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Crack growth monitoring using shear guided waves

by

Chien An Chua

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Department of Mechanical Engineering
Imperial College London
London SW7 2AZ

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Declaration of Originality

The content of this thesis is my own research work, which was completed under the supervision of Professor Peter Cawley. Appropriate references have been provided wherever the work of others has been used.

Chien An Chua

6th November 2019

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Abstract

Crack monitoring in critical sections of steel structures is a subject of growing interest. High frequency ultrasonic techniques have good detection sensitivities but poor inspection coverage per transducer that makes it impractical to monitor large areas. Corrosion detection and monitoring of pipelines can be performed by using low frequency guided waves, but are insufficiently sensitive for the detection of small cracks. This study evaluates the crack monitoring performance of a fundamental shear horizontal mode (SH0) system at frequencies just below the high-order mode cut-off. The scattering solutions valid at low frequencies were developed for both 2D and 3D cracks; most importantly the 3D solution showed that the SH0 reflection ratio is proportional to frequency to the power 1.5, to the effective crack size cubed, and is inversely proportional to the plate thickness and to the square root of the distance from the crack to the receiving sensor.

Finite element (FE) analysis was used to validate these power coefficients and to calculate the proportionality constant. The predicted 3D solution was validated using experimental data generated by a ring of transducers on a 6-inch diameter pipe with a progressively grown notch that simulates crack growth; baseline subtraction with temperature compensation was applied to compensate for changes in the environmental and operational conditions. It was found that the residual signals after baseline subtraction can be assumed to be normally distributed so the random fluctuations could be reduced by coherent averaging; it was thereby possible to reliably detect a 2 mm wide and 1 mm deep notch simulating a crack located one pipe diameter along the pipe from the transducer ring. The damage detection performance at different locations along the pipe was assessed by analysing receiver operating characteristic (ROC) curves generated by adding simulated defects to multiple experimental measurements without damage. At a fixed standoff distance, the damage detection performance increases with the square root of the number of

averaged signals, and is also improved by averaging the signals received by transducers covering the main lobe of the reflection from the defect; the common source method can be applied to reduce the effects of phase cancellation when using signals from multiple receivers. When the defect is located more than about one pipe circumference from the transducer ring, the optimal performance is obtained by averaging across all the transducers in the ring, corresponding to monitoring the $T(0,1)$ pipe mode.

The effects of temperature cycling and the presence of a large reflector, in this case a weld, near a simulated crack were investigated experimentally by data generated from electromagnetic acoustic transducers (EMATs) on a 6-inch diameter pipe with a weld. The results show that the measured reflection ratios after temperature compensation can still be assumed to be normally distributed, similar to the findings from the previous experiment, but there were uncompensated changes which result in larger variations towards the second half of the temperature cycling experiment. A notch was progressively grown near the weld of this same pipe and it was shown that a 1 mm wide and 0.5 mm deep notch located one pipe diameter along the pipe from the EMAT receiver can be detected; a 95% probability of detection with a 1% false alarm rate can be achieved after a sufficiently large number of measurements. In conclusion, an SH0 mode monitoring system with a short transducer standoff distance from the inspected area has good potential for crack monitoring applications, for example for girth welds in pipes, given that transducers designed for stable and long term use are utilised.

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Chapter 1

Introduction

1.1 Motivation

Pipelines and pressure vessels are examples of steel, plate-like structures where cracks can possibly be found. These cracks can grow over time and cause engineering components to fail, despite operating well below the allowable stress limits [7]. In the case of fatigue-induced cracks, the crack size and stress field data collected from one-off measurements can be used to estimate its growth rate and remnant life [8]. However, there are large uncertainties in these predictions due to the growth rate being typically proportional to the third or fourth power of stress and the square of its size [9].

Structural health monitoring (SHM) can reduce this uncertainty because frequent measurements of the crack size would improve accuracy of growth rate prediction, thus leading to a more accurate estimation of the remnant life. For example, the failure forecasting method was shown to be able to provide a more accurate prediction of the remnant life in comparison with estimates from the conventional method using crack length measurements and the Paris Law [10]. Non-destructive testing (NDT) techniques such as potential drop [11, 12], phased array ultrasonics [13, 14], time of flight diffraction [15, 16] and Rayleigh surface waves [17, 18] are existing methods which can be used to size cracks accurately during one-off inspection. Recently, SHM is gaining an increase in popularity as a replacement for traditional NDT methods because it can be carried out during plant operation, avoids the repeated access costs to difficult-to-access areas [19, 20] and

1. Introduction

has the ability to track the progression of the defect growth [21]. High frequency ultrasonic transducers are typically scanned over the surface of a structure in order to achieve the required inspection volume coverage; the volume covered at each transducer location being of the order of the volume of material below the transducer. This is a problem if the likely defect location is not known precisely. For example, a nominal 6-inch diameter pipe with a length of 6 metres has an outer surface area of about 3,166,725 mm². In a hypothetical scenario where full inspection coverage of this pipe is required, a minimum of 10,085 inspection locations using a 20 mm diameter ultrasonic thickness (UT) probe would be required. Hence, the manual spot ultrasonic thickness gauging technique is not practical for the rapid inspection of large areas of a structure [22].

Low frequency guided waves are currently used in pipeline inspection for corrosion in the petrochemical industry [23–25] because of relatively good detection sensitivities and high inspection coverage per transducer [26–29]. Generally, guided wave tests are carried out using a single, fundamental mode in a pulse-echo configuration [30], where typically, the fundamental torsional or T(0,1) wave pulse is induced into the pipe via transducers attached to the pipe outer wall, as shown in Fig. 1.1. The T(0,1) incident wave propa-



Figure 1.1: Guided wave screening of an insulated pipeline [1].

ates along the pipe and interacts with both defects and other features, such as welds,

which leads to scattering. With reference to the previous example of the inspection of a 6 inch diameter pipe with a length of 6 metres, a single inspection location using a ring of guided wave transducers can be used to achieve full inspection coverage. In a pulse-echo configuration, the backscattered waves are measured by the transducers, where conventionally, the signals from the different transducers in the ring are summed up to measure the reflected $T(0,1)$ mode from the defect; the reflection amplitude ratio R provides an estimate of the defect severity. Defect sizes in guided wave testing are typically quantified by the loss or change in cross-sectional area (CSA) of the pipe, because the transmitted $T(0,1)$ guided wave covers the entire pipe circumference.

Although most guided wave inspections are carried out using a single mode in a pulse-echo configuration [31], there is an infinite number of different modes that can exist within a plate-like structure. The number of guided wave modes within the structure can be limited if the selected frequency range of operation is below the cut-off frequencies of higher order modes [32]. For example in plates, there are only three fundamental guided wave modes that exist below the first-order modes, namely the symmetric (S_0), antisymmetric (A_0) and shear horizontal (SH_0). Dispersion is a phenomenon that is usually undesirable in an inspection system because it leads to amplitude reduction and increase of the signal duration [32]. Among the three aforementioned plate guided wave modes, the SH_0 guided wave is the only mode which is non-dispersive across all frequencies, leading to its use in several industrial applications such as for the quality control inspection of laser butt-welds in automobile part welding lines [33]. Increased detection sensitivity can be achieved by using higher order SH modes at higher inspection frequencies. For example, higher order SH modes can be applied to detect stress corrosion cracking defects in pipelines [34] and to detect corrosion and evaluate remaining wall thickness in polymer coated pipes [35]. SH_1 waves have also been shown to potentially be useful for the detection of gradual thinning defects under pipe supports [36]. Despite the fact that higher order SH modes offer advantages of increased detection sensitivity, it is still practically easier to operate below the higher order mode cut-off and deal with only the fundamental SH_0 mode due to the dispersive nature of higher order modes. It is also worth noting that the incident $T(0,1)$ mode used in guided wave inspection is essentially a plane SH_0 wave, as illustrated in Fig. 1.2.

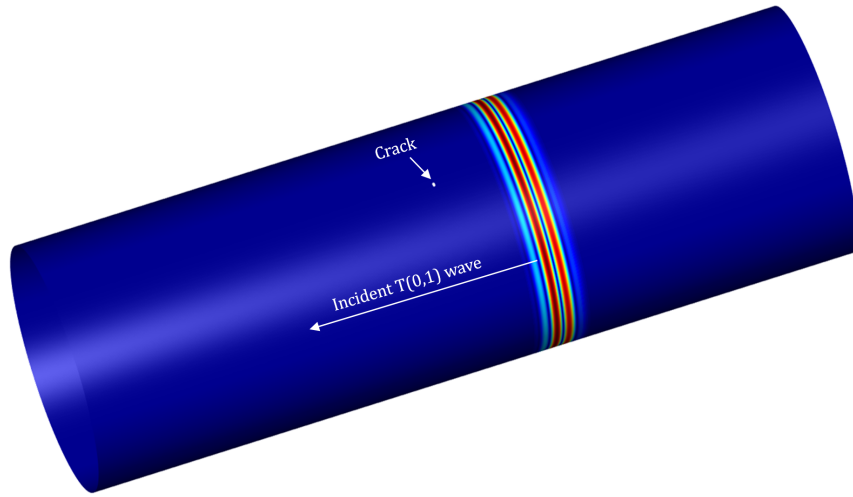


Figure 1.2: Snapshot of numerically simulated wave propagation in a pipe which shows the incident $T(0,1)$ wave.

One-off $T(0,1)$ guided wave inspections have been shown to reliably detect defects of the order of 5% CSA [37], which supports its use for the detection of relatively large corrosion patches. More recently, guided wave sensors employed in a monitoring fashion have been shown to improve sensitivity to approximately 1% CSA, allowing for the reliable detection of relatively smaller corrosion patches [38, 39] or relatively large crack-like defects [40]. However, small but critical cracks may represent $\ll 1\%$ CSA loss [41], meaning that existing commercially-available low frequency guided wave monitoring systems cannot be used for crack monitoring. Furthermore, the scattering from circumferential cracks tends to be smaller when its axial extent or crack width is negligible [42]. The issue with low detection sensitivity could be addressed by exploring the use of relatively higher frequency SH_0 guided waves at the expense of inspection coverage. Figure 1.3 shows the numerically simulated SH_0 scattering pattern from a surface-breaking crack in an unrolled pipe upon interaction with the $T(0,1)$ wave. As the backscattered SH_0 waves propagate away from the crack, the energy of the waves will continue to distribute spatially and thereby reducing the measurable amplitude of the waves. This means that deploying the transducers relatively closer to the monitored area of interest could increase the reflection ratio by capturing a higher measurable amplitude of the backscattered SH_0 wave.

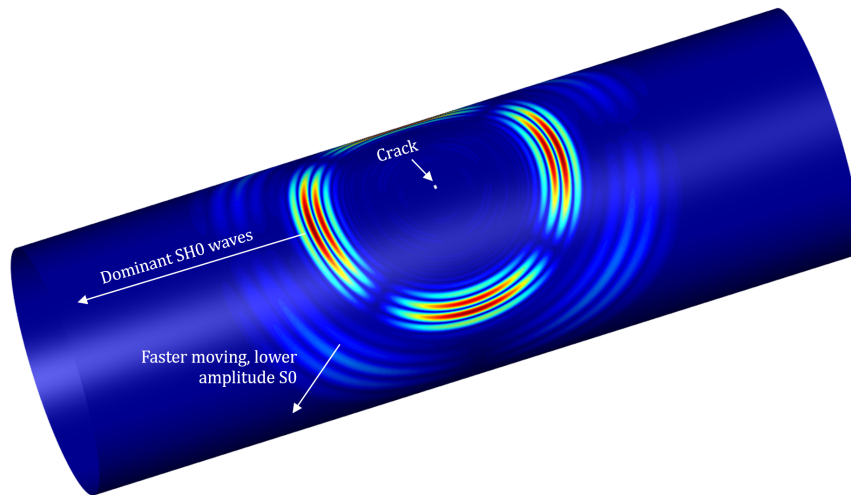


Figure 1.3: Snapshot of numerically simulated wave propagation in a pipe which shows the scattered field from a surface crack as a result of interaction with an incident $T(0,1)$ wave.

1.2 Aim

Overall, the main aim of the thesis is to investigate the feasibility of a short-range but relatively high frequency SH_0 guided wave system for applications in crack monitoring in plate-like structures. The SH_0 is the guided wave mode of choice selected for this study because it is non-dispersive, possesses a simple mode shape and is sensitive to the detection of cracks. Initially, the scattering behaviour of SH_0 from cracks needs to be investigated in order to understand how the measured reflection amplitude is affected by the crack, material and inspection wave properties. It is also of practical interest to understand the influence of the number of measurement samples, the number of receiving transducers and distance of the inspection zone from the transducer location on the monitoring performance of the system. This information could provide insight into understanding the strength and limitations of using an SH_0 guided wave system for possibly monitoring whole areas of structure, such as a full girth weld in a pipe.

1.3 Thesis outline

Chapter 2 reviews the basic physics of guided waves and the use of FE analysis to predict the scattering of shear guided waves from surface-breaking cracks of different sizes.

Chapter 3 begins with a literature review of the existing solutions on ultrasonic scattering over the past few decades. Then, the low frequency solution to the scattering of shear guided waves from both 2D and 3D cracks are derived and validated numerically. Chapter 4 describes the experiment and performance of a piezoelectric-based SH0 monitoring system for crack growth monitoring. Then, the framework used to generate synthetic data and evaluate monitoring performance across a wide range of parameters is outlined. Chapter 5 describes the experiment and performance of an EMAT-based SH0 monitoring system when the damage-free structure undergoes temperature cycling and when the crack is located near a large reflector. Chapter 6 summarises the key findings and contribution of the thesis and discusses the potential for future work.

Chapter 2

Theoretical Background

2.1 Introduction

Bulk waves are waves which propagate in infinite structures and are dependent only on the material properties. Shear and longitudinal waves are two types of bulk waves which can propagate non-dispersively in a homogeneous medium, since their velocities do not depend on the wave frequency. The interaction of bulk waves with the boundaries of the material leads to the development of guided waves, which can propagate at the boundaries or between the boundaries. Rayleigh surface waves [43] and Lamb waves [44] are examples of types of guided waves which can exist at the boundary and in between the boundaries respectively. Unlike bulk waves, guided waves are dependent on both the material properties and material boundaries. The wave propagation equations and physical boundary conditions need to be satisfied in order to obtain the solution to any guided wave problem. This theory has been covered extensively in widely available literature [45–47] and will be summarised briefly here to define notations and present cases related to this work. Specifically, this chapter presents the theory of shear horizontal guided wave propagation in plates and pipes, as well as background information on the finite element (FE) analysis and FE models used in this study to capture the scattering of guided waves from cracks.

2.2 Bulk waves

The general equation of motion which relates the particle displacement $\mathbf{u}$ in a material of density ρ to the stress field tensor $\boldsymbol{\sigma}$ in a material as a function of time t is given as

$$\rho \frac{\partial^2 \mathbf{u}}{\partial t^2} = \nabla \cdot \boldsymbol{\sigma}, \quad (2.1)$$

where ∇ is the vector differential operator ($\nabla = \hat{\mathbf{x}} \frac{\partial}{\partial x} + \hat{\mathbf{y}} \frac{\partial}{\partial y} + \hat{\mathbf{z}} \frac{\partial}{\partial z}$). Hooke's law is used to relate $\boldsymbol{\sigma}$ and strain $\boldsymbol{\epsilon}$ in an isotropic material

$$\boldsymbol{\sigma} = \mathbf{C} \cdot \boldsymbol{\epsilon}, \quad (2.2)$$

where $\mathbf{C}$ is the stiffness tensor. Theory of elasticity states that it is possible to reduce the components of the elastic stiffness tensor to two material or Lamé constants (λ, μ) in an isotropic, homogeneous and linear elastic material. By expressing the stress tensor in terms of displacements, Hooke's law equation is transformed into

$$\boldsymbol{\sigma} = \lambda \mathbf{I} \nabla \cdot \mathbf{u} + \mu (\nabla \cdot \mathbf{u} + \mathbf{u} \nabla^T), \quad (2.3)$$

where $\mathbf{I}$ is the identity matrix. The Lamé constants are related to the modulus of elasticity E and Poisson's ratio ν as given in

$$E = \frac{\mu(3\lambda + 2\mu)}{\lambda + \mu} \quad (2.4)$$

and

$$\nu = \frac{\lambda}{2(\lambda + \mu)}. \quad (2.5)$$

Substitution of Eq. (2.3) into Eq. (2.1) gives the Navier's differential equation of motion for an isotropic elastic medium, which is mathematically expressed as

$$\mu \nabla^2 \mathbf{u} + (\lambda + \mu) \nabla \nabla \cdot \mathbf{u} = \rho \frac{\partial^2 \mathbf{u}}{\partial t^2}, \quad (2.6)$$

where ∇^2 is the scalar differential operator ($\frac{\partial^2}{\partial x^2} + \frac{\partial^2}{\partial y^2} + \frac{\partial^2}{\partial z^2}$). The expansion of this vector equation into its three spatial components gives a set of scalar linear equations. Hence, the superposition of two or more valid solutions will still be a solution. In addition, an appropriate solution must be assumed since the equation cannot be integrated directly. Using the Helmholtz decomposition, the displacement $\mathbf{u}$ can be written as

$$\mathbf{u} = \nabla\phi + \nabla \times \boldsymbol{\psi}, \quad (2.7)$$

where ϕ is a compressional scalar potential and $\boldsymbol{\psi}$ is the shear vector potential. Since $\nabla\phi$ is an irrotational component, this means that $\nabla \times \boldsymbol{\psi}$ is a rotational component where $\nabla\phi = \mathbf{0}$, which can be referred to as the gauge condition [48]. The substitution of this expression for $\mathbf{u}(\mathbf{r}, t)$ into Eq. (2.6) yields

$$\nabla \left[(\lambda + 2\mu) \nabla^2 \phi - \rho \frac{\partial^2 \phi}{\partial t^2} \right] + \nabla \times \left[\mu \nabla^2 \boldsymbol{\psi} - \rho \frac{\partial^2 \boldsymbol{\psi}}{\partial t^2} \right] = \mathbf{0} \quad (2.8)$$

which is satisfied if both terms in the square brackets are equal to zero. This leads to the Helmholtz differential equations, which are described by

$$\frac{\partial^2 \phi}{\partial t^2} = c_L^2 \nabla^2 \phi \quad (2.9)$$

and

$$\frac{\partial^2 \boldsymbol{\psi}}{\partial t^2} = c_S^2 \nabla^2 \boldsymbol{\psi}, \quad (2.10)$$

where the former equation governs the longitudinal waves with phase velocity c_L , whilst the latter governs the transverse or shear waves with phase velocity c_S . Both phase velocities of the longitudinal and shear waves are given by

$$c_L = \left(\frac{\lambda + 2\mu}{\rho} \right)^{\frac{1}{2}} \quad (2.11)$$

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and

$$c_S = \left(\frac{\mu}{\rho}\right)^{\frac{1}{2}}. \quad (2.12)$$

As previously mentioned, there are only two type of waves which can propagate in an unbounded isotropic medium without interaction since Eq. (2.9) and Eq. (2.10) are not dependent on each other. The general solutions for the propagation of the two waves in the z -direction are described by

$$\phi = \phi_0 e^{i(k_L z - \omega t)} \quad (2.13)$$

and

$$\psi = \psi_0 e^{i(k_S z - \omega t)}, \quad (2.14)$$

where z is the spatial coordinate of the wave, $i = \sqrt{-1}$, ϕ_0 and ψ_0 are arbitrary initial constants and k_S and k_L are the shear and longitudinal wavenumbers respectively, as given by

$$k_{L,S} = \frac{\omega}{c_{L,S}}, \quad (2.15)$$

where ω is the angular frequency. The existence of only two solutions indicate that these two types of waves, often referred to as bulk waves, are the only existing elastic waves in an unbounded, isotropic, elastic, three-dimensional, solid medium. The particle motion of longitudinal and shear waves are parallel and perpendicular to the direction of wave propagation respectively [49], where the velocity ratio of longitudinal and transverse waves is dependent on the Poisson's ratio of the material [50]. The transverse motion of the shear waves can occur both horizontally and vertically, which are referred to as the shear horizontal and shear vertical waves [51].

