the unburned gas ahead of the flame, as molecular diffusion is relatively slow. However, relative to a stationary observer, the flame speed may increase up to 500 – 1000 m/s [14]. The vast majority of accidental VCEs reported in the literature were deflagrations. Thus, VCEs typically generate blast waves with lower peak overpressures but greater durations and impulses than those produced by detonating HEs releasing an equivalent amount of energy [12]. Unless specified otherwise, the term VCE refers to deflagration in this work.

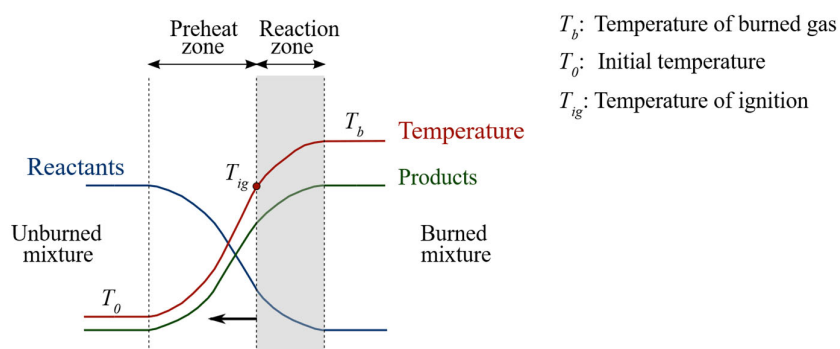


Figure 2.1. Schematic diagram of a laminar premixed flame. Adapted from [22].

In a detonation, the reaction front is propagated as a strong shock wave, which compresses the unburned material past its autoignition temperature [21]. Fuel-air mixtures initially at ambient pressure can reach detonation velocities of up to 2100 m/s [21]. Detonation of a gas cloud can initiate either directly due to high-energy ignition or as *deflagration-to-detonation transition* (DDT), a process in which a deflagration evolves into a detonation. Detonation of gas clouds is an uncommon event in society and examples of such detonations are normally from research experiments. However, a few cases of accidental detonations of gas clouds have been reported [23], [24]. This means that the risk for detonation cannot be ruled out without a careful evaluation of the scenario.

A detonation is the most violent form of combustion of a gas cloud and can cause the most severe damage due to the large overpressure generated by the supersonic shock front. The maximum pressures produced are in the order of 1800 to 2000 kPa [21]. On the other hand, the generated overpressure in deflagrations depends on the burning rate. Low flame speeds usually result in minor overpressure (i.e. flash fires), while higher flame speeds generate greater overpressure. The evolution of a deflagration is a complex process because flame speed and pressure build-up are not unique for a given fuel-air mixture. Instead, the resulting explosion strength varies over a wide range depending on many factors such as degree of confinement and congestion, turbulence, venting, location of ignition, and environmental conditions.

2.2.3 Effect of confinement and obstruction in deflagrations of fuel-air mixtures

The resulting flame speed and pressure in a VCE depend strongly on the geometrical conditions of the environment where the deflagration develops. This includes both the geometry and arrangement of obstacles within the cloud and the geometries enclosing all or part of the cloud.

When the flame propagates through the gas cloud, there are two main mechanisms leading to pressure increase: fast burning rate and flame propagation in a confined space [14]. These are described in notables works such as [14], [21], [25]. In general, an increased flame speed enables higher explosion pressure. If the gas cloud is ignited with a weak ignition source (e.g. a spark), the flame starts propagating at a laminar speed. The basic mechanism of propagation of the laminar flame front is molecular diffusion of heat and mass (see Figure 2.1). As the flame consumes the unburned material, the combustion products will expand up to 8 or 9 times the original volume. Because of this expansion, the unburned gas will flow ahead of the flame. Interaction of this flow with surfaces (e.g. ground and ceiling) and objects within the cloud will create velocity gradients that will stretch the flame front. This expands the original surface area of the flame, which in turn results in enhancement of the flame speed. When the flame speeds are sufficiently high, turbulent conditions may form ahead of the flame, both due to the velocity gradients breaking down into turbulent shear layers, and due to turbulent vortices generated in the wake of obstacles [25]. When the flame enters the turbulent region, it will be stretched and wrinkled further, which boosts the burning rate. Furthermore, for high-intensity turbulence, the small-scale vortices will enhance the transport of heat and mass across the flame front, which accelerates the flame further. The increased burning velocity will then cause the explosion pressure to rise.

If there is only one single row of obstacles in the path of the flame, the flow field will subside after passage of the flame front, and the flame front speed will promptly decay. However, if multiple layers of obstacles are present, the increased flame speed will enhance turbulence generation ahead of the flame around the subsequent layers of objects. This mechanism of flame acceleration due to repeated obstacles constitutes a strong positive feedback loop. This loop is illustrated in Figure 2.2.

The main effect of confinement is preventing the expansion of the flow in one or more directions. A deflagration propagating in a tube normally requires lower flame speed to produce the same explosion pressure than a flame propagating in unconfined conditions [14]. Furthermore, the interaction of the flow with confining surfaces also generates turbulence.

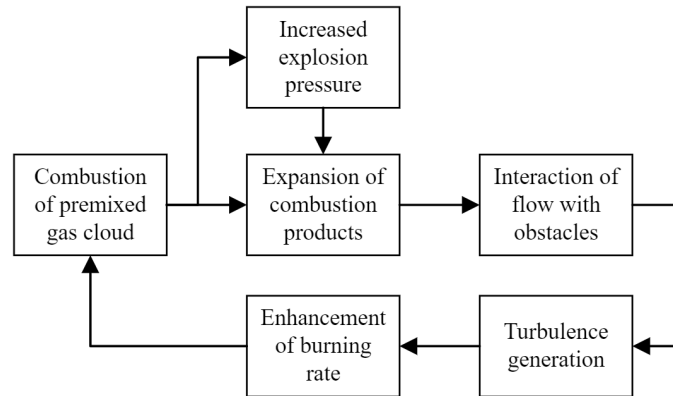


Figure 2.2. Positive feedback loop for flame acceleration due to repeated obstacles [14].

To characterise confinement and obstruction, it is common to use parameters such as volume blockage ratio (VBR), obstacle diameter, and pitch (spacing) between rows of obstacles. Research has shown that increasing the VBR will result in greater pressure [26], [27]. Furthermore, for the same VBR, smaller obstacle diameters will likely increase the explosion pressure. The reason for this is that smaller obstacles deployed over a greater number of repeated layers will allow the positive feedback loop in Figure 2.2 to cycle more times.

Other aspects, including the shape and arrangement of the obstacles, may also have a significant effect on the explosion pressure [14]. Square obstacles normally lead to higher pressure than cylindrical objects for the same VBR. The main reason for this is that the turbulence in the wake of obstacles with sharp corners is more intense than that produced by round obstacles. Regarding arrangement of the obstacles, staggered configurations may produce greater explosion pressure than structured arrangements.

2.2.4 Free-field blast waves

At a particular fixed point, a blast wave is experienced as a transient change in state parameters, such as pressure, density, and temperature [21]. In the context of blast-resistant design, it is common to characterise the blast wave based on its pressure-time history. The shape of the blast wave depends on the amount of energy released, the speed of release, and the distance from the explosion centre. According to Geng et al. [28], three blast wave regimes in free-field conditions can be distinguished, namely: shock wave, pressure wave, and acoustic wave. A schematic representation of the pressure-time history for each regime appears in Figure 2.3. A positive and negative phase can be distinguished for all cases. A shock wave is a fully developed compression wave, characterised by an immediate and drastic increase of pressure, followed by a rarefaction wave. The shock wave may be treated as a discontinuity for most practical purposes. The shock wave propagates at supersonic velocity relative to the gas immediately ahead of the shock, i.e. the gas ahead is undisturbed by the shock. Shock waves are typically produced by strong explosions, such as detonations or strong deflagrations. Deflagrations of intermediate strength usually generate pressure waves, for which a noticeable increase of positive pressure is achieved in a relatively short time. However, the rise time is not zero for this type of wave. Finally, weak explosions give rise to acoustic compression waves, characterised by a gradual increase of pressure, followed by a rarefaction wave. Pressure values for this type of blast wave are relatively low. It should be noted that some authors group pressure waves and acoustic waves into one single category [29].

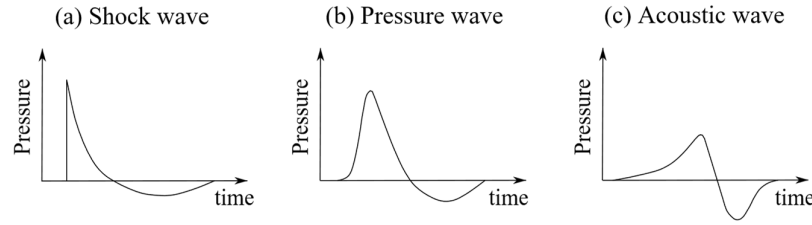


Figure 2.3. Schematic representation of the different blast shapes according to Geng et al. [28].

Blast waves are usually defined with the help of the so-called blast wave parameters. The different blast wave parameters are schematically described in Figure 2.4. The parameter ΔP represents the overpressure and can be determined with Eq. (2.1), in which P is the absolute pressure and P_0 is the ambient pressure (approximately 101.3 kPa at 15°C). The parameter ΔP^+ represents the peak overpressure. The use of overpressure is preferred in the literature because any damage in the structures will be caused by the pressure relative to the ambient pressure.

$$\Delta P = P - P_0 \quad (2.1)$$

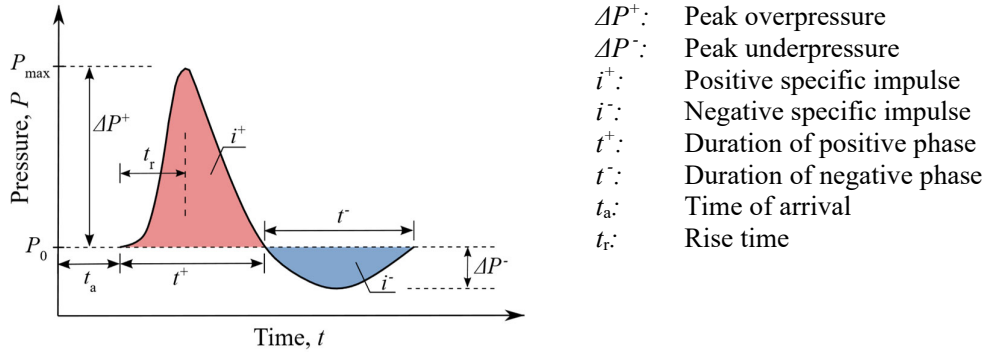


Figure 2.4. Definition of blast wave parameters.

As the blast wave propagates unimpeded by the environment, the explosion energy is distributed over an increasingly larger volume. This results in a decrease in the peak overpressure and the specific impulse as the distance from the explosion centre increases. This process is called free-field pressure decay.

2.2.5 Blast effects from high explosives

The detonation of a high-explosive charge generates a shock wave that propagates outwards from the explosion centre. The characteristics of the resulting blast load depend on both the amount of energy released during the detonation and the stand-off distance from the charge to the structure, R . In practice, the charge mass, W , is used as a measure of the released energy. For different explosive compounds, the charge mass is typically converted to an equivalent mass of TNT by multiplying with an equivalency factor—which may be different for calculations of overpressure and impulse [30]. A normalised description of the blast effects is then usually expressed in terms of the scaled distance, Z , defined according to Eq. (2.2). Using the scaled distance, blast wave parameters can be determined through various empirical relationships or charts, such as the well-known equations developed by Kingery and Bulmash [31], [32], which also form the basis for the air-blast calculations implemented in UFC 3-340-02 [33] and ConWep [34].

$$Z = \frac{R}{W^{1/3}} \quad (2.2)$$

Additional factors that influence the severity and spatial distribution of the blast effects from a HE detonation include the charge geometry, the properties of the casing (if any), and the orientation of the explosive relative to the structure. In engineering practice, these effects are often treated in a simplified manner. For example, the Swedish design standard for impulsively loaded structures, FKR 2011 [35], prescribes additional correction factors applied to the charge mass to account for such effects.

HE explosions are commonly classified according to the scaled distance. This classification reflects the expected damage mechanisms in the affected structures and may form the basis for code-specified design provisions, detailing requirements, and analysis methods. However, no universally accepted definition of the different distance ranges exists within the research community. In general, HE explosions are categorised as contact, near-range, or far-range events. Contact detonations are typically associated with scaled distances below $0.4 \text{ m/kg}^{1/3}$, for which localised damage effects dominate. In contrast, explosion scenarios with scaled distances greater than $1.2 \text{ m/kg}^{1/3}$ are generally regarded as far-range events [33], [36], [37]. Under such conditions, assumptions of uniformly distributed loading and global structural response are commonly considered appropriate.

The blast effects on the target structure are also dependent on the interaction of the blast wave with the surrounding ground and the structure. Indeed, with respect to the geometric characteristics of the environment, HE explosion hazards are commonly categorised as either unconfined or confined explosions [33], [38]. Unconfined explosions occur outside the structure and may be further classified as: (i) *free-air bursts*, where the structure is subjected directly to the incident blast wave; (ii) *air bursts*, where the detonation occurs at a limited height above the ground such that the incident shock wave is reflected from the ground before reaching the structure, leading to the formation of a Mach front; and (iii) *surface bursts*, where the charge is located on or very close to the ground surface. In contrast, confined explosions occur within the structure, and their effects are influenced by multiple reflections within the structure and the accumulation (or venting) of the detonation products. Depending on the degree of confinement, confined explosions may be fully vented, partially confined, or fully confined.

2.2.6 Evaluation of vapour gas explosions

Experimental investigation would be the ideal and most reliable strategy for estimating the possible consequences of vapour gas explosions. However, carrying out gas explosion experiments, particularly at large scales, is usually prohibitively expensive. Therefore, different models have been developed for predicting the consequences of gas explosion scenarios. In the literature, three categories are normally distinguished: empirical models, phenomenological models, and numerical models [39], [40], [41].

Empirical models consist of correlations or general guidelines developed from analysis of experimental results. These are simplified methods that allow for fast and (normally) conservative approximations of the blast loading caused by the gas explosion. Some of these methods are introduced in Section 2.2.7. However, empirical models have a number of limitations, including their limited ability to represent explosions in complex geometries and the difficulty of choosing an appropriate blast strength to characterise the explosion source. In

contrast, phenomenological models seek to describe the governing physical processes of an explosion through a simplified description of the geometry combined with correlations derived from both experimental data and theoretical models. The SCOPE model [42] and the CLICHE model [39] are representative examples of this type of model.

Numerical models are based on the fundamental partial differential equations governing fluid flow and the combustion process. These include the basic equations for conservation of mass, momentum and energy (Navier-Stokes equations) in addition to physical models to describe turbulence and combustion [43]. Such equations are numerically solved using the principles of computational fluid dynamics (CFD). CFD codes are normally classified into *simple* and *advanced* models. Simple CFD codes, such as EXSIM [43], FLACS-CFD [44], and PDRFoam [45], rely on the Porosity Distributed Resistance (PDR) approach [46] to greatly simplify the geometrical conditions by representing sub-grid elements (i.e. objects smaller than the grid cell size) as volume and area porosity and resistance factors. This approach allows for coarser grids, which enables simulations of large-scale cases. In contrast, more advanced codes attempt to describe the geometry as reliably as possible. High computing power requirements, long run times, specialised knowledge by the user, and high costs are the main limitations commonly associated with numerical models.

2.2.7 Blast loading from vapour cloud explosions

Different empirical methods to estimate the parameters of the blast load from VCEs can be found in the literature. Examples of such methods include the TNO Multi-Energy method (MEM) [47], the Baker-Strehlow-Tang (BST) method [48], [49], and the Congestion Assessment Method [50].

The MEM is one of the most used methods because of its simple and coherent conceptual framework. According to this method, only those regions within a gas cloud that are partially congested or confined can produce a strong blast. These regions are commonly called *blast sources*. Several blast sources can exist within a large gas cloud. In contrast, the remaining unconfined or uncongested gas volume will not contribute significantly to overpressure generation. For the determination of the blast wave parameters, the MEM provides blast charts for side-on static overpressure, dynamic overpressure, and positive phase duration as a function of the energy-scaled distance. Each chart consists of a set of 10 curves for different strength classes as illustrated in Figure 2.5. The energy-scaled distance depends on the estimated energy of the blast source, which the MEM relates to the volume of gas in the blast source. The most challenging aspect of the MEM is possibly the estimation of the blast strength. Note that the terms *blast strength*, *explosion strength*, *charge strength* and *source strength* are commonly used interchangeably in the literature to refer to the overpressure generated by the gas explosion at the source of blast. In this sense, greater strength means that the generated blast wave will exhibit greater peak overpressure. Guidelines for determining this parameter within the context of the MEM can be found in the literature, e.g. [41], [51], [52], [53]; however, it should be noted that such guidelines have mostly been adapted for conditions within the process industry.

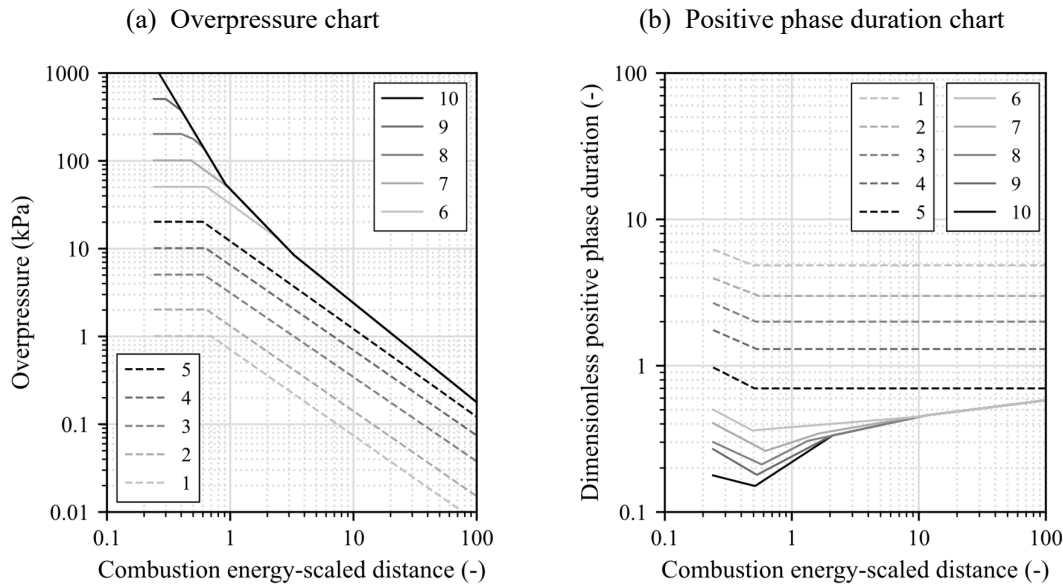


Figure 2.5. Charts for side-on overpressure and positive phase duration as a function of energy-scaled distance and strength class (1-10) according to the MEM. Adapted from [47].

2.2.8 Interaction of blast waves with structures

When the shock front impinges on a structure, two important processes occur: *reflection* and *diffraction* [54]. The former refers to pressure enhancement at the loaded surface caused by the abrupt stop of the movement of the particles. In a normal reflection, the reflected particles have a velocity equal in magnitude to the original velocity but with opposite direction, which produces the effect of a reflected shock front travelling back through a medium with a higher pressure than the surrounding atmosphere [19]. The reflected pressure depends on the incident (side-on) pressure and the incident angle. The reflection coefficient ranges between 2 (for very low incident pressure) and a theoretical upper limit of 8 (for very strong shocks) [20].

Diffraction causes different blast load characteristics on different sides of the structure, as illustrated in Figure 2.6. The greatest value of pressure is registered on the front face, where the pressure increases to the reflected value. The load experienced on the top and side faces corresponds to the side-on pressure, as no reflection occurs on surfaces parallel to the direction of propagation. When the blast wave has traversed the length of the building, the rear face is loaded with a pressure close to the side-on overpressure. However, depending on the length of the building, the side-on overpressure behind the structure may be noticeable lower than the side-on overpressure at the front of the structure due to free-field decay.

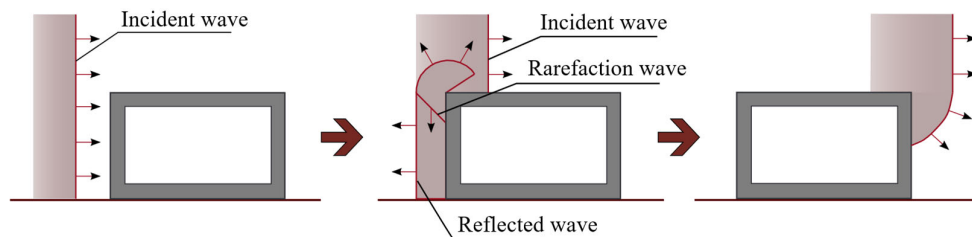


Figure 2.6. Diffraction of a blast wave around a finite-size structure. Adapted from [19], [55].

It should be noted that the difference between the pressure at the front face and that at the side and top faces gives rise to a rarefaction wave that travels inwards through the front face from the edges, effectively decreasing the pressure in the front face down to the stagnation pressure (i.e. the combined effect of the side-on overpressure and the dynamic pressure²) [55]. The influence of this process, called *clearing*, depends on the size of the structure. The typical pressure profile for finite-size front faces is shown in Figure 2.7.

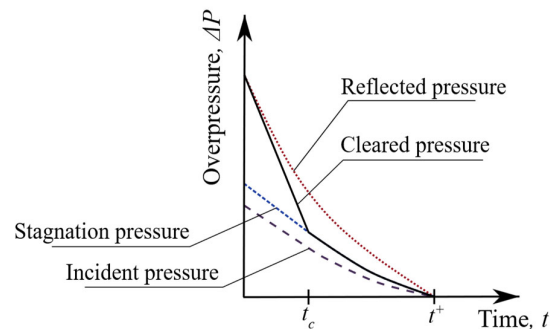


Figure 2.7. Blast load on the front face of a finite-size structure. Adapted from [55]

2.2.9 Propagation of blast waves in urban environments

The evolution and propagation of blast waves in urban environments differ substantially from those of free-field blast waves [10], [56], [57]. The interaction of the blast wave with obstacles along its path gives rise to physical phenomena such as reflection and diffraction (see Section 2.2.8), as well as superposition, which together significantly increase the complexity of the loading. In the literature, the effects of such phenomena are usually grouped into two concepts: channelling and shielding. *Channelling* refers to the enhancing effect of confinement due to nearby structures and coalescence of multiple waves that have reflected from or diffracted around buildings. *Shielding*, on the other hand, accounts for the attenuating effect from objects located between the studied building and the explosion centre. Figure 2.8 shows conditions propitious for channelling and shielding.

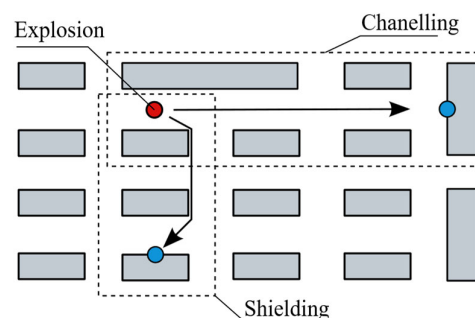


Figure 2.8. Schematic illustration of channelling and shielding in urban environments.

The evaluation of the blast load acting on a building due to an explosion in an urban environment has gained more importance in recent years. Due to the complexity of the different processes involved, advanced numerical models are commonly used [58], [59], although some simplified fast-running engineering approaches have also been developed, e.g. [60].

² The dynamic pressure is the *increase* in pressure that a moving fluid would experience if brought to rest [19].

2.3 Blast-loaded RC elements

2.3.1 General description

The response and damage mechanism of RC members subjected to blast loading are intrinsically connected to the scaled distance between the source of explosion and the structure [37]. Contact or close-in explosions typically result in severe localised damage. Cratering may occur on the exposed face due to concrete crushing under the intense pressure generated by the explosion. On the rear face, spalling may develop as a consequence of tensile stresses produced when the stress wave is reflected at the free surface. Spalling leads to ejection of concrete fragments, which can pose a significant hazard to occupants and equipment located inside the structure. Near-range explosions induce a combination of localised damage and global response. In contrast, far-range explosions tend to produce a more uniformly distributed pressure field, resulting primarily in global response.

Global response can generally be categorised into two principal modes: flexural response and shear response. Flexural behaviour is characterised by large deformations, extensive cracking primarily perpendicular to the element's main axis, yielding of the longitudinal reinforcement, and the formation of plastic hinges. Failure may occur through concrete crushing in the compressive zone or reinforcement fracture due to bending. Furthermore, if the member is properly restrained by the supports, membrane action may also develop and contribute to the overall capacity. Flexural response is generally considered the preferred response mode because it enables efficient energy dissipation through ductile deformation. This behaviour is more commonly observed in slender members with a low flexural resistance and low amount of longitudinal reinforcement.

Numerous studies have shown that RC elements subjected to blast loading exhibit a greater tendency towards a shear-dominated response than under equivalent static loading conditions [61], [62], [63], [64]. This phenomenon becomes more pronounced as the blast intensity increases. Two primary types of shear failure are commonly reported in the context of blast-resistant design: flexural-shear failure and direct shear failure.

Flexural-shear failure is associated with the formation of diagonal cracks resulting from the combined action of flexural and shear stresses. Since this mechanism requires the development of a certain deflection, it typically occurs after flexural response has been initiated. Direct shear failure, on the other hand, is a localised failure mode that occurs near the supports and is characterised by sliding along a well-defined failure plane [65], [66]. The associated cracks are typically oriented almost perpendicular to the longitudinal axis of the member. Unlike flexural-shear failure, direct shear failure develops at an early stage of the response, before significant bending deformation has occurred. Although localised in nature, direct shear failure is often considered a global failure mode because it is likely to lead to the complete loss of the load-carrying capacity of the structural member [67]. Shear failure is of particular concern because it is generally a brittle failure mode, especially in members lacking adequate shear reinforcement. Consequently, it severely limits the energy absorption capacity of the structure and should therefore be avoided in blast-resistant design.

Magnusson et al. [62] argued that dynamic loads possessing high-frequency content may activate higher-order vibration modes compared to what is possible under quasi-static loading. This implies that a larger proportion of the strain energy is connected to shear effects, as higher-order modes contribute more significantly to shear deformation than to flexural deformation.

Furthermore, shear failure often occurs during the early stage of the response and is closely related to the wave propagation phenomenon within the structural member. During the initial response phase, deformations tend to concentrate near the supports, while the remainder of the member behaves almost as a rigid body. This results in high shear demands, particularly under high-intensity blast loading. Shear-dominated behaviour is more commonly observed in heavily reinforced RC members with high flexural resistance and low shear span-to-depth ratios. A comprehensive review and contribution regarding shear failure mechanisms in impulsively loaded RC elements is reported by Peterson [68].

Current design approaches for blast-resistant structures are primarily based on guidelines and requirements presented in specialised design manuals and technical reports. Among these, the American UFC 3-340-02 [33] is arguably the most widely adopted guideline for the analysis and design of civil structures subjected to explosion threats [12]. In Sweden, structural design of blast-loaded structures is guided by the design standard FKR 2011 issued by the Swedish Fortifications Agency [35] and technical recommendations published by the Swedish Civil Defence and Resilience Agency, e.g. [69], [70]. In addition, compendia summarising current understanding and engineering practices have been developed for specific applications and industries. In Sweden, one such example is the report by Hassanzadeh et al. [71], prepared on behalf of the Swedish Radiation Safety Authority.

2.3.2 Evaluation of the response with a SDOF mass-spring system

There exist several methods for determining the response of structural elements subjected to blast loading, ranging from simplified methods—such as those based on energy principles [69]—to advanced, specialised methods with coupled fluid-structure interaction employing numerical simulations with CFD codes (for simulating the blast phenomenon) and finite element (FE) models (for evaluating the structural response) [72], [73]. In many situations, however, it is sufficient to achieve an approximate estimate of the maximum response at critical points of the element (e.g. deflection at midspan).