2.3 Guided waves

2.3.1 Plates

For the case of the unbounded stress-free plate, two common methods of solution can be used, which are the potential method and the method of superposition of partial waves [52]. The geometry of an isotropic plate is shown in Fig. 2.1, where T is the plate

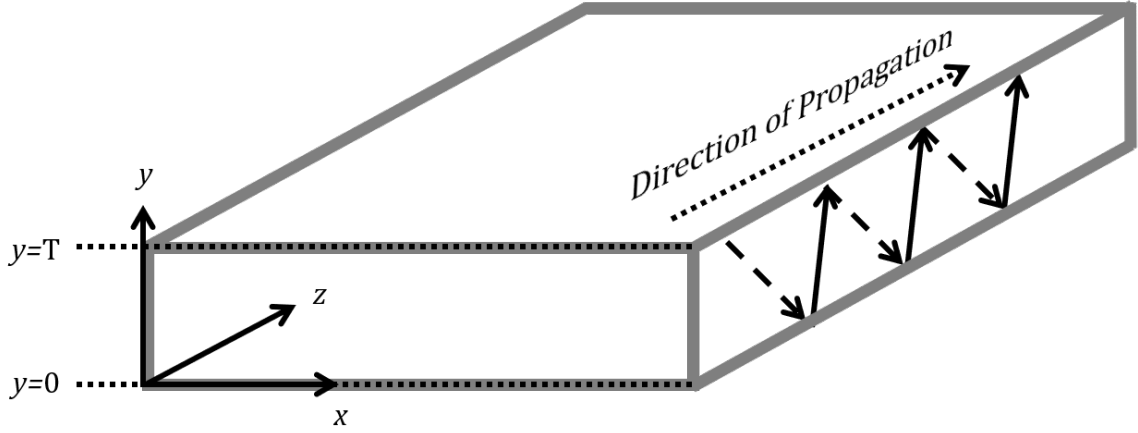


Figure 2.1: Diagram of an isotropic plate with internal reflecting waves shown.

thickness, z is the wave propagation direction and the planes at $y = 0$ and $y = T$ are the traction free boundary surfaces. Using the potential method, Eq. (2.9) and Eq. (2.10) can be solved with ϕ and ψ as unknown variables [46]. For the plane wave propagating in the z -direction, it can be assumed that the particle displacement in the x -direction is zero (i.e. $u_x = 0$), which leads to $\frac{\partial}{\partial x} = 0$ and only x -axis rotations are allowed (i.e. $\psi_z = \psi_y = 0$). Ultimately, the Helmholtz differential equations can be simplified to

$$\frac{\partial^2 \phi}{\partial t^2} = c_L^2 \left(\frac{\partial^2 \phi}{\partial z^2} + \frac{\partial^2 \phi}{\partial y^2} \right) \quad (2.16)$$

and

$$\frac{\partial^2 \psi}{\partial t^2} = c_S^2 \left(\frac{\partial^2 \psi}{\partial z^2} + \frac{\partial^2 \psi}{\partial y^2} \right), \quad (2.17)$$

where the solutions to these equations are known as Lamb waves. If the displacement component in the z and y directions are assumed to be zero (i.e. $u_z = u_y = 0$) and

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considering the solution for which $\phi = 0$, then Eq. (2.9) and Eq. (2.10) can be reduced to

$$\frac{\partial^2 \psi}{\partial t^2} = c_s \left(\frac{\partial^2 \psi}{\partial x^2} \right), \quad (2.18)$$

where the solution to these equations are referred to as the shear horizontal (SH) waves. The motion of particles in longitudinal waves are entirely in the propagation direction and the wave motion consists of dilation or volume changes only. The vibration of the SH waves is perpendicular to the wave propagation direction and the motion consists of shear in the medium with no volume change. Lamb waves are the combination of longitudinal waves (P) and vertically polarised shear waves (SV). The general solution of the bulk wave propagation problem, which is shown in Eq. (2.14), is applied to SH waves travelling in a plate. Hence, a solution can be obtained for Eq. (2.18) and described by

$$\psi = \psi_0(y) e^{i(k_z z - \omega t)}, \quad (2.19)$$

where k_z is the wavenumber in the direction of propagation $\hat{z}$ and ψ_0 is the displacement amplitude given by

$$\psi_0(y) = A \sin(k_y y) + B \cos(k_y y), \quad (2.20)$$

which describes the unknown vector potential in terms of through-thickness sinusoids, where k_y is the through-thickness wavenumber, A and B are arbitrary constants. In this case, the boundary conditions are $\tau_{yy} = \tau_{xy} = \tau_{zy} = 0$ and $\partial \psi_x / \partial y = 0$ at $y=T$ and $y=0$. Applying these conditions to Eq. 2.19 yields

$$\begin{aligned} A \cos(k_y T/2) - B \sin(k_y T/2) &= 0, \\ A \cos(k_y T/2) + B \sin(k_y T/2) &= 0, \end{aligned} \quad (2.21)$$

which results in the frequency equation

$$\cos(k_y T/2) \sin(k_y T/2) = 0. \quad (2.22)$$

This equation is satisfied by

$$k_y = \frac{\pi l}{T} \quad (l = 0, 1, 2, 3, 4, \dots), \quad (2.23)$$

where l is an integer that can assume values from zero to infinity. The bulk shear wavenumber, k_s , is related to k_y as described by

$$k_y^2 = k_s^2 + k_x^2 \quad (2.24)$$

and

$$k_s = \frac{\omega}{c_s}, \quad (2.25)$$

The results in Eq. 2.23 show that harmonic SH waves may propagate only under special conditions and by substituting Eq. (2.23) into Eq. (2.25), the dispersion relation can be obtained as

$$k_x^2 = \left(\frac{\omega}{c_x}\right)^2 = \left(\frac{\pi l}{t}\right)^2 - \left(\frac{\omega}{c_s}\right)^2. \quad (2.26)$$

By knowing that $k_x = \frac{\omega}{c_p}$, Eq. (2.26) can be expressed in terms of phase velocity c_p and frequency-thickness product, which yields

$$c_p(fT) = \pm 2c_s \left(\frac{fT}{\sqrt{4(fT)^2 - l^2 c_s^2}} \right). \quad (2.27)$$

Fig. 2.2 shows the first six SH modes in the frequency-thickness range from 0 to 10 MHz-mm on a plot of phase velocity and the frequency-thickness product for a steel plate, which can be obtained by numerically solving Eq. (2.27) and varying the order of l . These modes can be either symmetric or anti-symmetric and are represented by the odd or even counter respectively. For $l = 0$, the solution is the zeroth-order or fundamental SH symmetric mode (SH0), which is the only mode whose velocity is frequency independent (non-dispersive) and $c_p = c_s$. As the frequency-thickness product approaches infinity, the phase velocity for the other modes ($i > 0$) are frequency independent and their c_p value

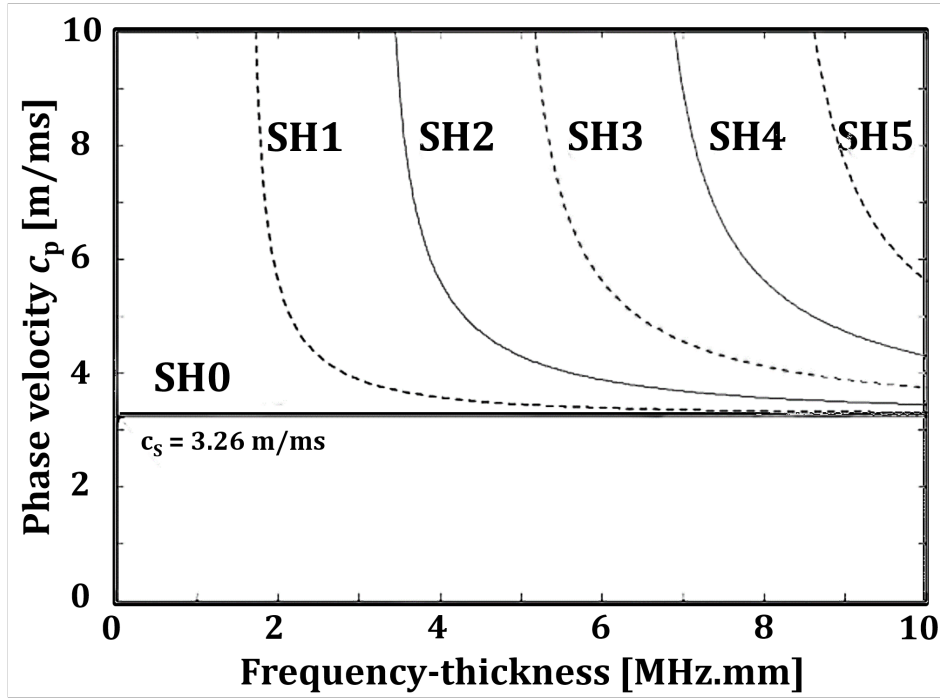


Figure 2.2: Phase velocity dispersion curves for the SH modes in a steel plate generated using Disperse [2], where the phase velocities of symmetric modes (solid lines) and anti-symmetric modes (dashed lines) as a function of frequency-thickness product are shown.

converges to c_S . As expressed by Eq. (2.27), all the SH modes exist at all frequencies but can only propagate at frequencies higher than their cut-off frequency, which can be calculated for the zero of the denominator of Eq. (2.27). The mode is said to have a non-propagating solution if the propagation is below the cut-off frequency and the cut-off frequency-thickness product for the i^{th} mode, which can be expressed as

$$fT_L = \frac{ic_L}{2}. \quad (2.28)$$

Auld describes this phenomena more vividly using partial wave theory and described the phenomena as waves which are reflected at the boundaries of the plate [52] as illustrated in Fig. 2.3. Therefore, the solution to this free-plate problem is acquired by the superposition of the downwards and upwards waves, where Snell's law governs the reflection of waves at the boundaries. At the cut-off frequency, the angle of incidence is zero and therefore the wave reflects back and forth across the thickness of the waveguide and does not cause any changes in the stress and displacement field along the direction of propagation. The

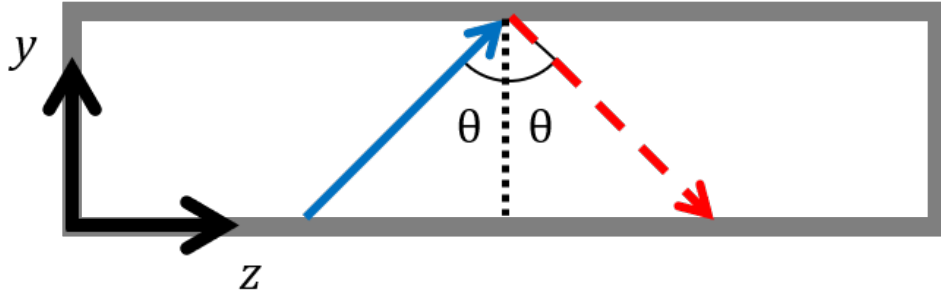


Figure 2.3: Schematic of the reflection of partial waves at the plate boundaries which is governed by Snell's law.

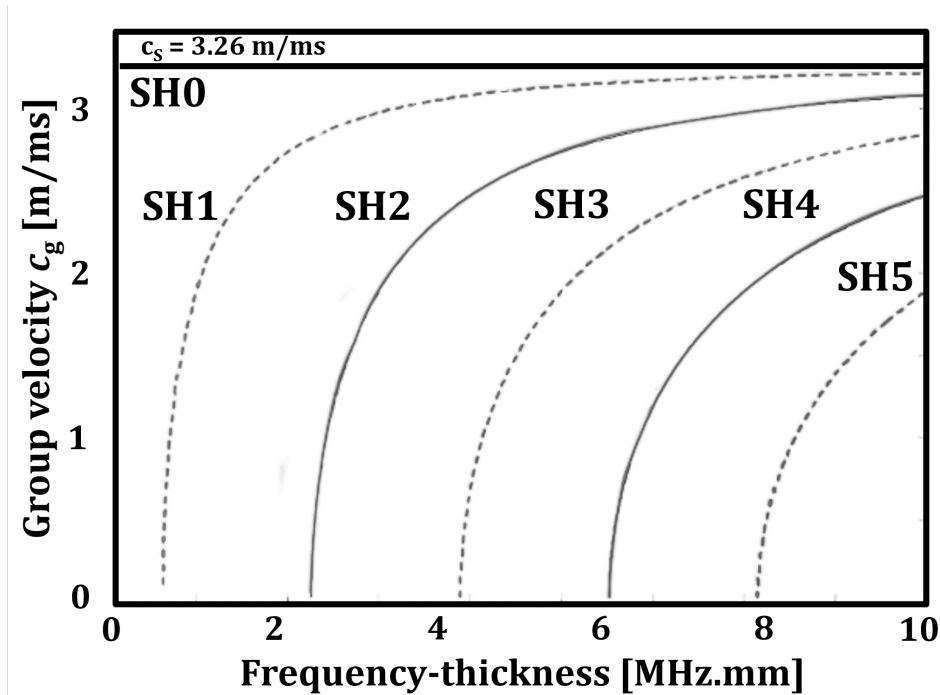


Figure 2.4: Group velocity dispersion curves for the SH modes in a steel plate generated using Disperse, where the group velocities of symmetric modes (solid lines) and anti-symmetric modes (dashed lines) as a function of frequency-thickness product are shown [2].

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group velocity c_g can be described by

$$c_g = \frac{\partial \omega}{\partial k_z}, \quad (2.29)$$

which represents the velocity at which the wave packet which contains a range of frequency component is propagating.

2.3.2 Pipes

There has been a significant amount of research conducted in the field of guided wave propagation in pipes or hollow cylinders. In the 1800s, Pochhammer [53] and Chree [54] developed the solutions to a set of governing equations, which can be found in more recent textbooks [45, 52]. A general analytical solution for the propagation of guided waves in an infinitely long 3D pipe was developed by Gazis [55], which was later validated empirically [56]. Fig. 2.5 describes the pipe geometry and the cylindrical coordinate system that has been introduced for convenience. Similar to the plate case, Gazi's solution

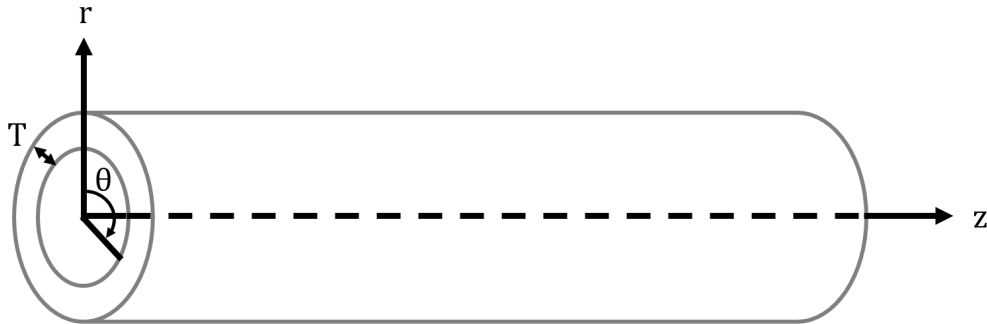


Figure 2.5: Schematic representation of pipe geometry in cylindrical coordinates (r, θ, z) .

for a pipe begins with the Navier differential equation of motion (Eq. (2.6)) and leads to the Helmholtz differential equations (Eq. (2.11 and 2.12)), but the solutions to the wave

equations are expressed as

$$\begin{aligned}
\phi &= f(r) \cos(n\theta) \cos(\omega t + k_z z), \\
\psi_r &= g_r(r) \sin(n\theta) \sin(\omega t + k_z z), \\
\psi_\theta &= g_\theta(r) \cos(n\theta) \sin(\omega t + k_z z), \\
\psi_z &= g_z(r) \sin(n\theta) \cos(\omega t + k_z z),
\end{aligned} \tag{2.30}$$

where k_z is the axial wavenumber and n is the circumferential order. The complete solution for the unknown functions of f and g can be found in [55]. By applying the cylindrical coordinate system to Eq. (2.7), the displacement at an arbitrary location in the medium can be described by

$$\begin{aligned}
u_r &= \frac{\partial \phi}{\partial r} + \frac{1}{r} \frac{\partial \psi_z}{\partial \theta} - \frac{\partial \psi_\theta}{\partial z}, \\
u_\theta &= \frac{1}{r} \frac{\partial \phi}{\partial \theta} + \frac{\partial \psi_r}{\partial z} - \frac{\partial \psi_z}{\partial r}, \\
u_z &= \frac{\partial \phi}{\partial z} + \frac{1}{r} \frac{\partial}{\partial r}(r \psi_\theta) - \frac{1}{r} \frac{\partial \psi_r}{\partial \theta}.
\end{aligned} \tag{2.31}$$

The stress tensor equations as a function of displacement field can be derived by using Hooke's equation and strain-displacement equations; the full mathematical procedure and solution can be found in [57]. The mode solutions to pipe guided wave propagation can be identified by using a dual index system (n, m) , where n denotes the circumferential order and m the consecutive order [58]. The complexity of the modes around the pipe circumference increases with the value of n , whilst the vibrational complexity in the through-thickness of the pipe increases with the value of m , where the fundamental and axially symmetric modes are denoted by $m = 1$ and $n = 0$ respectively. In the case for a pipe, there are generally three types of modes, namely the longitudinal (L(0,m)), torsional (T(0,m)) and flexural (F(1,m)) modes which have been labelled in Fig. 2.6. The similarities between the plate and pipe guided wave modes have been discussed in previous studies [59, 60], where it was shown that the L(0,m) pipe modes correspond to Lamb waves in plates, whilst the T(0,m) pipe modes correspond to SH waves in plates; the T(0,1) pipe mode is analogous to SH0 in a plate. Previous work has shown that if the curvature of the pipe is negligible, then the pipe problem can be approximated as an

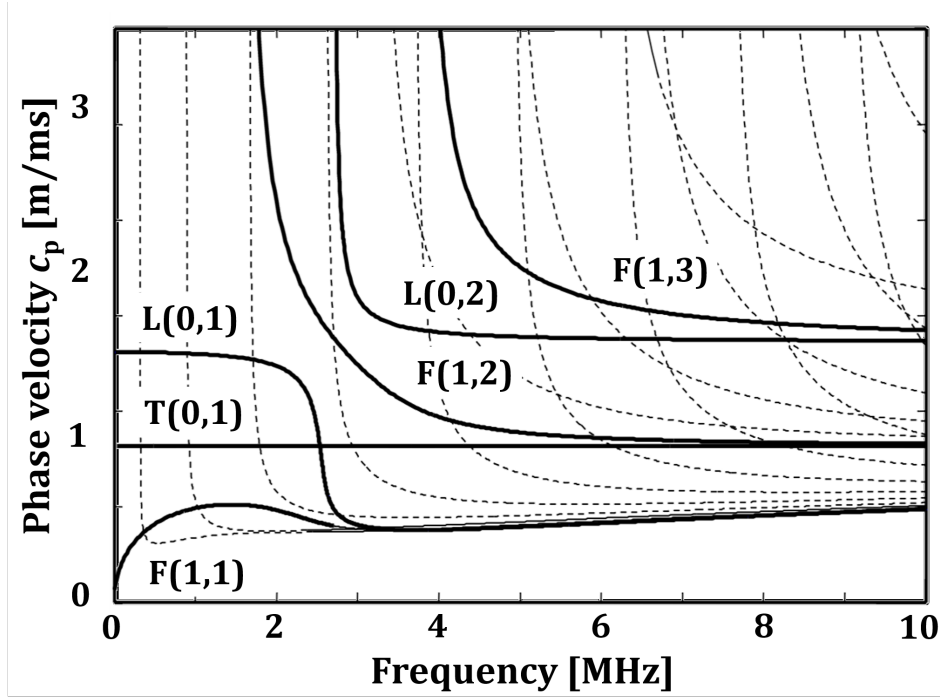


Figure 2.6: Phase velocity dispersion curves in a 6 inch steel pipe with a wall thickness of 11 mm, generated using Disperse [2].

unrolled plate [60,61]. The curvature effect of the pipe can be assumed to be negligible if the following two conditions are satisfied:

$$\begin{aligned} \text{pipe radius} &\gg \text{wall thickness}, \\ 2\pi \times \text{pipe radius} &\gg \lambda, \end{aligned} \tag{2.32}$$

The first condition suggests that the curvature of the pipe is effectively constant through the pipe thickness, whilst the second condition implies that the wavelength is much smaller than the pipe circumference such that the pipe curvature has a negligible effect on the wave propagation. A complete analysis and discussion of the limitations of this analogy can be found in [61].

2.4 Finite element modelling

The finite element (FE) method is a numerical method of finding approximate solutions to partial differential equations with boundary value problems by segmenting a large model

into small, finite elements. The finite elements are represented as simple equations which are then reconstructed into a more complex system of equations which captures the entire physical phenomena. The solution to this system is found by using one of several methods to minimise the associated error function [62]. There are two main methods of solving for time domain finite element models which are implicit and explicit analysis. On one hand, the implicit method finds a solution by solving an equation involving both the current state of the system and the later one, for example, by using the backward Euler time integration scheme [63]. This analysis method is termed as an unconditionally stable scheme since the size of the time step does not affect solution stability. This method also requires the calculation of the inverse of the global stiffness matrix as it is directly solving for displacement, $\mathbf{u}$; the matrix inversion step can also be computationally expensive when dealing with large models. On the other hand, explicit analysis calculates the state of the system at a later time using the solution at the current time. In contrast to implicit analysis, this method does not require the inversion of the complex stiffness matrix and is conditionally stable [62, 64].

Generally, explicit analysis is preferred over implicit for dynamic modelling since the inversion of the stiffness matrix is a computationally expensive step [64]. In this work, the FE mesh and input model were constructed and assembled using MATLAB[®] [65]. The wave propagation simulations are solved using Pogo, a simulation package used for explicit time-domain FE modelling of wave propagation [66]. The main advantage of Pogo is that it is a CUDA-enabled solver, which allows explicit time domain models to be solved on one or more graphics processing units (GPUs) which has been shown to be 100-200 times faster than other commercial CPU-based FE solvers [66]. The equation of dynamic equilibrium in its finite form can be expressed as

$$[M]\ddot{\mathbf{u}} + [C]\dot{\mathbf{u}} + [K]\mathbf{u} = [F_{ext}] \quad (2.33)$$

where $\mathbf{u}$ is the displacement vector, $\dot{\mathbf{u}}$ is the velocity vector, $\ddot{\mathbf{u}}$ is the acceleration vector, matrix $[M]$ is a diagonal mass matrix (material density dependent), $[C]$ is the viscous damping matrix (Rayleigh damping), matrix $[K]$ is the static stiffness matrix (Young's modulus and Poisson's ratio) and $[F_{ext}]$ are the external forces applied to the system;

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in the case where propagation is in an elastic material, there is no damping ($[C]=0$). Using a standard finite difference scheme, the vectors of the displacement, velocity and acceleration in Eq. 2.33 can be rewritten as

$$\begin{aligned}\ddot{\mathbf{u}} &= \frac{\mathbf{u}_{\text{next}} - 2\mathbf{u}_{\text{cur}} + \mathbf{u}_{\text{prev}}}{\delta t^2}, \\ \dot{\mathbf{u}} &= \frac{\mathbf{u}_{\text{next}} - \mathbf{u}_{\text{prev}}}{2\delta t}, \\ \mathbf{u} &= \mathbf{u}_{\text{cur}},\end{aligned}\tag{2.34}$$

where $\mathbf{u}_{\text{prev}}$, $\mathbf{u}_{\text{cur}}$ and $\mathbf{u}_{\text{next}}$ are the displacement vectors at the previous, current and next time steps respectively. An explicit scheme can be generated from Equations (2.33) and (2.34) by substitution and rearrangement to yield

$$\mathbf{u}_{\text{next}} = F' + C'\mathbf{u}_{\text{prev}} + K'\mathbf{u}_{\text{cur}}\tag{2.35}$$

where the modified matrices K' , C' and F' are given by

$$\begin{aligned}K' &= \left(M\frac{1}{\delta t^2} + C\frac{1}{2\delta t}\right)^{-1} \left(M\frac{2}{\delta t^2} - K\right) \\ C' &= \left(M\frac{1}{\delta t^2} + C\frac{1}{2\delta t}\right)^{-1} \left(C\frac{1}{2\delta t^2} - M\frac{1}{\delta t^2}\right) \\ F' &= \left(M\frac{1}{\delta t^2} + C\frac{1}{2\delta t}\right)^{-1} F.\end{aligned}\tag{2.36}$$

Small deformations of elements are assumed when modelling wave propagation. Figure 2.7 illustrates an example of a two dimensional (2D) linear square element, where Δx represents the element size. In order to allow FE simulations to converge and to solve

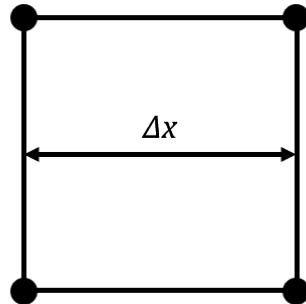


Figure 2.7: An illustration of a two dimensional linear square element.

the propagating waves, the value of the time increment δt and element size Δx must be carefully selected. Drozd [64] recommended using the time increment as calculated from

$$\delta t \approx \frac{x_{min}}{c_d \sqrt{G}}, \quad (2.37)$$

where x_{min} is the smallest nodal distance in the mesh, c_d is the velocity of the fastest wave and G is the dimensional order of the model. Hence, it must be ensured that δt is smaller than the time for the fastest wave to propagate. The Δx has to be chosen based on the complexity of the geometry and desired accuracy. Alleyne *et al.* [31] recommended that at least ten nodes per wavelength is used for a uniform square mesh (i.e. $\Delta x = \lambda_{min}/10$), whilst Drozd stated that using a mesh density of 30 elements per wavelength with triangular mesh typically provides good qualitative and quantitative results but still dependent on defect complexity, which will require local tuning and experimental validation [64]. Carandente used a tetrahedral mesh with density ranging from $\lambda/15$ to $\lambda/22$ depending on the defect complexity [67].