For this purpose, many structural elements can be represented by a simplified single-degree-of-freedom (SDOF) system, as illustrated in Figure 2.9. Consider a beam with mass m_{beam} (uniformly distributed), span L , width b , and subjected to a uniformly distributed blast pressure, $P(t)$. In the equivalent SDOF system, with the reference point at midspan, the mass of the structure acts as a lumped centre of mass and the structural resistance is described as a function of the displacement at this point. The structural response can then be represented by the dynamic equation of motion given by Eq. (2.3), in which m_b is the *equivalent* mass and $R_{\text{eq}}(u)$ is the *equivalent* resistance as a function of the displacement. The blast load is represented by the *equivalent* forcing function $F_{\text{eq}}(t)$ that acts on the centre of mass. The conversion of the continuous RC beam into the equivalent SDOF system is accomplished by using transformation factors for mass (κ_M), internal resistance (κ_R) and external load (κ_F). Thus, the dynamic equation of motion can be expressed in terms of the properties of the original system, as given by Eq. (2.4), in which $R(u)$ is the internal resistance of the original RC beam. It should be noted that the damping effects have been disregarded in this definition of the dynamic equation of motion. A comprehensive explanation and derivation of transformation factors for different situations can be found in e.g. [30], [74].

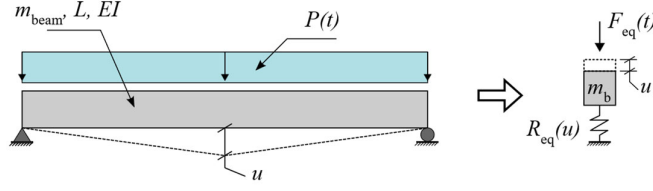


Figure 2.9. Transformation of a blast-loaded beam into an equivalent SDOF system.

$$m_b \ddot{u} + R_{eq}(u) = F_{eq}(t) \quad (2.3)$$

$$\kappa_M m_{beam} \ddot{u} + \kappa_R R(u) = \kappa_F b L P(t) = \kappa_F F(t) \quad (2.4)$$

By enforcing conservation of kinetic energy, the mass transformation factor of the system can be defined by Eq. (2.5), in which $\phi(x)$ is the assumed deflection shape function of the beam. The shape function depends on the boundary and loading conditions. For a simply supported beam subjected to a uniformly distributed load, $\phi(L/2) = 1$.

$$\kappa_M = \frac{1}{L} \int_0^L \phi^2(x) dx \quad (2.5)$$

The transformation factor for the external load can be derived considering conservation of external energy, as expressed by Eq. (2.6).

$$\kappa_F = \frac{1}{q(x)L} \int_0^L q(x) \phi(x) dx \quad (2.6)$$

Finally, the transformation factor for the internal resistance can be obtained by considering conservation of internal energy. The internal work is defined as the integral of the resistance function over the reference displacement. It is common to define the resistance in terms of the load distribution for which the dynamic analysis is conducted. Thus, the maximum resistance is the total static load that the RC element can sustain for the assumed load distribution. This definition, in conjunction with the principle that the external work applied on the structure must be in equilibrium with the internal work, implies that the resistance transformation factor must be equal to the load transformation factor, as indicated by Eq. (2.7).

$$\kappa_R = \kappa_F \quad (2.7)$$

Using the identity in Eq. (2.7), a simpler form of the dynamic equation of motion, as given by Eq. (2.8), is often used. It is obtained by dividing Eq. (2.4) with κ_F and defining the load-mass transformation factor, κ_{MF} , by Eq. (2.9).

$$\kappa_{MF} m_{beam} \ddot{u} + R(u) = F(t) \quad (2.8)$$

$$\kappa_{MF} = \frac{\kappa_M}{\kappa_F} \quad (2.9)$$

In practice, the resistance function $R(u)$ is usually described by an idealised ductile elastoplastic response, as shown in Figure 2.10. In the figure, R_M denotes the resistance to bending failure and $u_{M,d}$ the maximum allowed deformation. The enhancement of the material properties due to strain-rate effects is commonly incorporated in the calculation of the resistance in a simplified

manner by using dynamic increase factors (DIFs). A more detailed description of strain-rate effects and DIFs in RC structures can be found in e.g. [54], [75], [76], [77]. Furthermore, the assumption of ductile response implies that premature shear failure must be avoided to fully mobilise the flexural capacity.

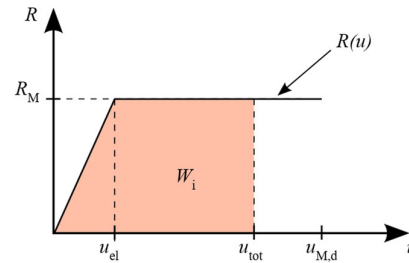


Figure 2.10. Idealised response of a RC beam failing under a ductile bending mechanism.

In Figure 2.10, the elastic portion of the resistance function extends from the origin to a theoretical yield point at a deformation u_{el} . The plastic portion is a line that extends horizontally from this yield point to failure. The failure criterion in the plastic domain is commonly specified by design codes as a rotational capacity at the support, θ_{pl} , which can be converted to the deformation capacity, $u_{M,d}$, at the element's midpoint. The part of the displacement corresponding to plastic deformation is not recoverable. That is, it is a permanent structural damage. The deformation u_{tot} is the maximum deformation caused by load. The energy dissipated by the structure (internal work, W_i) is equivalent to the area under the resistance curve up to u_{tot} .

It should be noted that the idealised function in Figure 2.10 may also be used for restrained RC elements, as specified by e.g. UFC 3-340-02 [33]. In that case, the effects of the support restraint are incorporated by allowing greater rotation limits, while consideration of concrete damage may be included by assuming that the top and bottom concrete cover has dislodged and do not contribute to the moment capacity.

Different approaches can be used to solve a SDOF system. For simple structures subjected to simple pressure pulses, the equation of motion can be solved analytically. The solution to simple standard cases is also available in charts in multiple design guidelines and books, e.g. [33], [74]. For more advanced cases, numerical methods may be needed [65], [78]. Examples of such numerical methods include the Newmark β method [79] and the Central Difference method.

2.4 Impact-loaded RC elements

2.4.1 General description

During their service life, reinforced concrete structures may be exposed to extreme dynamic actions resulting from accidental or intentional impacts. Although such events occur with relatively low probability, their consequences may be severe and, therefore, demand careful investigation. Impact loads are transient, concentrated loads of considerable magnitude that often act over a very short duration. They may arise from diverse sources, including vehicle collisions, dropped industrial equipment, falling debris or rocks, ice impacts, fragments generated by nearby explosions, and the collapse of overlying structures.

Impacts are commonly classified as either *hard* or *soft*, depending on the deformability of the impactor, which in turn governs the primary dissipative mechanism [80]. In a hard impact, the impactor essentially behaves as a rigid body and undergoes negligible deformation compared

with that of the impacted structure. Consequently, most of the kinetic energy associated with the impact is absorbed through damage and deformation of the structure. In contrast, in a soft impact, the impactor deforms substantially while the impacted structure remains comparatively undeformed, resulting in most of the input kinetic energy being dissipated within the impactor itself. The present study focuses exclusively on hard impacts on RC structures.

The behaviour of RC members subjected to impact loading has received significant attention from the research community. Comprehensive reviews of the existing literature are provided, for example, in [81], [82], [83]. More recent contributions have expanded the understanding of impact behaviour of RC members with advanced materials and novel reinforcement systems. These include the use of ultra-high-performance concrete (UHPC) [84], [85], fibre-reinforced polymer (FRP) reinforcement [84], [86], [87], carbon textile reinforcement [88], and steel fibres [89]. Zhang et al. [90] experimentally investigated the effects of an engineering cementitious composite (ECC) layer on the dynamic response and residual capacity of impact-loaded RC beams. Similarly, Liu et al. [91] examined the impact behaviour of beams fabricated with high-strength, high-ductility concrete (HSHDC), considering the influence of varying reinforcement ratios. Other recent research efforts have focused on approaches for determining sectional forces and developing shear-bending interaction diagrams under impact loading [92], [93], as well as on the response of RC members with restrained or fixed boundary conditions [94], [95]. Furthermore, Peterson et al. [96] investigated the influence of the shear span-to-depth ratios on the failure mechanisms of impact-loaded simply supported RC beams, highlighting its significance in the transition between flexure and shear-type failure modes.

The behaviour of structures subjected to impact loading differs significantly from that under static loading because of the influence of stress waves and inertia forces, the localised nature of impact loading, and strain-rate-dependent material properties. Several parameters may also significantly influence the impact response of RC elements, including the elements' geometry, mass, and boundary conditions, as well as the properties of the impactor (e.g. impact velocity, shape, orientation, mass, and rigidity). The presence of protective interlayers in the contact zone can also affect the response.

Throughout the response, dynamic equilibrium is maintained between the applied impact force, the support reaction forces, and the inertia forces. In addition, stress wave propagation plays a key role in the structural response and resistance, particularly during the initial response stage following contact [97]. Experimental and numerical studies have consistently shown that immediately after impact only a small part of the beam near the contact zone is accelerated, while the remainder remains stationary. This means that the reaction forces are initially zero, and the impact force is resisted solely by inertia forces [92], [97], [98]. These significant initial inertia forces generate high shear demands, which in turn may lead to the formation of diagonal shear cracks followed by shear plug failure. Due to this phenomenon, RC elements frequently exhibit shear-dominated failure mechanisms under high-intensity impact loading, even when their response under static loading is governed by flexure. Such shear-dominated response have been reported in numerous research studies, for example, in [98], [99], [100].

Local damage mechanisms due to impact loading include surface crushing, cratering, scabbing, penetration, complete perforation, and shear plug failures [101]. The extent of such damage is largely governed by the impact velocity and the mechanical properties of the concrete [80]. Indeed, local damage becomes increasingly pronounced under high-intensity, high-rate loading conditions [68]. Provided that the local effects do not result in system failure, the element

progressively enters a global response. Similarly to blast-loaded elements, this phase may be characterised by a ductile failure mode involving large deformations and the formation of distinct plastic-hinges, or by more brittle global shear-dominated failure modes.

If system failure due to local damages is avoided, the impact-induced stress wave propagates towards the supports, increasing the effective span of the member and mobilising a larger portion of its mass. The propagation of the impact wave generates moving regions of negative curvatures, which effectively create transient, fixed-like boundary conditions. Consequently, cracks develop on the top surface of the member (in members loaded on the top surface). These cracks normally close during the later stages of the response as the deformation pattern evolves. Eventually, the entire mass of the member is mobilised, and the deformed shape gradually approaches that observed under equivalent static loading conditions. The global response may then become flexure-dominated, characterised by the formation of vertical flexural cracks and yielding of the tension reinforcement. In this phase, reinforcement plays an important role in maintaining load-carrying capacity and ductility. Such a response is more commonly associated with low-intensity impacts with longer duration. It should be noted that, even when a shear plug forms during the local response phase, the presence of sufficient transverse reinforcement may still allow the element to develop a global response.

2.4.2 Classical impact theory

Consider the system in Figure 2.11. The body on the left-hand side, with mass m_1 , is moving towards the right-hand body at a constant velocity v_0 , that is $v_{m1,0} = v_0$. The other body, with mass m_2 , is at rest, that is $v_{m2,0} = 0$. The two bodies form an isolated system, and the momentum of this system must be preserved.

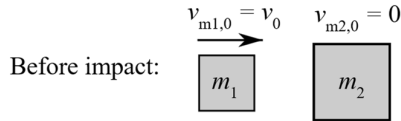


Figure 2.11. Two bodies before impact.

The kinetic energy, $E_{k,0}$, and momentum, p_0 , of the moving body just before impact are calculated with Eq. (2.10) and Eq. (2.11).

$$E_{k,0} = \frac{m_1 v_0^2}{2} \quad (2.10)$$

$$p_0 = m_1 v_0 \quad (2.11)$$

The velocities of the two bodies after impact can be described by Eq. (2.12) and Eq. (2.13), in which e_r is the coefficient of restitution, defined by Eq. (2.14). If the collision is perfectly elastic, $e_r = 1$. In contrast, a perfectly inelastic collision has $e_r = 0$.

$$v_{m1} = \frac{m_1 - e_r m_2}{m_1 + m_2} v_0 \quad (2.12)$$

$$v_{m2} = \frac{(1 + e_r) m_1}{m_1 + m_2} v_0 \quad (2.13)$$

$$e_r = \frac{|v_{m2} - v_{m1}|}{v_0} \quad (2.14)$$

If the collision is perfectly elastic, the total kinetic energy is preserved. Otherwise, some of the kinetic energy is dissipated during the impact event by dissipative mechanisms such as permanent indentation in the bodies or damping. The kinetic energy loss during the collision can be calculated using Eq. (2.15).

$$\Delta E_k = (1 - e_r^2) \frac{m_1 m_2}{2(m_1 + m_2)} v_0^2 \quad (2.15)$$

Therefore, the total kinetic energy in the system after collision, E_k , can be determined using Eq. (2.16).

$$E_k = E_{k,0} - \Delta E_k = \frac{m_1 + e_r^2 m_2}{m_1 + m_2} E_{k,0} \quad (2.16)$$

In an ideal inelastic collision ($e_r = 0$), the two bodies move together after impact. By virtue of Eq. (2.16), the remaining total kinetic energy in the system after the plastic collision is expressed by Eq. (2.17).

$$E_{k,pl} = \frac{m_1}{m_1 + m_2} E_{k,0} \quad (2.17)$$

2.4.3 Energy balance in an impact-loaded beam

2.4.3.1 External work

The impact force acting on a RC beam exerts an external work, W_e , on it. This work represents the energy that is transferred to the beam. Due to energy losses at contact, W_e is not equal to the impactor's initial kinetic energy, $E_{k,0}$, which can be calculated using Eq. (2.10). Indeed, part of this kinetic energy is dissipated at contact due to permanent indentations and damping in the different interacting material layers. This phenomenon is more relevant in situations where a protective interlayer dissipates a significant portion of the impact energy, reducing the effective energy that is finally transferred to the beam. This is represented by Eq. (2.18), in which the losses at contact have been grouped in the term $\Delta E_{\text{contact}}$. The quantity $\Delta E_{\text{contact}}$ is regarded as *external* losses from the beam's reference frame. Thus, by this definition, the losses $\Delta E_{\text{contact}}$ are not included in the work done on the beam.

$$W_e = E_{k,0} - \Delta E_{\text{contact}} \quad (2.18)$$

In the literature, three methods are commonly used to estimate the work done on an impact-loaded beam from experimental data [102], [103]. The first method consists in measuring the difference between the initial and residual kinetic energies of the impactor. However, this method yields the energy absorbed by the entire system, including losses during contact and potentially friction losses within the testing equipment. That is, it measures $W_e + \Delta E_{\text{contact}}$. Consequently, a complementary strategy is required to determine the amount of energy transferred to the beam. The second method estimates the work from the area integral of the impact force-deflection curve. This approach works best when the impactor and beam remain

in contact throughout the impact event. Significant discrepancies may arise if the beam and impactor separate for long periods. Furthermore, errors in the measurement of the impact force directly affect the calculated value of W_e . Additional errors may be introduced in cases involving significant fracture or detachment of beam fragments. Finally, some authors have estimated the energy dissipated by the beam from the area under the support reaction-deflection curve. However, this approach is questionable when there is a significant lag between contact and the development of the reaction force, or when the beam is not restrained against uplift. For these reasons, this method is not recommended by some authors [102].

This study adopted the second method, whereby the external work is estimated by integrating the impact force with respect to the beam's midspan deflection, u . The work done up to the maximum deflection, $u_{\max}$, can then be computed using Eq. (2.19), in which $F_i(t)$ is the impact force history determined from the experiment.

$$W_e(t = t_{u,\max}) = \int_0^{u_{\max}} F_i(t) du(t) \quad (2.19)$$

It should be noted that the result of Eq. (2.19) includes the elastic strain energy mobilised by the beam, which is subsequently released as the beam rebounds from its maximum deflection. An advantage of evaluating the work done at the time of maximum deflection is that the beam's kinetic energy is zero at this instant. In contrast, some authors integrate the impact force-deflection curve over the entire hysteretic loop, which ends when the impactor fully separates from the beam. This approach results in smaller values of the transferred energy, indicating the release of the elastic strain energy.

Under the assumption of an ideal inelastic collision, the applied external work at maximum deflection can also be estimated based on classical impact theory by assuming that the energy transferred to the beam equals the total kinetic energy remaining in the system, which is given by (2.17). This assumption, expressed in terms of beam and impactor mass in Eq. (2.20), is reasonable provided that the impactor and the beam move together following impact up to maximum deflection. In the equation, m_d and m_b denote the *equivalent* masses of the impactor and beam, respectively.

$$W_e(t = t_{u,\max}) = \frac{m_d}{m_d + m_b} E_{k,0} \quad (2.20)$$

If the impact occurs in the same direction as the gravitational acceleration, an additional external work, $W_{e,p}$, will act on the beam corresponding to the change of potential energy of the impactor and beam associated with the maximum deflection $u_{\max}$. If the impact occurs at the midpoint of the beam causing a plastic response, the additional work may be determined using Eq. (2.21), in which the real masses of the drop-weight (m_{drop}) and beam (m_{beam}) are used.

$$W_{e,p} = \left(m_{\text{drop}} + \frac{m_{\text{beam}}}{2} \right) g u_{\max} \quad (2.21)$$

2.4.3.2 Internal work

By the principle of energy conservation, the external work done by the impact force must be in equilibrium with the energy stored in the member as kinetic energy and the energy dissipated *internally* by other mechanisms. The general energy balance equation is given by Eq. (2.22).

$$W_e(t) = E_{k,b}(t) + E_i(t) + E_{fr}(t) + E_{damp}(t) \quad (2.22)$$

In which $E_{k,b}$ denotes the kinetic energy of the beam and E_i is the internal strain energy. The quantity E_{fr} denotes the work of fracture associated with local damage in the impact zone, such as scabbing or shear plugging. The last term, E_{damp} , comprises other energy losses, including damping in the beam, slip and friction in the supports, and slip between the concrete and reinforcement. For low-velocity impacts, the contribution of E_i is normally much greater than that of E_{fr} and E_{damp} . The internal strain energy, E_i , includes the work of deformation due to bending (E_b), shear (E_s), and membrane effects (E_m). Thus, E_i can be further expressed as:

$$E_i = E_b + E_s + E_m \quad (2.23)$$

Commonly, the energy dissipated by the shear and axial deformations is neglected, thus:

$$E_i \approx E_b \quad (2.24)$$

If the resistance function, $R(u)$, in terms of the midspan deflection is known, the internal strain energy due to bending at maximum deflection can be computed using Eq. (2.25).

$$E_b(t = t_{u,max}) = \int_0^{u_{max}} R(u) du \quad (2.25)$$

The internal strain energy due to bending action can be further divided into an elastic component, $E_{b,el}$, and a plastic component, $E_{b,pl}$. At the time of maximum deflection, $t_{u,max}$, the elastic strain energy is assumed to be fully mobilised. The elastic strain energy is then released when the beam rebounds after reaching its maximum deflection. The plastic strain energy, in contrast, is permanently consumed by crushing and cracking of the concrete and yielding of the steel reinforcement.

It should be noted that for restrained elements the contribution of the membrane effects may become significant. However, if the resistance function used in Eq. (2.25) was determined considering the membrane action effects, the contribution of the membrane effects to energy dissipation can be assumed to be implicitly considered. This is particularly relevant for the last stages of the resistance function, when the contribution of bending has decreased or been exhausted, and the load is carried mainly by catenary action.

At maximum deflection, the beam's velocity reduces to zero, which means that the kinetic energy is also zero. Thus, the energy balance can be expressed by Eq. (2.26).

$$W_e(t = t_{u,max}) = E_i(t = t_{u,max}) + E_{fr}(t = t_{u,max}) + E_{damp}(t = t_{u,max}) \quad (2.26)$$

Assuming that the internal strain energy is the main dissipative mechanism, and that the fracture energy and other energy losses (E_{fr} and E_{damp}) are negligible, the maximum deformation of the beam may be estimated based on energy balance principles by solving Eq. (2.27) [104], [105], which is derived by combining Eq. (2.24), (2.25) and (2.26).

$$W_e(t = t_{u,max}) + W_{e,p} = \int_0^{u_{max}} R(u) du \quad (2.27)$$

2.4.4 Evaluation of the response with a 2DOF mass-spring-damper system

Similarly to blast-loaded elements, the dynamic response of impact-loaded elements can be estimated by transforming the original system into a simplified two-degree-of-freedom (2DOF) system, as illustrated by Figure 2.12. In the figure, m_d and m_b are the *equivalent* masses of the drop-weight and the beam, while u_d and u_b represent the vertical displacement of the drop-weight and the beam's midpoint. The contact between the two bodies as well as the beam response are represented by the spring constants k_c and k_b , and damping coefficients c_c and c_b , respectively. The impact load is introduced into the system through the initial velocity of the mass m_d : $\dot{u}_d(0) = v_0$. The dynamic equations of motion for the 2DOF system are given by Eq. (2.28) to Eq. (2.30).

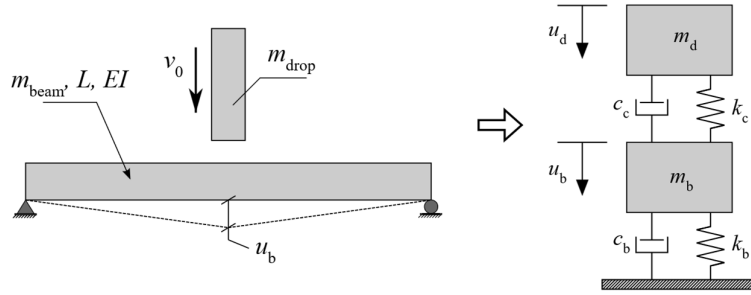


Figure 2.12. Equivalent 2DOF mass-spring-damper to represent the beam's impact response.

$$m_d \ddot{u}_d + F_{\text{imp}} = m_d g \quad (2.28)$$

$$m_b \ddot{u}_b + \frac{m_b \dot{u}_b}{2} + c_b \dot{u}_b + k_b u_b - F_{\text{imp}} = 0 \quad (2.29)$$

$$F_{\text{imp}} = F_k + F_c \quad (2.30)$$

The variable F_{imp} is the impact force, which equals the sum of the restoring force F_k and the damping force F_c , as given by (2.30). The impact force depends on several factors, including the impactor's mass, geometry, and initial velocity, the contact stiffness, and the material properties. The contact spring with stiffness k_c is often assumed to have a linear behaviour, which implies that F_k may be calculated according to Eq. (2.31). However, more complex approaches, such as those based on Hertz law, as given by Eq. (2.32), can provide a better description of the contact phenomenon [106]. The damping force may be estimated based on Eq. (2.33). It should be noted that the definition of k_c and c_c varies across the different methods available.

$$F_k = k_c (u_d - u_b) \quad (2.31)$$

$$F_k = k_c (u_d - u_b)^{3/2} \quad (2.32)$$

$$F_c = c_c (\dot{u}_d - \dot{u}_b) \quad (2.33)$$

The load-mass transformation factors required to convert the drop-weight and beam into an equivalent 2DOF system can be determined with an analogous procedure as that described in Section 2.3.2. In situations in which the stiffness of the drop-weight is much greater than that of the beam (i.e. a hard impact), it can be demonstrated that m_d equals the real drop-weight

mass. The load-mass transformation factor for the beam, $\kappa_{MF,b}$, has traditionally been determined by assuming a constant shape function during the entire impact event. However, in recent years, it has become more common to account for the effects of impact wave propagation along the element's span during the local response phase [107], [108], [109]. The propagation of the impact wave means that both the element's effective span and shape function vary with time. Consequently, both m_b and $\kappa_{MF,b}$, as well as the beam stiffness k_b are treated as time-dependent variables in the dynamic equations of motion. Furthermore, to preserve conservation of energy, the governing equations of motion should be modified to account for the rate of change of mass, $\dot{m}_b$, which explains the second term from the left in Eq. (2.29).

2.4.5 Evaluation of the response with empirical methods

2.4.5.1 Maximum deflection

The peak deflection under impulsive loading is arguably the most important response parameter, as it directly relates to the performance and load-carrying capacity of the structural element. Indeed, as discussed in Section 2.3.2, the maximum deflection is associated with the deformation limits specified by design standards. Therefore, several prediction methods have been developed to estimate it. The 2DOF mass-spring-damper system presented in Section 2.4.4 provides a relatively simple yet accurate approach to determine the maximum deflection. However, in certain situations, particularly during the preliminary design stage, it may be desirable to employ a method that can provide reasonably accurate deflection predictions using only a limited number of input parameters with minimal computational effort.

Several authors have reported a strong correlation between the input impact energy $E_{k,0}$, the static load capacity, P_{st} , and the maximum deflection, u_{max} . This relationship is commonly expressed by the general formulation in Eq. (2.34), in which η is an empirical coefficient that depends on the beam configuration, loading conditions, and failure mode. For simply supported RC beams with conventional reinforcement and flexural response, η has been found to vary between 0.52 and 0.63 [110], [111], [112]. In ultra-high performance concrete (UHPC) beams subjected to low-velocity impacts, the factor has been reported as 0.71 [113]. For beams reinforced with fibre-reinforced polymer (FRP), η has been reported to vary between 0.80 and 0.85 [114].

$$u_{max} = \eta \frac{E_{k,0}}{P_{st}} \quad (2.34)$$

Other authors have proposed more sophisticated empirical formulations that incorporate additional factors influencing the structural response, such as beam stiffness, concrete strength, and reinforcement ratios [87], [115].

2.4.5.2 Residual deflection

Another relevant response parameter is the residual deflection, which is commonly used as an indication of permanent damage, dissipated energy, and residual post-impact capacity. Similar to the peak deflection, several authors have proposed an empirical linear relationship between the residual plastic deflection u_{pl} , $E_{k,0}$, and P_{st} , as expressed by Eq. (2.35), in which the empirical parameter ϑ depends on the beam's properties, loading conditions, and failure mode. For simply supported RC beams with conventional reinforcement, ϑ has been found to vary between 0.42 and 0.46 [110], [111], [112].

$$u_{pl} = \vartheta \frac{E_{k,0}}{P_{st}} \quad (2.35)$$

2.5 Response of restrained RC elements

RC elements can have their free axial deformation and rotation restrained by the supports or neighbouring structural components. Depending on the degree of restraint, in-plane membrane forces may develop within the element, potentially enhancing its ultimate resistance and deformation capacity [116]. Restrained RC elements subjected to transverse loading typically exhibit two successive response phases: *compressive membrane action* (CMA), commonly called arching action; and *tensile membrane action* (TMA), also known as catenary action.

The response of a restrained RC element is compared with that of an equivalent unrestrained element in Figure 2.13. The outward movement of the element's ends is hindered by the BCs, inducing compressive membrane forces, which enable greater load-carrying capacity (point B). At point C, the membrane forces reduce to zero, and the element's ends begin to move inwards. If the steel reinforcement is adequately anchored at the supports, tensile axial forces develop, and the element enters the TMA phase. During the early part of this phase, bending may still contribute to the resistance. However, as cracking progresses through the depth of the section, the load is carried predominantly by the reinforcement through catenary action. Point D denotes the ultimate deformation limit, governed by fracture of the reinforcement. The reinforcement at different sections and layers may fracture at different times before system collapse at point D, which would be registered as discontinuous loss of capacity in the load-deflection curve. Based on the active reinforcement, Smith [117] divides the TMA phase into two stages: *Primary* and *secondary* catenary action. The former implies that no reinforcement has fractured. The latter begins following fracture of the extreme tension reinforcement (either at the support or midspan), provided that the element is doubly reinforced.