The 2D plane problem is commonly used because it can usually provide some insight for a three dimensional problem without requiring a large computational resource and time to solve. Fig. 2.8 shows two general 2D infinitesimal element shown to undergo stress and strain, where τ_z and τ_y are shear stresses, σ_z and σ_y are normal stresses in the z and y dimensions. Plane stress and plane strain are the two general types of problems used for plane analysis. With reference to the axis defined in Fig. 2.8, the former assumes that all the stress components in the x -dimension are zero, whilst the latter makes the assumption that the strain components in the x -dimension are zero [68]. On one hand, plane stress assumption is normally applied to a geometry where the x -dimension is much smaller relative to the z and y dimensions, uniform load applied act in the z or y dimension [68]. On the other hand, plane strain is commonly applied to a geometry where the x -dimension is much larger relative to the z and y dimensions and applied forces act in the z - y plane but do not vary in the x direction as illustrated in Fig. 2.9 [68]. Plane strain modelling technique is suitable for 2D modelling of many modes including the fundamental S0 mode [69]. For the case of the SH0 mode, 3D elements can be used

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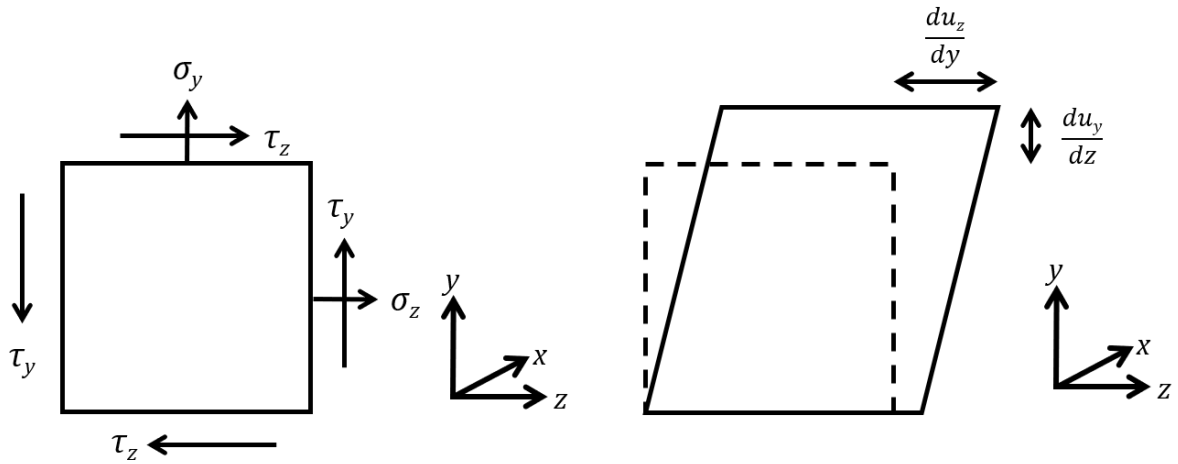


Figure 2.8: A general 2D infinitesimal element shown to be under stress (left) and strain (right).

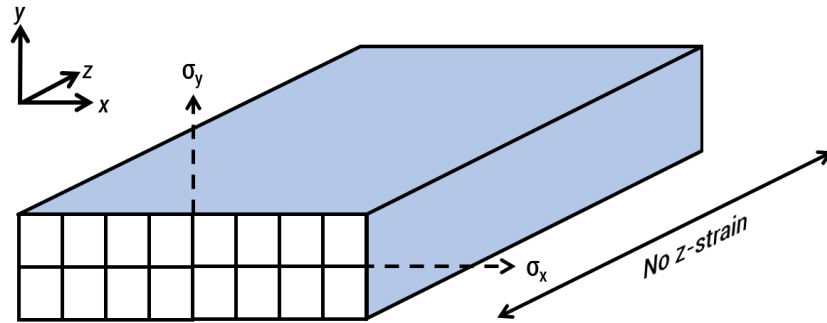


Figure 2.9: Cross-section of the 2D plane strain model.

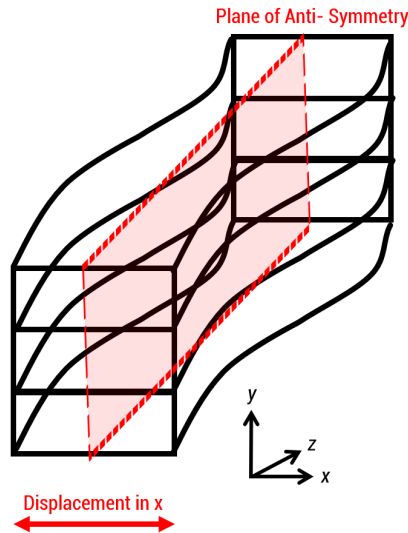


Figure 2.10: Quasi-2D model using 3D elements which has anti-symmetry assumptions and constraints. Anti-symmetry assumptions allow simplifying the model to 1 element thick in the x -direction.

to conceive a quasi-2D model, if 2D elastic elements that support displacement DOF in the out-of-plane direction are not available. Instead of modelling an infinitely wide plate, the plane of anti-symmetry during the nodal displacement in the x-axis can be exploited. This means that the infinitely wide plate can be represented by simply modelling the plane of anti-symmetry with the appropriate constraints that includes setting the values of in-plane displacement to zero ($u_z = u_y = 0$) as shown in Fig. 2.10 [70]. Both the quasi-2D and 3D finite element models presented in this thesis were constructed using C3D8 (eight-node tetrahedral) linear brick elements in a structured mesh configuration. Rotations of the node on its own axes or rotational DOF are not shown or considered when using types of finite element which only support three translational DOF per node. A 2D subsurface and surface-breaking crack can be modelled by disconnecting the FE

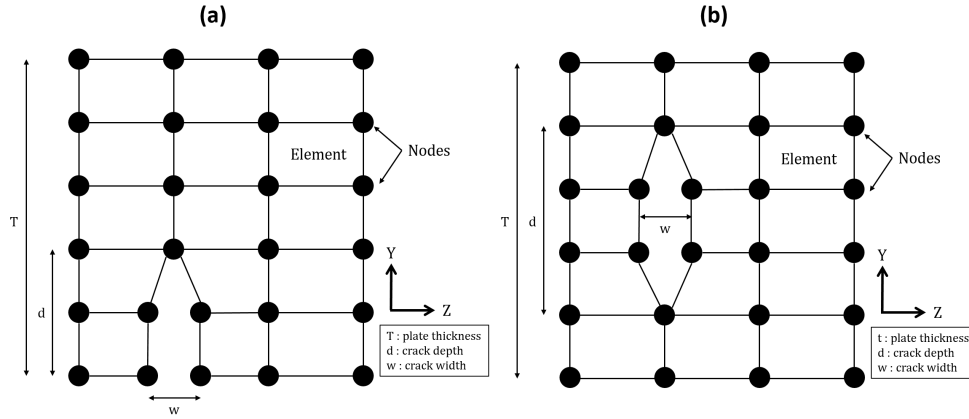


Figure 2.11: Finite element mesh details of a (a) surface-breaking (b) subsurface (right) crack in a 2D plate where the crack width has been exaggerated to visualize the node disconnection in the mesh.

mesh nodes as shown in Fig. 2.11. This technique has been used and studied extensively in previous literature [42, 71, 72]. The crack is made to be a zero-volume crack by setting the crack width, w , to zero. This same technique can also be extended to the 3D models used in this work. Specific details of both quasi-2D and 3D finite element models will be explained in the following chapter.

2.5 Conclusion

This chapter presented the fundamental theories of bulk and guided waves in an elastic, isotropic plate and a pipe. Both Lamb and SH waves can exist in an isotropic plate, with the details of the solution of SH wave propagation given. Then the three types of guided wave modes in pipes were highlighted, namely the longitudinal, torsional and flexural modes. The analogy between plate and pipe guided waves was briefly mentioned, which highlighted that pipes can be approximated to be an infinitely wide plate as long as the effects of pipe curvature can be neglected. Finally, a brief overview of the finite element method is presented, along with details on the introduction of cracks into FE via node disconnection.

Chapter 3

Scattering of SH0 from a Surface Breaking Crack

3.1 Introduction

Over the past few decades, there has been a significant amount of research conducted towards development of the solution to elastic wave scattering from cracks, for both bulk and guided waves. The interaction of guided waves with cracks is more challenging because of the larger number of modes which can exist within the geometry. Auld [52] derived a generalised formula for shear wave scattering from cracks in plates using the S-parameter formalism and the reciprocity arguments, which is similar to the bulk wave case. Datta *et al.* [73] extended this method to a surface crack located in a semi-infinite medium using matched asymptotic expansions, that was shown to be accurate at low frequencies. In the same year as this, King & Fortunko [74] applied variational solutions to explain SH guided wave scattering from cracks.

Numerical analysis has also been used to understand the scattering behaviour of SH0 from rectangular notches in plates as a function of defect axial extent, defect depth and inspection frequency. Demma *et al.* [5] observed that the reflection ratio is approximately linearly dependent on defect axial extent and frequency in the region where the ratio of defect axial extent to wavelength is less than 25%. Rajagopal & Lowe [75] extended this study to a 3D plate with through-thickness and part-thickness cracks. Ratassepp *et*

al. [72] investigated the SH0 scattering from a 2D through-thickness crack that is aligned with the direction of wave propagation. Extending from the work by Demma *et al.*, Carandente *et al.* [67] investigated the reflection of T(0,1) from an axisymmetric defect in a pipe. In both studies, the cracks with small axial extent were shown to yield a smaller SH0 reflection in comparison to a corrosion patch, despite possession of the same cross-sectional area. In addition, both the investigations suggested that a bigger reflection can be obtained by maximising the frequency of inspection. Recently, Combaniere *et al.* [76] studied the effect of tilted 2D surface-breaking cracks on SH0 scattering in plates and found that some energy can be trapped by the crack, which reduces the energy of the transmitted wave. It is generally desired for one to obtain an elegant and explicit solution that provides a reasonably accurate solution within a certain range of operating conditions. Approximate methods are usually favoured over exact solutions because of the limited number of guided wave scattering problems that can be solved exactly, including the complexity introduced by the existence of a plethora of guided wave modes. In this chapter, we seek an approximate solution to the SH0 reflection ratio from a relatively small surface-breaking crack (crack length and depth $\ll$ wall thickness) across a range of frequencies below the SH1 cut off, typically about 160 kHz in a 10 mm thick steel plate [2].

Since the intended incident SH0 wavelength is much larger than the crack size of interest, a scattering solution based on quasi-static, low frequency approximations is predicted to be sufficiently accurate. Baik & Thompson [77] investigated the use of a spring model to represent compliance across the crack faces in order to predict the reflection of low frequency ultrasonic waves from a crack in an infinite medium. Margetan *et al.* [78] extended this idea and applied this approach to thin cracks in plates for bulk longitudinal waves. More than two decades later, Bostrom & Golub [79] applied this concept for SH waves in a plate. More recently, Golub *et al.* [80] further developed this idea by deriving an analytical formula for the reflection of bulk shear waves from a circular crack in a 3D system. Despite the simplifications introduced by operating at the low frequency limit, these solutions are limited to certain canonical crack shapes and are still dependent on numerical solvers to compute the solution to integral equations.

Guided waves appear as an attractive solution for monitoring crack growth in welds be-

cause they can offer a good balance between sensitivity and volume coverage per transducer and sensitivity towards cracks with small axial length, as explained in the Introduction chapter. Since the amplitude of the guided wave reflection from small cracks would be required to quantify the extent of damage, then there is a need to understand the relationship between reflection amplitude, crack dimensions and inspection wave properties. A simple, quasi-static model is attractive because it would capture the essential physics in a simple but sufficiently accurate formulation and permits the exploration of the scattering behaviour as a function of crack size, waveguide thickness and inspection wave frequency. In the case of a 3D crack, we use dimensional analysis to derive the power relationships between the reflection amplitude ratio of SH0 waves from a rectangular shaped crack as a function of wave frequency, crack dimensions, waveguide thickness and far field distance from the defect. These results are validated using FE analysis and used to predict the constant of proportionality between the reflection amplitude ratio and the terms in the dimensional analysis.

This chapter begins with the 2D case and then proceeds to 3D. In Section 3.2, the spring interface model with quasi-static approximations was extended to derive a 2D analytical solution for the reflection amplitude ratio of SH0 mode from a surface-breaking crack, which was numerically validated using FE. In Section 3.3, dimensional analysis was used to derive a low frequency analytical solution for the reflection amplitude ratio of shear guided waves from a 3D surface-breaking crack, which was numerically validated using FE. Parts of this chapter have been adapted and extracted from publication [P1], with the permission of IEEE Transactions on Ultrasonics, Ferroelectrics and Frequency Control ((©2019 IEEE)).

3.2 2D surface crack

Previous studies have investigated the interaction of guided waves with 2D crack-like defects in plates, where the emphasis of the work has been on larger defects that removes up to 10% or more of the cross sectional area [5, 72, 75, 81]. It is therefore beneficial to revisit the 2D case and investigate details at the smaller crack size range. Initially, the low frequency solution to the scattering of a bulk shear wave from a periodic array of 2D

cracks between two identical elastic half spaces was developed, as adapted from Margetan *et al.* [78]. Then, this solution is extended to a surface-breaking crack in a plate. Finally, FE analysis was performed to validate the analytical solutions.

3.2.1 Analytical solution

First, let us consider an imperfect interface between two identical elastic half-spaces that contains a periodic array of 2D cracks as portrayed in Fig. 3.1(a), where d denotes the width of the cracks that are oriented perpendicular to the incident wave, and T is the period of the crack array. As defined by Margetan *et al.* [78], the finite interfacial stiffness κ of the imperfect interface for in-plane shear stress polarised along the length of the cracks can be expressed as

$$\kappa = \frac{4\mu}{C\pi d}, \quad (3.1)$$

where μ is the shear modulus of the elastic medium and C is the damage parameter that is defined as the relative fractional cross-section occupied by the crack and expressed as

$$C = \frac{d}{T}. \quad (3.2)$$

The reflection coefficient $\hat{R}$ between two identical elastic half-spaces separated by an imperfect interface of finite interfacial stiffness κ can be calculated as

$$\hat{R} = \frac{i\mu^2 k^2}{i\mu^2 k^2 + 2\kappa\mu k} = \frac{i\mu k}{i\mu k + 2\kappa}, \quad (3.3)$$

where i is $\sqrt{-1}$ and k is the shear wavenumber. At the low frequency limit, the values of $\kappa \gg \mu k$ (e.g. $\kappa/\mu = 5.093 \times 10^4 \text{ m}^{-1}$ and $k = 161 \text{ m}^{-1}$ when $T=10 \text{ mm}$, $\lambda=38 \text{ mm}$, $d = 0.5 \text{ mm}$), hence it can be assumed that

$$\hat{R} = \frac{i\mu k}{i\mu k + 2\kappa} \approx \frac{i\mu k}{2\kappa}, \quad (3.4)$$

Equation (3.1) can be substituted into Eq. (3.4) to calculate the low frequency asymptotic

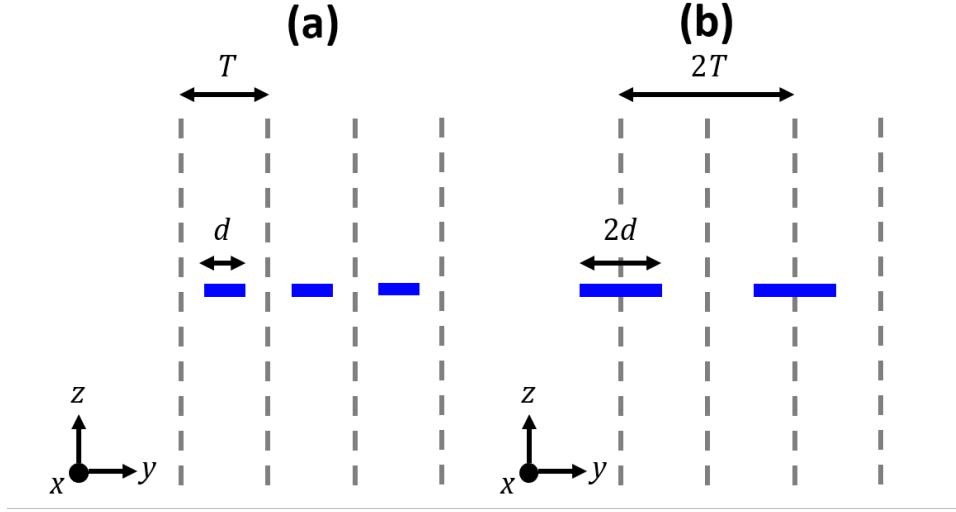


Figure 3.1: Schematic diagrams of two periodic stacks of plates, one with a (a) centre crack; (b) edge crack. Dashed lines represent the sectioning planes and therefore also the free surfaces of the plates produced by sectioning the infinite medium. (©2019 IEEE)

limit of the reflection ratio R in a dimensionless form as

$$R_{2-D,centre} = |\hat{R}| \approx \frac{\mu k}{2\kappa} = \frac{\pi^2 d^2}{4\lambda T}. \quad (3.5)$$

The normally incident, horizontally polarized shear wave produces no stress (i.e. $\tau_{xy} = \tau_{yx}$) on the planes represented by the dashed lines in Fig. 3.1. Furthermore, by virtue of symmetry, the scattered waves from the infinite periodic array of cracks cannot produce any stress on those planes either, hence these sectioning planes are stress-free.

The reflection amplitude ratio for a 2D surface crack can be deduced by considering a different sectioning strategy, as indicated in Fig. 3.1(b). The reflection ratio of the plane SH0 waves from a surface-breaking crack, $R_{2D,edge}$, in an isotropic medium can be approximated if the definition of the system described in Fig. 3.1(b) is substituted into Eq. (3.5) to yield

$$R_{2D,edge} \approx \frac{\pi^2 (2d)^2}{4\lambda (2T)} = \left(\frac{\pi^2}{2c_s} \right) \frac{d^2 f}{T} = 2R_{2D,centre}, \quad (3.6)$$

where f is the wave frequency and c_s is the phase velocity of the shear wave.

The static crack opening displacement possess a symmetric quadratic profile over the

depth d of the fully embedded crack with a maximum at the center and zero at both edges, hence the crack opening area is proportional to d^2 . The reflection ratio is proportional to the crack opening displacement averaged over plate thickness T , i.e., $R_{2D} \propto d^2/T$. Naturally, for an edge crack both d and T are doubled, so the reflection coefficient must double too, as suggested by Eq. (3.6). The underlying physical reason for the doubling of the reflection ratio for edge cracks is that for an embedded crack d represents the distance between two edges of zero crack opening displacement, while for an embedded crack d represents the distance between an edge of zero crack opening displacement and the loose "centre" of maximum crack opening displacement that lies in the plane of the free surface.

Hence, the reflection ratios for both subsurface and surface breaking cracks are proportional to frequency, to crack depth squared, and are inversely proportional to the plate thickness. Therefore in the low frequency (defect depth to wavelength ratio tending to zero) limit, the reflection ratio is not simply proportional to cross section area loss (d/T) as is commonly assumed in guided wave inspection where larger defects are considered [71, 82]. In addition, the result shows that it is advantageous to use higher frequencies, but the validity and accuracy of the solution reduces as frequency increases. Moreover, the solution is invalid above the SH1 cut-off, where more than one mode can propagate in the plate. Since most cracks of concern are likely to be surface breaking, it is beneficial that the sensitivity to a surface crack is double that to a subsurface crack located in the middle of the plate.

3.2.2 Numerical validation

In this work, FE analysis was used to model the interaction of the fundamental SH guided wave mode with a zero-volume surface-breaking crack in 2D, based on the methodology described in Chapter 2.4. The material properties used in this FE study are listed in Table 3.1. The overall layout of the FE model of a 2D plate is shown in Fig. 3.2. The nodes at one end of the plate are assigned as transmitter nodes and excited equally in the x-direction as shown in Fig. 3.2 with a 5 cycle, Hann windowed tone burst with a centre frequency of 85 kHz, to generate the fundamental shear horizontal SH0 guided wave mode. A set of receiver nodes was programmed to output the displacement in the

Table 3.1: Material properties used in numerical models. (©2019 IEEE)

Parameter	Value	Unit
Poisson's ratio	0.27	-
Density	7900	kg.m ⁻³
Young's modulus	207	GPa
Finite element type	C3D8	-

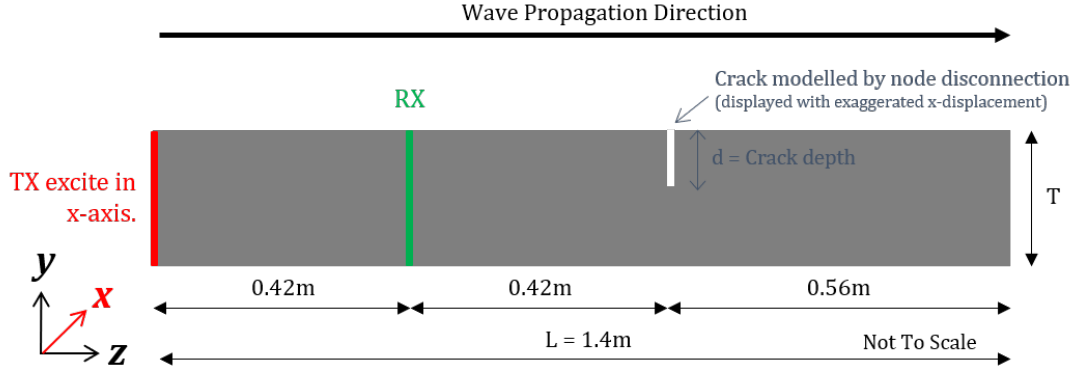


Figure 3.2: Overview of the FE model used to simulate the interaction of SH0 with a 2D crack.

x-direction, which is perpendicular to the scattering direction. The incident and scattered wave amplitudes, over a range of frequency components, were calculated by computing the discrete Fourier transform of the time signal corresponding to the incident and scattered wave respectively. The reflection ratio, R , is defined by

$$R = \left| \frac{u_{sc}}{u_{inc}} \right|, \quad (3.7)$$

where u_{sc} and u_{inc} are the backscattered and incident SH0 amplitudes respectively. The zero-volume crack was modelled by disconnecting the nodes across the crack face and the depth of the crack was varied from 0 to 40% of the plate thickness. Prior to this, a 2D mesh convergence study was carried out in order to check that the FE model setup could predict the results accurately and to determine the optimum element size which provides a good balance of accuracy and affordable computational cost. The results from this study are shown in Fig. 3.3, which shows that a mesh density of 10 elements per mm is sufficient for the convergence of the power coefficients.

3. Scattering of SH0 from a Surface Breaking Crack

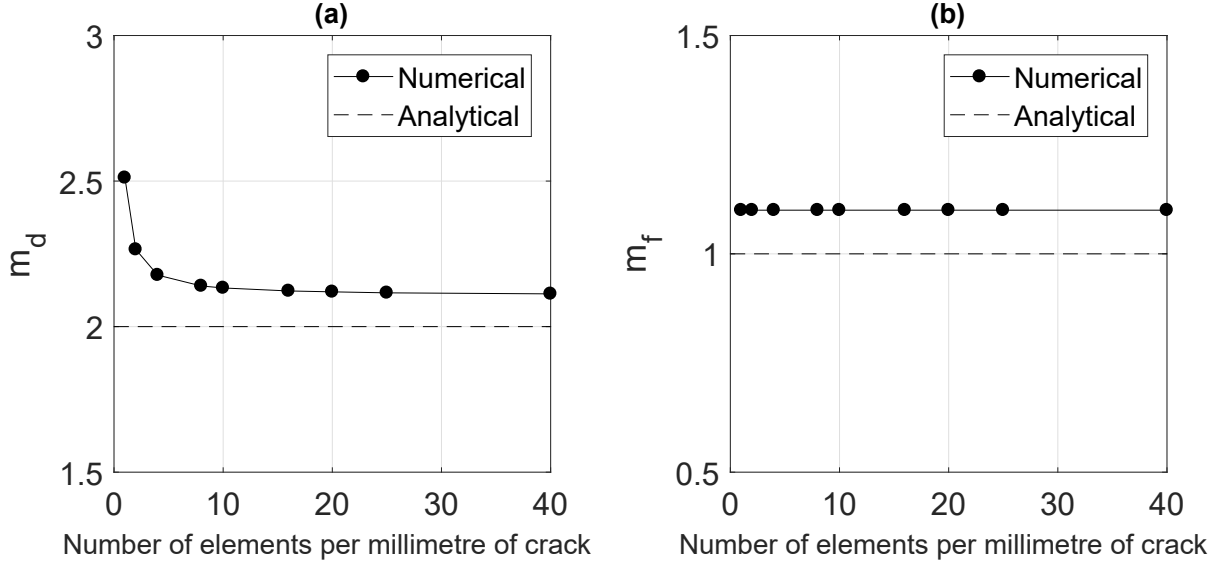


Figure 3.3: Results from the mesh convergence study showing the plot of the parameters (a) m_d and (b) m_f against the number of elements per millimetre of crack.

From Eq. (3.5), we can define and deduce that the 2D power coefficients for crack depth, plate thickness and frequency are $m_d = 2$, $m_T = -1$ and $m_f = 1$ respectively, given that

$$R_{2D} \propto d^{m_d} f^{m_f} T^{m_T}, \quad (3.8)$$

The objective of the numerical study was to validate these analytically derived power coefficients. The power coefficients m_d and m_f were obtained by best fitting the numerical data to regression curves of power function form. Similarly, the value of M_T was calculated from FE simulations for different plate thicknesses.

Table 3.2: Summary of 2D results

Parameters	Predicted Value	Numerical Value
m_f	1	1.03
m_d	2	2.11
m_T	-1	-1.05

The FE estimates of these 2D parameters (m_d , m_T and m_f) show good agreement with the analytical values as shown in Table 3.2, thus validating the analytical solution predicted in Eq. (3.5). The numerical solutions are not exactly as predicted most likely because the frequency of the numerically simulated incident wave is relatively high and deviates

from the low frequency limit assumptions. The next section proceeds to derive a similar low frequency analytical solution for the 3D case.

3.3 3D surface crack

Previous work has been carried out to understand guided wave interaction with 3D defects in plates [83,84] and pipes [37,39], the focus being on either holes, large cracks with lengths in the same order of magnitude as the wavelength or corrosion patches larger than 1% CSA. In this investigation, we shift our focus to small, critical cracks, typically smaller than 1% CSA, that are aligned perpendicular to the incident wave direction. Although the relationship between scattered wave amplitude and distance is well known, the frequency, crack size and crack shape dependent terms will be novel terms in the solution. In contrast to the 2D case, an added complication is introduced by the shape of the crack. This limits the use of existing analytical solutions because approximate solutions are generally developed for certain canonical shapes and still involve substantial computations to solve. Initially, dimensional analysis is used with the quasi-static assumption to obtain a physically meaningful equation that relates the SH0 reflection ratio to the physical variables which define the crack. Finally, finite element analysis is used to calculate the proportionality constant which cannot be obtained from dimensional analysis.

3.3.1 Analytical solution

In this section of this chapter, we derive the low frequency, asymptotic solution to the problem of scattering of elastic shear horizontal waves by a rectangular-shaped, surface breaking crack in 3D. The far-field reflection amplitude ratio or reflection coefficient R of the crack is defined as

$$R(r) = u_{sc}/u_{in}, \tag{3.9}$$

3. Scattering of SH0 from a Surface Breaking Crack

Let us now consider the scattering of a plane harmonic incident wave from a crack, where the dimensionless reflection ratio of the crack can be written as

$$R_{3D} = R_{3D} \left(\frac{c}{\lambda}, \frac{d}{\lambda}, \frac{r}{\lambda}, \frac{T}{\lambda}, \tau(\theta) \right), \quad (3.10)$$

where r is the distance from the crack to the point of observation, d is the crack depth, c is the crack width, T is the waveguide thickness and τ is the angular dependence function which is dependent on the scattering angle θ . The dimensionless reflection ratio $R(r)$ in Eq. (3.9) is complex, but for the sake of brevity, the common $e^{i(kr-\omega t)}$ phase term shall be omitted and R_{3D} will represent the magnitude of the complex dimensionless reflection coefficient. In this analysis, we seek an asymptotic approximation of R_{3D} when

$$d, c < T \ll \lambda < r, \quad (3.11)$$

and proceed to express R_{3D} as a power function

$$R_{3D} \propto \left(\frac{c}{\lambda} \right)^{M_c} \left(\frac{d}{\lambda} \right)^{M_d} \left(\frac{r}{\lambda} \right)^{M_r} \left(\frac{T}{\lambda} \right)^{M_T} \tau(\theta), \quad (3.12)$$

which is dependent on all the geometrical parameters of length dimension which are normalised to the incident wavelength, λ , where M_c , M_d , M_r and M_T are the power coefficients corresponding to crack length, crack depth, sampling distance and waveguide thickness, respectively. In the far field, the scattered wave amplitude depends on the number of dimensions, D , in which divergence can occur in the system. The radiated power of the wave is distributed over $D - 1$ transverse dimensions, relative to the propagation dimension and therefore, the power coefficient M_r , which describes the decay of R with distance, can be expressed as

$$M_r = \frac{1 - D}{2}, \quad (3.13)$$

where the factor of 2 in the denominator accounts for the square relationship between wave field amplitude and intensity. In guided wave testing, the defect sizes are typically expressed by the cross sectional loss they represent within the wave guide. Therefore, R_{3D}

can also be expressed as a function of crack cross-sectional area and shape by rewriting Eq. (3.12) as

$$R_{3D} \propto S(\xi) \left(\frac{a}{\lambda}\right)^{M_a} \left(\frac{r}{\lambda}\right)^{M_r} \left(\frac{T}{\lambda}\right)^{M_T} \tau(\theta), \quad (3.14)$$

where ξ is the crack aspect ratio defined as

$$\xi = \frac{c}{d}, \quad (3.15)$$

and the effective crack size a is described as

$$a = \sqrt{cd}, \quad (3.16)$$

and S is the normalised shape factor. The first-order perturbation term in the stress field generated by a crack at a distance far away from it does not explicitly depend on the crack geometry or the local stress distribution [85]. Consequently, both the crack geometry and the local stress distribution influence the far-field stress only through the so-called crack opening volume COV [85] and therefore, we can define S as

$$S(\xi) = \frac{COV(\xi)}{COV(\xi_0)}, \quad (3.17)$$

and ξ_0 is the crack aspect ratio which produces the maximum reflection ratio value. The reflection coefficient of shear waves from a small crack has been shown to be proportional to the crack opening volume COV , which is the crack opening displacement integrated over the area of the crack at the quasi-static limit [80, 86, 87].

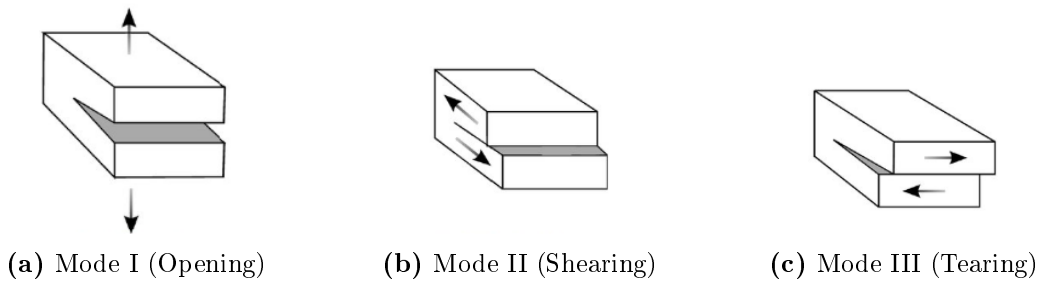


Figure 3.4: Illustrations of the three fracturing modes as adapted from [3].

3. Scattering of SH0 from a Surface Breaking Crack

Mode I, II and III are the three fracture modes which a crack can exhibit depending on direction of the applied driving force; these modes are displayed in Fig. 3.4. In the case of a rectangular crack embedded in the infinite medium and aligned with the shear loading direction, two opposite edges (d) of the crack will be in Mode II sliding edge displacement while the other two edges (c) of the crack will be in Mode III tearing edge displacement. Consequently, there is no difference between Modes II and III in the case of a rectangular crack in an infinite medium. After sectioning, the originally symmetric stress distribution remains the same over the sectioned-off half-crack as before, but only one of the "width" (c) edges is retained in Mode III tearing deformation while the opposite edge on the free surface becomes stress-free. Although Kassir [88] successfully developed a closed-form integral solution to the crack opening displacement for a rectangular crack under shear loading, cumbersome numerical methods are still required in order to get an exact solution. Therefore, a finite element simulation package, COMSOL [89], was used to compute the COV values for a rectangular crack under static, shear loading. From regression analysis, the relationship between aspect ratio and normalised shape factor can be described by

$$S(\xi) = \left(\frac{1}{2} \left(\frac{\xi}{\xi_0} + \frac{\xi_0}{\xi} \right) \right)^n, \quad (3.18)$$

where n is a parameter which depends on the elastic properties of the material. The regression function $S(\xi)$ given in Eq. (3.18) is not only a function of the aspect ratio $\xi = c/d$, but also that of Poisson's ratio ν . However, over the limited range of $0.27 \leq \nu \leq 0.33$ of practical interest in this study, the best fitting parameter pair $\xi_0 = 1.38$ and $n = -0.37$ yield a relative root-mean square regression error of less than 1.2% over the aspect ratio range of $0.1 \leq \xi \leq 20$, therefore the weak influence of Poisson's ratio can be neglected. Equation (3.14) can be simplified by removing the wavelength terms and the proportionality sign, which gives

$$R_{3D} = K r^{M_r} a^{M_a} f^{M_f} T^{M_T} S\tau(\theta), \quad (3.19)$$

where K is the proportionality constant and

$$M_f = M_a + M_r + M_T. \quad (3.20)$$

In the case of guided wave propagation in a 3D plate structure, the number of dimensions in which divergence can occur is 2, thus yielding $M_r = -0.5$ from Eq. (3.13). The crack opening produced by the incoming plane wave excites a reflected SH0 mode in the plate whose amplitude is inversely proportional to the plate thickness, so $M_T = -1$. This is because when $T \ll \lambda$, as indicated in Eq. (3.11), the SH0 reflection amplitude ratio is proportional to the COV averaged over the thickness of the plate T . At the low frequency limit where Eq. (3.11) is valid, R is proportional to the crack opening volume, so $M_a = 3$. Substituting these values into Eq. (3.13) yields $M_f = 1.5$, so

$$R_{3D} = K r^{-0.5} a^3 T^{-1} f^{1.5} S. \quad (3.21)$$

Hence, the reflection amplitude ratio R from a surface breaking crack is proportional to $f^{1.5}$ and effective crack size cubed, whilst inversely proportional to the plate thickness and the square root of the sampling distance. Since the far-field R of the crack is defined in this paper as the ratio of the scattered u_{sc} and incident u_{in} displacement amplitudes, R is not a unique function of the parameters of the scatterer (size, shape, etc.) and the incident wave (frequency, wave speed, incident angle, etc.). Instead, $u_{sc} = u_{sc}(r)$ is also a function of the spatial coordinate vector r of the point of observation relative to the scatterer i.e., $R(r) = u_{sc}(r)/u_{in}$. At low-frequencies, the far-field scattered displacement field can be approximated as follows

$$R(r) = COV.G(r), \quad (3.22)$$

where COV is the static crack opening volume and $G(r)$ is the appropriate Green's function [90]. According to Eq. (3.22), the frequency dependence of the reflection coefficient is a combination of that of the crack opening volume and that of Green's function. The static crack opening volume is a function of the crack size and shape and the elastic properties of the host medium. It is proportional to crack opening shear stress $\tau = ik\mu u_{in}$

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which in turn is linearly proportional to frequency. The appropriate Green's function can be obtained from Lamb's solution [91] which yields a proportionality of $G(r) \propto k^{1/2}$ for the cylindrically diverging scattered shear wave, hence the reflection coefficient exhibits a frequency dependence of $f^{1.5}$.

The cubic dependence on the effective crack size is consistent with findings from previous studies that showed that the scattering amplitude is directly proportional to the crack opening volume [80, 86, 87]. Similar to the results in 2D, the result shows that it is advantageous to operate at higher frequencies, but again the analysis is not valid above the SH1 cut-off where more than one mode can propagate. The beam divergence effect causes the measured reflection ratios to be smaller as the sensors are located further away the crack. Conversely, the closer the measurement point is to the crack, the higher the measurable amplitude but more measurement points will be required to obtain full inspection coverage of the entire plate width or pipe circumference due to a smaller beam divergence effect. The next section describes the FE methodology for the 3D case, which is used to validate the analytical prediction and predicting the proportionality constant, which could not be predicted from dimensional analysis alone.

3.3.2 Numerical validation

In our case, it was more convenient to model a pipe, rather than a plate, because of the ease of generating an infinite, plane shear horizontal incident wave (the T(0,1) torsional mode in the pipe) without the need for absorbing layers that leads to an increase in model size and unwanted scattering at the layer interface [64, 92]. Furthermore, there is a general interest for the application of guided wave crack monitoring in steel pipes. The overall layout of the FE model and the pipe cross-sectional view at the axial location of the defect are shown in Fig. 3.5(a) and (b) respectively. The nodes at one end of the pipe are assigned as transmitter nodes and excited equally in the circumferential direction with a 5 cycle, Hann windowed tone burst with a centre frequency of 85 kHz, to generate the fundamental torsional T(0,1) guided wave mode. This is equivalent to an infinite plane SH0 in a plate, as displayed in Fig. 3.6(a). A set of receiver nodes programmed to output the displacement component perpendicular to the scattering direction (i.e. the

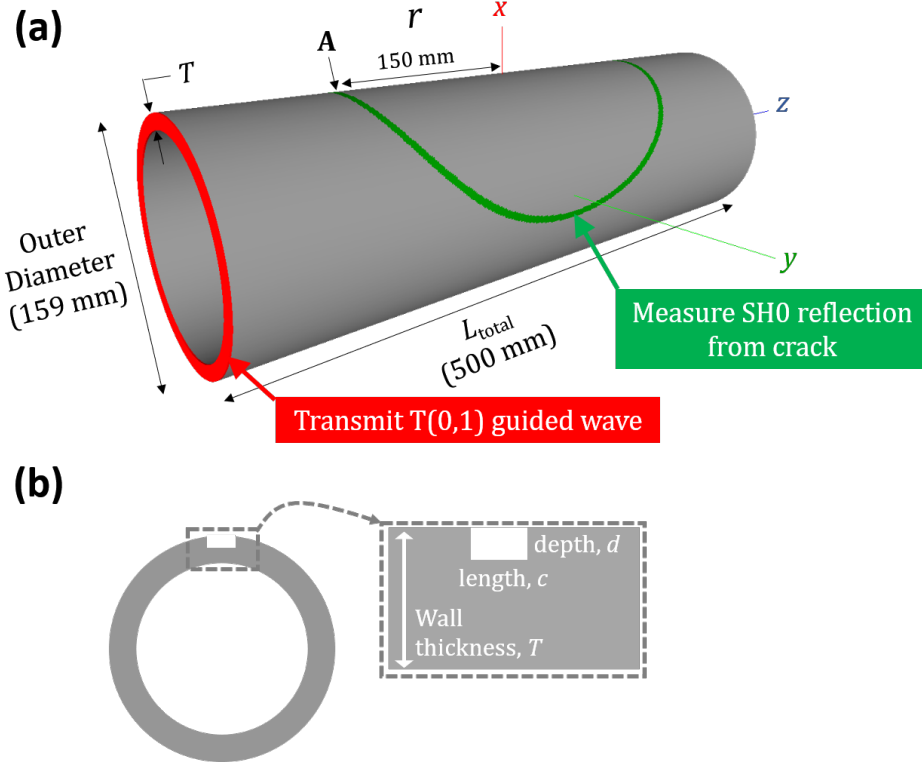


Figure 3.5: (a) Overview of the 3D FE model used in this study. (b) Pipe cross-sectional view at the axial location of the crack. (©2019 IEEE)

pure y-direction displacement at the top dead centre position of the pipe, which is labelled as A), was defined to be a fixed distance of $r = 150$ mm around the crack as shown in Fig. 3.5(a). The receiver points are shown in Fig. 3.5(a) and are positioned such that the scattered SH0 waves are measured before they mode convert into pipe guided wave modes and allowing the computation of the angular dependence of the scattered SH0 wave from a crack and the numerical results shown in Fig. 3.7. From regression analysis, the relationship between the scattering angle θ and the angular dependence factor τ can be described by

$$\tau(\theta) = \cos(2\theta), \quad (3.23)$$

and the numerical results shown in Fig. 3.7.

The incident and scattered wave amplitudes, over a range of frequency components, were calculated by computing the discrete Fourier transform of the time signal corresponding to the incident and scattered wave respectively. Since the reflection ratio is proportional

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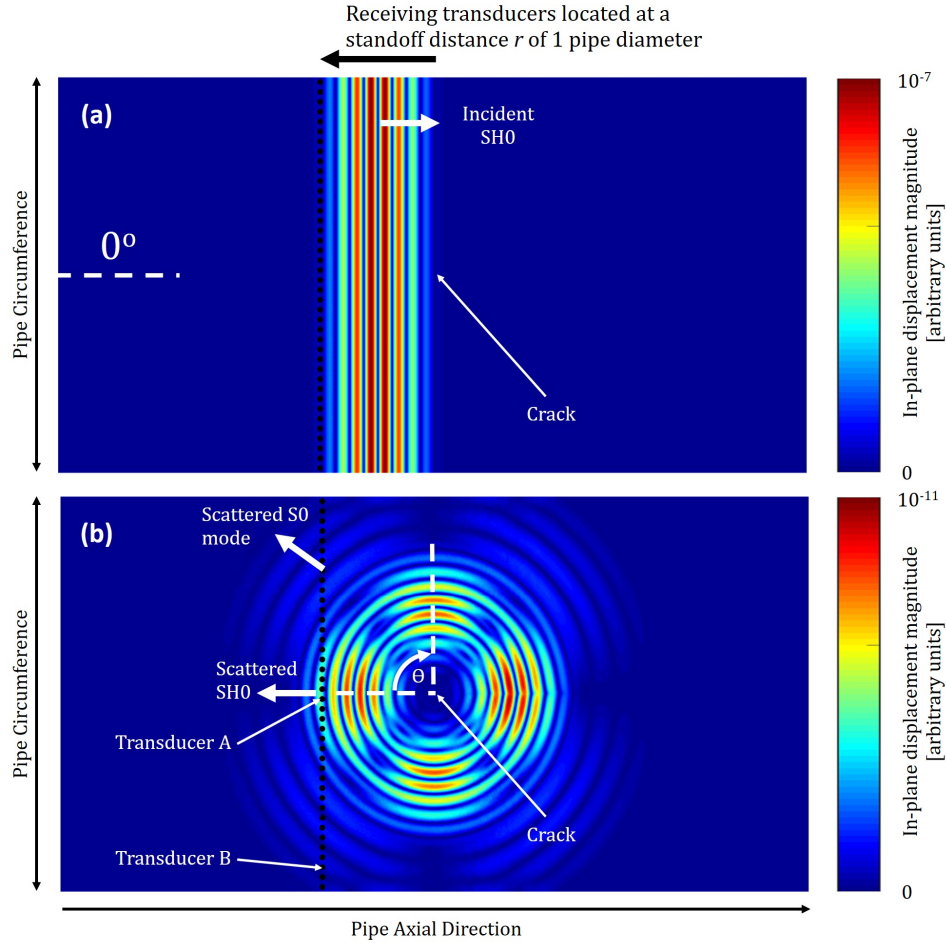


Figure 3.6: Snapshots of FE predictions of wave propagation in unrolled pipe (a) incident torsional wave (equivalent to SH0 mode in infinite plate); (b) scattered field from surface crack showing dominant SH0 mode and faster S0 mode. The white dots illustrate an array of receiving transducers that are located at a distance of one pipe diameter and equally spaced around the pipe circumference, as typically used in guided wave inspection of pipes. At a given standoff distance from the crack, the receiving transducer labelled A will measure a significantly higher SH0 amplitude than at transducer B. (©2019 IEEE)

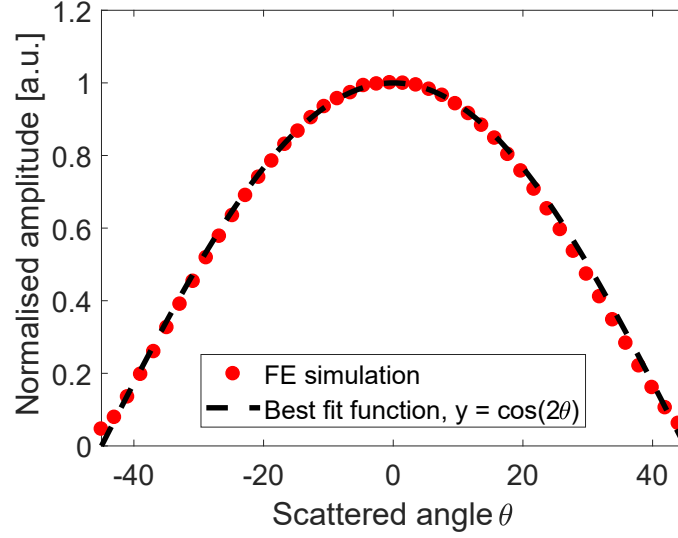


Figure 3.7: Plot of normalised reflection amplitude against scattering angle θ as defined in Fig. 3.6. FE data indicated by the red markers and obtained from receivers defined in Fig. 3.5 and a function of best fit represented by the dashed black line.

to the COV at the quasi-static limit, we can calculate S , as defined in Eq. (3.17), from our FE results using

$$S = \frac{COV(\xi)}{COV(\xi_0)} = \frac{R_\xi}{R_{\xi_0}}, \quad (3.24)$$

where R_{ξ_0} is the maximum reflection ratio for a given effective crack size. Similar to the 2D case, the crack was modelled by disconnecting the nodes across the crack face, which effectively gives a zero-volume crack, and the depth of the crack was varied from 0 to 40% of the pipe wall thickness. Although the results from the mesh convergence study in Chapter 3.2.2 concluded that a mesh density of 10 elements per mm is desired, the 3D simulations were limited to a mesh density of 8 elements per mm due to limitations in computational hardware. This is because in our FE model, a mesh density increase from 8 to 10 elements per mm will result in a significant increase from 500 million to 800 million degrees of freedom. Figure 3.6(b) shows the FE simulated backscattered waves from the crack where the SH0 and S0 mode has been labelled. The SH0 and S0 modes were identified by exploiting their mode shapes; the A0 modes can be suppressed in an unrolled pipe wave field by summing the in-plane displacements at the external and internal pipe surfaces to yield only S0 and SH0 contributions, as shown in Fig. 3.8(a). Alternatively the same results can also be achieved by the subtracting the out-of-plane displacements at

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the external and internal pipe surfaces to yield the S0 contribution only, as shown in Fig. 3.8(b). The A0 mode can be isolated as shown in Fig. 3.8(c) by summing the out-of-plane displacements at the external and internal pipe surfaces.