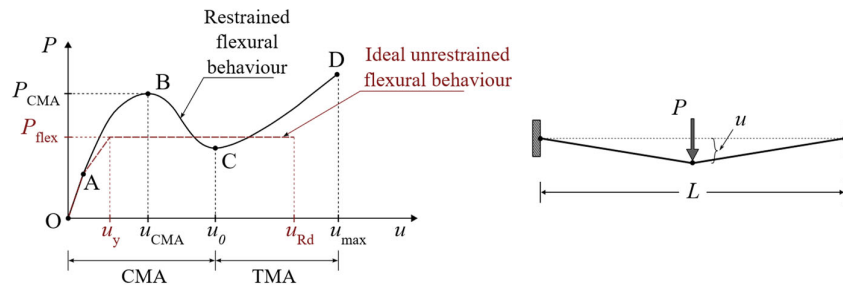


Figure 2.13. Typical resistance function of a restrained RC member compared with that of an equivalent unrestrained system. Adapted from **Paper VI**.

Membrane action in RC elements, particularly those subjected to extreme dynamic actions, has been the subject of extensive research since it was first identified by Ockleston in 1954 [118]. Significant contributions from the previous century, such as the works of Park [119], [120], Keenan [121], and Black [122], focused mostly on membrane action in RC slabs subjected to uniform loads, including blast loading. Tensile membrane action has also been recognised as a valuable reserve capacity for emergency load redistribution following column removal [123]. Indeed, this application arguably became the predominant focus of membrane action research during the past two decades. More recent studies have extended the scope to include other element types, including precast concrete beam–column sub-assemblages [124] and beam-slab

substructure [125], [126]. In contrast, the response of restrained RC elements subjected to impact loads has received significantly less attention. Notable contributions include the works of Cui et al. [127], Liu et al. [128], and Wang et al. [129].

Utilisation of membrane action in standard design practice remains limited. Contributing factors include uncertainty over the restraint at the boundary conditions, the extensive damage and large deformations associated with TMA, and the absence of a universally accepted design model. However, the additional capacity due to membrane action could be highly valuable in extreme loading scenarios, for which more uncertain design assumptions may be justified and severe damage may be tolerated provided collapse is avoided. This indicates that, with a better understanding of membrane action and appropriate predictive methods, more efficient and economical designs could be realised for structures required to resist severe dynamic events.

The prediction of the response of restrained RC elements has also been of interest within the research community. A range of analytical and numerical models has been developed, either to determine the ultimate load-carrying capacity or to estimate the complete load-deflection function. Normally, the two stages of membrane action, CMA and TMA, are treated separately. Among the analytical models developed for the CMA phase, the formulation proposed by Park [119] is one of the most widely recognised. Several authors have extended Park's model to incorporate additional features [130], [131], [132], [133], [134]. Analytical models describing the TMA phase are typically derived from an assumed catenary mechanism. For one-way systems, this assumption leads to relatively simple analytical expressions. One of the most widely adopted models is that proposed by Park [120] for uniformly loaded, two-way spanning rectangular slabs. Indeed, some design guidelines and standards, including the UFC 3-340-02 [33], are based on Park's work. More recent developments include the method proposed by Pham and Tan [135], in which the global response of beam-column sub-assemblages subjected to a concentrated load is represented by an analytically derived multi-linear resistance curve. Similarly, Moserrat-López [136] developed a method for predicting the complete response based on limit analysis supported on experimental research and advanced finite element (FE) analysis.

Research on the dynamic response of restrained RC elements remains limited compared with the extensive body of research available on their static response. Furthermore, the majority of studies addressing dynamic response have focused on elements subjected to blast loading. Recent contributions in this area include the work of Jia et al. [137], who numerically investigated the influence of membrane action on the response of RC one-way slabs subjected to blast loading and subsequently developed a SDOF model that accounts for both membrane action and strain-rate effects. Peterson et al. [138] examined, among other aspects, the development of compressive membrane action in RC walls exposed to blast loading within a shock tube and proposed a simplified spring model for predicting axial force development based on static axial stiffness and rigid-body kinematics. Similarly, Cui et al. [139] presented a methodology for incorporating membrane action into the simplified dynamic analysis of restrained RC beams subjected to blast loading.

In contrast, relatively few studies have specifically examined the impact response of restrained RC beams. Among the limited contributions available, the work of Cui et al. [95], [127] is particularly noteworthy. They employed an analytical approach to describe the local response phase, whereas the global response was represented by a SDOF system. Although the method demonstrated satisfactory predictive accuracy, it also has some limitations. Notably, the contact

phenomenon is not explicitly modelled. Furthermore, the method does not directly provide estimates of the impact force or membrane forces. Liu et al. [128] adopted an equivalent SDOF model to predict the response of restrained RC beams subjected to a dynamic load at midspan. In their study, simplified equivalent multi-linear resistance functions were developed to represent beam stiffness depending on the impact velocity. However, the method does not explicitly distinguish between the local and global response stages and requires prior knowledge of the applied load. Therefore, further work is needed to expand the capabilities of such models to predict the dynamic response of impulsively loaded restrained RC beams.

Other significant gaps remain to be addressed. A major limitation among the existing analytical models for the CMA phase is that they generally do not account for reinforcement fracture. This simplification may lead to inaccuracies in the estimation of the load-displacement curve and the available internal strain energy. The omission of reinforcement fracture is likely attributable to the fact that it is seldom observed during the CMA phase in slender RC elements or in elements reinforced with highly ductile steel reinforcement. Nevertheless, studies such as [117], [140], [141] have demonstrated that reinforcement fracture can occur during the CMA phase, and it can significantly influence the response.

Finally, several numerical approaches based on the FE method and with varying levels of complexity have also been proposed; see e.g. [123], [140], [142], [143]. These models are generally capable of predicting the structural response with a relatively high level of accuracy and can capture complex behavioural phenomena that are difficult to represent analytically. However, they often require advanced modelling techniques, significant computational resources, and specialised expertise, which may limit their applicability in practical engineering applications. There is therefore a need for robust and validated simplified FE models that can be implemented using commercially available software. Such models would provide a practical means of bridging the gap between advanced research methodologies and engineering practice, facilitating the assessment of restrained RC structures under extreme loading conditions.

Vapour cloud explosions in traffic environments

3.1 Overview

This chapter summarises the work conducted to address research questions **RQ-1** and **RQ-2**, within **Research Area I**, which concern the simplified prediction of blast loads from vapour cloud explosions (VCEs) in traffic environments. The work was presented in detail in **Papers I** and **II**.

Estimating the blast loading from VCEs on urban roads is a challenging task. Unlike high-explosive detonations, the severity of VCEs can vary greatly depending on factors such as confinement, congestion, and mixture composition and concentration. This variability complicates the prediction of the blast load generated by the VCE. Simplified engineering methods, such as the TNO Multi-Energy method (MEM) and the Baker-Strehlow-Tang (BST) method, are commonly used to estimate the relevant blast wave parameters. However, these approaches require the specification of the strength of the blast source as a key input, making accurate characterisation of the explosion scenario essential. While guidelines for the choice of the blast strength for different standard conditions exist—primarily in settings within the petrochemical industry—these recommendations are not directly applicable to traffic environments. In such cases, the strength of the blast source may be more accurately estimated through comparison with experimental data or advanced numerical simulations. However, relevant experimental studies concerning gas explosions on roads are scarce, and new experiments are often prohibitively expensive. Similarly, advanced numerical simulations are often computationally demanding and require expertise beyond that of most structural engineers and risk analysts. As a result, practitioners often rely on their own experience and judgement when estimating the strength of VCEs in traffic scenarios, which can lead to considerable disparities and variability in the predicted blast wave parameters.

Paper I focused on identifying the scenario parameters that had the greatest influence on the explosion *strength*. In this context, *strength* refers to the overpressure generated by the VCE. At this stage, a large number of parameters and scenarios were analysed. Thus, the study employed a two-stage factorial design approach to conduct the parametric study using a practical level of computational resources. **Paper I** also included a preliminary evaluation of the MEM for the scenarios of interest to determine the subsequent direction of the research.

Paper II expanded upon the work presented in **Paper I** by conducting one-factor-at-a-time (OFAT) analyses aimed at confirming the main effects of the most influential parameters. Based on these results, the paper developed and verified guidelines for applying the MEM for estimating the blast load generated by VCEs in the environment of interest. This was done by correlating both the strength class of the explosion and the effective gas volume at the source of strong blast—the primary inputs in the MEM—to the most relevant scenario parameters. In addition to evaluating the explosion strength, **Paper II** also examined the prediction of the impulse to enable the full description of the blast load.

The study was conducted using numerical simulations based on computational fluid dynamics. The software FLACS-CFD [44] was used. One of the reasons for this choice was the widespread use and recognition of FLACS-CFD within the industry around the globe. Furthermore, several research studies concerning both the evaluation of gas explosion scenarios using FLACS-CFD and the development and validation of the code are available in the literature [144], [145].

Prior to the work in **Paper I**, a series of preliminary studies—aimed at validating the selected tool and identifying appropriate modelling techniques and model parameters for the conditions under consideration—was conducted. That work was not included as part of this thesis, but it can be consulted in [146]. Initially, a grid sensitivity analysis was conducted. A strong grid-dependency was identified in FLACS-CFD. Consequently, comparisons with gas explosion experiments were carried out in order to determine an appropriate grid cell size. Based on these comparisons, it was concluded that six grid cells along the smallest dimension in the region under the vehicle provided reasonable results. This resulted in a grid cell size of 50 mm, which was used for all models in the main numerical study.

3.2 Description of the studied scenarios

The scenarios investigated were based on a hypothetical release of a flammable gas during transport in an open traffic environment. The released fuel gas was assumed to disperse and mix with the surrounding air, forming a vapour cloud that engulfed several vehicles located on or near the road. A range of scenarios were derived from this common setting. To enable the evaluation of multiple parameters across a large number of scenarios, several simplifications were introduced. Key simplifications concerned the shape and concentration of the gas cloud and the geometry of the vehicles. The geometry of the mock-up vehicle is shown in Figure 3.1.

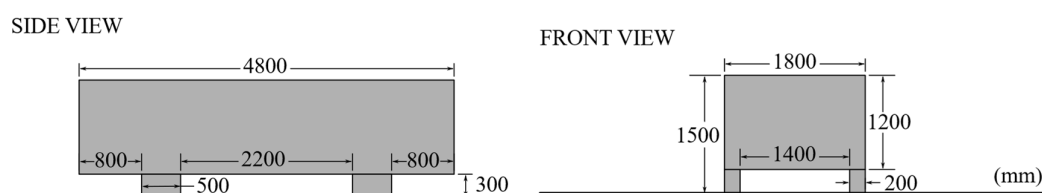


Figure 3.1. Geometry of the mock-up vehicle.

A schematic representation of the gas cloud and the configuration of the group of vehicles is presented in Figure 3.2. The studied scenarios were described in terms of six parameters:

- **A**: Distance between vehicles.
- **B**: Extension of the gas cloud in the horizontal plane outside the group of vehicles, measured from the edge of the group of vehicles.
- **C**: Height of the gas cloud, measured from the ground.

- D : Number of vehicles in the transverse direction (y -direction in Figure 3.2).
- E : Number of vehicles in the longitudinal direction (x -direction in Figure 3.2).
- F : Location of the ignition point.

An important decision made during the design of the case study concerned the choice of the fuel gas. Due to the heavy computational demands, it was decided to evaluate only a single fuel gas type. Therefore, only *stoichiometric* mixtures of *propane* and air were considered. Propane was selected because it appears to be the most widely transported flammable gas in Sweden and it frequently forms the basis of risk analyses conducted in the country [147], [148].

Different scenarios were generated by varying the scenario parameters. In **Paper I**, the investigation was divided in two stages. Initially, a screening 2^{6-1} fractional factorial design, involving the six basic defining parameters at two levels each, was carried out. This factorial design comprised 32 combinations of factors. The four most significant factors emerging from the screening analysis were subsequently evaluated using a 3^{4-1} fractional factorial design, in which 27 scenarios were analysed. In total, 53 scenarios were evaluated with the factorial design approach (six scenarios appear in both factorial designs).

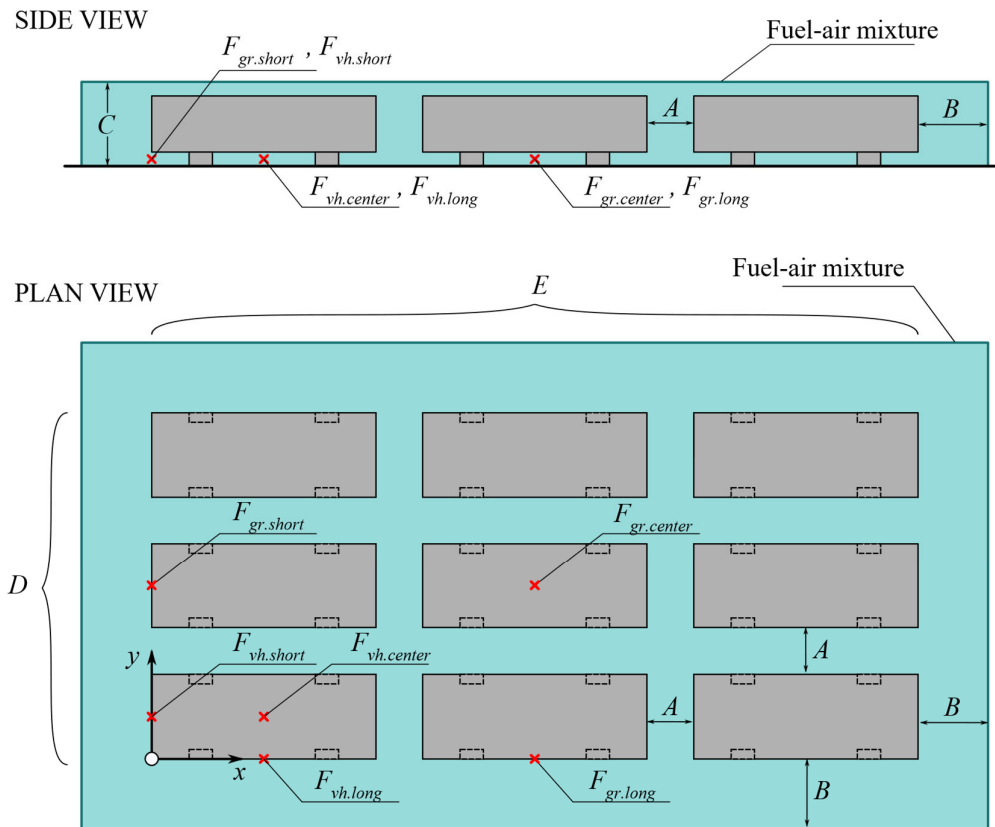


Figure 3.2. Parameters defining the studied VCE scenarios as described by Lozano [149].

Paper II conducted OFAT analyses aimed at confirming the main effects of the most influential parameters identified in **Paper I**. A total of 38 scenarios were analysed during this stage. Finally, **Paper II** developed and verified guidelines for applying the MEM to the scenario of interest based on the simulation results. For this purpose, additional 23 new scenarios were examined.

3.3 Main results

3.3.1 Explosion overpressure

To facilitate characterisation of the overpressure distribution, the results were “flattened” in the vertical direction, such that the value at each point (x, y) corresponds to the greatest overpressure along the z -direction. This means that the values in the two-dimensional contour plots may have been collected at different values of z . Moreover, for the calculation of the maximum overpressure, a uniform filter with a kernel size of 10×10 cells (i.e. 0.5×0.5 m) was first applied to the contour plots to smoothen out potential extreme peaks of overpressure. This filtering procedure was only done when calculating the maximum overpressure. All contour plots of overpressure presented in this thesis, as well as in **Papers I** and **II**, are unfiltered.

For the scenarios examined in this study—in which the gas mixture consisted of equivalent stoichiometric clouds of propane and air—the maximum overpressure values ranged from 2 kPa to 380 kPa. The 25th percentile and 75th percentile of the dataset were approximately 9 kPa and 35 kPa, respectively. Moreover, around 90% of all scenarios produced overpressure values lower than 50 kPa. That is, although explosion strength greater than 50 kPa may occur in the environment of interest, such levels arise only in a limited number of cases and under the most unfavourable conditions considered in this study. Furthermore, it was found that high overpressure values developed only within localised areas with relatively small source radii (in relation to the overall size of the congested region). This implies that the energy contributing to high explosion strength was limited relative to the total gas inventory. This behaviour can be observed in the contour plot of peak overpressure for the most critical scenario in **Paper I**, shown in Figure 3.3(a). It is evident that the highest overpressure values occurred in localised zones just outside the group of vehicles, opposite the location of the ignition point. The presence of this localised, stronger explosion outside the group of vehicles is a consequence of flame acceleration and pressure build-up as the flame travelled through the congested and confined region.

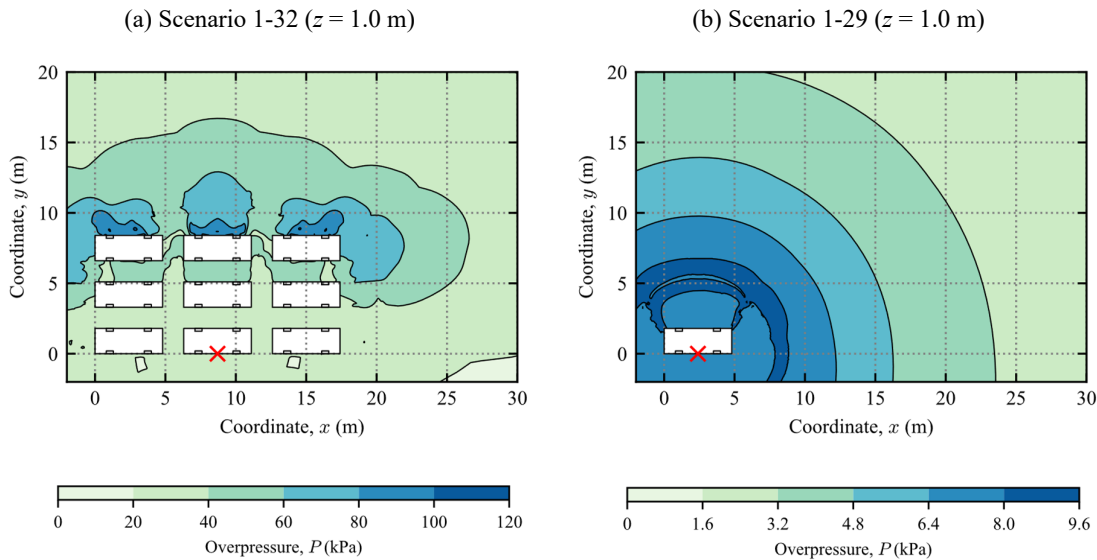


Figure 3.3. Contour plot of maximum overpressure for two scenarios from **Paper I**. Defining parameters in common: $A = 1.5$ m, $B = 4.0$ m, $C = 3.6$ m, $F = F_{gr.long}$. The red cross represents the location of the ignition point.

In contrast, weaker explosions in general exhibited a more symmetrical distribution of maximum overpressure. Indeed, in several cases, the pressure decay resembled the distribution commonly associated with hemispherical explosions, as shown in Figure 3.3(b) for a representative scenario.

3.3.2 Parametric study

The number of vehicles in the transverse direction (factor D) emerged as the most influential parameter from the 2^{6-1} factorial design in **Paper I**. In contrast, the number of vehicles in the longitudinal direction (factor E) exhibited the least impact on the outcome. In addition to factor D , factors A , B , and C , as well as their interactions with factor D , were also found to exert an important influence on the maximum overpressure generated by the VCE. For this reason, the effects of these four factors were evaluated in greater detail in the subsequent 3^{4-1} factorial design. The main effects obtained from this factorial design are presented in Figure 3.4, where a distinct trend can be discerned for the main effect of each factor.

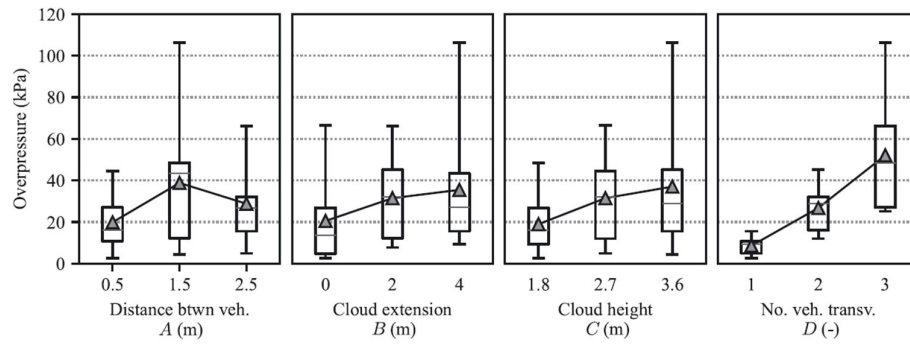


Figure 3.4. Main effects obtained from the 3^{4-1} fractional factorial design. Taken from **Paper I**.

To verify these trends, **Paper II** carried out OFAT analyses, which allowed a wider range of levels to be explored for each factor. The results of these analyses are summarised in Figure 3.5. Overall, the trends suggested by the 2^{6-1} factorial design were confirmed. However, a new behaviour, outside the range covered by the study in **Paper I**, was identified for large values of factor D .

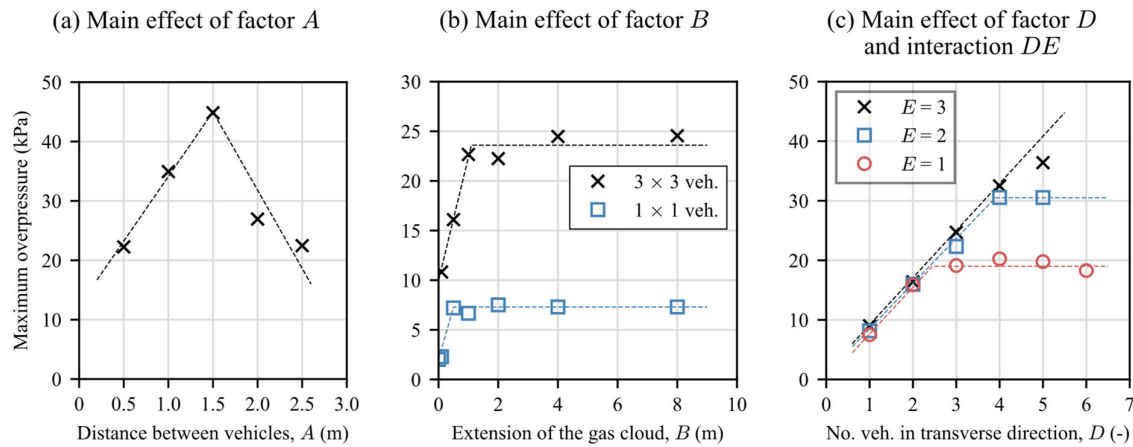


Figure 3.5. Summary of the results from the one-factor-at-a-time analyses in **Paper II**.

The results presented in **Paper I** showed that increasing factor D consistently led to greater peak overpressure values. This behaviour was attributed to two mechanisms: (i) the increase in flame-travel distance through confined and congested conditions provided by increasing the number of vehicles; and (ii) the interaction of the flow underneath the vehicles with the wheels. Thus, according to this description, the difference in the effects of factors D and E lies primarily on the flow's primary direction in relation to the orientation of the wheels. Therefore, further analyses were carried out in **Paper I** to confirm the influence of the shape and orientation of the obstacles underneath the vehicles on the resulting overpressure.

The analysis in **Paper II** revealed that the effect of D levels off as its value increases. Furthermore, the value at which the effect of D plateaus increases with increasing E . That is, the interaction DE was found to be significant for scenarios outside the domain covered by the factorial design in **Paper I**. These findings indicate that for elongated configurations the attenuating effect of side venting overcomes the overpressure enhancement due to turbulence across the congested region.

The separation distance between vehicles (factor A) was found to have a pronounced non-linear effect. Up to a certain critical distance, increasing A resulted in greater overpressure. This may be related to a combination of extended flame-travel length while maintaining sufficient turbulence levels to promote flame acceleration and pressure build-up. However, beyond this critical distance, further increasing A yielded a reduced maximum overpressure value. This was attributed to the decrease in flame speed as pressure relief in the region between vehicles becomes more significant. In this study, the critical distance was 1.5 m, approximately equal to the vehicle width. Finally, assuming that the main effect follows a linear behaviour for $A > 1.5$ m, it can be concluded that scenarios with $A \approx 3.0$ m would produce similar overpressure values compared to cases with one single vehicle. This suggests that for scenarios with $A \geq 3.0$ m, each individual vehicle may be considered as a separate explosion. It is reasonable to assume that these findings depend on the vehicle geometry, although this could not be verified in the present study because the vehicle geometry was kept constant throughout.

The factors associated with the dimensions of the gas cloud (factors B and C) initially exhibited a positive influence on maximum overpressure. However, their effects levelled off as the factor levels continued increasing. In other words, pressure enhancement subsided once a critical gas volume was reached and further increase in cloud size did not produce a significant increase in overpressure. Two main mechanisms are believed to govern this behaviour. First, flow exiting the congested region generates a recirculation zone behind the group of vehicles, where flame acceleration and pressure build-up is still possible. As B increases, more flammable material is set into turbulent flow in the recirculation zone before ignition, which results in greater overpressure. However, since the recirculation zone has a limited size, increasing the volume of flammable material beyond a certain threshold will receive no further contribution from the recirculation zone. The second mechanism is the delay of venting of the combustion products through the sides of the configuration due to a larger gas volume outside the congested region, which in turn increases the flame speed inside the congested region, leading to greater overpressure.

The parametric study in **Papers I** and **II** focused mainly on the maximum overpressure. However, it is also of interest to investigate the effect of the relevant factors on the *mean* peak overpressure, which primarily governs the blast characteristics in the far field. The mean peak overpressure was defined as the average of the peak overpressure values within a bounding box

extending 0.5 m in all directions from the outer edge of the outmost vehicles. The main effects of the scenario parameters on the mean peak overpressure were found to follow a similar trend to that observed for the maximum overpressure. As an example, Figure 3.6 shows the main effects of factors *A*, *B* and *C* on the mean peak overpressure obtained from the scenarios in **Paper II**. Overall, the mean peak overpressure was markedly lower than the corresponding maximum overpressure. The variability across scenarios was also more subdued.

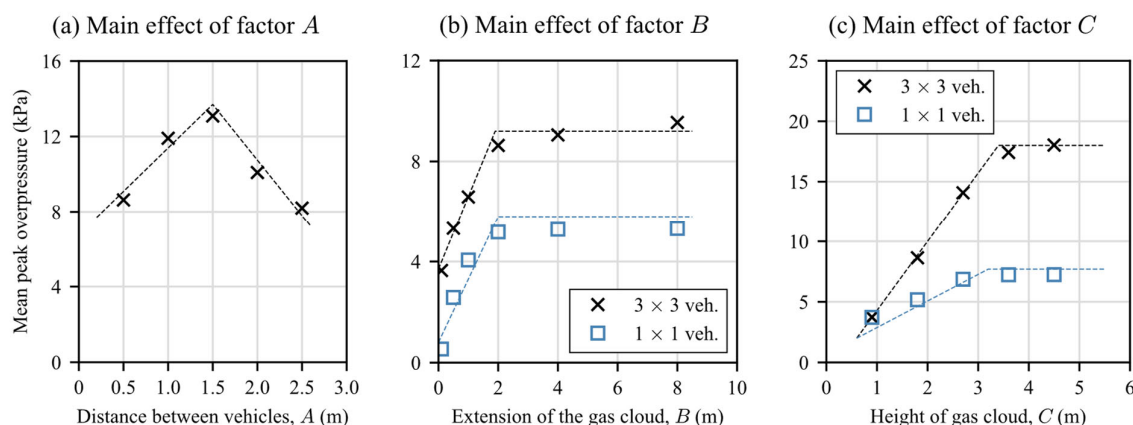


Figure 3.6. Summary of the results from one-factor-at-a-time analyses concerning the mean peak overpressure in the scenarios in **Paper II**.