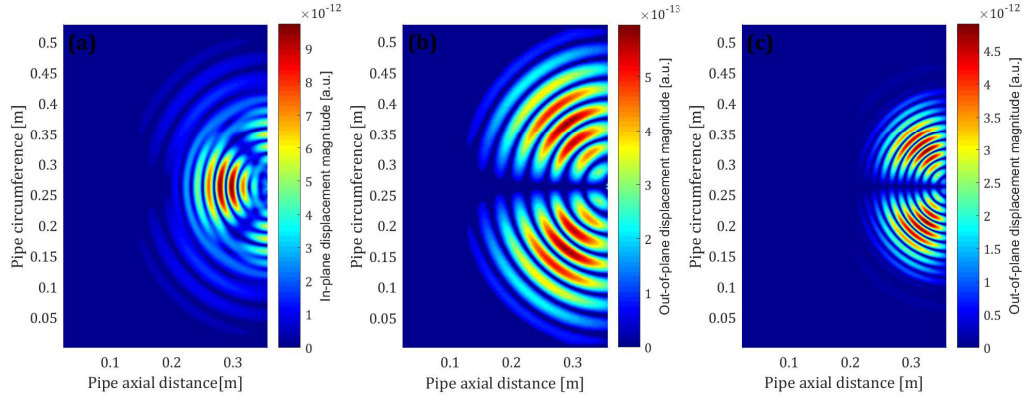


Figure 3.8: Snapshots of FE prediction of wave propagation in unrolled pipe showing the backscattered (a) SH0 + S0 and (b) S0 and (c) A0 waves from a 1 mm x 2 mm surface crack computed by (a) $|u_{\text{ext},\text{in}} + u_{\text{int},\text{in}}|$ (b) $|u_{\text{ext},\text{out}} - u_{\text{int},\text{out}}|$ and (c) $|u_{\text{ext},\text{out}} + u_{\text{int},\text{out}}|$ respectively, where $u_{\text{ext},\text{in}}$ and $u_{\text{int},\text{in}}$ are the in-plane displacements at the external and internal pipe surfaces while $u_{\text{ext},\text{out}}$ and $u_{\text{int},\text{out}}$ represent the out-of-plane displacements at the external and internal pipe surfaces.

As performed in Chapter 3.2.2, the objectives of the numerical study were to validate the analytically derived power coefficients and to calculate the proportionality constant, K , of Eq. (3.21). Since the value of R for any defect size within our scope of study can be computed from FE, then K can be computed by directly solving for K using Eq. (3.21). The power coefficients M_a and M_f were obtained by best fitting the numerical data to regression curves of power function form. The values of M_r and M_T were calculated from different r values and FE simulations for different pipe wall thicknesses respectively. The FE estimates of these four parameters show reasonable agreement with the analytical values as shown in Table 3.3. The small discrepancies may be partly due to the deviation of this system from the low frequency limit, because the choice of SH0 guided wave frequencies are relatively high, and also the mesh density used, as discussed above.

Fig. 3.9 shows the normalised shape factor, S , plotted against the crack aspect ratio, ξ , on a log scale. The solid markers in this figure plot the S values that were calculated using FE results substituted into Eq. (3.24), whilst the solid line is a plot of Eq. (3.18) with $\xi_0 = 1.38$ and $n = -0.37$, which is a regression curve that best fits the values of S

Table 3.3: Summary of 3D results (©2019 IEEE)

Parameters	Predicted Value	Numerical Value
K	-	1.1×10^{-3}
M_r	-0.50	-0.50
M_f	1.5	1.61
M_a	3	3.13
M_T	-1	-1.10
ξ_0	1.38	1.40
n	-0.37	-0.38

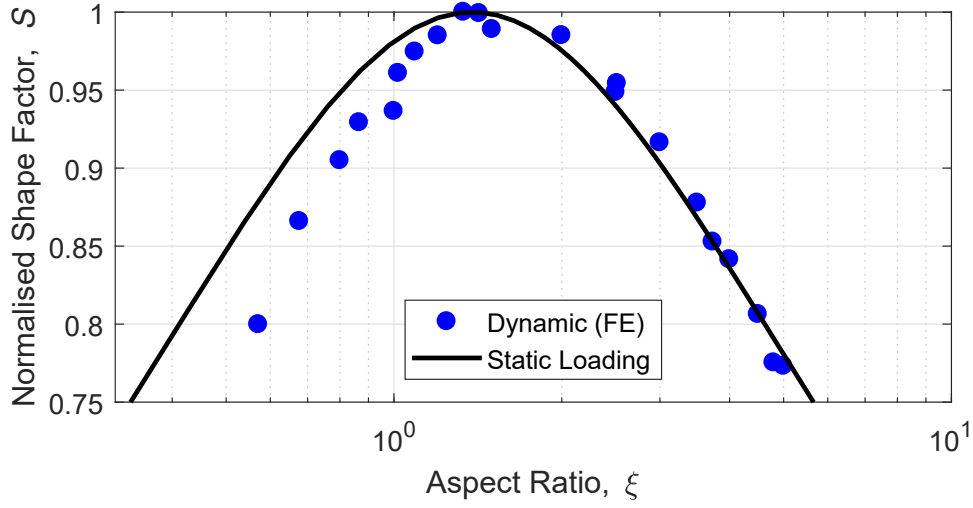


Figure 3.9: Normalised shape factor as a function of crack aspect ratio. (©2019 IEEE)

calculated from crack opening volumes under static loading. Fig. 3.5 shows that the FE results agree well with the static solution at higher aspect ratios but the two solutions diverge as the aspect ratio decreases; this corresponds to the crack shape becoming long in the circumferential direction and shallow in depth. In this regime, the crack depth is defined by a small number of elements in the FE analysis, which tends to lead to a decrease in the accuracy of the numerical solution [93]. The lack of smoothness of the data points in Fig. 3.9 is most likely caused by the FE mesh density varying when modelling different crack aspect ratios. Similar to the procedure used in the static case, the values of ξ_0 and n in the dynamic case can be calculated by using an error minimising routine that best fits the FE data to the regression equation described in Eq. (3.18). Only the range of S values for $\xi > 1$ were considered for this analysis because of the limitation in

FE accuracy as described above. Overall, these estimates show good agreement with the values obtained from the static solution as shown in Table 3.3.

3.4 Summary

In this chapter, the SH0 scattering solution from a surface-breaking crack in a plate-like structure is derived for both 2D and 3D cases, which is valid at low frequencies. The two-dimensional (2D) analytical solution for the scattering of SH0 from surface and subsurface cracks was derived using an imperfect-interface model with quasi-static approximations; it was shown that the reflection from a surface crack is double that from a crack in the middle of the plate. In contrast to assumptions commonly made in guided wave testing where the reflection is assumed to be proportional to cross-sectional area loss, the 2D solution also showed that the reflection ratio at the low frequency limit is proportional to frequency and crack depth squared, and is inversely proportional to plate thickness.

The investigation was extended to a three-dimensional (3D) system, where dimensional analysis was used with quasi-static assumptions to derive the SH0 reflection ratio as a function of crack size, crack shape and waveguide thickness, whilst accounting for the divergence of the scattered field. At the low frequency limit, the SH0 reflection ratio was shown to be proportional to frequency to the power 1.5, to the effective crack size cubed, and to be inversely proportional to the plate thickness and to the square root of the distance from the crack to the receiving transducer. The cubic dependence on the effective crack size is consistent with previous findings, showing that the scattering amplitude is directly proportional to the crack opening volume; again the reflection ratio is not simply proportional to the cross section loss produced by the crack. Dimensional analysis and static crack opening displacement solutions were used to derive a low frequency analytical solution for the reflection amplitude ratio of shear guided waves from a 3D surface-breaking crack. Both 2D and 3D results indicate that operating at higher frequencies is beneficial, but this is limited by the SH1 cut-off. The beam divergence effect in 3D will result in a higher measured reflection ratio as the distance from the sensors to the defect decreases, but as the distance reduces, more measurement points are required to obtain a full inspection coverage of the plate width or pipe circumference. In the case of

a pipe, this phenomenon suggests that it would be advantageous to measure the scattered SH0 waves at stand-off distances lower than the pipe circumference so that the received signal is not complicated by helical path arrivals.

Finite element (FE) analysis was used to validate the power coefficients from the dimensional analysis and to obtain the proportionality constant. The comparison between the numerical and analytical values showed good agreement, with small discrepancies due to challenges in 3D mesh refinement and the divergence of the FE predicted case from the low frequency limit, which the analytical prediction is based on. The next chapter proceeds to the experimental validation of the numerical and analytical 3D solutions, as well as the evaluation of the monitoring performance of an SH0 guided wave system.

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Chapter 4

Monitoring Crack Growth Using SH0 Guided Waves

4.1 Introduction

R/σ As mentioned in the Introduction chapter, the damage sensitivity from one-off guided wave inspections can be significantly improved when a monitoring configuration is employed. For example in guided wave inspection of pipelines, the signal-to-noise ratio (SNR) is dependent on the location of the defect on the pipe; scattered waves from a defect can be masked by larger reflections from features, such as welds and supports, and greatly reduce the SNR. Baseline subtraction is a method that can be applied to isolate the damage signal for damage detection thus improving the SNR. In general, this method involves collecting signals from a structure with an unknown damage state and subtracting it from a signal previously collected on the same structure with a known damage condition, with the aim to detect damage growth. The resulting residual signal would ideally only contain information about the damage, assuming that the baseline component of the recorded signals remains stable over time as illustrated in Fig. 4.1(a-c). In practice, the baseline component of a recorded signal can change over time as a function of temperature and other environmental variations [94], which can lead to unwanted noise in the residual signal as shown in Fig. 4.1(d-f). There are many alternate damage evaluation methods [39,95–98]; however, the simple baseline subtraction residual method with compensation is used here

4. Monitoring Crack Growth Using SH0 Guided Waves

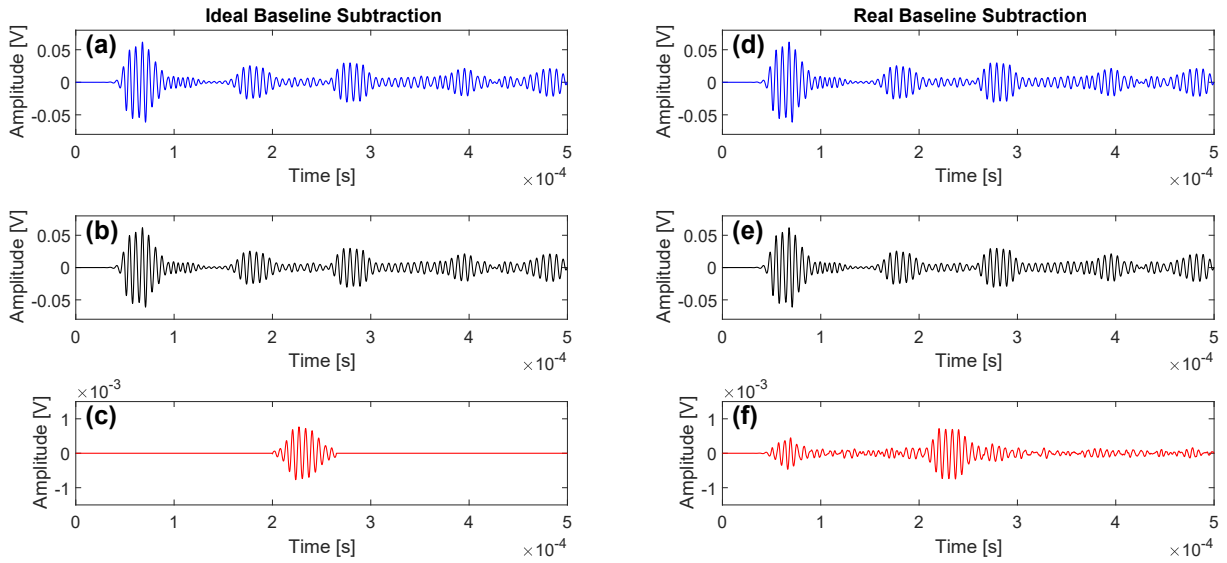


Figure 4.1: Illustrations of current and baseline signals collected under ideal conditions (a-b) that are subtracted from each other to yield the ideal residual signal (c) which contains a high defect SNR, in comparison to the current and baseline signals collected under real and practical conditions (d-e), which yields the residual signal (f) that possesses a poorer defect SNR. Data obtained from experiments described in Chapter 5.

to demonstrate the advantages of monitoring over one-off inspections. Guided waves can

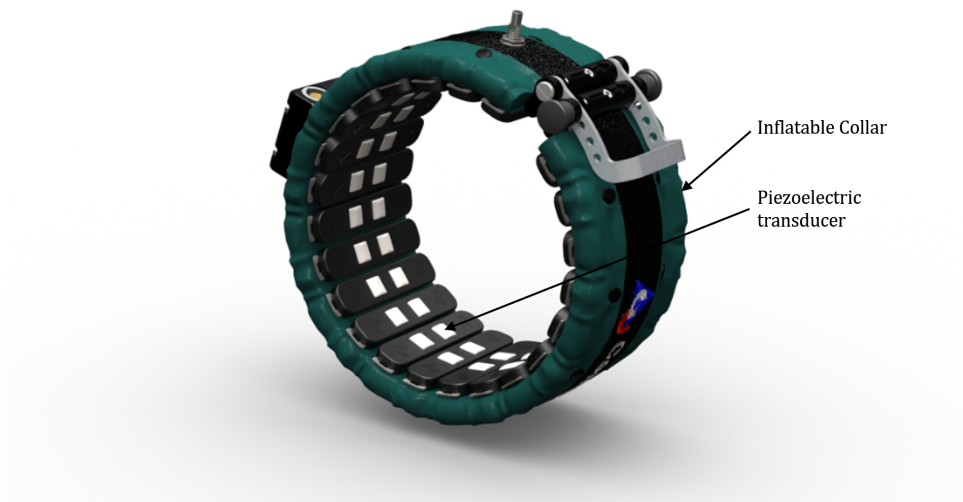


Figure 4.2: An example of a commercially available inflatable transducer ring that comprises two rows of equally spaced piezoelectric transducers [1].

be excited in pipes by attaching a ring of piezoelectric transducers to a pipe, where an example of a commercially available guided wave transduction equipment is shown in Fig. 4.2. In both one-off guided wave inspection and guided wave monitoring, the signals

from the different transducers in the ring are summed to measure the reflected $T(0,1)$ mode from the defect; the reflection amplitude ratio R provides an estimate of the defect severity. Accurate measurements of the $T(0,1)$ mode reflection are obtained at distances

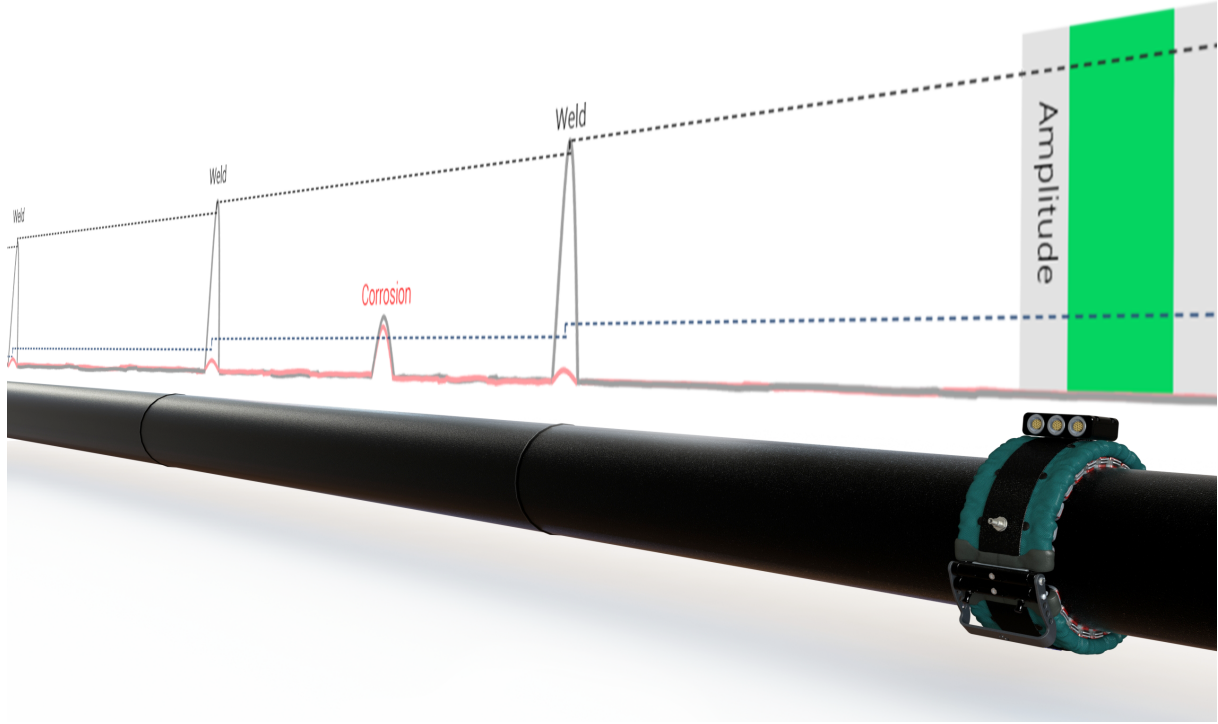


Figure 4.3: An illustration of an inflatable guided wave transducer ring on a pipe section and a plot of reflection amplitude against pipe axial distance. The black and red traces represent the $T(0,1)$ and flexural mode reflections respectively [1].

more than several pipe circumferences away from a feature when the reflections from the defect have fully developed into $T(0,1)$ and other pipe guided wave modes, as illustrated in Fig. 4.3. However, at distances close to the defect, the plane $T(0,1)$ mode, which is equivalent to the SH0 mode in a plate, is scattered into an SH0-like mode that spreads with distance as shown in the snapshots from an FE simulation given in Fig. 4.4(a-b). The amplitude is highest close to the defect but if this is to be detected, then a large number of transducers around the pipe circumference is needed to ensure full coverage; there is a trade-off between signal amplitude and the number of transducers required. Therefore, instead of summing up the signals from all receiving transducers, it may be beneficial to look at individual signals or the sum of a few of them, which could offer improvement in signal-to-noise ratio (SNR). Since the SH0-like wave is affected by beam divergence, the minimum number of measurement points required to obtain full pipe in-

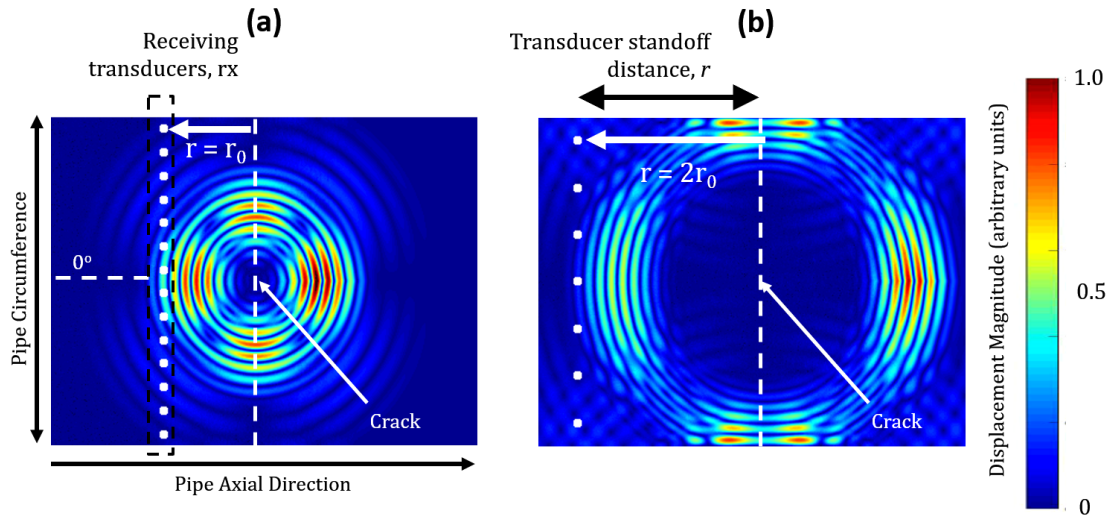


Figure 4.4: Numerically simulated scattered wave field from a surface breaking crack in an unrolled pipe, with a diameter of 6 inches. The receiving transducers are indicated by the white dots and illustrate the minimum number of receiving transducers required for full circumferential inspection coverage, when the transducer standoff distance is equal to (a) r_0 and (b) $2r_0$ where r_0 is the pipe radius.

spection coverage is dependent on the transducer standoff distance r . Another way to improve sensitivity is to increase the inspection wave frequency, but the received signals become more complicated at frequencies above the higher mode cut-off. In cases where a particular axial location on the pipe is to be inspected e.g. a weld, then it is possible to optimise the location of the transduction system relative to the weld in order to minimise the number of transducers required for full coverage, while still maintaining the required sensitivity. Hence, this chapter investigates the effectiveness of a short-range but relatively high frequency guided wave system to monitor critical sections of structures, predicting the influence of the number of receiving transducers and distance of inspection zone from transducers.

Evaluating the damage detection ability of a system at different locations is very costly because it involves an extensive programme of damage growth in a structure over a long period, as well as using an impractical number of sensors in the operational environment. These limitations can be overcome by using the superposition method, which involves superimposing defect reflections obtained from finite element (FE) simulations onto large numbers of defect-free measurements. In this way, a synthetic or hybrid dataset is gener-

ated and used to evaluate the damage detection capability, taking into account the effect of environmental variations on the response of the structure. This methodology has been successfully used to estimate the damage detection ability of permanently installed monitoring systems [39, 99]. The receiver operating characteristic (ROC) curve is a useful metric for evaluating the damage detection reliability as it plots the probability of detection (POD) as a function of the probability of false alarm (PFA) [100, 101]. This chapter begins with details of the crack growth experiment on the pipe and the damage detection methodology used to quantify the damage. The yielded experimental results are then compared to both analytical and numerical predictions from Chapter 3.3. Following from this, the procedure of creating a hybrid dataset by superposing the real baseline data with the FE predicted defect reflections is discussed. Concluding remarks on the reliability of using an SH0 guided wave for crack growth monitoring are drawn together in the final section. Parts of this chapter have been adapted and extracted from publication [P2].

4.2 Crack growth in a steel pipe

This section investigates crack growth monitoring in a straight pipe section by measuring the backscattered SH0 guided waves via a guided wave transducer ring mounted on the pipe. Initially, the experimental setup is described, followed by the damage detection methodology and lastly the comparison of the experimental results to both numerical and analytical predictions.

4.2.1 Experimental setup

The experimental setup used to collect guided wave signals is shown in Fig. 4.5. It comprises a 1.2 m long, 6-inch nominal bore steel pipe (NPS6 Schedule 80/XS ASTM A106-B) with an 11 mm thick wall and an outer diameter (OD) of 168.28 mm that is supported on steel V-blocks lined with plywood. In this setup, the pipe can be approximated as an unrolled plate because the effect of pipe curvature can be neglected (pipe radius (76 mm here) $\gg$ wall thickness (11 mm) and pipe circumference (478 mm) $\gg$ wavelength (27 mm) [60, 61]).

4. Monitoring Crack Growth Using SH0 Guided Waves

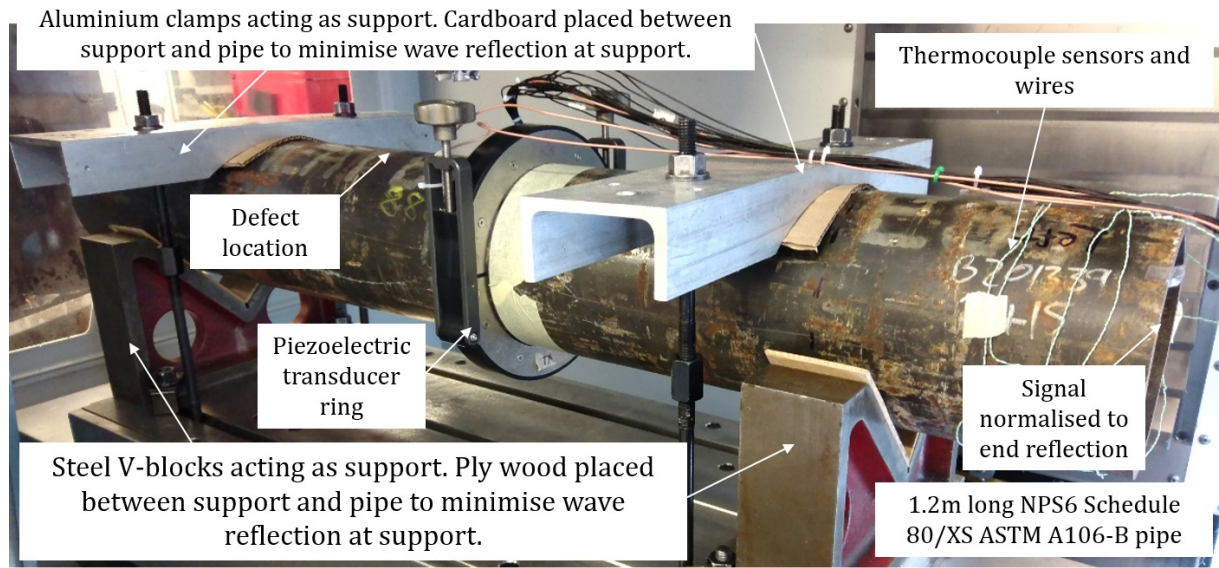


Figure 4.5: Photo of the experimental setup.

A crack was simulated on the pipe at approximately 150 mm from the transducer, by progressively milling a notch, with a circumferential length c and depth d in the thickness direction, at the same predefined location using a milling cutter tool with a diameter of 1 mm, that is equivalent to the axial length of the notch. The experiment was setup on the bed of a computer numerical control (CNC) machine and secured in place with aluminium clamps lined with cardboard, so that the defect could be grown in stages whilst ensuring that the milling tool returned to the same location for each increment. Guided wave reflections from the supports are minimised by using the cardboard and plywood, which have soft surfaces [102]. A commercially available guided wave transducer ring was clamped on to the pipe at the location shown in Fig. 4.5. Figure 4.6 outlines the details of this transducer ring, which comprises two rows of 40 piezoelectric transducers that are oriented to induce and receive circumferential motion in the pipe. Since a pitch catch configuration was used in the experiment, the region within the time signal which cannot be used to classify a defect or dead zone was relatively small. A Handyscope HS5 waveform generator with transmit-receive amplifiers was used to generate a 5-cycle Hann windowed wave with an 85 kHz centre frequency. As shown in Fig. 4.6, the first row of transducers were wired in parallel and used to excite the $T(0,1)$ mode; the top half of the second row of transducers were programmed to receive as individual channels and the bottom half of the transducers wired in parallel as a single channel, with a multiplexer being used to

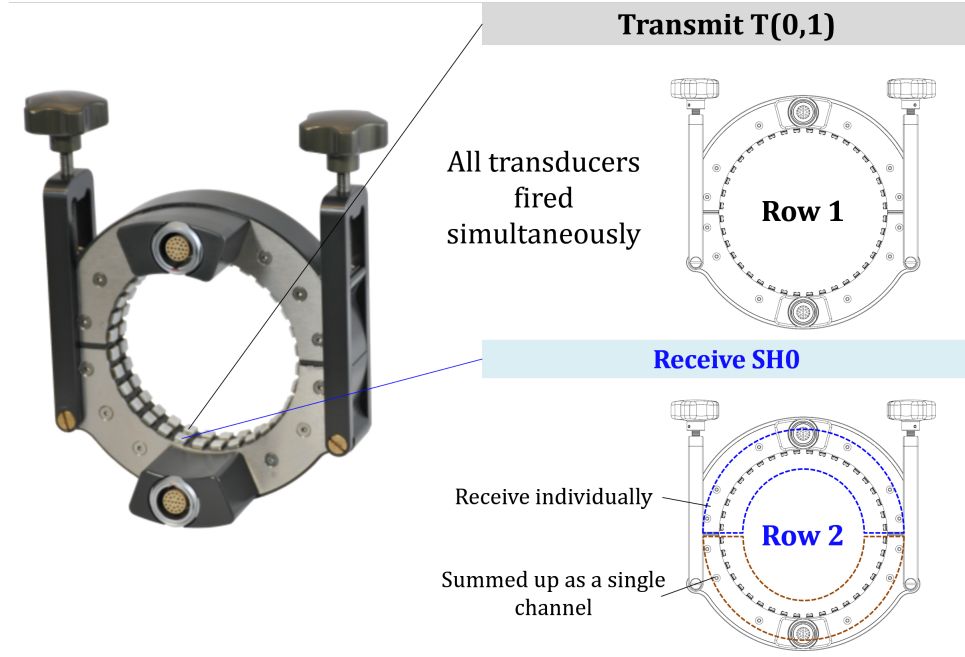


Figure 4.6: Details of the piezoelectric-based transducer ring.

collect the signal from each channel in turn. The second row of transducers were wired in the described format due to the number of channel limitations in the multiplexer at the time of the study. About 250 defect-free, baseline measurements were taken, along with temperature measurements of the pipe and the room. After this, the notch was grown progressively, the size at each step being given in Table 4.1; at each step, guided wave measurements were collected at 15 minute intervals over a minimum of two days, in order to check stability and to allow signal averaging. By standing off the transducers less

Table 4.1: Experiment variables

Step Interval	Defect Depth, d [mm]	Defect Length, c [mm]
0	0	0
1	1.0	2.0
2	1.5	3.0
3	2.0	4.0
4	2.4	4.8
5	2.8	5.6

than a pipe circumference away from the crack, the clear arrival of the backscattered SH0 waves without any helical paths can be captured. Therefore, the SH0-like wave could be

captured by the receiving transducers, as illustrated in Fig. 4.4(a-b).

4.2.2 Damage detection in pipe

Damage in the structure can be detected by comparing the baseline measurement records with those following the introduction of the defect. Figures 4.7(a-b) show two examples of the time traces from the same single receiver located at the 0° position on the pipe as illustrated in Fig. 4.4(a-b), at the start and after the growth of the fifth defect step, the signals being normalised to the reflection from the pipe end on the other side of the transducer ring, as shown in Fig. 4.5. Although the defect location is known during the experiment, the defect signal cannot be easily detected from either Fig. 4.7(a) or (b) because of the low signal-to-noise ratio (SNR). This low SNR is attributed to the presence of coherent, circumferential S0 and A0 waves that are generated along with the T(0,1) wave [103]. S0 waves can propagate around the pipe circumference, meaning that both the propagation direction and displacement are in the circumferential direction. Similarly, A0 can propagate circumferentially and it would have displacement components in both the circumferential and radial directions. Since the receiving transducer measures displacement parallel to the pipe circumferential direction, the receiving transducer is capable of measuring S0 and A0 modes. In order to improve the detection sensitivity, the baseline subtraction residual method can be applied, the baseline signal being defined as the first signal from the damage-free dataset. The environmental variations in the datasets are compensated for by applying a scale transform-based temperature compensation algorithm [104]. This method approximates the effect of temperature changes as a compression or dilation in the time domain, and reverses the effect by selecting an optimal stretch factor that is determined by maximising the correlation coefficient between the baseline and measurement signals. The residual signals are then calculated by subtracting the measured signal from the baseline after temperature compensation. Finally, we define the baseline subtracted residual reflection ratio, R , as the ratio of the maximum amplitude across a time window of duration equal to that of the input signal centred on the potential defect location of interest to the maximum amplitude in a similar window centred on the pipe end on the other side of the transducer ring from the defect. Fig.

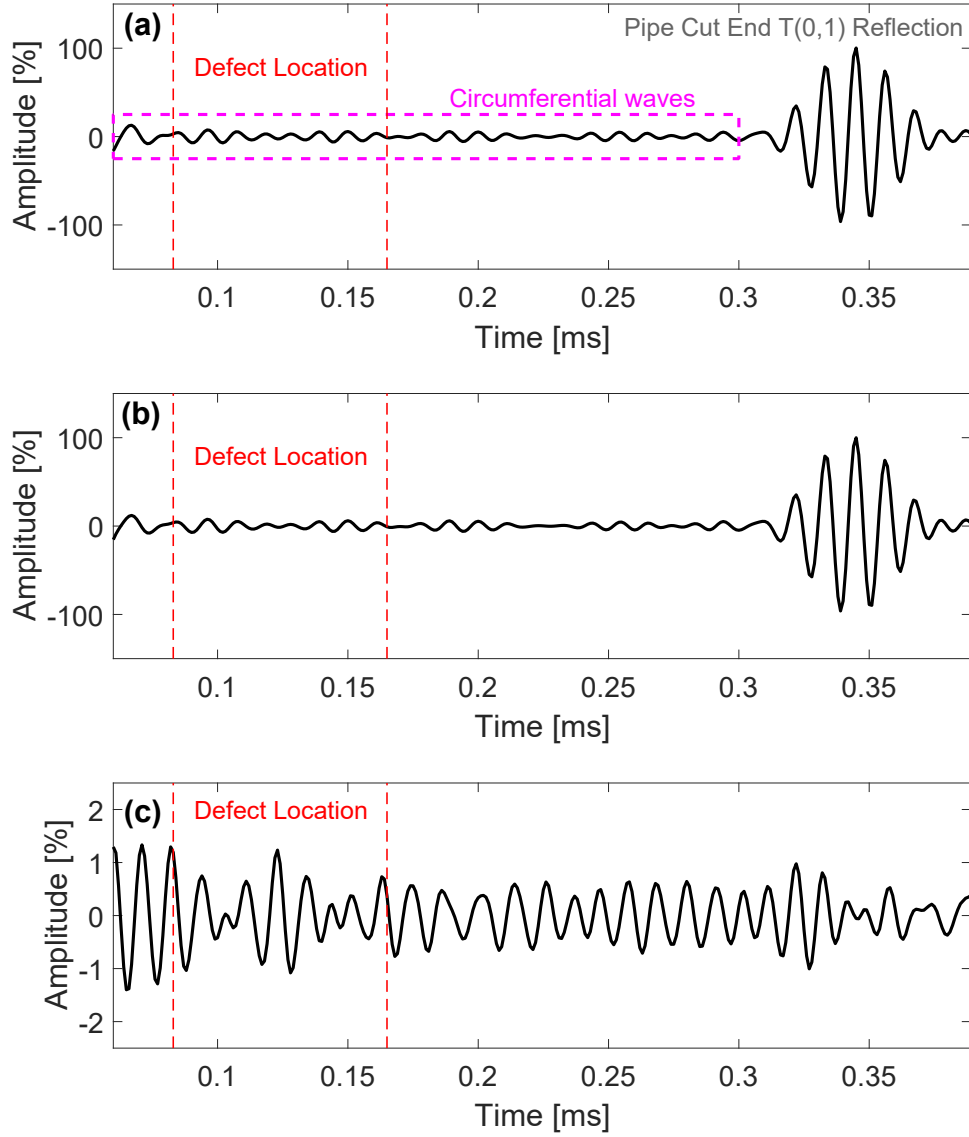


Figure 4.7: Temperature compensated time trace signals from a single receiver transducer (a) without presence of damage (b) with damage detailed in Step #5 of Table 4.1 and (c) residual signal of time trace given in (b). Note the different amplitude scale in (c).

4. Monitoring Crack Growth Using SH0 Guided Waves

4.8(a) shows R plotted as a function of monitoring sample number, sampled at 15 minute interval over a period of 2 weeks, where the times of defect growth listed in Table 4.1 are indicated by the vertical dashed lines.

The reflection ratios in this figure have been offset by a constant value, to effectively zero the mean of the data set for the undamaged pipe. The objective of the test was to detect changes from the initial state so the mean value of R over the period before damage introduction was defined as zero. From Fig. 4.8, it can be observed that the

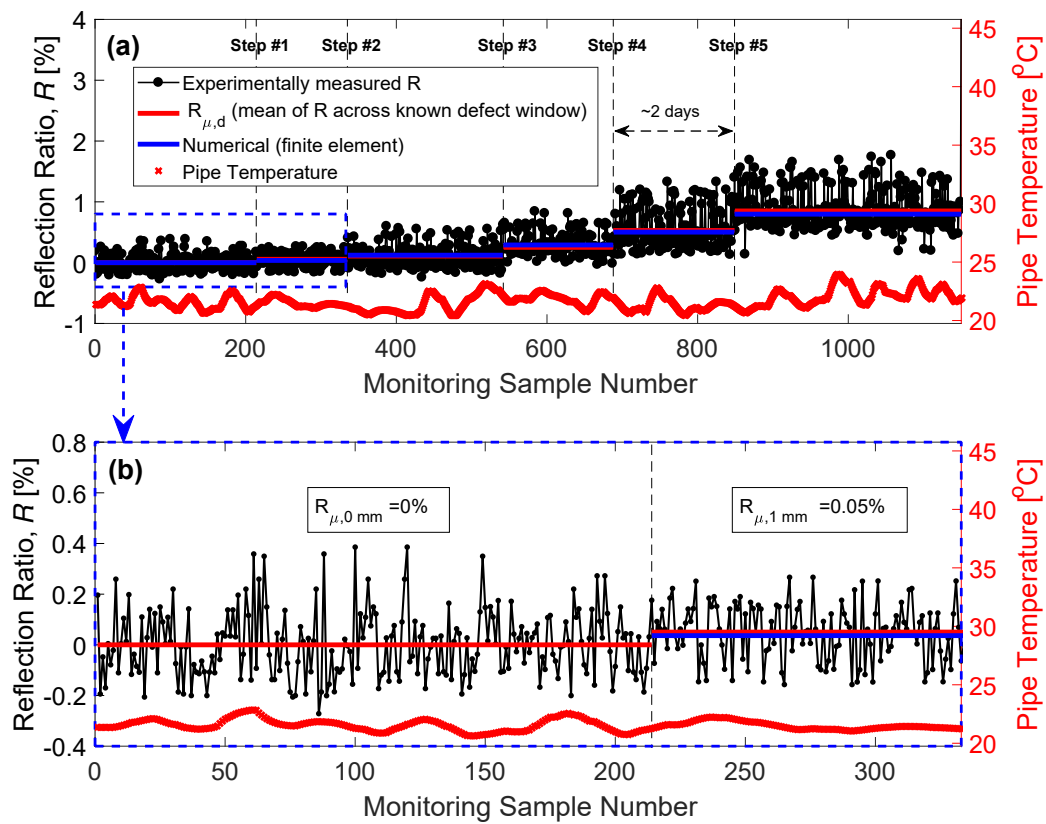


Figure 4.8: Plots of R from a single receiving transducer at about 10° relative to the defect circumferential location and pipe temperature (red), against monitoring sample number (a) over the entire experiment period (b) after the introduction of the first defect step. Red and blue solid lines show mean values at different step sizes and results from numerical simulations respectively.

experimentally measured R contains a significant degree of noise due to variations in the environmental and operational conditions (EOC), thus causing changes in the residual component of the signals. The distribution of the values of R obtained at each damage step was investigated by applying the Lilliefors test for normality [105], where the yielded test statistic values were found to be less than their respective critical values required

for a normal distribution to be assumed at a significance level of 5%; the histograms of these distributions can be found in Fig. 4.9. Therefore it is reasonable to assume that

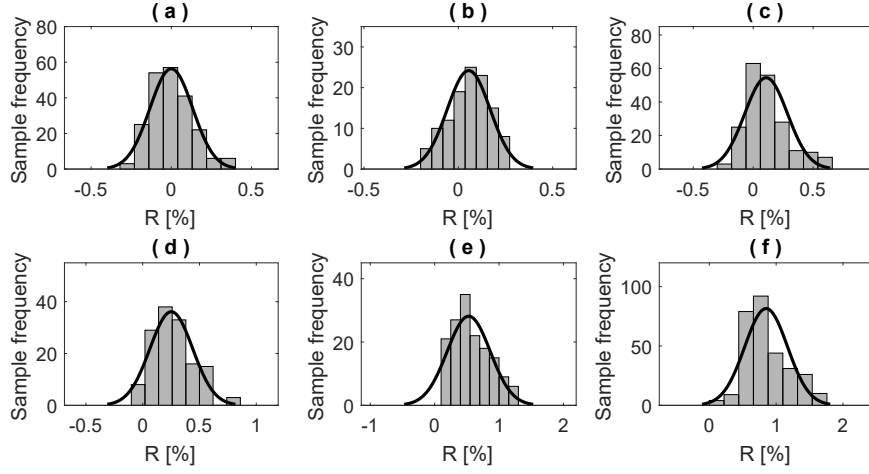


Figure 4.9: Histogram of experimentally measured reflection ratio R (a) before the introduction of the defect (b) when the notch depth is 1 mm (c) 1.5 mm (d) 2.0 mm (e) 2.4 mm and (f) 2.8 mm.

the residual measurements are normally distributed, so averaging can be used to improve the estimates of reflection ratio at each step size, which is denoted by R_μ .

4.2.3 Comparison with analytical and numerical predictions

The experimentally obtained values of R for each defect size were plotted in Fig. 4.10. The analytically predicted values of R from a zero axial width crack and a 1 mm wide notch were calculated using

$$R_{3D} = Kr^{-0.5}a^3T^{-1}f^{1.5}\left(\frac{1}{2}\left(\frac{\xi}{\xi_0} + \frac{\xi_0}{\xi}\right)\right)^n, \quad (4.1)$$

which can be obtained by combining Eq. 3.21 and Eq. 3.18 from Chapter 3, and plotted in Fig. 4.10. Table 4.2 lists the parameter values used for the analytical predictions.

In the case of the 1 mm wide notch, a new FE calculated proportionality constant, $K = K_1$, was used instead of $K = K_0$, to account for the fact that the reflection ratio is also a function of the ratio of the axial extent of the defect to the wavelength, reaching a maximum at a length of a quarter wavelength [42]; the 1 mm length of the notches tested here represents 2.5% wavelength at the 85 kHz centre frequency. Additionally, the

Table 4.2: Parameter values used for analytical prediction of the SH0 reflection ratio from a surface breaking crack.

Parameters	Numerical Value
K_0	1.1×10^{-3}
K_1	2.2×10^{-3}
M_r	-0.50
M_f	1.5
M_a	3
M_T	-1
ξ_0	1.38
n	-0.37

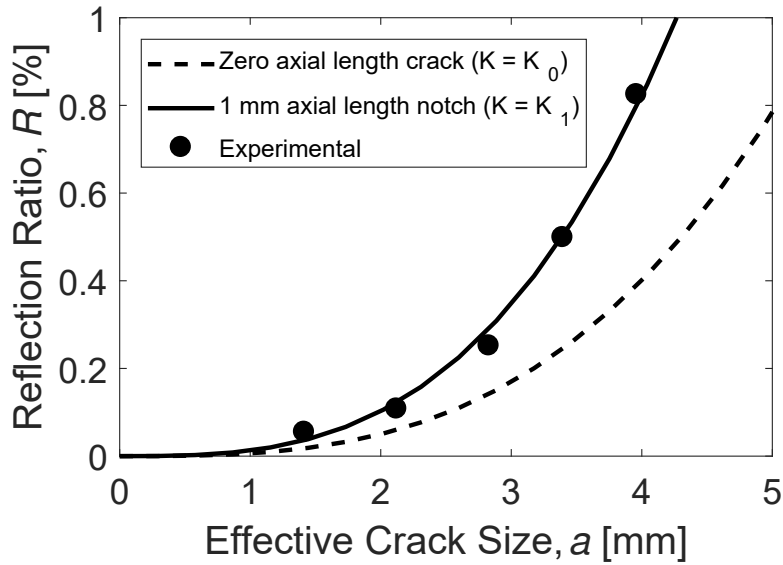


Figure 4.10: Predictions of the SH0 reflection ratio from a surface-breaking notch with 1 mm axial length (solid line) and crack with zero axial length (dashed line) using Eq. 4.1, as a function of the effective crack size, $a = \sqrt{cd}$, where c and d are the circumferential length and depth in the thickness direction. The data obtained experimentally are given by the solid markers [4].

FE results show that $K_1 \approx 2K_0$, which agrees with findings from a previous study by Demma *et al.* [42], which shows that the reflection ratio doubles as the axial extent of the notch increases from 0 to 2.5% wavelength, as displayed in Fig. 4.11. The crack gives a

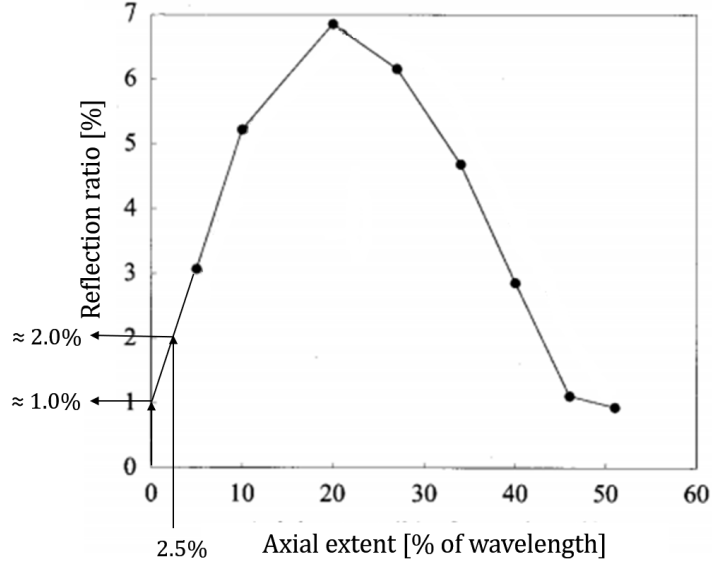


Figure 4.11: Plot of reflection ratio against axial extent as a percentage of the incident T(0,1) wavelength. Results obtained from previous work [5] by FE simulations of a 3 inch pipe with a notch of 20% depth and 25% circumferential extent at 55 kHz.

somewhat smaller reflection than the notch, as would be expected [5], but the notch gives a reasonable indication of the order of magnitude of the reflection expected from defects of different sizes. Although K_1 is approximately double the value of K , the reflection ratio change from the zero length crack case is still modest, approximately 0.05% for a 1 mm long and 2 mm deep crack. Overall, the results from Fig. 4.10 show very good agreement between the experimental results and the predictions, showing that the simple quasi static approximation discussed in Chapter 3 is applicable in this frequency range.