Paper II also investigated the relationship between the most influential parameters and both the strength class and the mobilised gas volume at the source of strong blast. The objective was to develop recommendations for application of the MEM in the environment of interest. The resulting guidelines are summarised in Section 6.1.

3.4 Reflections

The study identified a strong relationship between the number of vehicles in the transverse direction and the resulting overpressure. Additional analyses revealed that this dependency was associated with turbulence generation around the wheels and the orientation of the wheel in relation to the primary flow direction. This behaviour is analogous to the well-established relationship between flame speed (and thus overpressure) and the number of obstacles in congested industrial settings [25], [26], [27], although the obstacles in such settings are typically of smaller scale (e.g. piping systems). These findings suggest that flame acceleration and pressure build-up are dictated not only by the number of vehicles, but also, and perhaps to a significant extent, by the number and orientation of smaller geometric obstructions beneath them. This hypothesis is supported by the comparative study of Lozano et al. [150], which considered three configurations: a 2×2 vehicle arrangement, an equivalent configuration with the wheels removed (i.e. “floating” vehicles), and an unconfined scenario. The configuration with floating vehicles yielded a maximum overpressure approximately twice that of the unconfined case, reflecting the influence of the main body of the vehicles. In contrast, the full-vehicle configuration generated an overpressure approximately four times greater than that of the unconfined scenario, highlighting the critical role of small-scale geometric features in increasing explosion severity.

The influence of the number of vehicles was also found to be sensitive to the aspect ratio of the configuration. Elongated layouts were found to promote venting, thereby reducing the effect of

the number of vehicles. These results are consistent with similar observations based on computational fluid dynamics reported by Puttock [151] for elongated, narrow areas in process plant environments. This effect was not captured in the initial factorial analysis in **Paper I** but was revealed by the OFAT study in **Paper II**. This highlights the effectiveness of combining the two adopted approaches to identify the most influential parameters and reduce the risk of missing important effects.

Notably, several qualitative guidelines for practical application of the MEM and similar approaches do not explicitly account for the size of the configuration, instead focusing on parameters such as volumetric blockage ratio (VBR) and congestion level. In contrast, the present study revealed that the overall dimensions and aspect ratio of the configuration may significantly influence explosion strength in a traffic environment. Nevertheless, the influence of configuration size, or more correctly the distance travelled by the flame, has been recognised in quantitative approaches for estimating the maximum overpressure, such as those proposed by Eggen [52] and Li et al. [152].

A key finding was that the gas cloud volume influenced not only the energy released by the explosion, but also the resulting strength class. The overpressure was found to increase as the cloud geometry transitioned from a shallow “pancake” shape to a cloud height extending above the group of vehicles. A similar effect was noted for increments of the dimensions of the gas cloud in the horizontal directions. However, after a certain gas volume, no further significant increase was obtained. Such effect of the gas volume is not originally accounted for in the traditional MEM framework. Therefore, it would be beneficial to confirm this behaviour with experiments or additional numerical calculations. It should also be noted that Puttock [151] and Cates [153] previously reported that the gas volume extending 2 m in all directions from the edge of the congested region should be considered as part of the source of blast. However, they did not explicitly link this extended volume to an increase in the maximum overpressure.

Based on the above, the guidelines developed to adapt the MEM to traffic environments in **Paper II** (see Section 6.1) conservatively assume that the vehicle group is fully immersed in a stoichiometric fuel-air cloud of sufficient volume such that effect of the gas volume on overpressure is maximised. While previous studies indicate that equivalent gas clouds of such volumes may be possible given the right conditions [154], [155], the resulting estimates should be interpreted as upper-bound values suitable for worst-case assessments in the absence of detailed dispersion data. In practice, however, a detailed dispersion analysis may predict smaller equivalent gas clouds.

A straightforward solution to reduce conservatism in the predicted blast load is to use the reduced gas volume obtained from a dispersion analysis while retaining the strength class recommended in the guidelines. However, this approach is expected to have a limited influence on the near-field overpressure predictions. An alternative strategy is to introduce reduction factors to adjust the predicted blast load. Such reduction factors could explicitly account for the effects of the scenario parameters whose effects were deliberately maximised in the derivation of the guidelines. For instance, the reduction factor associated with parameter A could be taken as 1.0 for scenarios with $A \leq 1.5$ m, and then decrease linearly with increasing A . Similarly, the reduction factors accounting for the effects of B and C could increase linearly from a minimum value at $B = 0$ and $C = 0$ m to 1.0 at $B = 2.0$ m and $C = 2.5$ m, remaining constant for larger values of B and C . However, the applicability of these conclusions to other vehicle geometries and traffic layouts should be verified in future research.

Finally, it is important to emphasise that the predictions obtained using the MEM in conjunction with the developed guidelines cannot be expected to be more accurate than those produced by the reference CFD simulations on which they are based. Although the CFD software employed has undergone extensive validation, its predictive accuracy can generally be expected to be no better than approximately $\pm 30\%$, which represent the error margins commonly adopted in the validation of similar CFD applications [39], [156], [157]. Indeed, for certain cases, predictions with the CFD code may be considered satisfactory if they fall within a factor of two from the experimental results [39]. This relatively broad acceptance range reflects several inherent sources of uncertainties. First, CFD simulations usually rely on modelling assumptions and simplifications in the governing equations, such as the use of simplified averaged models for describing turbulent fluctuations instead of directly solving the full spectrum of turbulent flow (which would be extremely costly). Second, explosion experiments often exhibit low repeatability, meaning that tests performed under nominally identical conditions may produce markedly different results. Consequently, defining a single reference value for validation is inherently challenging. Third, the use of the Porosity Distributed Resistance (PDR) approach and associated sub-grid models introduces grid dependency into the calculations [158], [159], which makes it difficult to use a general mesh cell size that is equally appropriate for all regions of the model and across multiple scenarios.

Chapter 4

Probability of shear failure in blast-loaded slender RC one-way slabs

4.1 Overview

This chapter addresses **RQ-3** within **Research Area II**, which concerns the probability of brittle shear failure in blast-loaded slender RC one-way slabs.

RC slabs subjected to blast loads are primarily designed to dissipate the imposed energy by means of large inelastic deformations, rather than relying on large load resistance. This means that brittle shear failure with limited deformation capacity must be avoided. However, due to uncertainties involved in the design of blast-loaded structures, it may be difficult to calculate the real bending and shear resistance of the element. This makes determining the governing failure mode difficult. Such uncertainties stem, for example, from the statistical variation of the material and geometric properties, the simplifications in the resistance models, unknown characteristics of the blast load, and the use of the lower characteristic values of material properties and partial safety factors in the current design standards.

Several research studies regarding shear behaviour of blast-loaded RC elements are available in the literature. For instance, Magnusson et al. [62] investigated the influence of the characteristics of the load and the structural wave propagation on the shear behaviour of blast-loaded RC beams. Some studies regarding the influence of shear effects on the moment capacity of statically and dynamically loaded RC structures are also available, see e.g. [30]. However, no research concerning the effects of statistical uncertainties in the interaction between shear resistance and bending resistance is available in the literature, to the knowledge of the author.

The strategy chosen to address the research question was a combination of simplified analytical models with the Monte Carlo method [13]. The resistance models in Eurocode 2 [160] were adopted. Furthermore, suitable probability density functions for the most relevant uncertainties were extracted from the literature. The methodology and results of this part of the research project were presented in **Paper III**.

It is important to emphasise that the overall goal of this study was to raise awareness of how uncertainties may affect the failure modes of blast-loaded RC elements. Therefore, to facilitate a concise presentation of the underlying concepts, some deliberate simplifications were introduced. A key simplification concerns the estimation of sectional forces. Specifically, the

study employed an equivalent static load approach, whereby the shear force at the critical section was determined from an equivalent uniformly distributed load corresponding to the flexural capacity of the element.

It should also be noted that shear failure may be triggered by additional dynamic and inertial effects arising under impulsive loading conditions. The present work did not aim to address those mechanisms. A comprehensive treatment of shear failure in RC members subjected to impulsive loading is presented, for instance, by Peterson [68]. Instead, the focus was placed on the uncertainties associated with the materials, geometry, blast load, and resistance models. To minimise the influence of other mechanisms promoting shear failure, the study was limited to slabs with span-to-effective depth ratio (L/d) greater than 9.6, which is the lower limit for slender elements according to the definition by Reineck et al. [161].

4.2 Case study

The case study examined 244 simply supported RC one-way *strips* of slabs with varying dimensions and reinforcement amounts and grouped into six slab types, as listed in Table 4.1. Half of the specimens had no shear reinforcement, while the remaining slabs were provided with the minimum amount of shear reinforcement required by Eurocode 2. A schematic representation of the geometrical conditions of the specimens appears in Figure 4.1.

Table 4.1. Geometry of the studied slab types in **Paper III**.

Slab type	L (mm)	h (mm)	d (mm)	L/d (-)	s (mm)
1	3000	300	252	11.9	189
2	4000	300	252	15.9	189
3	4000	400	352	11.4	212
4	5000	300	252	19.8	189
5	5000	400	352	14.2	212
6	5000	500	452	11.1	212

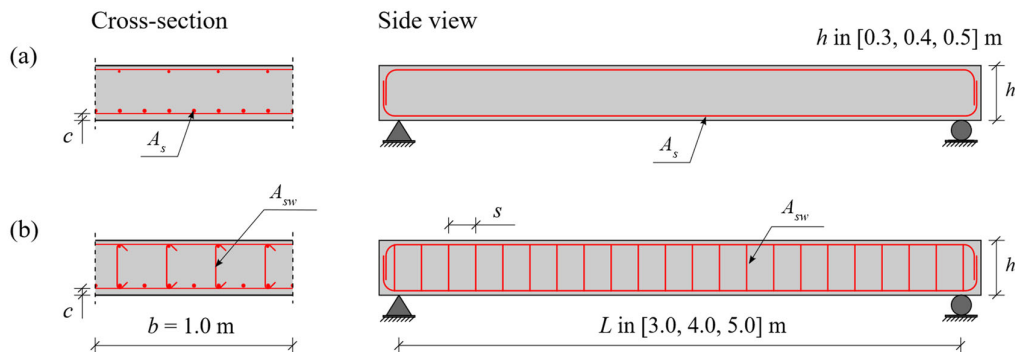


Figure 4.1. Geometry and reinforcement layout of the specimens studied in **Paper III**: (a) slabs without stirrups; (b) slabs with stirrups. Reproduced with permission from SNCSC.

The sample of slab specimens was generated from the six slab types by varying the amount of longitudinal reinforcement. The following criteria were adopted:

- The slabs were subjected to a uniformly distributed blast load. Nine distinct blast scenarios were considered, representing variations in charge mass and stand-off distance relative to the reference design bomb for civil shelters in Sweden [162]. The reference case consists of a spherical free-air burst generated by a 125 kg charge of TNT located at 5 m from the target structure.
- The slabs were designed under the assumption of a purely flexural response. For each combination of slab type and blast scenario, the design objective was to obtain three slabs with values of maximum dynamic deflection corresponding to predefined levels of utilisation of the plastic rotational capacity according to Eurocode 2: 20%, 50%, and 80%, respectively.
- The dynamic response of the slabs was analysed using an equivalent SDOF model (see Section 2.3.2).
- The shear capacity was not verified during the design stage. Instead, the probability of shear failure—the principal focus of this study—was evaluated in a subsequent stage of the analysis.

The problem was formulated as a limit state function, G , defined by Eq. (4.1), in which R_V is the resistance to shear failure while R_M is the equivalent resistance to bending failure. In the context of this work, the resistance is equal to the total load at failure and was determined using Eq. (4.2) and (4.3), in which V_R denotes the shear capacity, α_V the distance to the critical shear section from the support, and M_R the bending capacity at midspan.

$$G = R_V/R_M \quad (4.1)$$

$$R_V = V_R L \left(\frac{L}{2} - \alpha_V \right)^{-1} \quad (4.2)$$

$$R_M = \frac{8M_R}{L} \quad (4.3)$$

Both R_V and R_M are random variables due to the uncertainties related to the materials, geometry, resistance models, and blast loading. Thus, the limit state variable, G , is also a random variable. From Eq. (4.1), it is evident that shear failure would be the critical failure mode in those cases in which $G < 1$. Conversely, bending failure would govern the failure of the slab in cases in which $G > 1$.

The uncertainty distribution of G was determined using the conventional Monte Carlo method. The probability of premature shear failure occurring *before* bending failure was then calculated by solving Eq. (4.4), in which N denotes the total number of iterations performed for a given specimen (set as $N = 10^6$) and n is the number of iterations for which the condition $G < 1$ was satisfied.

$$P_f = P\{G < 1\} = n/N \quad (4.4)$$

4.3 Main results

4.3.1 Description of uncertainties

The uncertainties associated with the following variables were deemed the most relevant for this study:

- Material properties: Concrete compressive strength, f_c ; yield strength of longitudinal reinforcement, f_y ; yield strength of shear reinforcement, f_{yw} .
- Geometric properties: Error of the effective depth of the cross-section, Δd .
- Resistance models: Bending capacity, θ_{MR} ; shear capacity without stirrups, θ_{VRc} ; shear capacity with stirrups, θ_{VRs} .
- Dynamic increase factors: DIF for compressive strength of concrete, $\theta_{DIF.c}$; DIF for yield strength of longitudinal reinforcement, $\theta_{DIF.y}$.
- Blast load: Overpressure, θ_p ; impulse, θ_i .

The probability density functions (PDFs) and associated parameters for most of the sources of uncertainty were taken from the literature. However, the literature review revealed that the uncertainty distribution for the model for shear capacity without stirrups is highly sensitive to the geometry and reinforcement amount of the RC elements. Consequently, this model uncertainty was characterised by comparison with the ACI-DAfStb database [163], considering only those specimens in the database with similar characteristics to the elements studied here. Goodness-of-fit tests were applied to several candidate PDFs to identify the most appropriate representation.

Figure 4.2(a) shows the ratio between the experimental capacity of the selected RC beams in the database and their theoretical capacity according to the resistance model in Eurocode 2. This ratio corresponds to the model uncertainty θ_{VRc} . Figure 4.2(b) compares the empirical distribution of θ_{VRc} with the four best-fitting PDFs. The results indicate that the Burr XII distribution provides the most suitable fit for describing the distribution of θ_{VRc} .

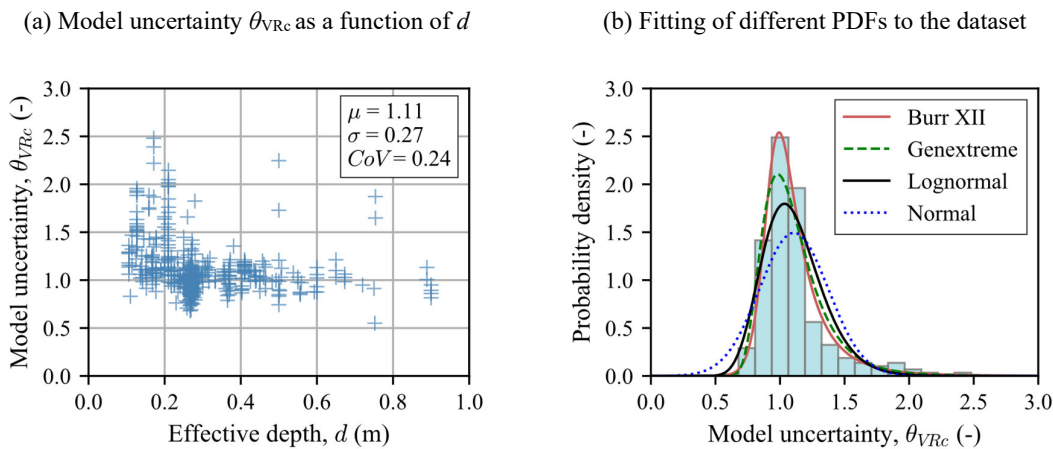


Figure 4.2. Statistical description of the uncertainty associated with Eurocode 2's model for shear resistance without stirrups based on the ACI-DAfStb database [163].

Figure 4.3 presents histograms of values randomly sampled from the uncertainty distributions of the three types of resistance model considered. It should be noted that the mean of θ_{VRs} is 1.47, compared with 1.11 for θ_{VRc} , indicating that the model uncertainty for the shear capacity with stirrups is more favourable compared with the shear capacity without stirrups.

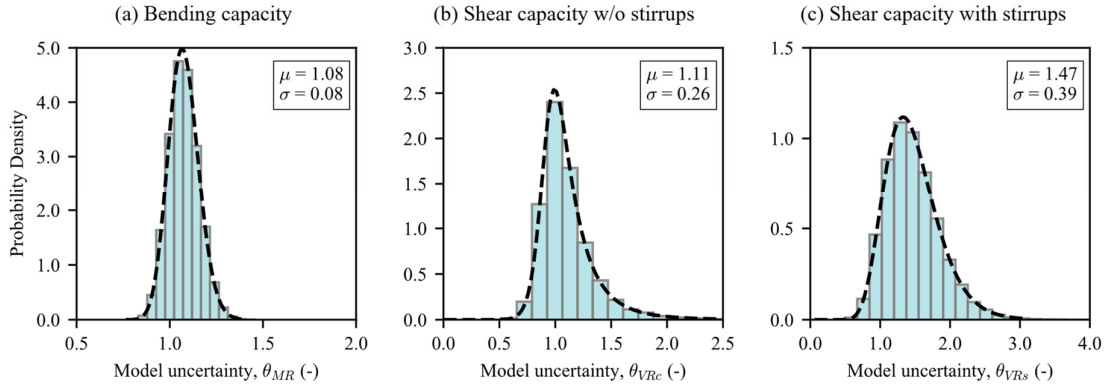


Figure 4.3. Randomly generated samples of the uncertainties associated with the resistance models. Adapted from **Paper III** (reproduced with permission from SNCSC).

4.3.2 Results of the Monte Carlo simulations

The results obtained using the Monte Carlo method are first presented for two reference specimens with $h = 300$ mm and $L = 3$ m in Figure 4.4. The figure shows the distribution of the limit state variable for both the reference specimen without stirrups, G_c , and the specimen with stirrups, G_s . The element without stirrups had a longitudinal reinforcement ratio of $\rho_l = 0.4\%$, while the element with stirrups had $\rho_l = 1.1\%$. These reference specimens are of particular interest because the ratio of design shear demand to design shear capacity was equal to 1.0 for them. In other words, the utilisation of the design shear capacity in these specimens was 100%.

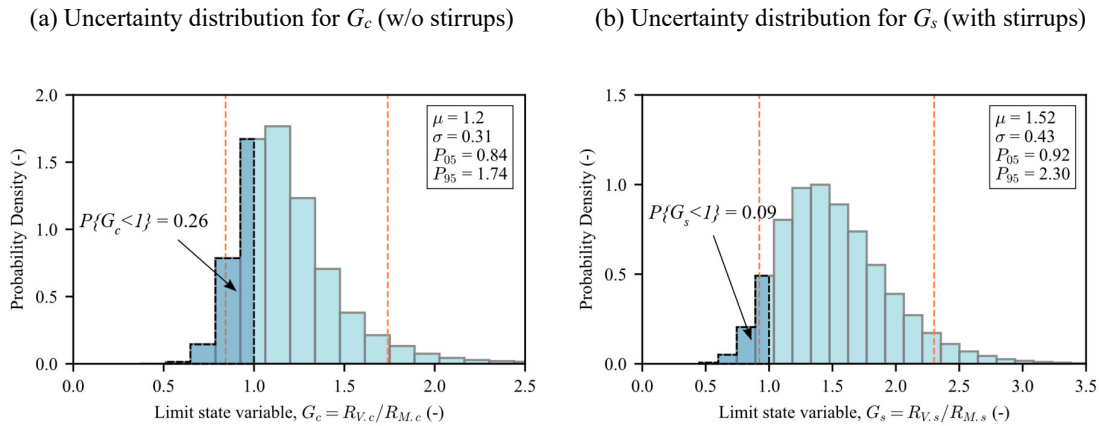


Figure 4.4. Uncertainty distribution of the limit state variable for reference specimens with $h = 300$ mm and $L = 3.0$ m. G_c refers to the specimen without stirrups, $\rho_l = 0.4\%$. G_s refers to the specimen with stirrups, $\rho_l = 1.1\%$. Adapted from **Paper III** (reproduced with permission from SNCSC).

The calculated probability of premature shear failure for the reference specimen without shear reinforcement was $P_{fc} = 26\%$. For the element with shear reinforcement, the corresponding

probability was $P_{f.s} = 9\%$. These results indicate that the reference specimens—originally designed so that their bending and shear capacities would be reached at the same load—are more likely to fail in bending when subjected to uniform blast loading.

The correlation between the variability of the input parameters and the limit state variables, G_c and G_s , was also assessed using the Spearman's rank correlation coefficient. The uncertainty associated with the shear resistance models was found to have the strongest positive correlation with the variability of the limit state variables. That is, an increase in θ_{VRc} (or θ_{VRs}) corresponded to an increase in G_c (or G_s), and therefore to a lower probability of shear failure. The uncertainty associated with the resistance model for bending capacity was the second most influential factor. Therefore, accurately quantifying the uncertainties associated with the resistance models is essential for a reliable estimation of the probability of premature shear failure. Similar conclusions regarding the correlation between the input and output variables were observed for all specimens in the sample.

The analysis of the entire sample showed that, in general, members with shear reinforcement have a more favourable response regarding the preferred failure mode. Indeed, the probability of premature shear failure, $P_{f.s}$, exceeded 50% only in nine of the specimens with stirrups, while about half of the studied specimens without stirrups exhibited $P_{f.c} > 50\%$.

The probability of shear failure was also found to increase with increasing reinforcement ratio, both in specimens with and without shear reinforcement. This is primarily due to the fact that a higher reinforcement ratio increases the flexural capacity, which in turn increases the shear demand on the element. This outcome implies that designs that are overly conservative with respect to flexural strength may inadvertently increase the risk of premature shear failure. These findings suggest that stricter limits on the maximum reinforcement ratio should be considered for beams subjected to blast loadings. Similarly, underutilising the element's rotational capacity—which tends to produce elements with higher reinforcement ratios—was observed to increase the probability of shear failure under the same applied impulse. Finally, for a constant reinforcement ratio, the probability of shear failure increased with a decrease in the span-to-depth ratio.

For situations in which the determined probability of premature shear is deemed excessive, the study proposed using an overstrength factor, γ_{Rd} , in accordance with the principles of capacity design [164], to guarantee higher levels of confidence concerning the preferred failure mode. Such an overstrength factor can be used to magnify the design shear demand, V_{Ed} , pertaining to the desired ductile response of the element. This approach is presented in more detail in Section 6.2.

4.4 Reflections

Paper III leveraged the Monte Carlo method to assess the probability of shear failure occurring before bending failure in slender RC one-way slabs subjected to a uniformly distributed load. To enable evaluation of millions of simulations for each specimen, simplified analytical models were adopted for the response and capacity of the studied specimens. One important simplification concerns the determination of the shear force, which was calculated based on an equivalent static load. However, beams subjected to a blast load of high intensity and short duration may experience a different time-dependent distribution of shear forces [62]. Likewise, the study adopted the shear capacity model in Eurocode 2, initially intended for beams subjected to quasi-static loading. In practice, the dynamic shear capacity is expected to be greater due to

strain-rate and inertial effects [63]. These assumptions introduce additional uncertainties that are not directly considered in the calculations, and that require further investigation in future work. However, it was judged that these two deliberate simplifications balance each other to some extent. Moreover, the adopted procedure is a commonly employed design assumption, supported by guidelines such as UFC 3-340-02 [33] and Cormie et al. [20].

The study proposed overstrength factors for different reliability targets that can be integrated into designs based on Eurocode 2. The applicability of these overstrength factors to alternative shear resistance models has not been examined. Nevertheless, the framework used in this work could readily be applied to derive equivalent overstrength factors for other models. To do so, the uncertainty associated with the selected shear resistance model would first need to be quantified. Alternatively, the analytical approach previously presented by Lozano [149] could be employed. However, it should be noted that that analytical method was derived under the assumption that the uncertainties related to the resistance models followed a lognormal distribution and is therefore expected to be less accurate for cases in which the model uncertainty is better represented by other probability distributions.

It is also worth noting that the probability examined in this thesis was the probability of shear failure occurring *before* bending failure. An alternative approach would be to evaluate the total risk associated with each failure mode separately. In this context, risk may be defined as the product of the probability of occurrence of a particular failure mode and its consequences. Indeed, comparing the individual risks associated with bending and shear failure could provide a rational basis for establishing acceptable target probabilities of premature shear failure, and, consequently, for determining appropriate overstrength factors.

Chapter 5

Impact response of simply supported and restrained RC beams

5.1 Overview

This chapter addresses **RQ-4** and **RQ-5**, within **Research Area II**, which concern the structural response, failure mechanisms, and energy absorption capacity of impact-loaded slender RC beams with different boundary conditions (BCs).

The research questions were examined using the results from three experimental series. The first two series (Series I and II) investigated the behaviour of simply supported beams subjected to single and repeated impact loading. The findings of these tests were compiled in **Paper IV**. The third series (Series III) focused on the response of restrained RC beams. This final series addressed a notable gap in the existing literature, namely, the behaviour of restrained element under impact loading. Hence, to enable a more detailed analysis of the final series, the results were reported in two separate papers: **Paper V** examined the response under static loading only, while **Paper VI** presented the results concerning impact loading and post-impact residual capacity.

Digital image correlation (DIC) was employed to analyse the structural response of the beams. During the static tests, imaging was done at a rate of 30 frames/minute, providing high-resolution measurements. For the impact tests, high-speed cameras operating at a rate of 5000 Hz (Series I and II) and 4000 Hz (Series III) were used. In addition, the experimental setup in the final series was instrumented with load cells and strain gauges—to measure the reaction forces—as well as an accelerometer embedded in the drop-weight for posterior estimation of the impact force.

After completion of the impact tests, the impact-damaged beams were tested under quasi-static loading. A three-point bending configuration was used wherever possible. However, due to some cases of extensive damage at the midspan region, the specimens in Series II were tested in a four-point bending setup. It should be noted that, despite the quasi-static nature of the loading, the term *static loading* is used hereafter for the sake of brevity.

5.2 Impact-loaded simply supported RC beams

5.2.1 General description of the experiments

The experimental programme reported in **Paper IV** comprised two test series, each consisting of 18 specimens. Within each series, 12 specimens were subjected to impact loading and subsequently tested under static loading to assess their residual capacity. The remaining six specimens were tested under static loading only and served as reference specimens. All specimens were nominally identical in terms of geometry and reinforcement detailing, as shown in Figure 5.1; although the material properties differed between the two series. The beams were designed for bending failure under static loading according to Eurocode 2, with ratios of design bending capacity to shear capacity of 0.60 and 0.82 for Series I and II, respectively.

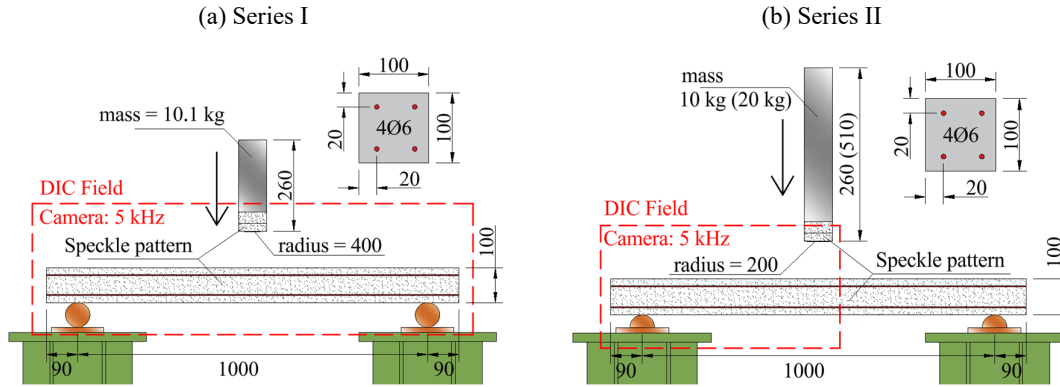


Figure 5.1. Geometry, reinforcement layout, and setup of the impact tests in **Paper IV**.

In Series I, the primary objective was to evaluate the influence of the drop height while maintaining a constant drop-weight mass, and to assess the repeatability of the results. Each specimen was subjected to a single impact. The tests in Series I were originally reported by Lozano and Makdesi [165]. Series II investigated the effects of varying both the drop-weight mass and drop height, as well as the influence of repeated impacts. The tests in Series II were conducted by Jönsson and Stenseke [166]. The complete dataset for both series is available in [167]. Table 5.1 gives the impact conditions for both series, in which H is the drop height, n is the number of impacts, v_0 is the initial impact velocity, and $E_{k,0}$ is the impactor's kinetic energy.

Table 5.1. Details of the test series from **Paper IV**.

Series	Specimen	Drop-weight (kg)	H (m)	n (-)	v_0 (m/s)	$E_{k,0}$ (kJ)	$nE_{k,0}$ (kJ)
I-Ref	IB-07, IB-08, IB-09, IB-16, IB-17, IB-18		Quasi-static tests				
I-1	IB-01, IB-02, IB-03, IB-10, IB-11, IB-12	10.1	2.5	1	7.0	248	248
I-2	IB-04, IB-05, IB-06, IB-13, IB-14, IB-15	10.1	5.0	1	9.9	495	495
II-Ref	IIB-07, IIB-08, IIB-09, IIB-16, IIB-17, IIB-18		Quasi-static tests				
II-1	IIB-04, IIB-05, IIB-06	10.0	2.5	4	7.0	245	981
II-2	IIB-01, IIB-02, IIB-03	10.0	5.0	2	9.9	491	981
II-3	IIB-13, IIB-14, IIB-15	20.0	2.5	2	7.0	491	981
II-4	IIB-10, IIB-11, IIB-12	20.0	5.0	1	9.9	981	981

5.2.2 Main results

Most beams in Series I and II exhibited a ductile flexural response under both static and impact loading. However, some specimens developed significant shear cracks during impact loading, which in one case limited the capacity under post-impact static loading.

The time-dependent deflection of the beams during the impact tests was analysed using the DIC technique. Consistent with the observations reported in the literature, only a small region near the point of impact underwent transverse deformation in the instants after contact. As time progressed, the impact-induced stress wave propagated towards the supports, progressively increasing the effective span of the beam and mobilising a greater portion of its mass. The transient regions of negative curvature associated with the stress wave propagation generated cracks on the top surface of the beams. These cracks subsequently closed during the later stages of the response. Eventually, the entire beam mass was engaged, and the deformed shape converged into a shape resembling the characteristic three-hinged plastic mechanism.

In beams subjected to a *single* impact, the crack pattern was observed to vary with the input impact energy. Under low-energy impacts (subseries I-1), the beams developed predominantly flexural cracks—similar to the crack pattern observed in the reference specimens subjected to static loading only—although some flexural cracks in the midspan region transitioned into flexural-shear cracks, as illustrated for IB-02 in Figure 5.2(a). Beams subjected to stronger impacts (subseries I-2 and II-4) exhibited distinct diagonal shear cracks at midspan corresponding to the shear-plug mechanism typically associated with impact loading, as shown in Figure 5.2(b) for IB-04. For the same impact energy, these cracks were more pronounced for impacts caused by a lighter drop-weight striking with greater velocity. During subsequent static loading, the crack pattern largely followed that initiated during the impact tests, indicating that the initial impact damage governed the behaviour under subsequent static loading.

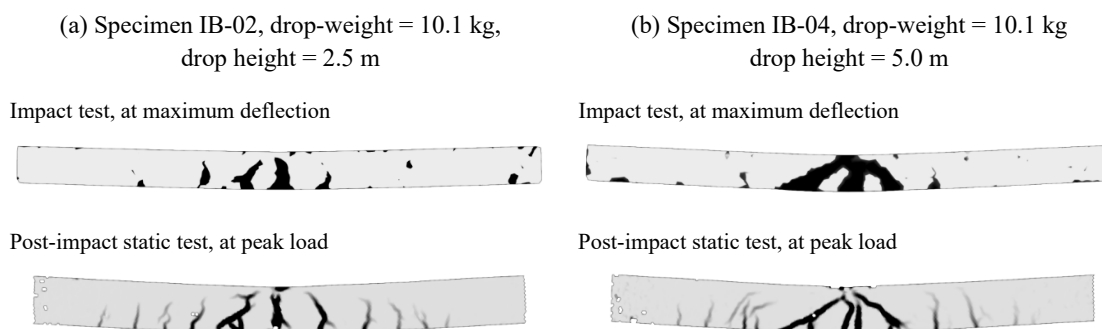


Figure 5.2. Major strain fields obtained using DIC to represent the crack pattern in selected specimens from Series I. Adapted from [165].

In beams subjected to *multiple impacts*, most of the primary cracks were initiated during the first impact and continued to develop with consecutive impacts. However, the beams subjected to four impacts (subseries II-1), which exhibited predominantly vertical cracks in the first three strikes, also developed major inclined shear cracks near the impact zone during the fourth strike. Repeated impact loading also led to crushing of the concrete in the compression zone, as well as scabbing of the bottom concrete cover at midspan. Furthermore, the extent and pattern of damage displayed increasing variability as the number of impacts increased, leading to greater

scatter in the results. This indicates potential difficulties and uncertainties in the prediction of the dynamic response of beams subjected to multiple impacts.

Finally, although most specimens exhibited a flexure-dominated response during impact, the DIC analysis revealed that two beams in Series I (IB-13 and IB-15) developed a significant inclined shear crack located approximately at one-third of the span from the support. These cracks initiated near the mid-depth point of the section and extended towards the top and bottom surfaces at an angle of approximately 45° . The presence of inclined shear cracks did not appear to significantly influence the impact response of either beam. However, the pre-existing crack in beam IB-13 led to a shear failure during the post-impact static test, resulting in a substantial reduction in both load-carrying and energy absorption capacity. Figure 5.3 shows the crack pattern observed during impact and subsequent static test of beam IB-13. Therefore, among six nominally identical specimens subjected to the same impact conditions in subseries I-2, two exhibited pronounced inclined shear cracks outside the midspan region. Of these two beams, one transitioned into a brittle shear failure during the subsequent static test, which severely limited its energy absorption capacity. This variability in response highlights the influence of material and geometric uncertainties in impulsively loaded RC elements and partly motivated the investigations presented in **Paper III**.

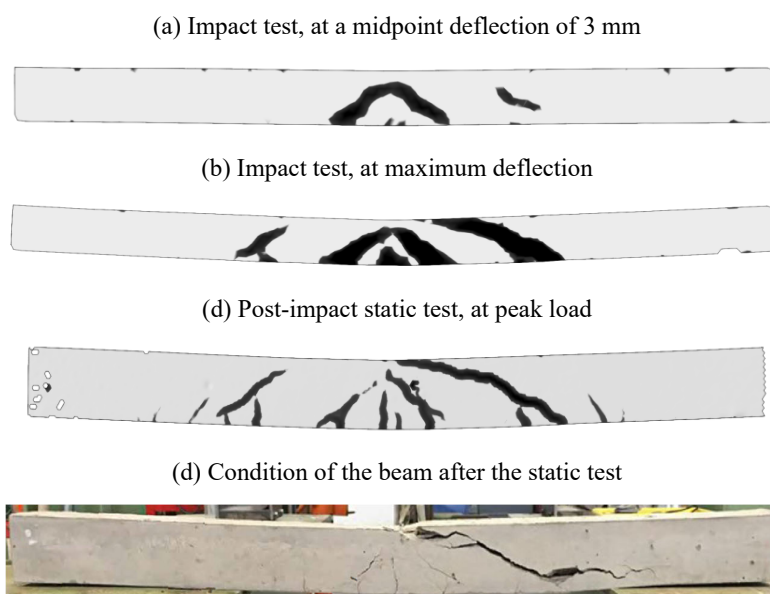


Figure 5.3. Crack pattern of specimen IB-13 under impact and static loading obtained using DIC. The photograph in (d) was taken on the unpainted face and the image was afterwards flipped around the central vertical axis. Adapted from [165].

5.2.3 Energy absorption capacity of simply supported RC beams

5.2.3.1 Beams subjected to a single impact

The study examined the energy absorption capacity (EAC) under different loading conditions. For the static tests, the dissipated energy was determined by integrating the load-deflection curve. That is, the EAC was assumed to equal the beam's internal strain energy. For the impact tests, energy dissipation was estimated using Eq. (2.20), based on the classical impact theory under the assumption of an ideal inelastic collision. The calculated value is regarded as a

reasonable lower bound of the energy transferred to and dissipated by the beam. The *total* EAC is the sum of the energy dissipated during the impact tests and the post-impact static test.

In general terms, the total energy dissipated by the beams subjected to combined impact and subsequent static loading was similar to or, in most cases, exceeded the EAC of the corresponding elements tested under static loading only. This enhancement is partly attributed to the improved dynamic material properties as well as additional dissipative mechanisms, such as damping and fracture in the concrete. Additionally, in some cases, the impact is believed to have induced a more favourable behaviour, such as more extensive cracking and local deterioration of the bond between concrete and reinforcement at midspan, which produced a more advantageous strain distribution in the reinforcement bars.

The enhancement of the EAC can be evaluated by the ratio η_E , defined by Eq. (5.1). To facilitate comparison across specimens and experimental campaigns, the impact loading is also described in terms of the *dissipated energy index*, e_i , defined by (5.2). This index normalises the input impact energy with regard to the mass of the beam and drop-weight as well as the beam's static capacity.

$$\eta_E = \frac{\text{Total EAC under impact and subsequent static loading}}{\text{EAC under static loading only}} \quad (5.1)$$

$$e_i = \frac{\text{Energy dissipated under impact loading}}{\text{EAC under static loading only}} \quad (5.2)$$

In beams subjected to a single impact (subseries I-1, I-2 and II-4), increasing the impact energy ($e_i = 0.23, 0.46$ and 1.21) resulted in greater values of the ratio η_E ($0.96, 1.42$, and 1.61). An increase in the total EAC with increasing impact energy has also been observed in previous studies. For instance, such a conclusion can be reached from the experiments reported by Dok et al. [168], [169] for flexure- and shear-dominated high-strength concrete beams, respectively. Further support comes from Yilmaz et al. [170], who reported an increase in the total EAC with increasing values of e_i in normal-strength RC beams.

The residual response of the specimens also varied with impact intensity. During the post-impact static tests, the beams subjected to the weakest impact (subseries I-1) consistently followed the load-deflection curve of the undamaged reference specimens. Thus, the load-deflection curve of the undamaged specimens may be regarded as an envelope of the residual capacity of the beams subjected to the weakest impact, which could be leveraged to estimate the residual response when the permanent deformation caused by the impact is known. The specimens in subseries I-2, which were subjected to an impact of intermediate intensity, generally showed a more ductile response under post-impact static loading than the undamaged specimens, resulting in greater total EAC. In some cases, the impact-damaged beams were able to mobilise more strain energy under static loading than the undamaged counterparts. This behaviour may be attributed to the larger cracked zone at midspan in conjunction with deterioration of the bond, which together could potentially allow for a greater length of reinforcement to yield and a more uniform strain distribution. That is, the impact damage may have induced a favourable mechanism for the post-impact static loading. Finally, the beams in

subseries II-4, which were subjected to the strongest impact, developed a maximum midspan deflection during impact comparable to the deformation at failure of the statically loaded reference specimens. The permanent plastic deformation after impact was also comparatively large. However, contrary to what might be inferred from inspection of the residual plastic deformation, the beams retained significant residual load-carrying and deformation capacity.

5.2.3.2 Beams subjected to repeated impact loading

Similar to the specimens subjected to a single impact, the beams subjected to multiple impacts showed, on average, a total EAC that was comparable to or greater than that of the statically loaded counterparts. Moreover, for an equal cumulative impact energy, a general trend of decreasing total EAC with increasing number of impacts was observed. This behaviour was attributed to the accumulation of local damage, which inhibited the beam's residual capacity.

The specimens in Series II were subjected to the same cumulative impact energy. Nevertheless, the total dissipated impact energy differed across all cases due to the influence of the drop-weight mass relative to the beam mass and the proportion of energy dissipated by the beam's elastic response. Indeed, the beams subjected to the largest number of impacts (with the lowest intensity per strike) in total dissipated less impact energy than the beams subjected to a single strong impact. Consequently, the residual plastic deflection was largest in the beams subjected to a single strong impact. However, in some cases, larger permanent plastic deflections did not result in lower residual load-carrying or energy absorption capacity. This finding suggests that estimating the residual capacity based on the observable permanent post-impact deflection may be misleading.

The influence of repeated impacts with equal impact energy per strike can be evaluated by comparing the results for specimens in subseries I-1 (one impact) and II-1 (four impacts)—both loaded by a drop-weight of 10 kg from a drop height of 2.5 m—and the specimens in subseries I-2 (one impact) and II-2 (two impacts)—both loaded by a drop-weight of 10 kg from a drop height 5.0 m. In most cases, the specimens subjected to repeated impact loading achieved lower total EAC than their counterparts loaded by a single impact. However, in one exceptional case, one specimen from series II-1 (four impacts) was able to mobilise much greater total EAC than the specimens subjected to a single impact.

Taken together, these findings indicate that repeated impact loading may result in reduced total EAC due to accumulated damage, and the probability of such degradation appears to increase with impact severity. However, if the onset and progression of local damage is delayed, multiple impacts may instead enable greater total energy absorption capacity.

5.3 Response of restrained RC beams

5.3.1 General description of the experiments

The response of restrained RC beams was examined jointly through **Papers V** and **VI**. The research series comprised 12 specimens with a free span of 2.0 m that were monolithically cast with strong column stubs. The column stubs were, in turn, fixed to braced steel frames that provided rotational and longitudinal restraint. The experimental setup used for the impact tests is shown in Figure 5.4. The same general setup was adopted for the static tests, although with a hydraulic actuator for application of the load. The specimens had variable slenderness and reinforcement ratios. The beams were designed to exhibit a flexural response under static

loading according to Eurocode 2. The ratio of the design bending capacity to design shear capacity without stirrups ranged from 0.40 to 0.63. If the contribution of shear reinforcement is considered, the corresponding ratios varied between 0.13 and 0.34. Six beams were subjected to static loading only (**Paper V**). The remaining six beams were loaded under impact and subsequent static loading (**Paper VI**). The impact load was applied using a steel drop-weight with a mass of either 40 or 80 kg. Table 5.2 summarises the geometry, reinforcement layout and impact conditions.

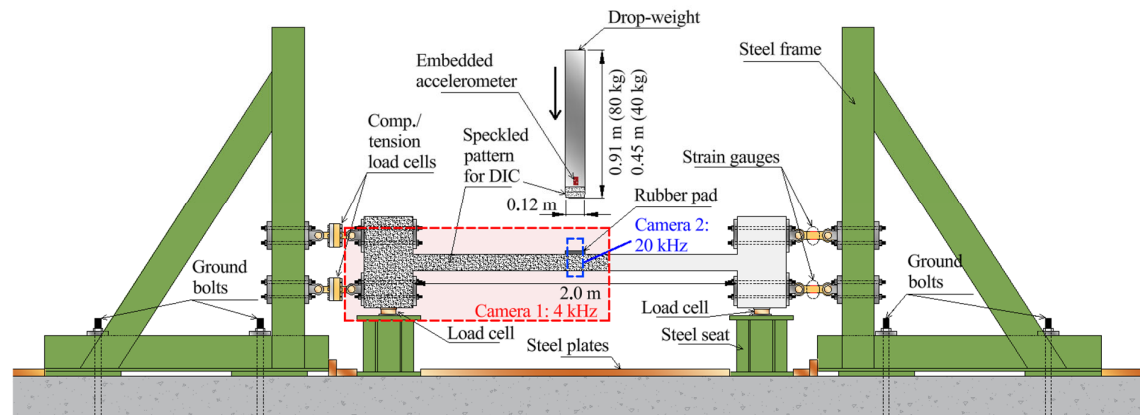


Figure 5.4. General description of the setup of the impact tests in **Paper VI**.

Table 5.2. Details of the test series from **Papers V and VI**.

	Specimen*	h (mm)	L/h (-)	Longitudinal Reinf. (mm)		Reinf. ratio (%)		Drop- weight (kg)	H (m)	n (-)	$E_{k,0}$ (kJ)	$nE_{k,0}$ (kJ)
				Top	Bottom	Top	Bottom					
Paper V	IIIB-01	100	20.0	2ø6	2ø6	0.39	0.37					
	IIIB-03	100	20.0	2ø8	2ø8	0.71	0.66					
	IIIB-05**	100	20.0	2ø6	2ø6	0.39	0.37					
	IIIB-07**	100	20.0	2ø8	2ø8	0.71	0.66					
	IIIB-09	160	12.5	2ø6	2ø6	0.21	0.21					
	IIIB-11	160	12.5	2ø8	2ø8	0.38	0.38					
Paper VI	IIIB-02	100	20.0	2ø6	2ø6	0.39	0.37	80	4.7	1	3.69	3.69
	IIIB-04	100	20.0	2ø8	2ø8	0.71	0.66	80	4.7	2	3.69	7.38
	IIIB-06**	100	20.0	2ø6	2ø6	0.39	0.37	40	4.7	2	1.84	3.69
	IIIB-08**	100	20.0	2ø8	2ø8	0.71	0.66	80	4.7	1	3.69	3.69
	IIIB-10	160	12.5	2ø6	2ø6	0.21	0.21	40	4.5	5	1.77	8.83
	IIIB-12	160	12.5	2ø8	2ø8	0.38	0.38	80	4.5	4	3.53	14.13

* The naming convention for the beams was modified (compared to the one used in **Papers V and VI**) to conform to the notations used in Table 5.1.

** These beams had only six stirrups located at midspan around the impact zone.

The primary focus of this study was the evaluation of the flexural response of impact-loaded beams. To promote a ductile flexural failure mode, a rubber pad was placed between the concrete beam and the steel drop-weight. Previous studies have shown that the use of a rubber interlayer can reduce the impact force and increase the load duration, thereby reducing the risk of shear-dominated failure modes [102], [171].

5.3.2 Main results concerning restrained RC beams under static loading

As reported in **Paper V**, all beams displayed a flexural response under static loading, characterised by cracks growing predominantly perpendicular to the beam's main axis. No evidence of shear mechanism was observed, even in those specimens without shear reinforcement outside the midspan region.

In all specimens, rebar fracture occurred first at the bottom at midspan. This may be attributed to the rotational restraint at the supports not being perfectly rigid, resulting in lower moment at the supports than at midspan. In addition, an initial connection “gap” was observed in the experimental setup, meaning that a certain degree of deformation was required at the supports before the restraint became fully engaged. A similar phenomenon has been reported by other authors that have used similar experimental setups [134]. Because of this gap, the positive bending moment at midspan could develop before any significant support moments and horizontal reaction forces were mobilised.

During the tests, the right support initially exhibited unexpectedly large translations after a certain load level. Therefore, different measures were implemented to increase the restraint level. Figure 5.4 shows the final conditions. Steel plates were installed between the support frames to further restrain their relative inward translation. Likewise, steel plates bolted to the RC floor were installed outside the frames to further limit their outward translation. As a result, the BCs varied across all specimens and exhibited non-linear behaviour. However, the BCs could be fully characterised through a combination of DIC analysis and the measurements from the load cells.

Although the BCs varied across specimens, the degree of restraint was sufficient to mobilise the characteristic CMA behaviour in all beams. Consequently, all beams showed an enhanced load resistance compared with an equivalent unrestrained system, as showcased for two specimens in Figure 5.5. The ratio between the peak CMA resistance and the theoretical unrestrained resistance, P_{flex} , calculated according to Eurocode 2, ranged from 1.18 to 1.92. The resistance enhancement was greatest in the beams with the lower span-to-depth ratio ($L/h = 12.5$). More slender beams ($L/h = 20$) with low reinforcement ratios also benefited greatly from this enhancement. In contrast, the smallest enhancement was observed in beams with $L/h = 20$ and greater reinforcement ratios. Overall, these observations suggest that the resistance enhancement is inversely related to both the span-to-depth ratio and the reinforcement ratio.

Significant differences were observed across the specimens in terms of their post-peak behaviour within the CMA stage and after the onset of the TMA phase. These differences reflected the influence of the reinforcement ratio, span-to-depth ratio, and reinforcement fracture strain. The beams with $L/h = 12.5$ developed failure of all extreme tension

reinforcement during post-peak CMA response. This caused a temporary instability mechanism whereby the beam dropped and separated from the actuator. However, the test could be resumed, with the load being carried through *secondary* catenary action. The beams with $L/h = 20$ and lower reinforcement ratio and reinforcement fracture strain developed rebar fracture almost simultaneously with, or shortly before, the onset of the TMA phase, which limited the capacity under catenary action. In contrast, the beams with $L/h = 20$ and greater reinforcement ratio and fracture strain could utilise *primary* catenary action, although the extreme tension reinforcement fractured before the TMA phase was halfway completed. These results indicate that assuming that only the “compression” reinforcement contributes to TMA would not be overly conservative. These results further showed that the span-to-depth ratio has a significant influence on rebar fracture and the utilisation of TMA. Likewise, low reinforcement ratios or low fracture strains were also found to promote earlier rebar fracture.

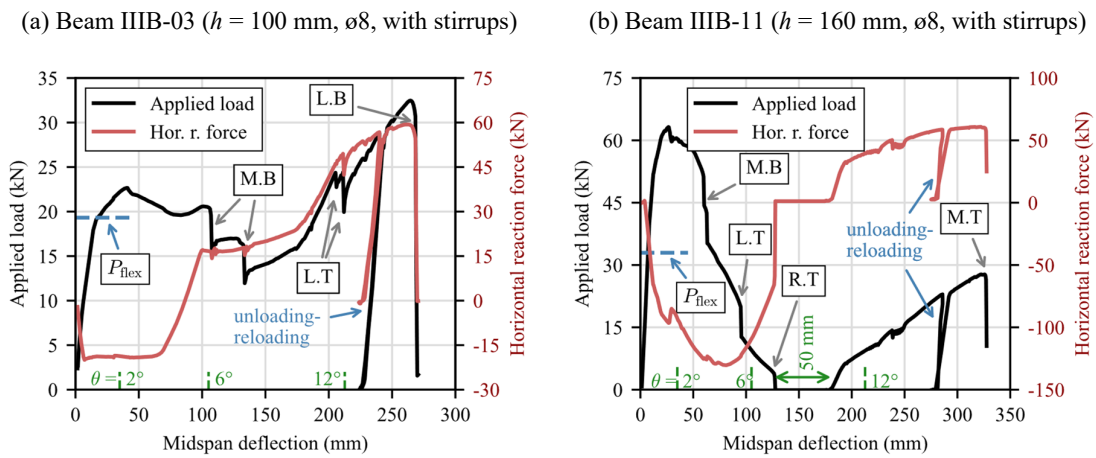


Figure 5.5. Static response of two restrained specimens from **Paper V**. P_{flex} is the flexural load-carrying capacity without axial force. θ is the support rotation. M.B (midspan, bottom), M.T (midspan, top), L.B (left end, bottom), L.T (left end, top), and R.T (right end, top) indicate location of rebar fracture.

The energy absorption capacity of the statically loaded elements was also evaluated in **Paper V**. In general, beams with reinforcement with greater fracture strain and higher reinforcement ratios produced greater energy absorption capacity. Among beams with the same reinforcement amount, those with lower span-to-depth ratios showed greater EAC, even though the reinforcement ratio was less. This was largely due to the larger contribution of CMA to the total energy absorption capacity. Thus, disregarding CMA contribution would hinder optimal utilisation of the element’s EAC. However, correct evaluation of rebar fracture during the CMA phase is required to properly estimate the contribution of CMA to the overall energy absorption capacity.

A numerical FE model was developed to evaluate the static response of restrained RC beams subjected to three-point bending. The model, briefly described in Section 6.3.2, was used to examine the influence of the degree of restraint on the response under static loading. A reference case was defined with a translational stiffness of 1×10^8 N/m and a rotational stiffness of 1×10^7 Nm/rad. These values were adopted in **Paper IV** as representative of the restraint provided by the test setup. The results of this evaluation are summarised in **Appendix A**. The

results indicate that the load-carrying capacity, deformation limit, and energy absorption capacity all increased with increasing restraint level. Furthermore, variations in longitudinal restraint appear to have a more pronounced effect on the response than variations in the rotational restraint. The study also showed that full fixation is not necessary to mobilise the additional capacity associated with membrane action. These findings highlight the potential for exploiting the reserve capacity of partially restrained members within structural systems. To achieve this, it is necessary to quantify the minimum degree of restraint needed to activate membrane action under different conditions, whether in the form of catenary action alone or a combination of arching action and catenary action. A notable contribution to this area is the work of Almusallam [172], who investigated the conditions necessary for developing arching and catenary action in RC frames.

5.3.3 Main results concerning restrained RC beams under impact loading

5.3.3.1 Impact wave propagation

The translational restraint was progressively increased during the static tests, as described in Section 5.3.2. However, all impact tests were conducted using the final configuration of the boundary conditions, as illustrated in Figure 5.4.

Similarly to the response observed on the impact-loaded simply supported beams in **Paper IV**, only a portion of the restrained beam reacted immediately after contact. As the impact wave travelled along the span, the effective span increased. The associated transient negative curvature generated cracking on the top surface. However, unlike in the simply supported beams, the top cracks near the support did not close at a later stage. By analysing the velocity-time history, as described by Ulzurrin et al. [173], the effective propagation speed of the impact wave was estimated to be, on average, 400 and 450 m/s for specimens with $L/h = 20$ and $L/h = 12.5$, respectively. These values are significantly lower than the theoretical shear wave speed through a continuum medium ($\sqrt{G_c/\rho} \approx 2500$ m/s).

It should be noted that the propagation phenomenon could not be characterised with high precision due to the relatively low spatial and temporal resolution of the high-speed imaging used in this study. Indeed, significant scatter was observed concerning the prediction of the impact wave speed. This uncertainty may be further aggravated by the different approaches used in the literature for determining the impact wave speed, which may be based, for instance, on deflection or velocity profiles, or reaction force measurements. More accurate measurements of the wave speed can be obtained through dedicated experimental studies, such as that of Duran Vergara et al. [174]. Nevertheless, the estimated values are within the range reported by other authors [108], [127], [175], and provide a reasonable basis for the numerical description of the beam's response, as described in Section 6.3.3.

5.3.3.2 Damage evolution

Cracks in the impact-loaded beams grew predominantly in the vertical direction, even within the impact zone, demonstrating flexural response in all specimens. A few specimens exhibited some flexure-shear cracks in the impact zone, but a distinct shear-plug pattern did not fully develop. This may be partly the result of using an interlayer of rubber and the presence of stirrups in the midspan region.

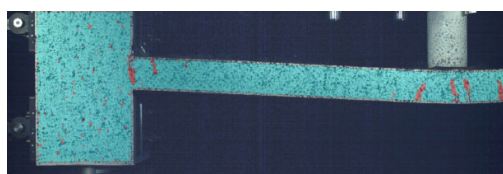
Local damage in the impact region included concrete crushing in the contact zone as well as spalling at the edges of the cross section on the top surface. No significant spalling on the opposite face was observed. The degree of local damage was found to be proportional to the mass of the striker. That is, greater drop-weight mass caused more damage. Furthermore, beams with greater mass and cross-sectional depth experienced less local damage for the same drop-weight mass and drop height.