4.3 Evaluation of monitoring performance

In this section, the focus is on evaluating the detection performance at different distances from the transducer ring with different numbers of receiving transducers. The defect considered is the smallest one in the experimental programme, the 1 mm deep by 2 mm wide notch with a 1 mm axial extent, equivalent to 0.065% cross-sectional area loss in

the pipe. The damage detection scheme described in the previous section is applied to synthetic datasets generated with different values of the total number of measurement samples or sample length L , number of receiving transducers rx and transducer standoff distance from the defect r , as defined in Fig. 4.4(a-b).

4.3.1 Generation of synthetic data

A finite-element model of a pipe with the same geometrical properties as the one used in the experiment was created, as illustrated in Fig. 4.12. The incident $T(0,1)$ mode was generated by equal, circumferential excitation at one end of the pipe with the same 5-cycle, Hann windowed, 85 kHz centre frequency tone burst used in the monitoring experiment. Predicted signals reflected from the notch were recorded for all the surface nodes located in the backscattered direction of the crack. A notch was modelled by removing elements from the mesh, simulating the geometrical properties of the notch used in the experiment. Similar to Chapter 3, the model was run using Pogo, a package

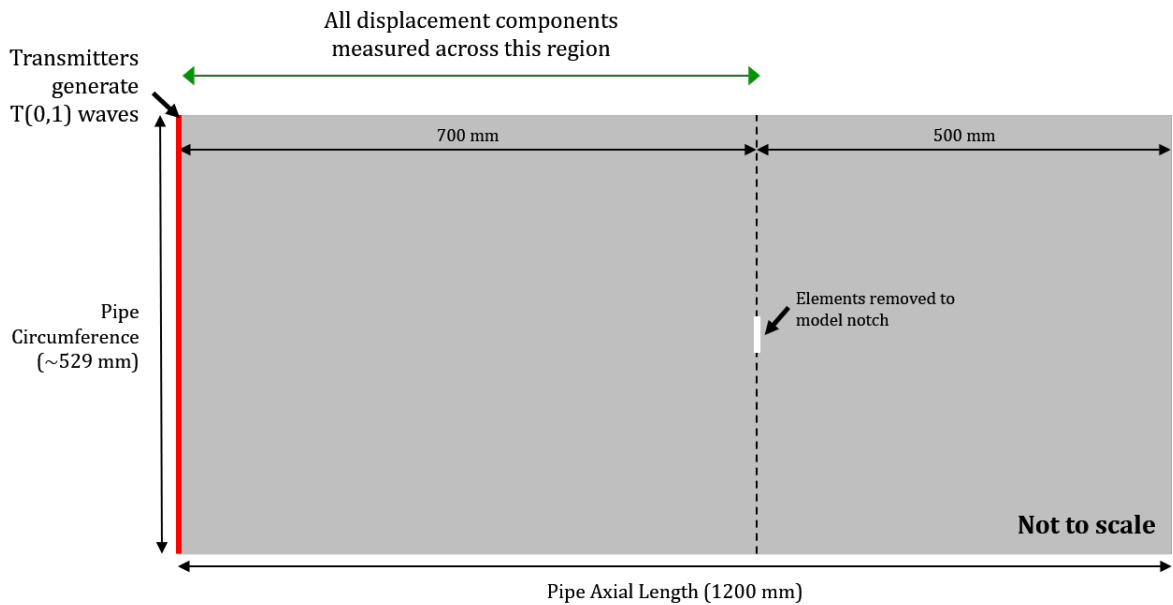


Figure 4.12: Schematic of the FE model displayed as an unrolled pipe which was used to generate data for the synthetic dataset.

used for explicit time-domain FE modelling of wave propagation [66]. The pipe material properties used were non-attenuative mild steel with a density of 7900 kg/m^3 , Young's modulus of 207 GPa and Poisson's ratio of 0.27. The mesh size used in the simulations

was 0.2 mm and the shortest SH0 wavelength of 27 mm was considered, which adheres to the general rule of more than 10-30 elements per wavelength [64]. Fig. 4.13 shows an example of the FE simulated residual time trace from a single receiver transducer located with the same transducer standoff distance as the experiment. The synthetic damage

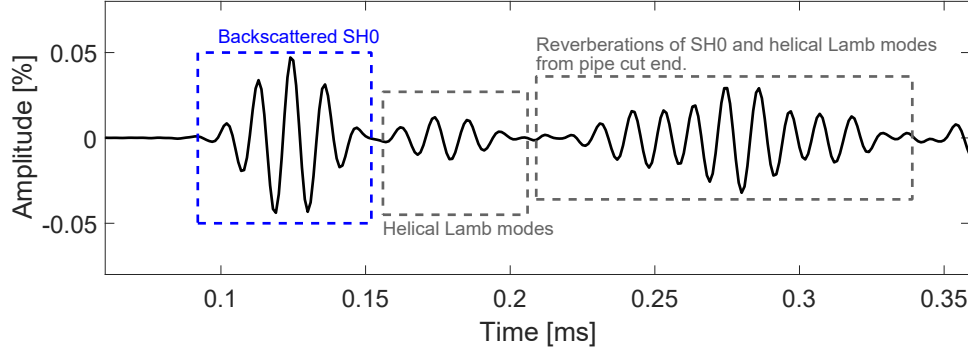


Figure 4.13: Synthetic defect signal generated from finite element simulations of the interaction of an incident plane SH0 wave with a 1 mm deep and 2 mm wide notch for a single receiver transducer located 150 mm away from the crack.

signals were produced by superposing the predicted defect reflections such as those of Fig. 4.13 with the experimentally measured signals with no damage such as those of Fig. 4.7(a). The synthetic dataset simulating a defect was then processed in the same way as the corresponding experimental data to give the reflection ratio. As mentioned in Section 4.1, it is of interest to investigate the improvements in SNR that can be obtained by averaging across a number of receiver transducers. Both experimental and synthetic values of R for multiple receiving transducers connected in parallel can be computed by summing the signals from the desired number of receiving transducers located around the pipe, the sum being taken over an equal number of transducers clockwise and anticlockwise from the defect location; in practical application where the circumferential location of the defect is unknown, signals formed from the sum of signals from the chosen number of transducers centred at each circumferential location around the pipe in turn would be considered. Fig. 4.14 shows the mean and standard deviation of the reflection ratios R , which were computed by summing the signals from different numbers of transducers and subtracting the mean baseline signal computed from the same number of transducers for both the experimental signals, with and without the first defect step, and the synthetic signals produced by adding the predicted defect reflection to the baseline signals. The

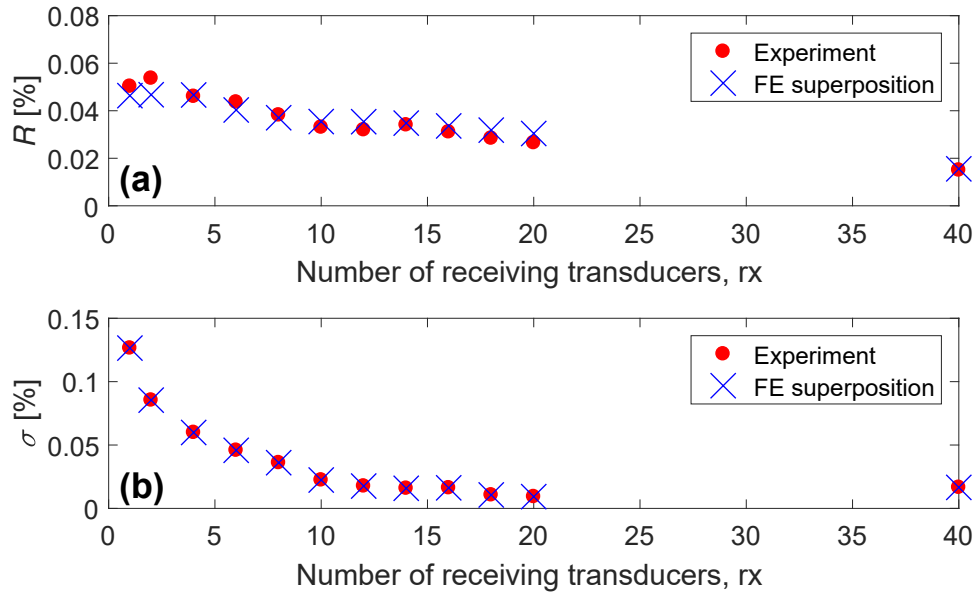


Figure 4.14: Plot of (a) means and (b) standard deviations of the reflection ratios R computed experimentally (circle markers) and from the FE superposition method (cross markers) as a function of the number of receiving transducers for a 1 mm deep and 2 mm wide mm notch.

good agreement between the synthetic and experimental data validates the use of the superposition approach to generate reflection ratios; this will be used below to predict the performance of the system for defects at different locations along the pipe. As a result, the performance of the monitoring system can be evaluated at different axial distances from the defect.

4.3.2 Receiver operating characteristic curves

The results from the previous section show that variations in the residual signal still persist despite the application of temperature compensation procedures and the absence of defect growth. These random changes manifest as scatter in the measured values of R about a mean value. Since the results above show that R can be modelled by a normal distribution, we can generate the probability distributions for R before and after the introduction of a defect, which enables the evaluation of the ROC curve for detecting a step of a certain size, based on a limited number of measurement samples and number of receiving transducers rx . In this study the total sample length is defined as $2L$ with L samples before and after the introduction of a step. In NDE and SHM, the ROC curve plots the probability of detection (POD) versus the probability of false alarm (PFA). The

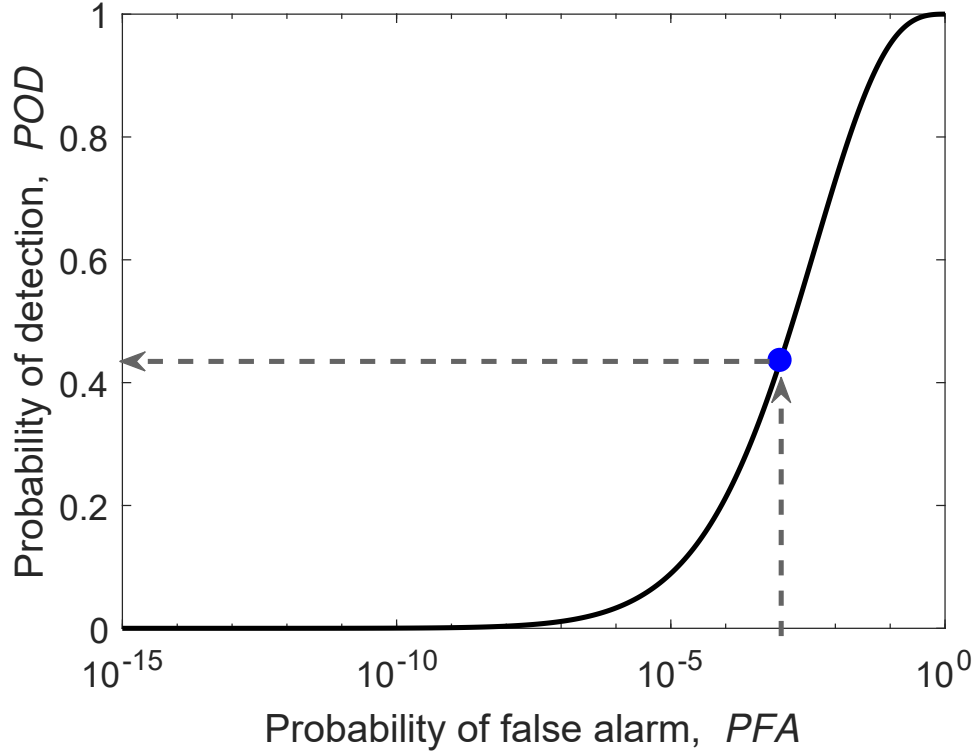


Figure 4.15: Receiver operating characteristic (ROC) curve computed from the probability distributions of R from a single receiving transducer at a sample length $L = 50$ at a transducer standoff distance of one pipe diameter from a 1 mm x 2 mm notch, with x-axis displayed logarithmically. The ROC can be used to estimate the POD for a given false alarm rate.

definitions of POD and PFA used in this study were adapted from the work by Rummel and Matzkanin [106]. A perfect detection system would give a curve that goes through point (0,1), indicating 100% POD with no false alarms. In contrast, a random guess system will generate a 45° diagonal line on a linear plot (with equal x and y axes), which means that the POD and PFA are equal. The ROC curve is generated by sweeping a threshold across the probability distributions of R and calculating the corresponding POD and PFA values. Fig. 4.15 shows an example of the ROC curve for a sample length L of 50 points with the defect introduced after a further L points, so the total sample length is $2L$. This data was measured from a single receiver transducer ($rx=1$) located at a distance of one pipe diameter away ($r=OD$) from a 1 mm deep by 2 mm wide notch, with the x-axis displayed on a log scale.

4.3.3 Effect of number of receiver transducers monitoring at multiple points around circumference

As mentioned in the previous section, summing up the signals from multiple receivers around the pipe, or increasing the receiver coverage around the pipe circumference, can yield varying crack detection performance. Figure 4.16 shows the B-scan image generated from FE baseline subtracted guided wave data measured from 40 equally spaced receiving transducer around the circumference of a pipe at a standoff distance of 150 mm from a 1 mm deep and 2 mm wide mm notch. It is shown that the width of the high-energy backscattered SH0 pulse is about 37.5% of the pipe circumference. Figure 4.17 shows the

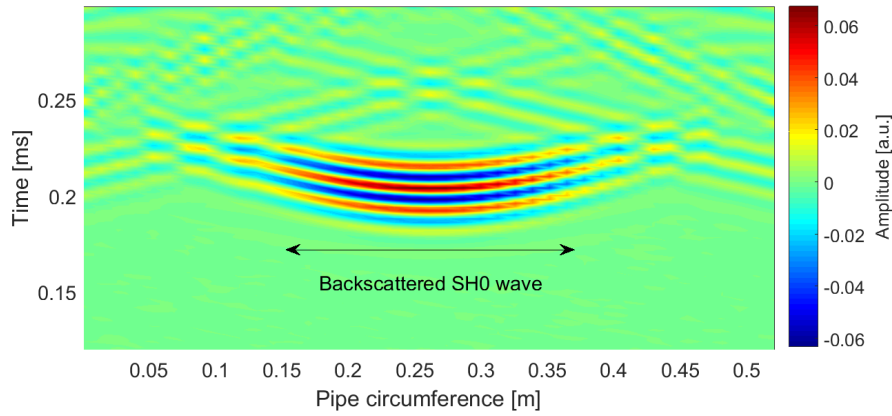


Figure 4.16: B-scan image generated from synthetic baseline subtracted guided wave data measured from 40 equally spaced receiving transducer around the circumference of a pipe at a standoff distance of 150 mm from a 1 mm deep and 2 mm wide mm notch.

ROC curves for different numbers of receiving transducers r_x for a fixed sample length $L=10$ at a transducer standoff distance of one pipe diameter away from the defect ($r=OD$). Using the ROC curves, the POD for a given PFA of 0.1% can be plotted as a function of receiver coverage, expressed as a percentage of the pipe circumference, as shown in Fig. 4.18. This figure shows that as the number of receiving transducers increases from a single transducer, the performance improves, providing maximum performance at 37.5% receiver coverage, which is equivalent to 15 receiving transducers at the circumferential spacing of 10 mm employed in the ring of Fig. 4.6. This is roughly equivalent to the circumferential extent of the main lobe of the reflection from the defect (see Fig. 4.4) at this standoff. A receiver coverage of 100% is equivalent to summing up the signals from the maximum

number of 40 equally-spaced piezoelectric transducers around the transducer solid ring as shown in Fig. 4.6. This is labeled as the T(0,1) case because it is equivalent to the measurement of the T(0,1) response in conventional guided wave screening techniques.

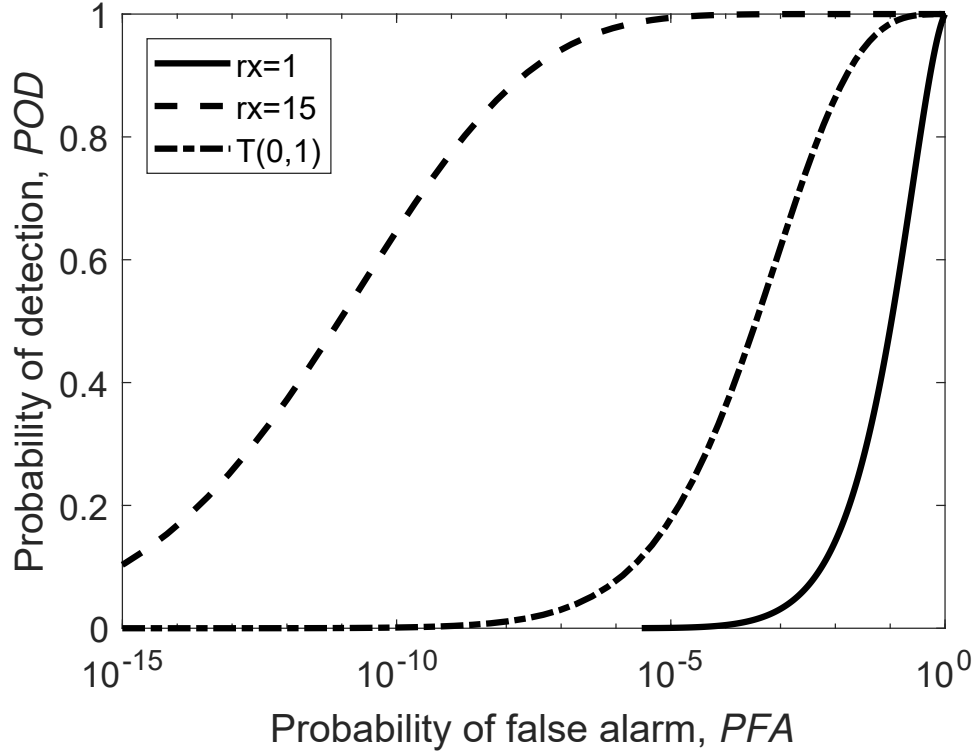


Figure 4.17: The ROC curves from different number of receiving transducers at a sample length of $L = 10$, at a transducer standoff distance of one pipe diameter from the defect ($r=OD$).

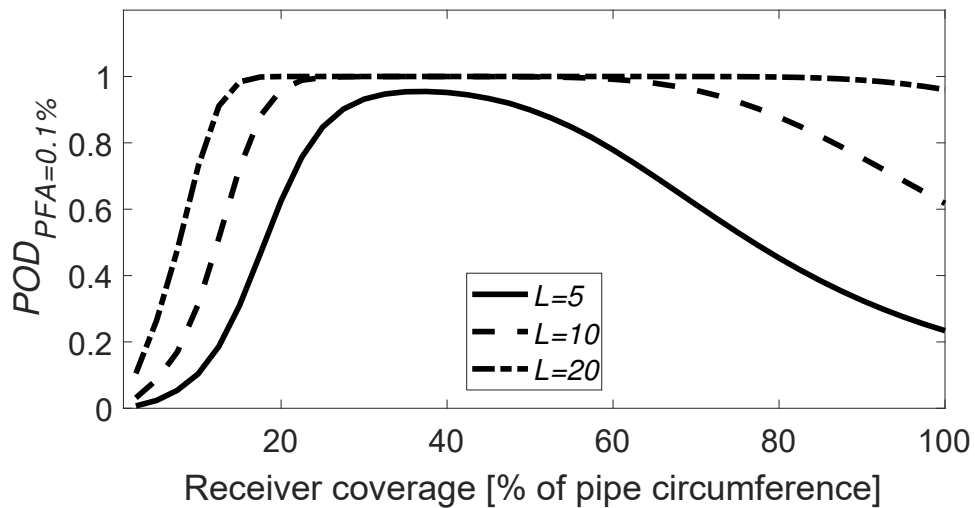


Figure 4.18: Probability-of-detection POD for a false alarm rate of 0.1% as a function of receiver coverage expressed as a percentage of the pipe circumference, for different sample lengths L , at a transducer standoff distance r of one pipe diameter away ($r=OD$).

Although a single receiver yields a higher measured amplitude of SH0 waves in comparison to the T(0,1) case, the performance is relatively poor because the noise generated from uncompensated EOC effects is high, yielding a low SNR. The sum of the signals from an optimum number of receiving transducers for a given transducer configuration and pipe geometry would perform better than both the T(0,1) and single receiver transducer cases, because it would capture a relatively higher portion of the backscattered SH0 waves, as illustrated in Fig. 4.4, while reducing the noise level via an averaging effect across this sum of transducers.

4.3.4 Effects of sampling length

Figure 4.19 shows the ROC curves at different values of L , for a single receiving transducer located one pipe diameter away from the defect ($r=OD$), the total sample length being $2L$. Again, the POD for a given PFA of 0.1% can be plotted as a function of L to yield Fig. 4.20. This figure shows that as the number of measurement samples L increases, the performance of the system improves. The distribution of the measured values of the

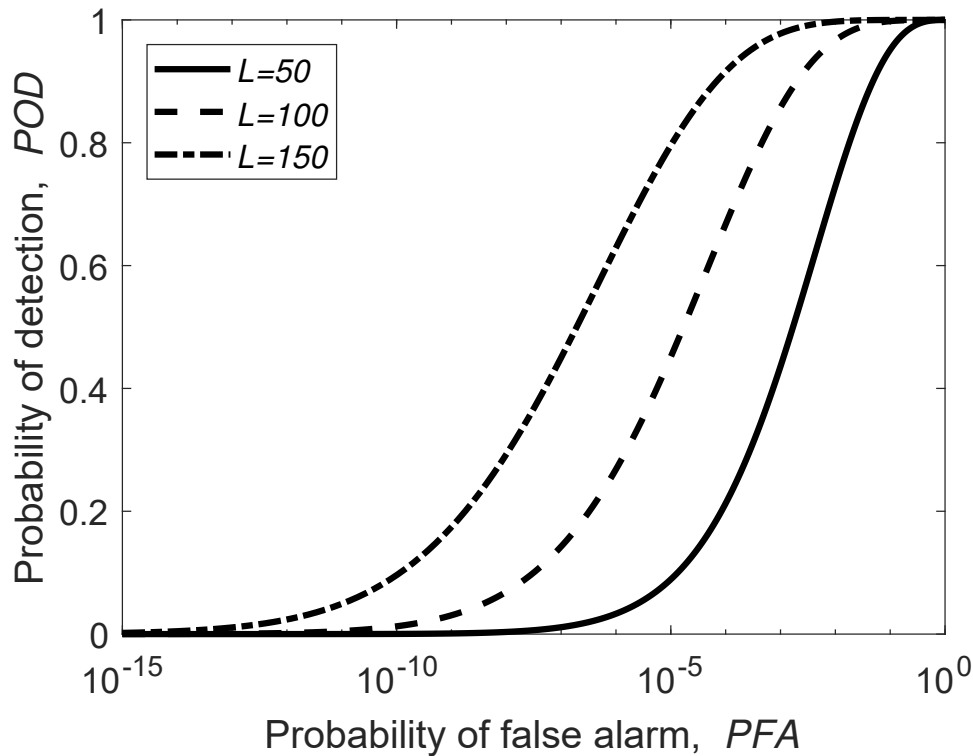


Figure 4.19: ROC curves for different sample lengths, for a single receiving transducer ($rx=1$) at a transducer standoff distance of one pipe diameter away ($r=OD$).