5.3.3.3 Evaluation of membrane action

The evolution of membrane action can be examined by analysing the horizontal reaction force. The horizontal reaction force was first registered on average at 0.9 ms after contact, indicating that the longitudinal wave speed was 1450 m/s on average. This value is markedly lower than the theoretical speed for a continuous medium, $\sqrt{E/\rho} \approx 4000$ m/s. Thus, the results indicate that membrane action engaged at an early stage in the response.

Members subjected to a relatively weak impact only developed CMA in the first strike. Due to the accumulated damage, some of these elements transitioned into TMA with repeated impacts. In contrast, members subjected to a stronger impact mobilised TMA in the first strike. The crack pattern was found to have a connection to the phase of the membrane action. The specimens that stayed within the CMA phase developed a crack pattern comparable to that typical of flexural action (i.e. top cracks at the support, bottom cracks at midspan), as shown in Figure 5.6(a). In contrast, the beams that reached a stage of pure catenary action developed vertical cracks across the full depth that were uniformly distributed along the span. This is illustrated in Figure 5.6(b). Pure catenary action, with little contribution from bending, was observed in specimens that achieved very large deflection or that experienced fracture of the extreme tension reinforcement at the supports and midspan. These findings indicate that posterior inspection of cracking may help determine which regime was mobilised during the impact loading. However, some beams that developed tensile membrane forces still showed a crack pattern similar to that associated with flexural response. This was attributed to a significant contribution of bending during the TMA phase, even at large deflection values.

(a) Typical crack pattern in CMA (IIIB-06, $n = 1$)



(b) Typical crack pattern in TMA (IIIB-02, $n = 1$)

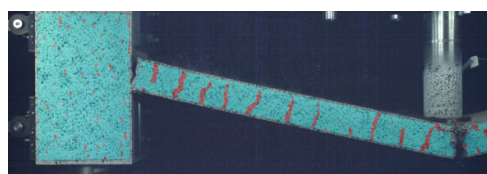


Figure 5.6. Crack pattern under different membrane action regimes.

Finally, beams that remained within the CMA phase were found to maintain a residual membrane force at the end of the impact test. In contrast, beams that transitioned into the TMA regime had no significant remaining force after completion of the impact tests.

5.3.3.4 Evaluation of the impact force

The typical impact force profile comprised a primary impact force peak, followed by a quasi-static plateau, which was defined either by a smooth profile or a jagged profile with consecutive minor peaks. The results indicate that the magnitude of the impact force during the quasi-static phase relates to the beam's flexural stiffness. It was further observed that beams with greater

mass and section depth experienced a greater peak impact force for the same impact event. That is, in contrast with the conclusions in [176], [177], the beam's mass and section properties appear to also influence the primary impact force peak in restrained beams. In contrast, the total duration of the load pulse decreased on beams with greater mass and flexural stiffness for the same impact conditions.

5.3.3.5 *Static response of impact-damaged restrained beams*

After completion of the impact tests, the beams were subjected to static three-point bending to assess their residual capacity. Due to previous damage, most beams (apart from IIIB-10) responded almost immediately in catenary action. In general, the degree of observable damage (number and width of cracks, spalling, and crushing) related well to the residual capacity. Specimen IIIB-10, with the least severe damage overall, showed the best performance after impact, exhibiting both CMA and TMA phases. On the other hand, specimen IIIB-12, which experienced the most extended damage due to impact loading, showed the greatest degradation of the static capacity. However, specimen IIIB-04 showed substantial residual capacity, even though it displayed extensive cracking after the impact tests. The extended cracked zone at midspan is believed to have enabled a more uniform distribution of reinforcement strain in this beam, which may explain why IIIB-04 was the only beam that did not suffer any rebar fracture at midspan during the static tests. This implies that some favourable crack pattern may be generated during the initial impact loading and contribute towards better residual performance.

5.3.4 **Energy absorption capacity of restrained RC beams**

The energy dissipated by the beam during impact loading was determined indirectly by integrating the impact force over the midspan deflection, according to Eq. (2.19). A similar approach was used, for instance, in [178], [179], [180]. The beam dissipated this energy through internal strain work and damping. In all cases, the energy that was transferred to the beam was lower than the input impact energy. The reason for this is that part of the input impact energy was dissipated through local deformation of the rubber pad and concrete surface as well as contact damping. The ratio of dissipated energy to input impact energy, which varied approximately between 0.60 and 0.88, was found to depend on the masses of the drop-weight and the beam. For the same drop-weight mass and similar drop height, the ratio decreased for a larger beam mass. Likewise, among beams of equal mass, the ratio decreased with a smaller drop-weight mass.

In the study presented in **Paper IV** the energy dissipated by the simply supported beams was not measured but inferred using classical impact theory assuming an ideal inelastic collision, see Section 2.4.3.1. A similar approach would consistently underestimate the energy transferred to the restrained beams, which indicates that the impact on the restrained beams was not fully inelastic. Nonetheless, a strong correlation was found between the experimentally determined values of dissipated energy, E_i , and the prediction made assuming an ideal inelastic collision, $E_{i,pl}$. Such correlation is illustrated in Figure 5.7. The results for IIIB-02 were not included in the figure, as some of the energy was stored as kinetic energy at the time of failure. The strong correlation indicates that the prediction based on an ideal inelastic collision is theoretically sound, although out of phase with the experimental values concerning the restrained beams.

Similar to the results obtained for the elements subjected to only static loading, the total energy dissipated by the specimens reinforced with $\varnothing 8$ -mm rebars was greater than the total energy dissipated by the corresponding beams with $\varnothing 6$ -mm rebars. This is an effect of both the

increased load capacity due to the larger reinforcement amount and the greater fracture strain of the ø8-mm rebars. Likewise, beams with lower span-to-depth ratios displayed greater total energy absorption capacity compared to more slender beams with the same reinforcement amount.

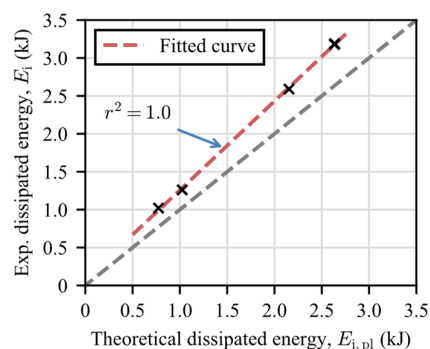


Figure 5.7. Relationship between the prediction of impact energy transferred to the restrained beams assuming an ideal plastic impact, $E_{i,pl}$, and the experimental values, E_i .

The total EAC of the beams subjected to impact and subsequent static loading exceeded the EAC of the corresponding beams subjected to static loading only. Some of the restrained beams exhibited a greater enhancement in EAC than the simply supported beams in **Paper IV**, which may indicate a beneficial effect of the membrane forces. However, the differences in the methods used to determine the energy dissipated by the beams, together with the limited number of restrained specimens, prevents this hypothesis from being confirmed in this study.

Notable differences between beams with the same nominal geometry and longitudinal reinforcement amount were found. Specimens IIIB-04 and IIIB-08 dissipated similar energy levels at each impact event. However, the total energy absorption capacity of IIIB-08 was in the order of 60% of that of IIIB-04, even though IIIB-04 was subjected to one more impact. This is a very large and unexpected deviation, particularly considering that the influence of stirrups has been judged to be minor. This discrepancy may be explained by the extended cracked zone at midspan in IIIB-04 compared to a major concentrated crack in IIIB-08, meaning that strain concentration was more pronounced in the latter, prompting earlier reinforcement failure. This indicates that the damage caused by the first impact dictates how the beam behaves in subsequent impacts or static loading and may influence the total energy that can be absorbed.

The results further suggest that beams subjected to multiple weaker impacts can mobilise greater total energy absorption capacity than beams subjected to fewer but stronger impacts. For instance, specimen IIIB-02 could dissipate 2.55 kJ before collapse, whereas IIIB-06—nominally identical to IIIB-02 apart from lacking stirrups outside the midspan region and subjected to two weaker impacts—could mobilised a total of 3.10 kJ. In addition, IIIB-10, subjected to five relatively weak impacts, had the greatest enhancement in EAC with respect to the static capacity across all studied specimens. These findings contrast with the results observed for simply supported beams, for which a single stronger impact was found to lead to larger total energy absorption capacity. Two factors are believed to drive this difference. First, for simply supported beams it was previously argued that the accumulated damage from repeated impacts hindered the overall energy absorption capacity. In the impact tests of the

restrained beams, a rubber interlayer was used. This had the effect of decreasing the local damage and increasing the load duration [102], [171], [181]. As a result, more impacts could be applied without much detrimental damage on the beams compared to direct contact. Second, the restrained beams had a wider cross-section, which contributed to limiting the extent of local damage.

Overall, the study indicates that different aspects may influence the enhancement of the energy absorption capacity. First, damage evolution, largely driven by the damage pattern during the first strike, may result in different levels of enhancement, as observed for specimens IIIB-04 and IIIB-08. The results also suggest a relationship between such enhancement and the allowed number of impacts. The implication of this is that if a certain beam had been subjected to stronger but fewer impacts, its total EAC would have been lower. However, this hypothesis needs to be confirmed with further research. It is also reasonable to expect that the greater enhancement connected to more repeated impacts is possible only in situations where degradation of the overall capacity due to local damage in the impact zone is minimised, by, for example, a protective interlayer.

5.4 Reflections

Across all test series, it was observed that the total energy absorption capacity under combined impact loading and subsequent static loading exceeded that of the corresponding elements subjected to static loading only. These findings suggest that more efficient structural designs may be achievable if this phenomenon is better understood and appropriate design and analysis methods are developed to account for it in engineering practice.

The findings further showed that commonly used damage indicators for assessing the post-impact residual resistance—such as the permanent plastic deformation and extent of cracking—may not provide a reliable indication of the residual energy absorption capacity.

The influence of uncertainties on the structural response became more pronounced with an increasing number of impacts. Indeed, substantial variability was observed in both the total energy absorption capacity and the residual static load-carrying capacity as the number of impacts increased. For the restrained beams, variability in the reinforcement fracture strain was also found to have a meaningful influence. These findings highlight the need to account for such uncertainties through appropriate design factors. Moreover, stricter safety factors may be warranted for repeated impacts.

Finally, the study demonstrated the benefits of restrained boundary conditions on the response of RC elements. Both the load-carrying capacity, deformation limit, and overall energy absorption capacity can be enhanced by restrained BCs. Moreover, evaluation of the degree of restraint using the developed FE model showed that perfect or near-perfect restraint is not a requirement for developing membrane action. Therefore, it would be beneficial to investigate the minimum levels of longitudinal and rotational restraint that are needed to utilise membrane action in practical design situations.

Chapter 6

Prediction methods

6.1 Blast loads from gas explosions in traffic environments

Based on the results of the CFD analyses, the study presented in **Paper II** developed guidelines for using the MEM for estimating the blast load generated by VCEs in scenarios populated by vehicles. The results of interest are the peak overpressure, ΔP^+ , and peak positive impulse, i^+ . The guidelines define the total size of the source of blast, V_{source} , based on the number of vehicles (factors D and E) and the separation distance between them (factor A), as given by Eq. (6.1) to (6.4). The variable V_{car} denotes the volume occupied by one vehicle. Additionally, a portion of the vapour cloud volume outside the congested region should be included in the blast source. This contribution is described by the parameters e_x , e_y , and e_z , which are determined with Eq. (6.5) and (6.6). It should be noted that the length of the blast source should not exceed $L_{\text{source,max}} = 2.5b_{\text{source}}$. Further information regarding the different variables is provided in Appendix C of **Paper II**.

$$l_{\text{source}} = E \cdot l_{\text{car}} + (E - 1) \cdot A + 2e_x \quad (6.1)$$

$$b_{\text{source}} = D \cdot b_{\text{car}} + (D - 1) \cdot A + 2e_y \quad (6.2)$$

$$h_{\text{source}} = h_{\text{car}} + e_z \quad (6.3)$$

$$V_{\text{source}} = l_{\text{source}} \cdot b_{\text{source}} \cdot h_{\text{source}} - D \cdot E \cdot V_{\text{car}} \quad (6.4)$$

$$e_x = e_y = 2.0 \text{ m} \quad (6.5)$$

$$e_z = 1.0 \text{ m} \quad (6.6)$$

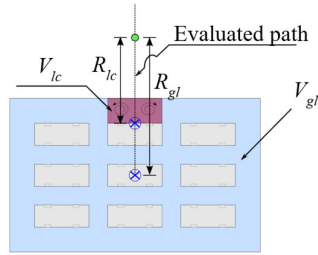
To account for the inherent asymmetry in the distribution of overpressure and impulse in the near field, the blast source is divided into a localised and a global source with volume V_{lc} and V_{gl} , respectively. The localised source is located just outside the group of vehicles, and its volume is determined by the projection of one vehicle and the extension of the gas cloud outside the group of vehicles (e_y), as expressed by Eq. (6.7). The volume of the global source is given by (6.8).

$$V_{\text{lc}} = (2.0 \text{ m}) \cdot l_{\text{car}} h_{\text{car}} \quad (6.7)$$

$$V_{gl} = V_{source} - V_{lc} \quad (6.8)$$

The distinction between localised and global explosion implies that two different stand-off distances must be considered when estimating the blast load at a given point: (i) the distance from the global source, R_{gl} , measured from the centre of the configuration; and (ii) the distance from the localised source, R_{lc} , measured from the edge of the vehicle group along a straight line connecting the centre of the configuration to the target point, as illustrated in Figure 6.1.

(a) Evaluated path perpendicular to main direction



(b) Evaluated path at 45°

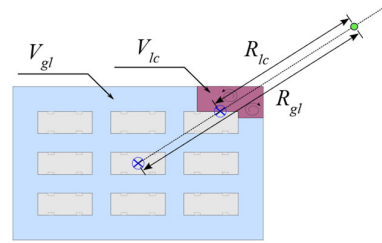


Figure 6.1. Description of the localised blast source and calculation of the stand-off distances for two representative paths. Taken from **Paper II**.

Finally, the selection of the strength class is based on the number of vehicles in the transverse direction (factor D), which was found to have the most important influence on the explosion strength. Table 6.1 presents general recommendations for the choice of strength classes for the global and localised explosions, S_{gl} and S_{lc} , respectively, together with the corresponding maximum overpressure according to the MEM, P_{MEM} . Adjustments to these recommendations are necessary when evaluating other conditions, such as elongated configurations or different mixture compositions. Further guidance for considering (or disregarding) the localised explosion is also presented in **Paper II**.

Table 6.1. Recommended strength class for the localised and global explosion.

No. of vehicles in the transverse direction	Localised source		Global source	
	S_{lc} (-)	P_{MEM} (kPa)	S_{gl} (-)	P_{MEM} (kPa)
1	4	10	4	10
2	6	50	5	20
3	7	100	6	50
4	8	200	7	100
5	9	500	8	200
≥ 6	≥ 9	≥ 500	≥ 8	≥ 200

The prediction of peak impulse using the proposed guidelines was found to have good agreement with the CFD results, although greater deviations were obtained for the strongest explosions ($S > 8$). Achieving acceptable and consistent predictions of peak overpressure throughout the domain was more challenging, largely because of the assumption of point-symmetrical blast wave propagation in the MEM and the relatively large steps between strength classes in the MEM. However, it was possible to obtain reasonable and conservative predictions of peak overpressure. Moreover, the accuracy of the MEM predictions generally improved with increasing distance from the explosion centre. Figure 6.2 compares the results of the CFD calculations with the corresponding MEM predictions for a representative scenario involving

3×3 vehicles. The figure showcases the capability of the adjusted MEM to reproduce the localised zones with high overpressure values.

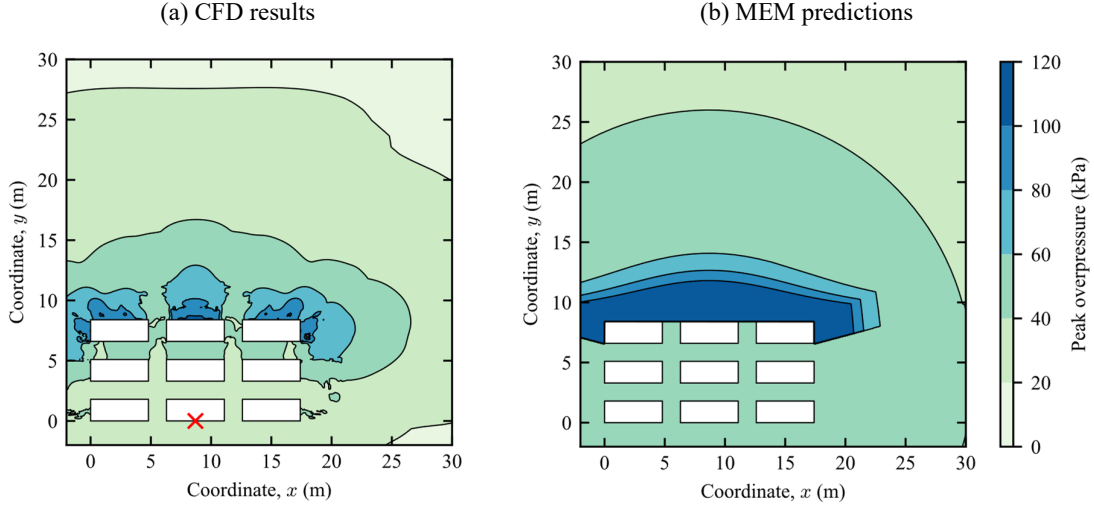


Figure 6.2. Comparison of the contour plot of peak overpressure for a scenario with 3×3 vehicles obtained with CFD analysis and the MEM. Adapted from **Paper II**.

6.2 Probability of premature shear failure in blast-loaded RC beams

To guarantee higher levels of confidence concerning ductile bending failure in blast-loaded simply supported RC one-way slabs, **Paper III** introduced a reliability-based overstrength factor, $\gamma_{R,d}$, that can be applied to amplify the design shear demand. The proposed approach was inspired by the principles of capacity design commonly adopted within seismic design philosophy. Accordingly, the shear capacity of the blast-loaded RC element should satisfy the following design condition:

$$V_{R,d} \geq \gamma_{R,d} V_{E,d} \quad (6.9)$$

Let the utilisation of the design shear capacity, χ_v , be defined by Eq. (6.10). Based on the results from the Monte Carlo simulations in **Paper III**, it was found that the probability of premature shear failure associated with a given value of χ_v could be expressed by the modified logistic function defined by Eq. (6.11), in which $k = 9.54$ and $a = 0.88$ for specimens without shear reinforcement, and $k = 7.34$ and $a = 0.70$ for specimens with shear reinforcement.

$$\chi_v = \frac{V_{E,d}}{V_{R,d}} = \frac{1}{\gamma_{R,d}} \quad (6.10)$$

$$P_f(\chi_v) = 1 - \frac{1}{1 + \exp[-k(\chi_v^{-1} - a)]} \quad (6.11)$$

By combining Eq. (6.10) and Eq. (6.11), the overstrength factor required to achieve a specific target probability of shear failure can be determined. Table 6.2 presents the calculated overstrength factors corresponding to different reliability levels for blast-loaded RC one-way slabs without stirrups ($\gamma_{R,d,e}$) and with stirrups ($\gamma_{R,d,s}$). These factors can be directly integrated into the design process, provided that the resistance models specified in Eurocode 2 are employed and the strain-rate effects on the material properties are considered, for instance by

using the DIFs recommended in UFC 3-340-02 [33]. However, if the strain-rate effects are disregarded during the design process, larger overstrength factors than those given in Table 6.2 are required to achieve the same level of reliability.

Table 6.2. Calculated overstrength factors to reduce the probability of premature shear failure in blast-loaded RC one-way slabs without stirrups, $\gamma_{Rd,c}$, and with stirrups, $\gamma_{Rd,s}$.

Probability of premature shear failure, P_f	$\gamma_{Rd,c}$	$\gamma_{Rd,s}$
$\leq 10.0\%$	1.12	1.00
$\leq 5.0\%$	1.20	1.08
$\leq 2.0\%$	1.30	1.20
$\leq 1.0\%$	1.37	1.28
$\leq 0.1\%$	1.61	1.57

For situations in which the proposed overstrength factors are not applicable, suitable factors may be determined by implementing the framework presented in **Paper III**. This requires the statistical characterisation of the uncertainties associated with the incompatible parameters. Alternatively, the approximative analytical procedure presented by Lozano [149] may be employed. However, it should be noted that this analytical procedure is only valid when the uncertainties associated with the resistance models can be represented by a lognormal distribution.

6.3 Response of impact-loaded RC beams

6.3.1 Scope and outline of the section

This section presents a series of simplified methods for predicting relevant response parameters of impact-loaded RC beams. Numerical, analytical and empirical approaches are discussed. For single-impact scenarios, the principal parameters of interest include peak deflection, membrane force, impact force, energy dissipated by the beam, and permanent plastic deformation. For repeated impact loading, the parameters of interest are the total energy absorption capacity and the allowable number of impacts.

Before addressing the impact response, the section introduces an approach for determining the static load-deflection curve of restrained beams. This feature constitutes an essential input for the subsequent evaluation of the behaviour of restrained beams subjected to impact loading.

6.3.2 Load-deflection curve of restrained RC beams under static loading

Paper V introduced an analytical approach for predicting the load-deflection and horizontal reaction force-deflection curves of restrained RC beams subjected to static three-point bending. The method treats the CMA and TMA phases separately. Continuity between the two stages is maintained by using the reinforcement strain at the end of the CMA phase as the initial condition at the onset of TMA.

The method for describing the CMA response builds on the framework proposed by Kang and Tan [130], which is based on Park's model [119]. The method assumes a three-hinged plastic system in which rotational demands concentrate at hinges at the supports and midspan, and the beam segments in between the hinges are assumed to be rigid regarding flexural action. Geometric compatibility is satisfied by accounting for support rotations and translations, as well

as the axial deformation of the beam. Force equilibrium at the critical sections is also enforced. In addition, the evolution of the reinforcement strain at the supports is related to the global deformation by assuming a linear decrease of the strain from its maximum value at the yield line to zero at the point of zero moment, as originally proposed by Kang and Tan [130]. However, for calculating the reinforcement strain at midspan, a relationship between the support and midspan curvatures is adopted. Reinforcement fracture was also incorporated. The effect of tension stiffening, which affects the distribution of reinforcement strain, is accounted for in a simplified manner by assuming a reduced fracture strain equal to half of the experimentally determined mean value. Non-linear BCs at the support may be considered during the CMA phase. However, during the analysis of the TMA phase, the horizontal translation was assumed to be fully restrained.

In addition to the analytical approach, a numerical FE model was built in ABAQUS/Standard [182]. The definition of the model was deliberately kept simple, using Timoshenko beam elements to represent the concrete material, and truss elements to model the reinforcement. Bond-slip behaviour was incorporated by combining the models in Model Code 90 [183] and Model Code 2010 [184]. Moreover, the post-peak softening behaviour of the concrete was regularised with regard to the element size. Compared with the analytical model, the FE model enabled a more complex description of the BCs.

The predictions obtained from both the analytical and FE models were evaluated against the experimental results. Figure 6.3 shows the comparison for two representative specimens. Regarding the analytical method, good agreement was achieved in the prediction of the response in the CMA phase. However, greater discrepancies were obtained for the TMA phase. These deviations were primarily caused by the simplified representation of the BCs in the analytical model. Thus, to reduce the risk of overestimating the load-carrying capacity during the TMA phase, a more conservative alternative is to consider only the contribution of the compression reinforcement in the TMA phase.

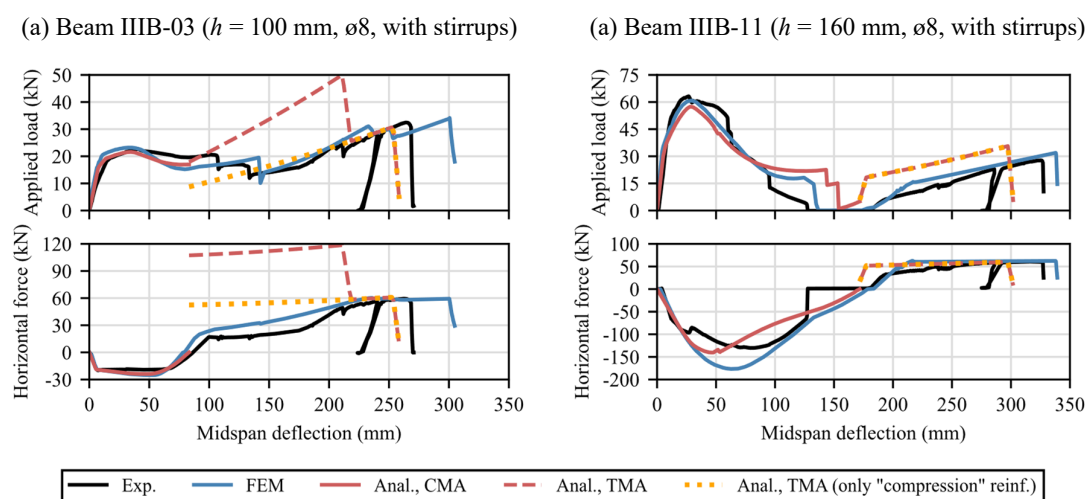


Figure 6.3. Comparison of the response under static loading between experiments, FE model, and analytical solution for two selected specimens. Adapted from *Paper V*.

The FE model demonstrated good predictive capabilities considering the prediction of peak CMA resistance, post-peak behaviour, rebar fracture events, and deformation limit. Moreover, the results showed that the FE model is more suitable than the analytical model for cases

involving complex boundary conditions, which cannot be represented by the simplified assumptions adopted in the analytical model.

The analytical model was further validated against the FE model under the assumption of ideal linear elastic BCs. Figure 6.4 presents the comparison between the two approaches for two representative specimens. Under these ideal conditions, the analytical model displayed good agreement with the FE model, indicating that the analytical model produces reasonable results when the BCs align with its underlying assumptions.

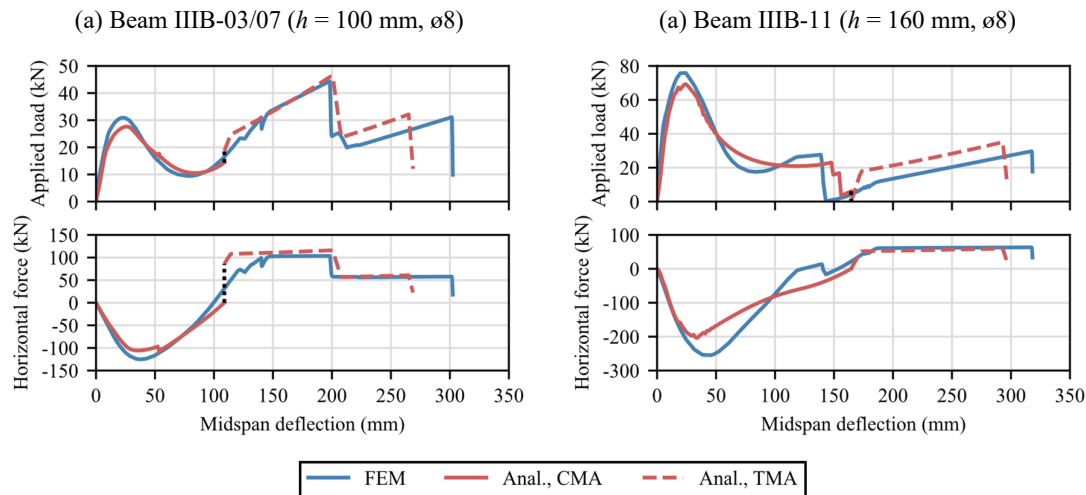


Figure 6.4. Comparison between the FE model and analytical solution with idealised linear elastic BCs for two selected specimens. Adapted from **Paper V**.

6.3.3 2DOF model for impact response of restrained RC beams

A 2DOF mass-spring-damper system was implemented in **Paper VI** to describe the response of the restrained beams under impact loading. A brief description of the theoretical background of the 2DOF system is provided in Section 2.4.4. The scope of the model was limited to the initial impact on undamaged beams and the first unloading cycle, as the behaviour during the rebound phase was not investigated.

The model accounts for the variation of effective span and activated beam mass associated with the impact wave propagation. Hence, a local and a global response are distinguished. During the local phase, the effective span increases progressively until the entire span is activated and the full beam mass is engaged. The reduced effective span during this phase results in an increased flexural stiffness, which is inversely proportional to the effective span. During the global phase, the entire beam mass and span are assumed to be engaged.

The load-deflection response of the beam is based on the analytical approach developed in **Paper V**—and briefly described in Section 6.3.2—in conjunction with the DIFs specified by UFC 3-340-02 [33]. The secant stiffness of the beam at a certain deflection is derived from this load-deflection curve. Damping of the beam was also considered.

The impact force is modelled as the sum of a restoring force, related to the contact stiffness, and a dissipative damping force. The loading phase of the impact force was described using the Hertz-damp model [106], while unloading from the peak force was assumed to follow a linear relationship. The contact stiffness was calibrated against the experimental data. It should be noted that analytical expressions for estimating the contact stiffness for collisions between two

bodies can be derived from Hertz's contact theory [87], [109]. However, equivalent expressions for collisions involving three bodies, as was the case in the impact tests of the restrained beams, are not readily available.

To calculate the damping coefficient at contact, the coefficient of restitution needs to be determined first. This parameter was estimated by analysing the velocity-time histories of the drop-weight and the beam (at midspan). The results showed that the velocities of the two bodies converged at peak indentation. Beyond this point, the beam's velocity continued increasing, while the drop-weight velocity decreased, indicating that the indentation had started to decrease. However, because of its internal resistance, the beam reached its peak velocity shortly after peak indentation, before starting to decrease. From this point onward, the two velocity-time histories exhibited a similar evolution. The velocities of both bodies at the instant when the beam reached its peak velocity were used to estimate the velocities that the two bodies would have maintained after the collision in the absence of the beam's internal resistance. Therefore, Eq. (2.12) and Eq. (2.13), which describe the post-impact velocities based on classical impact theory, could be solved to find the best value of e_r that provided the best fit across all specimens. This analysis yielded a coefficient of restitution of $e_r \approx 0.15$.

In general, the 2DOF model showed good predictive capabilities. It provided reasonable predictions of the maximum deflection, dissipated energy, and specific impulse, with maximum errors of 10%, 8% and 16% respectively. The most significant shortcoming of the model was that it failed to predict the collapse of specimen IIB-02 under impact. Posterior DIC analysis showed that this specimen experienced fracture of the bottom reinforcement much earlier than predicted by the analytical model used to estimate the load-deflection curve. Consequently, the available internal strain energy was overpredicted by the 2DOF model. This discrepancy is believed to have been caused by reinforcement bars with unusually low fracture strain.

The 2DOF model also enables the prediction of the impact force profile. Figure 6.5 compares the predicted impact force profile with the corresponding experimental results for two representative beams. The 2DOF model slightly underpredicted the peak impact force. However, the impulse associated with the first, major peak accounted for approximately 30% of the total impulse content. Hence, the small discrepancies in the magnitude of the peak force did not translate into a significant underestimation of the impulse. Moreover, it should be noted that the magnitude of the experimentally measured peak force is sensitive to the filtering technique applied on the acceleration signal.

Finally, the 2DOF model allows for indirect estimation of the horizontal reaction force, which can be used as an indication of the membrane force. This calculation is performed after solving the dynamic equations of motion, by determining the horizontal reaction force corresponding to the beam resistance at each time step. Figure 6.6 showcases the prediction of the horizontal reaction force for two selected specimens. As the 2DOF system was implemented only for the first unloading cycle, the predicted curve in Figure 6.6(b) cuts off when the first unloading cycle is finished. Overall, the model provides a reasonable estimate of the trend displayed by the horizontal reaction force. Where applicable, the transition to the TMA phase was also predicted well. However, the predicted horizontal reaction force curve does not capture the multiple peaks and valleys observed in the experimental curve, which may be associated with higher-order modes. Nevertheless, the method provides an acceptable indication of the evolution of the membrane force.

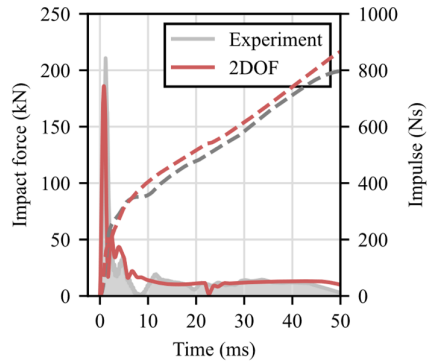
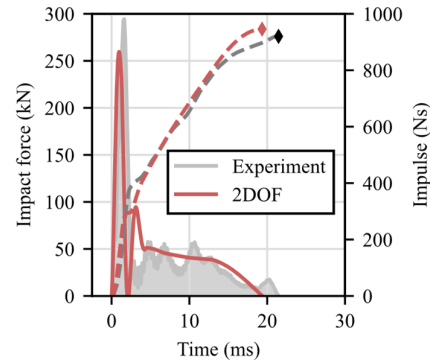
(a) Specimen IIIB-04, $n = 1$, drop-weight: 80 kg(b) Specimen IIIB-12, $n = 1$, drop-weight: 80 kg

Figure 6.5. Example of prediction of impact force profile and impulse with the 2DOF model. Adapted from **Paper VI**.

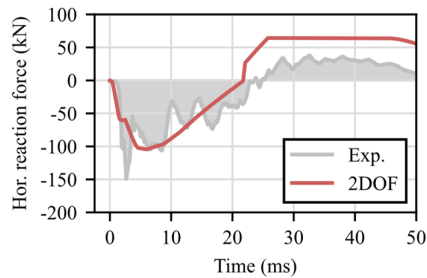
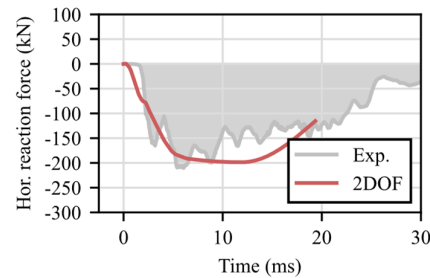
(a) Specimen IIIB-04, $n = 1$, drop-weight: 80 kg(b) Specimen IIIB-12, $n = 1$, drop-weight: 80 kg

Figure 6.6. Example of prediction of horizontal reaction force with the 2DOF model. Adapted from **Paper VI**.

6.3.4 Estimation of the impact response with empirical and analytical methods

6.3.4.1 Estimation of maximum deflection based on empirical relationships

Previous studies have identified a relationship between peak deflection, impact energy, and static resistance, as described in Section 2.4.5.1. Such a relationship is of interest due to its simplicity and ease of application. This study validated this relationship using the simply supported beams presented in **Paper IV**. The evaluation is illustrated in Figure 6.7. A strong linear relationship was confirmed, with a slope of 0.62. The 95% confidence interval for the slope constant is [0.59, 0.65]. It is worth highlighting that the regression line shown in the figure was constrained to pass through the origin, which is physically consistent. Nevertheless, the additional error introduced by this constraint is minor for this dataset.

The good linear relationship observed in the experimental dataset from **Paper IV** is likely partly attributable to the fact that all the specimens shared identical nominal geometrical properties and reinforcement layout. Greater scatter should be expected when considering specimens with varying properties. To investigate this further, Figure 6.8 extends the analysis by incorporating experimental results reported by Johansson et al. [105], Tachibana et al. [112], and Kishi and Mikami [111]. Specimens exhibiting a distinct shear-dominated failure mechanism were excluded from the dataset. In addition, it was verified that the design shear resistance of the

selected specimens exceeded their design flexural resistance. In total, 91 test results were included in the analysis. The dataset consists of specimens with dimensions within the ranges: $h = [0.1, 0.4]$ m, $b = [0.1, 0.3]$ m, and $L = [1.0, 4.0]$ m. As the experimental static load-carrying capacity was not available for all specimens, the theoretical ultimate resistance according to Eurocode 2, P_{EC} , was calculated and used for each specimen. These calculations considered only the bottom longitudinal reinforcement and neglected strain hardening of the reinforcing steel.

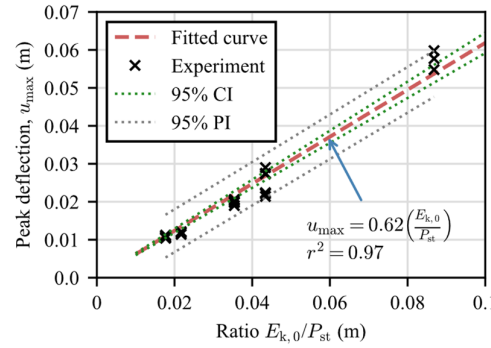


Figure 6.7. Relationship between maximum deflection (u_{max}), impact energy ($E_{k,0}$) and static resistance (P_{st}) for the simply supported specimens in **Paper IV**. CI: confidence interval, PI: prediction interval.

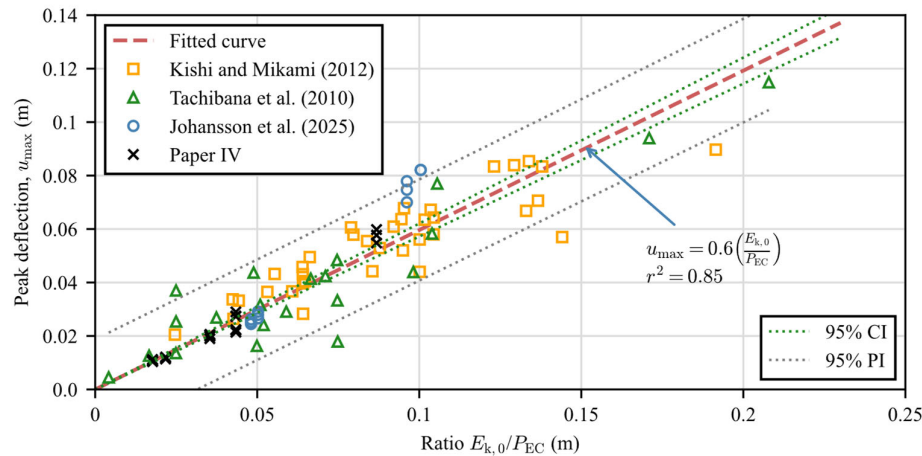


Figure 6.8. Relationship between maximum deflection (u_{max}), impact energy ($E_{k,0}$) and theoretical static resistance according to Eurocode 2 (P_{EC}) for the simply supported specimens in **Paper IV** and references [105], [111], [112].

Figure 6.8 shows that the slope of the regression line for the expanded dataset is 0.60, with a 95% confidence interval of $[0.57, 0.62]$. This value is slightly lower than that obtained in Figure 6.7. The reduction can be partially attributed to the fact that P_{EC} was generally lower than the corresponding experimental value, P_{st} , for the specimens for which P_{st} was known. The calculated slope falls between the values reported by Nghiem and Kang [110], based on an analysis using ACI Code 318-14 [185], and by Kishi and Mikami [111], based on the provisions of the JSCE (Japan Society of Civil Engineers): 0.58 and 0.63, respectively. Figure 6.8 also shows that the scatter in the results increased noticeably when the dataset was expanded. Indeed,

this comparison indicates that the scatter previously obtained for the tests in **Paper IV** was relatively minor compared with that of other datasets.

A key limitation of the empirical approach is that it predicts equal deflection for beams subjected to the same impact energy, regardless of the combination of drop-weight mass and impact velocity used to generate that impact. This contrasts with the experimental results reported in **Paper IV** and by Tachibana et al. [112], which indicate that, for a given impact energy, larger deflections are produced when the energy is imparted by a heavier drop-weight striking at a lower velocity. Thus, additional parameters related to the beam stiffness, geometry, and impact conditions may be required to achieve a more general predictive model. Nevertheless, the empirical results provide valuable insight into the response of the beams and may be particularly useful in early stages of design. Furthermore, more conservative predictions can be achieved by adopting, for instance, the upper bound of the 95% prediction interval or another appropriate confidence level. However, care must be exercised when applying the model outside the range covered by the calibration dataset.

A similar relationship was examined for the restrained beams in **Paper VI**. A key challenge in this case concerns the definition of P_{st} under restrained conditions. Since catenary action did not fully develop during the first strike in most of the tested specimens, it was deemed reasonable to adopt the peak static CMA resistance, $P_{CMA,st}$, as the reference capacity. For specimen IIIB-02, which failed under impact loading, it may be more appropriate to use an average resistance value that accounts for both the CMA and TMA phases, which would yield a value lower than $P_{CMA,st}$. However, had the specimen not failed during impact, its maximum deflection would likely have exceeded the measured deflection at failure. Therefore, it is considered that the effect of adopting $P_{CMA,st}$ for IIIB-02 is reasonably balanced by the use of the deflection measured at collapse.

Figure 6.9 presents the results for the restrained beams, revealing a clear positive correlation between $E_{k,0}/P_{CMA,st}$ and the maximum deflection. However, in this case, a power-law function of the general form given by Eq. (6.12) was found to provide the most suitable yet simple representation of the relationship between the studied parameters. Indeed, this formulation yields physically realistic results for small values of $E_{k,0}/P_{CMA,st}$. Finally, it should be noted that the linear relationship given by Eq. (2.34) may be regarded as a special case of the power-law function with $\beta = 1$.

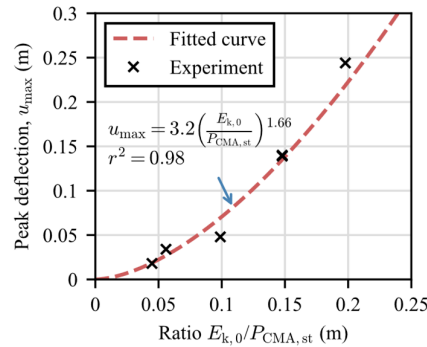


Figure 6.9. Relationship between maximum deflection (u_{max}), impact energy ($E_{k,0}$), and peak CMA resistance ($P_{CMA,st}$) for the restrained RC specimens in **Paper VI**.

$$u_{\max} = \eta \left(\frac{E_{k,0}}{P_{st}} \right)^\beta \quad (6.12)$$

Given the limited availability of experimental data on restrained RC beams subjected to impact loading, an artificial database comprising 20 specimens was generated using the 2DOF model described in Section 6.3.3 to confirm the observed relationship. The 2DOF model was previously validated and found to provide reasonable predictions of the peak deflection. The database was generated from the original six test specimens by varying the drop-weight mass between 10 and 140 kg, while keeping the specimen geometry, reinforcement configuration, and impact velocity unchanged. In addition, the two restrained specimens tested by Cui et al. [127], [186] were included. The results, presented in Figure 6.10, provide further support for a power-law fit, although the corresponding fit curve exhibits somewhat different parameters.

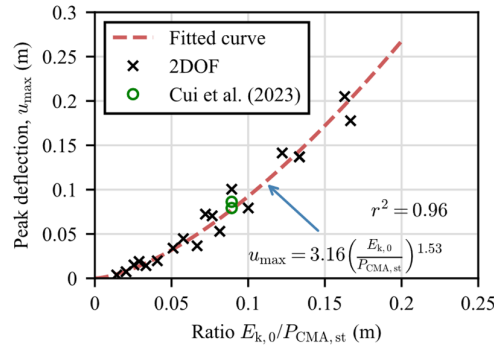


Figure 6.10. Relationship between maximum deflection ($u_{\max}$), impact energy ($E_{k,0}$), and peak CMA resistance ($P_{CMA,st}$) for artificial restrained RC specimens analysed with the 2DOF model and the restrained RC beams in the study by Cui et al. [127].

6.3.4.2 Estimation of maximum deflection based on energy balance

According to the principle of conservation of energy, and neglecting other minor energy-dissipation mechanisms, the external work applied to the beam by the impact load must be balanced by the sum of its kinetic energy and internal strain energy. At the instant of maximum deflection, the beam velocity and, consequently, the kinetic energy become zero. This condition can be leveraged to estimate the maximum deflection. If damping effects are disregarded, the energy balance may be expressed by Eq. (6.13). The left-hand side of the expression represents the external work exerted on the beam by the impactor, determined based on classical impact theory. The contribution of the change of potential energy of the beam and drop-weight, from the initial position up to peak deflection, is also included. The right-hand side of the expression represents the internal strain energy in the beam, in which $R(u)$ denotes the resistance function including strain-rate effects.

$$\frac{m_d + e_r^2 m_b}{m_d + m_b} E_{k,0} + \left(m_{\text{drop}} + \frac{m_{\text{beam}}}{2} \right) \cdot g \cdot u_{\max} = \int_0^{u_{\max}} R(u) du \quad (6.13)$$

The predictive capabilities of Eq. (6.13) were validated against the experimental results for both the simply supported and restrained beams. For the former, a perfectly inelastic collision was assumed, thus $e_r = 0$. For the restrained beams, a value of $e_r = 0.15$ was adopted, as described in Section 6.3.3. The resistance function of the simply supported beams was represented by a bilinear load-deflection curve, in which the resistance increases linearly from zero to the

ultimate resistance and remains constant thereafter. The elastic flexural stiffness was calculated using the moment of inertia of the cracked concrete section. The ultimate resistance was determined in accordance with Eurocode 2 together with the DIFs recommended in UFC 3-340-02, with values of 1.25, 1.23 and 1.05 for the concrete compressive strength, the yield strength, and the ultimate strength of the reinforcing steel. The resistance function of the restrained beams was determined using the analytical method described in Section 6.3.2, adopting the same DIFs used for the simply supported beams. The load-deflection curves for the restrained beams are presented in **Paper VI**.

Figure 6.11 compares the experimentally measured and the predicted values of maximum deflection. In all cases, the analytical method based on energy balance overestimates the deflection, although the deviations remain within an acceptable range ($< 30\%$). One contributing factor is that the method does not account for the increased beam stiffness during the local response phase due to a shorter effective span. Additionally, the minor contribution to energy dissipation from damping effects is neglected in the analysis. For the simply supported beams, a further source of discrepancy is the use of the theoretical resistance according to Eurocode 2, which was found to be lower than the experimentally measured values.

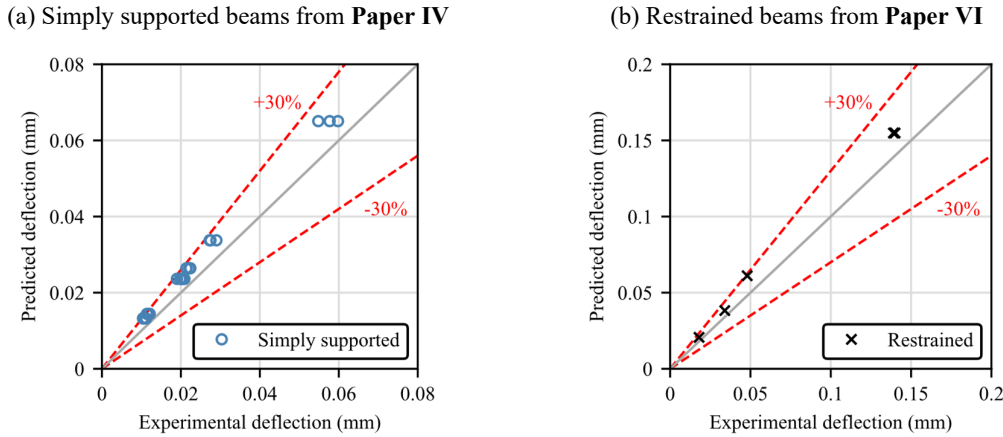


Figure 6.11. Validation of midspan deflection predictions with the energy balance method.

It should also be noted that the external work predicted by the left-hand side expression in Eq. (6.13) was consistently lower than the experimentally determined external work for all restrained beams. However, this error appears to partially compensate for the errors introduced by disregarding the local response phase of the beam and the energy dissipation through damping.

6.3.4.3 Estimation of residual deflection based on empirical relationships

Similar to the empirical relationship described for the maximum deflection in Section 6.3.4.2, an equivalent relationship between the residual plastic deflection, u_{pl} , the impact energy, and the static resistance has also been reported in the literature, as previously discussed in Section 2.4.5.2. Thus, it is of interest to examine such a relationship for the simply supported specimens presented in **Paper IV**. Figure 6.12 illustrates this relationship, which confirms a significant positive relationship. A satisfactory fit was obtained even when the regression line was constrained to pass through the origin ($r^2 = 0.9$), although the associated error was greater than that observed for the prediction of the maximum deflection in Figure 6.7. The estimated slope for the regression line is 0.44, which is in close agreement with the value of 0.42 reported by

Kishi and Mikami [111] and Nghiem and Kang [110] for a larger database of simply supported RC beams.

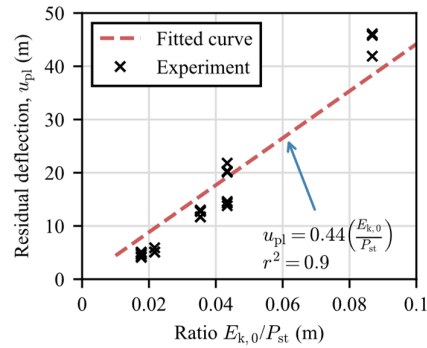


Figure 6.12. Relationship between residual deflection (u_{pl}), impact energy ($E_{k,0}$), and static resistance (P_{st}) for the simply supported specimens in **Paper IV**.

A similar relationship was investigated for the restrained beams, as shown in Figure 6.13. Although a positive relationship was observed, the scatter was noticeably greater than that obtained for the simply supported beams. A potential outlier can be identified at $E_{k,0}/P_{CMA,st} = 0.1$; however, given the limited size of the dataset, it is difficult to determine whether this observation is statistically significant. Furthermore, the overall trend suggests that a linear fit would not provide physically correct predictions for low values of $E_{k,0}/P_{CMA,st}$. Thus, a power-law function was fitted to the data, providing an acceptable representation of the behaviour. However, further research is needed to confirm this relationship.

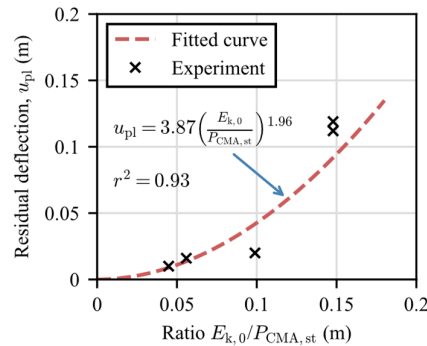


Figure 6.13. Relationship between residual deflection (u_{pl}), impact energy ($E_{k,0}$), and peak CMA resistance ($P_{CMA,st}$) for the restrained RC specimens in **Paper VI**.

6.3.5 Dynamic energy absorption capacity and number of impacts

As described in **Paper VI**, the results from the tests of the restrained beams suggest a positive relationship between the number of impacts and the enhancement of the total energy absorption capacity relative to the corresponding static capacity, η_E . Figure 6.14(a) illustrates this relationship. Although the positive relationship is evident, the scatter of the results is considerable, and the corresponding r^2 -coefficient is relatively low. One possible explanation for this is that the number of impacts may not adequately represent the total energy absorbed by the element, as most specimens retained certain residual energy absorption capacity, $E_{st,res}$, after the impact tests. Therefore, to account for this residual capacity, the total number of impacts to failure, n_R , was defined using Eq. (6.14). In this approach, the residual capacity is

converted into an equivalent number of impacts, $n_{eq,res}$, by normalising it with respect to the energy dissipated per impact, E_i . A stronger correlation is obtained when n_R is used instead, as illustrated in Figure 6.14(b).

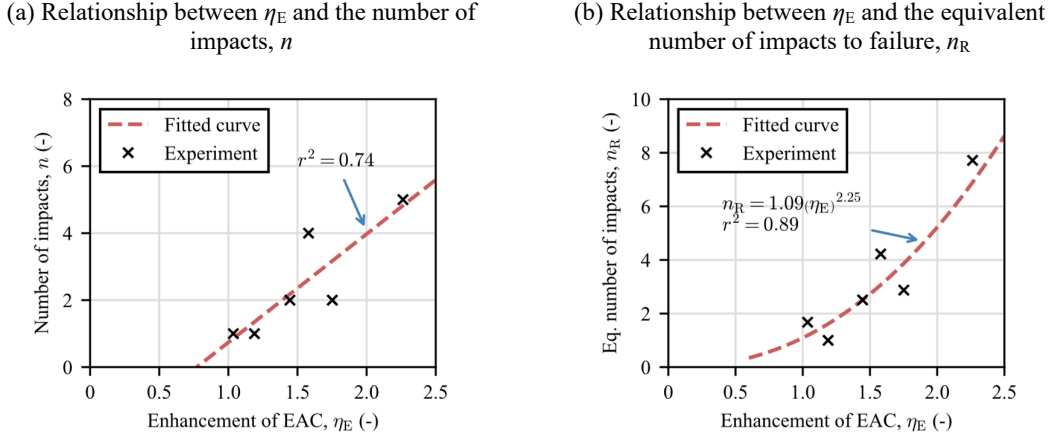


Figure 6.14. Relationship between the number of impacts and the enhancement of the energy absorption capacity (η_E).

$$n_R = n + n_{eq,res} = n + E_{st,res} / E_i \quad (6.14)$$

The relationship presented in Figure 6.14, although informative, may have limited practical applicability, as the number of impacts of a given intensity required to produce failure may not be known. Thus, it is of interest to be able to estimate n_R from known parameters. A strong inverse relationship was identified between n_R and the dissipated energy index, e_i , defined by Eq. (5.2). This relationship is illustrated in Figure 6.15(a). The observed correlation is well described by a power-law function, as shown in the figure.

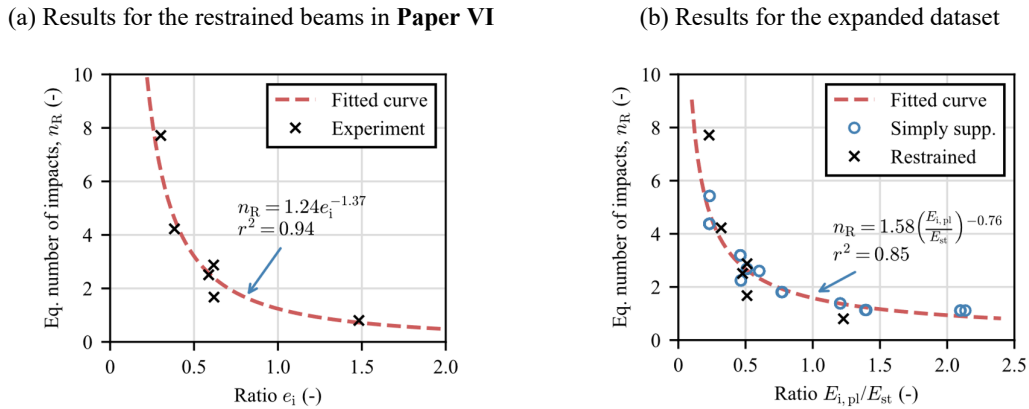


Figure 6.15. Relationship between the equivalent number of impacts to failure (n_R) and: (a) the dissipated energy index (e_i), for the specimens in **Paper VI**; and (b) the ratio $E_{i,pl}/E_{st}$, for the specimens in **Papers IV and VI** and Johansson et al. [105].

Given the limited availability of experimental research concerning repeated impact loading on restrained RC beams, further research work is needed to confirm the relationship presented in Figure 6.15(a). However, as an alternative approach to validate the observed correlation, Eq. (6.14) was employed to compute the equivalent number of impacts to failure for the impact-

loaded simply supported beams in **Paper IV** and Johansson et al. [105]. The results are given in Figure 6.15(b). In the figure, an average value is reported for nominally identical simply supported beams subjected to the same impact conditions. Furthermore, to achieve consistency across all experimental campaigns, the ratio $E_{i,pl}/E_{st}$ was used instead of the dissipated energy index, e_i . The parameter $E_{i,pl}$ is the theoretical value of the energy transferred to the beam assuming an ideal plastic collision, which can be determined using Eq. (2.20). Such substitution is expected to work well for the restrained beams in light of the strong relationship between E_i and $E_{i,pl}$ presented in Figure 5.7.

Figure 6.15(b) shows that the simply supported beams and restrained beams exhibit a similar trend, with both conforming reasonably well to a power-law regression. The results also suggest the presence of a vertical asymptote at very small values of $E_{i,pl}/E_{st}$, representing a lower threshold of the impact energy below which no significant damage is expected. Furthermore, the results suggest a horizontal asymptote at approximately $n_R = 1.0$ for large values of $E_{i,pl}/E_{st}$, indicating impact intensities sufficient to cause failure in a single impact.

The regression curves shown in Figure 6.15 may be used to calculate n_R for a given impact scenario—provided that the static energy absorption capacity is known—which can then be used to estimate the total energy absorption capacity. This is exemplified in Figure 6.16 for both restrained and simply supported beams. As observed, most of the predictions fall within a $\pm 30\%$ confidence interval.

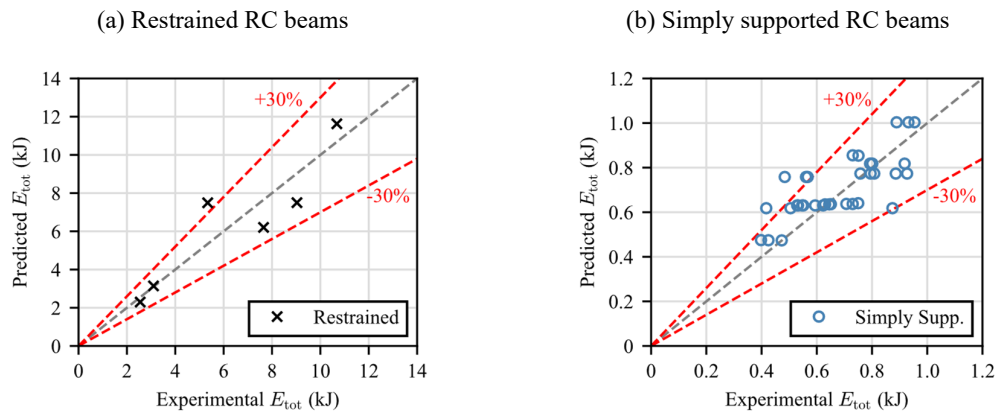


Figure 6.16. Validation of the empirical estimation of the total energy absorption capacity for: (a) the restrained specimens in **Paper VI**, and (b) the simply supported specimens in **Papers IV** and Johansson et al. [105].

Conclusions and future work

7.1 Overview

The overarching aim of this thesis was to advance the understanding of extreme dynamic loading and its effects on reinforced concrete structures and to develop predictive methods suitable for practical applications. The research was organised into two main research areas. Within these research areas, three objectives and five specific research questions (RQ) were formulated.

The first stage of this research (**Research Area I**) focused on vapour cloud explosions (VCEs) in traffic environments. The critical aspect addressed concerned the inconsistencies currently affecting the prediction of blast loads from VCEs in such environments, arising from the lack of engineering methods specifically tailored to these conditions. The study employed advanced CFD calculations to examine the effects of multiple parameters on the overpressure and impulse generated by VCEs in predefined traffic scenarios, and to provide recommendations for implementing one of the most widely used simplified methods—the Multi-Energy Method (MEM)—to such scenarios. The study enabled ranking of the scenario parameters based on their influence on the resulting overpressure and impulse. Moreover, it was found that some parameters displayed a distinct non-linear relationship with the resulting overpressure. Some important interactions between the effects of different parameters were also observed. Given the high computational demands of the calculations, the study combined a two-stage analysis based on factorial design principles with one-factor-at-a-time (OFAT) analyses to sequentially identify the most significant parameters and interactions that could thereafter be used as the main input for relating the scenario characteristics to the strength classes in the MEM.

The second stage of the research (**Research Area II**) focused on the response of RC one-way members to impulsive actions, with energy absorption capacity as the common aspect among all the conducted studies. Initially, the Monte Carlo method was used to investigate the probability of shear failure occurring prior to bending failure in blast-loaded slender RC slabs, accounting for the uncertainties associated with material properties, geometry, and resistance and load models. Thus, this analytical study addressed a topic which, to the knowledge of the author, has received relatively little attention in the existing literature. The results indicated that the probability of shear failure was generally lower than that of bending failure in the studied specimens. Moreover, among all examined sources of uncertainty, the uncertainties related to the resistance models were identified as the most significant.

The research further investigated the impact response of simply supported and restrained RC beams. The study demonstrated that RC elements exhibited greater energy absorption capacity under combined impact and subsequent static loading compared to equivalent members subjected only to static loading. In addition, restrained RC beams were shown to achieve greater load-carrying capacity and deformation limits. These findings highlight the potential for more efficient designs of RC structures exposed to impulsive loading. The work also addressed two notable gaps in the current state-of-the-art: the effects of repeated low-velocity impacts on the structural response, and the influence of restrained boundary conditions under impact loading. Finally, different simplified analysis methods were developed or validated to support the assessment of key response parameters.

7.2 Specific conclusions within Research Area I

RQ-1: *Which geometric parameters most strongly influence the severity of VCEs in traffic environments, such as roads and car parks?*

- The number of vehicles in the transverse direction (i.e. vehicles placed side by side) had the most significant effect on the explosion strength. In contrast, the number of vehicles in the longitudinal direction had a markedly lower influence. This difference was connected to the interplay between the main flow direction and the orientation and shape of the wheels. This finding suggests that the number of lanes on a road has greater influence on the explosion strength than the length of the queue of vehicles. Additionally, car parks where vehicles park side by side have a higher risk for powerful explosions.
- It was observed that there exists a critical separation distance between vehicles for which the overpressure is maximised. For the vehicle geometry adopted in this study, this distance was approximately 1.5 m. This critical value is expected to depend on the vehicle geometry, although this could not be verified in this study.
- Contrary to the assumption in the MEM and other similar approaches, the volume of the premixed gas cloud was found to influence the explosion strength. Increasing the size of the cloud outside the group of vehicles initially produced an increase in the resulting overpressure. However, after a certain extension beyond the edge of the group of vehicles, no further significant increase in overpressure was obtained. That is, the effect of the gas cloud volume appears to level off after a certain volume is reached.

RQ-2: *What simplified approach, supported by advanced numerical calculations, can be employed to achieve reliable and conservative estimates of the blast load generated by VCEs occurring on roads and car parks?*

- Although all the parameters examined affected the blast strength to some extent, it was found that the severity of the explosion could be reasonably estimated based primarily on the number of vehicles in the transverse direction. Based on this observation, the study developed recommendations for implementing the MEM in scenarios populated by vehicles. To make this simplification applicable to different scenarios, the influence of the other parameters was maximised in the scenarios used for fitting the MEM to the CFD results. The recommendations also provide a strategy to account for the type of fuel gas, although the proposed approach is limited to gases with reactivity comparable to or lower than that of propane.
- The aspect ratio of the configuration (i.e. the relationship between the number of vehicles in the transverse and longitudinal direction) was identified as a factor limiting

the influence of the number of vehicles in the transverse direction. Therefore, the guidelines account for this factor in the choice of the strength class.

- While relatively large values of overpressure were observed for some of the scenarios with the most unfavourable conditions, these were obtained only in localised areas with small effect radius. This implies that the energy content associated with the greatest overpressure values is limited in relation to the total gas inventory. Consequently, the proposed guidelines divide the blast source into a localised, stronger explosion and a general explosion of lower strength. This distinction also provides a strategy to account for the asymmetric distribution of overpressure observed in the scenarios, which is not considered by the original MEM or similar approaches.
- The peak impulse prediction obtained following the MEM guidelines exhibited good agreement with the corresponding CFD results. However, achieving satisfactory predictions of peak overpressure was more challenging, mainly due to the contrast between the assumption of point-symmetrical blast wave propagation in the MEM and the inherent asymmetry of the scenarios of interest, together with the relatively large steps between strength classes in the MEM. However, it was possible to obtain reasonable and conservative predictions, with the accuracy improving with increasing distance from the explosion centre.
- The proposed MEM guidelines assume an equivalent gas cloud at a stoichiometric concentration and of sufficient volume so that the effect of the gas volume on the overpressure is maximised. This assumption results in large vapour clouds. Therefore, the method should be regarded as providing an upper-bound estimate for the blast load in the scenarios of interest. Lower blast loads may be obtained in situations in which a reduced gas volume is justified, for instance through gas dispersion analysis. In such cases, the effective volume of the blast source may be reduced accordingly.

7.3 Specific conclusions within Research Area II

RQ-3: *How can the probability of premature shear failure in blast-loaded slender RC one-way slabs—accounting for material, resistance, geometry, and load uncertainties—be quantified and reduced through simplified methods to ensure a high confidence regarding ductile failure?*

- The Monte Carlo method, in combination with analytical models for resistance and dynamic response, was identified as an effective framework for conducting probabilistic studies of the phenomenon of interest. An alternative analytical approach was also developed, although this approach is limited to situations in which the uncertainties associated with the resistance models can be adequately represented by a lognormal distribution.
- The probability of shear failure occurring before bending failure in the studied RC slabs, which were designed using the resistance models in Eurocode 2, was found to be at most 26% in members without shear reinforcement and at most 9% in slabs with shear reinforcement. This indicates that there is an intrinsic safety margin within the evaluated design procedure concerning the preferred failure mode.
- The probability of premature shear failure was shown to be positively correlated to the amount of longitudinal reinforcement and applied impulse. That is, specimens with greater reinforcement amount or subjected to greater impulse are at greater risk of premature shear failure. This suggests that stricter upper limits on the allowable amount of longitudinal reinforcement should be considered for blast-loaded RC elements.

- Disregarding or significantly underestimating the influence of strain-rate effects on the strength of the materials under bending action at the design stage may considerably increase the risk of premature shear failure of blast-loaded RC slabs.
- The probability of achieving ductile bending failure (preferred) in RC one-way slabs subjected to blast loading may be increased by magnifying the design shear force with an overstrength factor. Values of such overstrength factor as a function of the target reliability level were provided. For members without stirrups, the required overstrength factor varies between 1.12 and 1.61 to limit the probability of premature shear failure to less than 10% and 1%, respectively. For members with stirrups, the overstrength factor ranges from 1.00 to 1.57 for the same target probabilities of premature shear failure.

RQ-4: *How are the response and total energy absorption capacity of RC beams influenced by impact loading, including repeated impacts, and different boundary conditions?*

- The energy absorption capacity in beams subjected to combined impact and subsequent static loading was found to exceed the energy absorption capacity of corresponding members subjected to only static loading. This was attributed to additional dissipative mechanisms mobilised during impact loading—such as local fracture in the concrete and damping—and enhanced dynamic material strengths.
- The results suggest that impact loading may, in some cases, induce favourable damage and crack patterns that promote a more favourable uniform distribution of reinforcement strain during post-impact static loading. This behaviour can delay reinforcement fracture and enable greater energy absorption capacity than what would be expected based only on the visible extent of damage.
- Among the beams with *restrained* boundary conditions, the enhancement in energy absorption capacity was more pronounced in specimens subjected to a larger number of low-intensity impacts. Conversely, the tests on *simply supported* beams indicated that, for a given cumulative impact energy, fewer high-intensity impacts could result in greater total energy absorption capacity than more low-intensity impacts. These contrasting observations are believed to be associated with the extent of local damage sustained by the beams. The restrained beams were fitted with a rubber pad at the impact location, which attenuated or delayed local damage. In contrast, the simply supported beams displayed greater accumulation of local damage, likely due to the direct contact between the drop-weight and the concrete surface, as well as the smaller cross-sectional width. Therefore, it appears that greater total energy absorption capacity is possible if local damage can be prevented or delayed.
- The span-to-depth ratio, reinforcement ratio, and reinforcement fracture strain were observed to influence the way arching action and catenary action develop and their overall contribution to the total energy absorption capacity. More slender beams with reinforcement with larger fracture strain were able to mobilise primary catenary action without experiencing prior reinforcement fracture. In such beams, the TMA phase provided the largest contribution to the overall energy absorption capacity. In contrast, beams with lower slenderness or lower fracture strain were prone to a rapid post-peak loss of capacity and early rebar fracture within the CMA phase, which limited the utilisation of the capacity in the TMA phase. In these specimens, the contribution from

the CMA dominated. These findings highlight the need for simplified predictive models capable of accurately describing CMA and capturing early reinforcement fracture.

- Under repeated impacts, the final crack pattern was largely determined by the first impact. However, additional cracking behaviour may arise in elements that have sustained considerable local damage. In addition, the results showed that the crack pattern in restrained beams may change if the element undergoes transition from CMA to TMA under a subsequent impact.
- Among the simply supported beams, the crack pattern during the first strike showed a dependency on the intensity and velocity of the impact. The beams subjected to a weak impact displayed a crack pattern resembling the characteristic pattern associated with a static flexural response. However, nominally identical beams subjected to stronger impacts exhibited major diagonal shear cracks near the impact zone. Likewise, for a constant impact energy, diagonal shear cracking was more pronounced for higher impact velocities.
- No evidence of relevant shear cracking was observed among the restrained beams. This may be partly attributed to the presence of a rubber interlayer and shear reinforcement in the impact zone. During the first impact, the restrained beams subjected to a relatively weak impact remained within the CMA phase and their cracking pattern was consistent with the characteristic pattern associated with flexural response. In contrast, beams subjected to a relatively stronger impact developed TMA during the first strike. The beams that fully developed catenary action showed a crack pattern consisting of vertical cracks extending through the full depth and distributed relatively uniformly along the span. However, the specimens that developed tensile membrane forces but retained a significant contribution from bending capacity displayed an intermediate crack pattern.

RQ-5: *What simplified analytical or numerical methods can be utilised to describe the response—including load-carrying capacity, deflection, reinforcement fracture, and membrane force—of restrained RC beams subjected to static and impact loading?*

- Some empirical relationships were found to provide an acceptable level of accuracy regarding prediction of maximum deflection and residual deflection of impact-loaded RC beams with both simply supported and restrained boundary conditions.
- A simplified FE model and an analytical approach for predicting the static load-deflection curve in restrained RC beams were implemented and validated. Both models were designed to predict midspan deflection, rebar fracture, and membrane force. The FE model showed good agreement with the experimental results, accurately describing the peak CMA resistance and the onset of reinforcement fracture. The analytical approach provided reasonable predictions compared with the experiments. However, it tended to underpredict the peak CMA resistance and, in some cases, delayed the first reinforcement fracture, resulting in overestimation of the TMA capacity. These discrepancies were attributed to the simplified representation of the boundary conditions in the analytical formulation. Nevertheless, the analytical model performed well under idealised linear elastic boundary conditions.
- A 2DOF model for predicting the response of restrained RC beams under single impact was developed. The model accounts for both the local response phase—including contact and impact wave propagation—and the global response phase. The model

demonstrated good predictive capabilities for peak deflection, impact force profile, and dissipated energy. It also provided an acceptable estimate of the membrane force.

- The research identified a relationship between the number of impacts required to trigger failure and the ratio between the dissipated energy per impact and the static energy absorption capacity. Using such an approach, and provided that the static energy absorption capacity is known, it is possible to obtain a reasonable estimate of the allowed number of impacts for a given impact intensity, which in turn can be used to estimate the total energy absorption capacity under impact loading. However, these conclusions need to be verified with extended experimental or numerical research.
- The results suggest that commonly used damage indicators, such as the residual plastic deformation, may not provide a reliable indication of the residual energy absorption capacity following impact loading. Indeed, some specimens exhibiting post-impact permanent deflections comparable to the deflection at collapse under equivalent static loading retained a substantial residual energy absorption capacity.

7.4 Suggestions for future research

Based on the work conducted in this thesis, multiple areas of importance for future work were identified. A key aspect concerns experimental investigation regarding VCEs in open environments populated by vehicles, a topic which is currently underexplored in the literature. It is also of interest to investigate further the dispersion of flammable gases and formation of premixed vapour clouds in such conditions, which could provide better insight regarding equivalent stoichiometric cloud volumes. In addition, publicly available statistical data concerning transport of dangerous goods—particularly flammable and explosive materials—and associated accidents is currently limited. It would therefore be beneficial to address this gap through a comprehensive survey study which could support decision-making when estimating blast loads in connection with accidents during transport.

Furthermore, it is of interest to extend the findings of this study to traffic configurations comprising vehicles of varying sizes and shapes. In addition, although the developed guidelines provide a general strategy for treating different types of gases, additional research is required to further develop and validate this aspect. In particular, investigation of scenarios involving premixed clouds of hydrogen and air would be of significant interest.

Given the prevalence of tunnels and overbuilds in urban environments—through which the transport of dangerous goods may be permitted—it is also important to examine vapour cloud explosions under such confined conditions. The development of comparable engineering methods to facilitate the prediction of blast loads in these environments would represent a valuable contribution.

The recommendations for implementing the MEM were deliberately formulated as an upper-bound approach to ensure conservative estimates. However, when more detailed information about the dispersed vapour cloud is available, it may be advantageous to reduce this conservatism. Thus, a potential direction for future research is the development of strategies to adjust the predicted blast load that integrate results from dispersion analyses with the influence of additional parameters not directly accounted for in the selection of the strength class.

Several relevant areas for future work concerning the effects of uncertainties on the failure mode of blast-loaded RC structures are also recommended. Firstly, the experimental evidence of the effects of uncertainties in the interaction between shear and bending failure in RC

members subjected to impulsive loading remains limited. Addressing this gap would be highly beneficial, as it would enable more controlled and systematic studies of this complex interaction, ultimately leading to improved modelling approaches and safer design provisions. Secondly, the present study relied on simplified analytical models to estimate both the resistance and the dynamic response of RC slabs. While this approach enabled extensive parametric analyses, it necessarily involved simplifications of the underlying physical behaviour. Future studies would therefore benefit from employing advanced non-linear FE models, which can more accurately capture the dynamic response. Finally, the conclusions in this work are intrinsically connected to the shear resistance models in Eurocode 2. Thus, it would be of interest to implement a similar probabilistic framework to generalise the conclusions to alternative resistance models, such as those in the upcoming second generation of Eurocode 2.

Future research can also build upon the present work in several important directions aimed at improving the understanding of the response of RC structures to impulsive loading. A key research need is to better quantify the level of restraint required to (partially) mobilise membrane action in RC elements under static and dynamic loading. In parallel, it would be valuable to characterise typical restraint conditions for common structural systems, considering variables such as geometry, reinforcement ratio, and reinforcement layouts. Such knowledge would provide a more rational basis for restraint assumptions during analysis and design.

Furthermore, the experimental programme in the study concerning restrained boundary conditions was limited to RC members with symmetrical, continuous reinforcement subjected to low-velocity impacts. In addition, the number of tested specimens was relatively small. Thus, further experimental investigations are needed to validate and generalise the findings. In particular, it would be valuable to expand the experimental scope to include different reinforcement layouts and loading scenarios (e.g. multiple point loads or non-symmetrical point loading), as well as size effect, which would strengthen the applicability of the findings. Similarly, the analytical and numerical models developed within this work have only been validated for beams with symmetrical and continuous reinforcement. Extending these models to account for alternative reinforcement arrangements, including discontinuous or asymmetric layouts, would enhance their robustness and practical utility. Another promising area for research is the investigation of the hysteretic behaviour of restrained RC beams under static and impact loading. Such studies would enable a more comprehensive understanding of the full cycle of the dynamic response.

Advanced numerical modelling also offers significant potential to increase the understanding of the response of restrained RC members. The development of advanced non-linear FE models, calibrated against the experimental data generated in this thesis, could enable reliable extrapolation to scenarios not directly tested. These may include impacts at different locations, higher-velocity events, or possibly blast loads. Such models could serve as a powerful tool for parametric studies and development of simplified engineering approaches.

Finally, future work should aim to improve the practical applicability of the developed models by incorporating relevant design factors. In addition, it would be beneficial to extend the findings from isolated elements to complete structural systems, such as frames, which would provide a more realistic representation for typical engineering applications.

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Appendix A: Influence of the degree of restraint on the static response of RC elements

A numerical FE model was developed to predict the load-deflection response of restrained RC beams under static three-point bending, as described in Section 6.3.2. The FE model enables evaluation of the influence of the restraint at the boundaries on the load-deflection curve and the energy absorption capacity. In this appendix, the effects of both translational and rotational restraints are investigated.

The study adopted a reference case with the same geometry and reinforcement arrangement as specimen IIIB-04 from **Paper VI**. The reference boundary conditions were defined as linear elastic with a translational stiffness $K_{T,ref} = 1 \times 10^8$ N/m and a rotational stiffness $K_{R,ref} = 1 \times 10^7$ Nm/rad. These values represent the characteristic restraint levels associated with the experimental setup, as described in **Paper V**. Two one-factor-at-a-time analyses were conducted, in which the translational or rotational stiffness was varied while keeping the other stiffness constant at its reference value. In each analysis, the stiffness under consideration was initially increased and decreased by a factor of 10 relative to its reference value.

Figure A.1(a) shows the influence of the translational stiffness on the load-deflection curve. For comparison, the results for a case with horizontally guided BCs (i.e. restrained rotation and free horizontal translation) are included. Evidently, the peak resistance in the CMA phase is significantly influenced by the longitudinal restraint, with greater restraint resulting in greater peak CMA resistance. Moreover, increasing the longitudinal stiffness reduced the deflection at peak load and promoted earlier transition to catenary action. Conversely, when the restraint stiffness was low, catenary action could not be mobilised effectively, leading to smaller membrane forces. Reinforcement fracture also appeared to occur earlier at lower restraint levels. This may be related to a greater rotation for the same deflection caused by the inwards movement at the ends of the beam. Similar conclusions were reported by Yu and Tan [123].

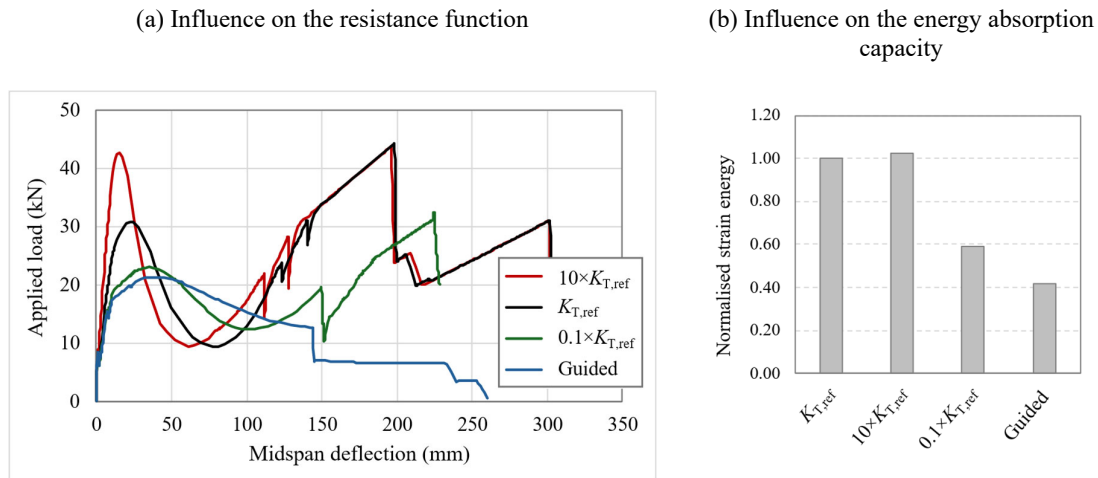


Figure A.1. Effect of varying the translational stiffness on the response of a reference specimen identical to IIIB-04. Reference translational stiffness: $K_{T,ref} = 1 \times 10^8$ N/m.

Figure A.1(b) presents the available internal strain energy as a function of the translational restraint. The strain energy was normalised with respect to the reference case. For the two cases with the greatest stiffness, despite the clear differences in the load-deflection curve within the

CAM phase, only a minor influence was obtained in the energy absorption capacity. That is, increasing the restraint level from the reference case—while keeping the rotational stiffness constant—had not significant benefit. However, the available strain energy in the case with a translational stiffness 10 times lower than the reference value was approximately 60% of the reference case, caused primarily by the less effective utilisation of the catenary action. Finally, the case with guided BCs could only mobilise approximately 40% of the reference energy absorption capacity.

The influence of the rotational restraint level is illustrated in Figure A.2. For comparison, the figure also includes results for a case with pinned BCs (i.e. free rotation and restrained translation). In contrast to the evaluation of longitudinal stiffness, varying the reference rotational restraint by a factor of 10 in either direction produced no meaningful change in the structural response nor in the energy absorption capacity. For that reason, more cases were examined. Lower rotational stiffness values led to a simultaneous decrease in the peak CMA resistance and a noticeable flattening of the response curve, with the snap-through phase being considerably attenuated. For the cases with higher restraint levels, the onset of catenary action remained largely unaffected despite reductions in rotational stiffness. Additionally, lower rotational stiffness values resulted in earlier reinforcement fracture. Finally, the internal strain energy decreased with decreasing rotational restraint level; however, a substantial reduction in restraint level with respect to the reference case is required for this effect to become pronounced.

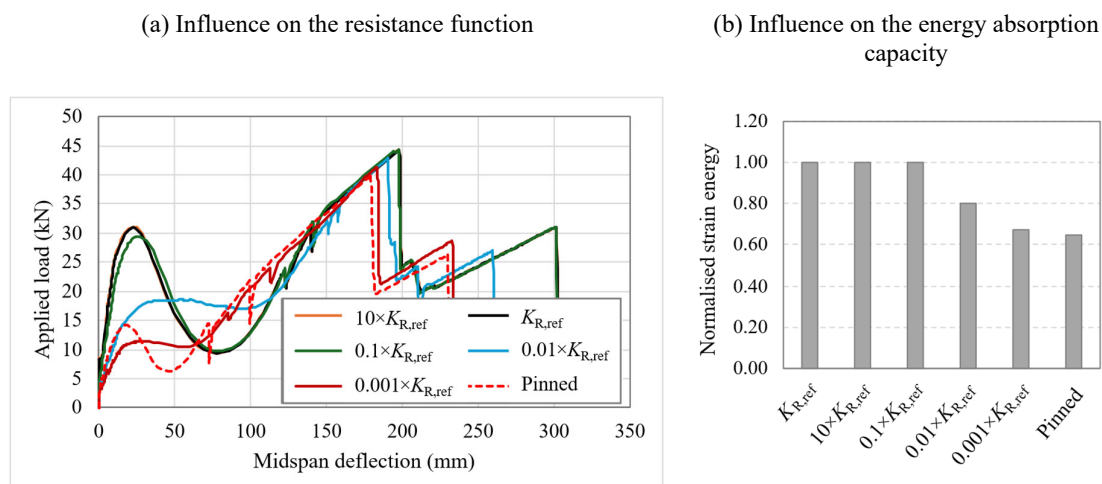


Figure A.2. Effect of varying the rotational stiffness on the response of a reference specimen identical to IIIB-04. Reference rotational stiffness: $K_{R,ref} = 1 \times 10^7$ Nm/rad.

Taken together, these results demonstrate that restrained boundary conditions can significantly enhance the resistance and energy absorption capacity of RC beams. The influence of the longitudinal stiffness was found to be more pronounced than that of the rotational restraint. Furthermore, the results show that full fixation is not required to utilise this additional capacity. This finding highlights the potential for exploiting this reserve capacity in partially restrained elements within structural systems.

Appended Papers I-VI

(NOTE: The papers are not included in the online version of the thesis)