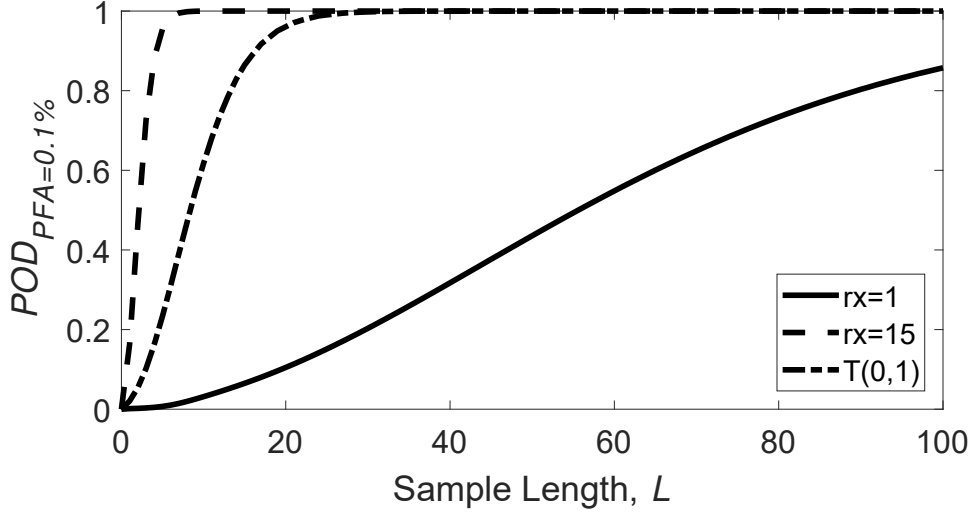


Figure 4.20: POD at PFA of 0.1% as a function of sample length from different number of receiving transducers at a transducer standoff distance of one pipe diameter ($r=OD$).

reflection ratio R can be modelled by a normal distribution whose population mean R_μ provides a good estimate of the true reflection ratio. Consequently, the accuracy of the R_μ estimate increases with $\sqrt{L}$ [107].

4.3.5 Effects of transducer standoff distance

Figure 4.21 shows the ROC curves at different transducer standoff distances r , from a single receiver transducer ($rx=1$) at a sample length $L=5$. This figure shows that the performance decreases with increasing standoff distance because the scattered SH0 amplitude decreases with distance from the defect by a factor of $\sqrt{r}$ due to the effect of beam divergence [4]. Figure 4.22 shows the POD at a PFA of 0.1% for a single receiver transducer as a function of the distance of the defect from the transducer ring r obtained from the set of calculations used to plot Figure 4.21. Figure 4.22 also shows the corresponding results for the T(0,1) mode, where all transducers are used, and for 15 receiving transducers. At distances of above 2.5 pipe diameters, the T(0,1) mode outperforms the 15 transducers. By considering all possible numbers of transducers it is possible to define an optimum performance at any given distance. The improvement in the performance of the system becomes negligible and converges to the T(0,1) case using all transducers after a distance of around three pipe diameters, which is approximately the circumferential length of the pipe; beyond this, reflected energy following helical paths around the pipe increasingly

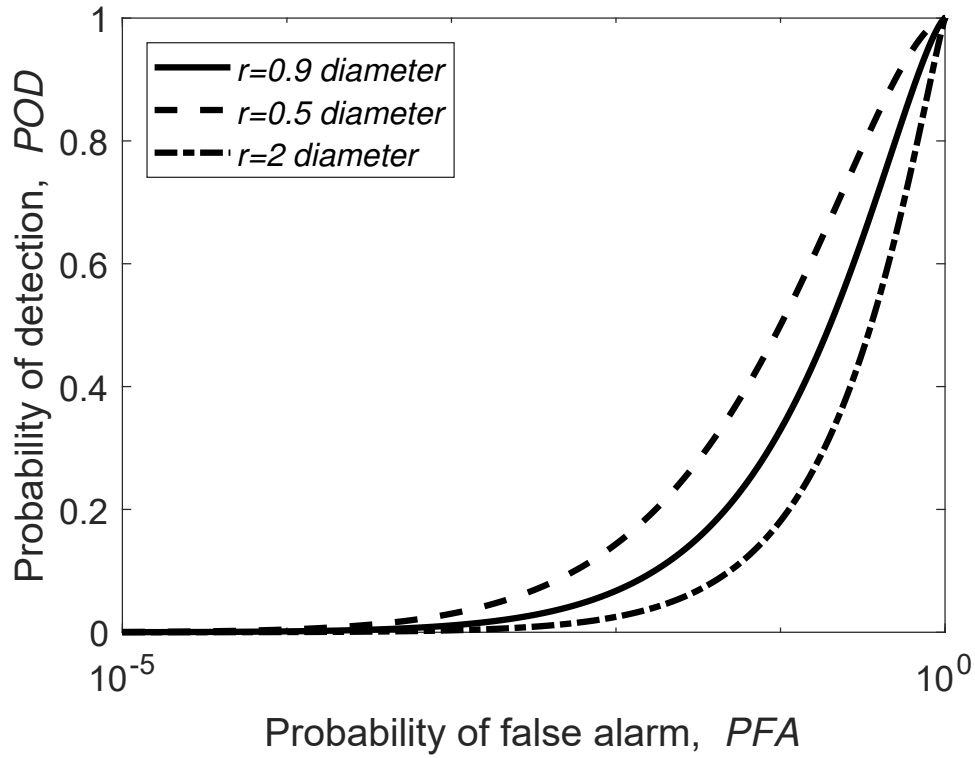


Figure 4.21: ROC curves for different transducer standoff distances, from a single receiver transducer ($rx=1$) at a sample length $L=5$. The solid line reflects the standoff distance used in the experiment.

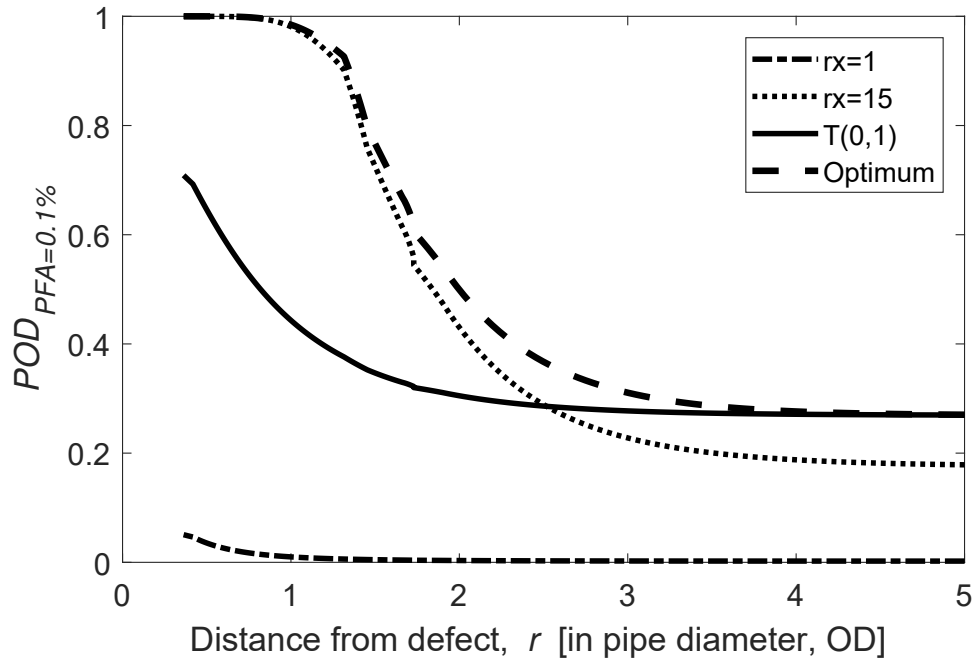


Figure 4.22: POD for a false alarm rate of 0.1% from different number of receiving transducers at a sample length $L=5$, as a function of transducer stand-off distance r .

combines to form the pipe guided wave modes including $T(0,1)$. In principle it is possible to obtain a specified performance at any distance and with any number of transducers simply by varying the sample length L . Figure 4.23 shows the required sample length for 95% POD and 0.1% PFA for different numbers of transducers and different distances. As expected from Figure 4.22, a very large number of samples is required for the single transducer case. This study has considered step changes in defect size as simple averaging

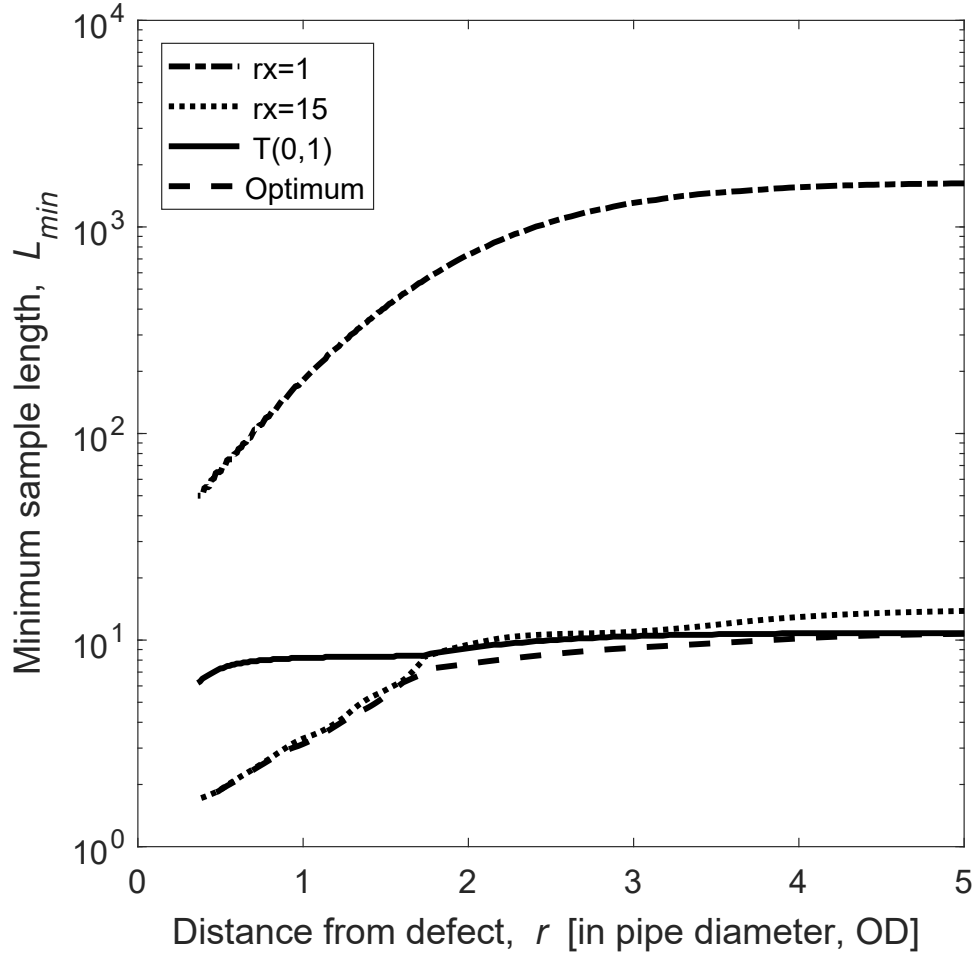


Figure 4.23: Minimum sample length, L_{min} , required to achieve 95% POD at a 0.1% false alarm rate against transducer standoff distance r , for different number of receiving transducers rx , with the y-axis displayed logarithmically.

of signals obtained at different times is satisfactory. In practice, defects are likely to grow gradually so rather than assuming a constant reflection over the data length, it would be appropriate to fit the data to a curve; provided the wall loss proceeds at the same function of time over the period considered, the accuracy of the fit will increase with the number of points, as in Figure 4.23. In practice, it is likely to be satisfactory simply to represent

the wall loss as a piecewise linear function of time so that a simple linear fit is done over a given measurement window.

4.3.6 Addressing phase cancellation in signals from multiple receivers

Since we are operating below the SH1 cut-off frequency with a short transducer standoff distance from the crack (approximately 4 wavelengths apart), the error attributed to phase cancellation during signal summation was modest; the phase difference between two adjacent transducers was 0.6° . However, the phase difference increases to 12° between the first and fifth transducers around the ring, which corresponds to 25% of the pipe circumference. The error attributed to phase differences would increase as the signals from a larger number of receivers are summed; a lower inspection frequency reduces this error. Alternatively, imaging algorithms typically used in high frequency ultrasonic inspection, such as the common source method (CSM) can be applied. Similar to our existing setup of transmission of the T(0,1) or plane SH0 wave, the CSM in phased array ultrasonics involves transmission of a plane bulk wave by transmitting from all transducer elements simultaneously and involves creation of an image from the scattered data by evaluating the time-of-flight between the scatterer and the receivers [108–112]. The application of CSM on a similar experimental pipe setup as this work has been investigated by previous authors [61, 113, 114], for the purpose of characterising pipe defects using shear guided wave transducers with standoff distances of more than one pipe circumference from the defect. In their work, the spatial phase shifts undergone by the multiple backscattered pipe guided wave modes can be accounted for by backpropagating the modes along the axial distance of the pipe.

The intensity of the CSM image, $I(\mathbf{r})$, formed at any point of the scanned area is given by:

$$I(\mathbf{r}) = \left| \sum_{j=1}^N h_j \left(\frac{\mathbf{r} \cdot \mathbf{Y} + |\mathbf{r} - \mathbf{r}_R^j|}{c} \right) \right|, \quad (4.2)$$

where N is the number of elements, h_j is the Hilbert transform of the temporal signal

received by receiver j , c is the wave velocity, $\mathbf{r}$ is the position vector of the image point, $\mathbf{r}_R^j$ is the position vector of the j^{th} receiving transducer and $\mathbf{Y}$ is the unit vector in the pipe axial direction. In our case, an unrolled pipe is assumed so $\mathbf{r}$ is the position vector in the 2D plane of an unrolled pipe. Figures 4.24(a-b) show the CSM images constructed from

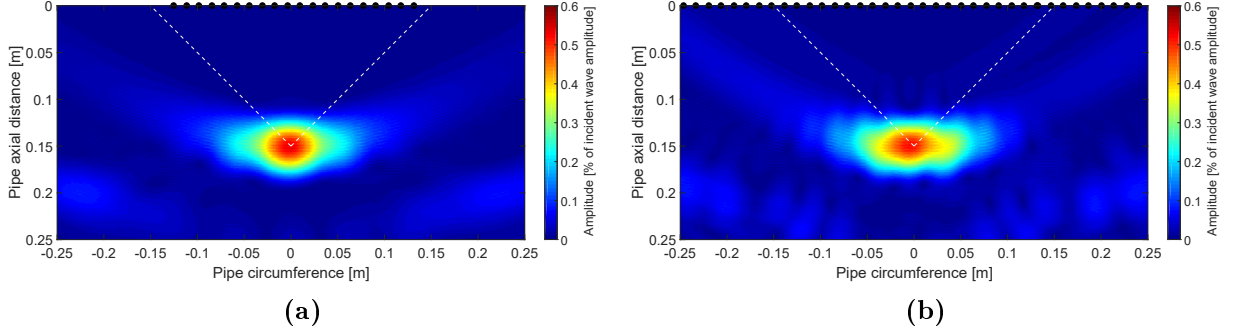


Figure 4.24: Images of the unrolled pipe with a 2 mm x 1 mm notch constructed using the common source method (CSM), from the baseline subtracted FE simulated signals measured using (a) 20 and (b) 40 receiving transducers. The image colour scale is displayed as a percentage of the incident wave amplitude. The region of high intensity in these images is attributed to the scattered SH0 by the notch in the pipe located 150 mm from the receiving transducers which are labelled by the black dots. The white line represents the 90 degree arc from the scatterer.

the baseline subtracted FE simulated signals measured using both 20 and 40 receiving transducers that are circumferentially centred on a notch, respectively on a pipe model described in Fig. 4.12, where the region of high colour intensity is caused by the scattering from the 2 mm wide and 1 mm deep notch. The configuration given in Fig. 4.24(a) reflects the experimental configuration described in Fig. 4.6 where individual signals from only 20 receivers were collected. Figure 4.25 shows how the maximum amplitude of the CSM image varies with the number of equally spaced receiving transducers circumferentially centred on a notch located 150 mm away, so that the receivers lie within the 90 degree arc of the scatterer; 20 receiving transducers yields the maximum amplitude with little improvement or poorer results beyond this as the backscattered SH0 wave intensity diminishes when $\theta > 45^\circ$ and $\theta < -45^\circ$. Both these figures suggest that the maximum amplitude at each pixel of the image can be achieved by using the number of receiving transducers that optimises the coverage of a 90 degree arc from the pixel where a shear horizontal wave scatterer can be found; the scattered wave field from a surface breaking crack is shown in Fig. 4.4.

As mentioned previously, the partial number of receivers configuration chosen in the

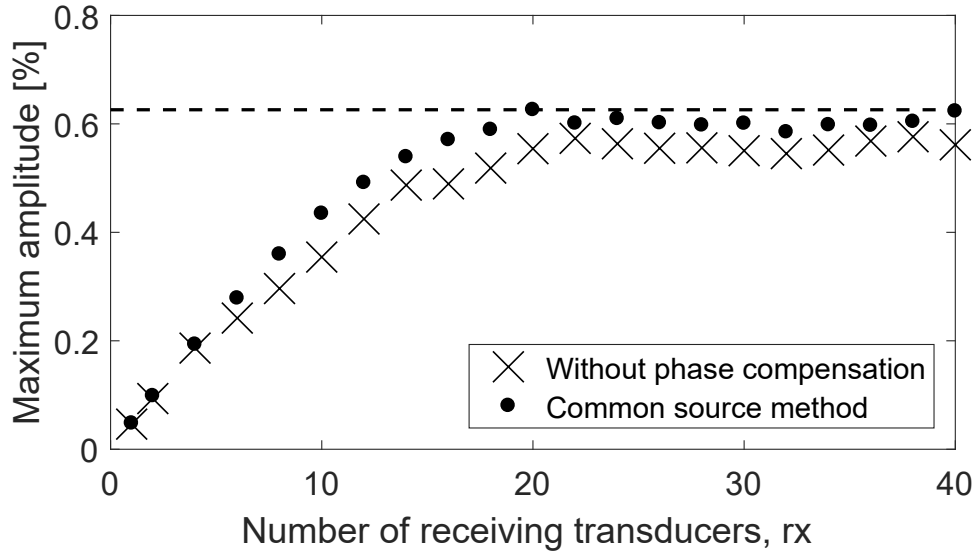


Figure 4.25: Plot of the maximum backscattered amplitude before and after phase compensation and expressed a percentage of the incident wave amplitude against the number of receiving transducers used to form the CSM image. The horizontal dashed line represents the maximum amplitude achieved by using 20 receiving transducers.

CSM image shown in Fig. 4.24(a) is currently not optimised for detecting defects that are outside the zone between the white dashed lines and the receivers. An optimised solution for maximising the detection of shear horizontal waves at each pixel can be obtained by applying Eq. 4.2 only across the receivers which are within a 90 degree arc of the pixel, as illustrated by Fig. 4.26. This technique of limiting the angle of wave propagation in the image formation, which effectively varies the aperture size as a function of a specified angle, has been applied in a previous work [115]. The intensity value at the crack location can be compared to the reflection ratio R calculated previously in Fig. 4.14 by dividing it by the number of receivers used to form the image. The ratio of R (obtained from the CSM images) to the standard deviations from experimental measurements σ (see Fig. 4.14) are plotted against number of receiving transducers as shown in Fig. 4.27, and compared to the similar values calculated from summing the FE simulated signals from the receivers without phase compensation. The results in Fig. 4.27 show that the phase compensation of the backscattered SH0 wave signal across multiple receiving transducers can be compensated through the use of the CSM algorithm to yield higher reflection ratios but that the error is negligible below about 5 receiving transducers as predicted in Fig. 4.27. Figure 4.28(a-b) shows the CSM images constructed using a single

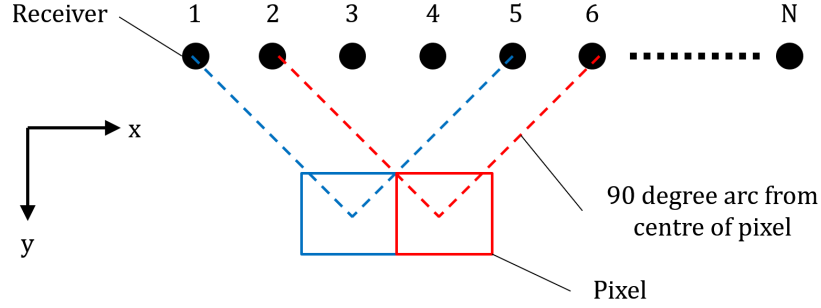


Figure 4.26: Schematic diagram illustrating the layout of the proposed CSM configuration in an unrolled pipe that provides full coverage of the pipe with maximum intensity from shear horizontal wave scatterer; each pixel is calculated only by summing signals from receivers within a 90 degree arc from the pixel centre.

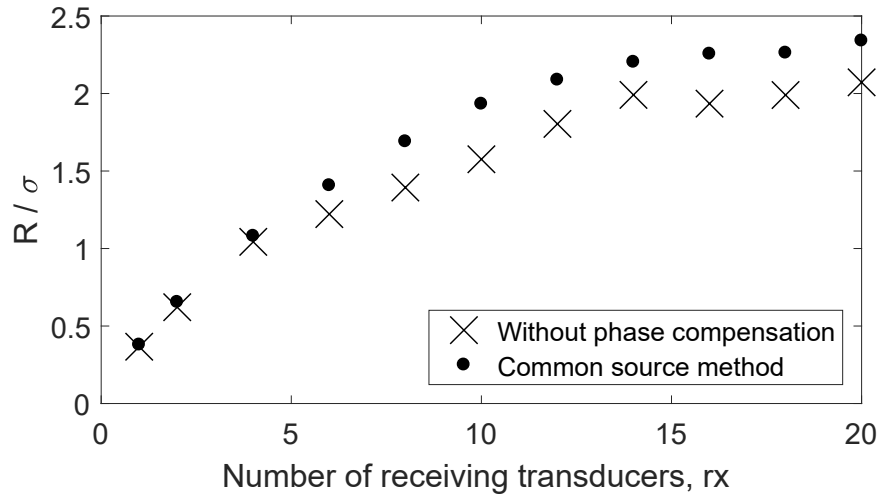


Figure 4.27: Plot of R/σ computed from the baseline subtracted FE signals with and without phase compensation (using CSM) as a function of the number of receiving transducers for a 1 mm deep and 2 mm wide notch. A higher R/σ value leads to an increased performance in signal change detection during structural health monitoring and hence a higher probability of detection for a given false alarm rate.

4. Monitoring Crack Growth Using SH0 Guided Waves

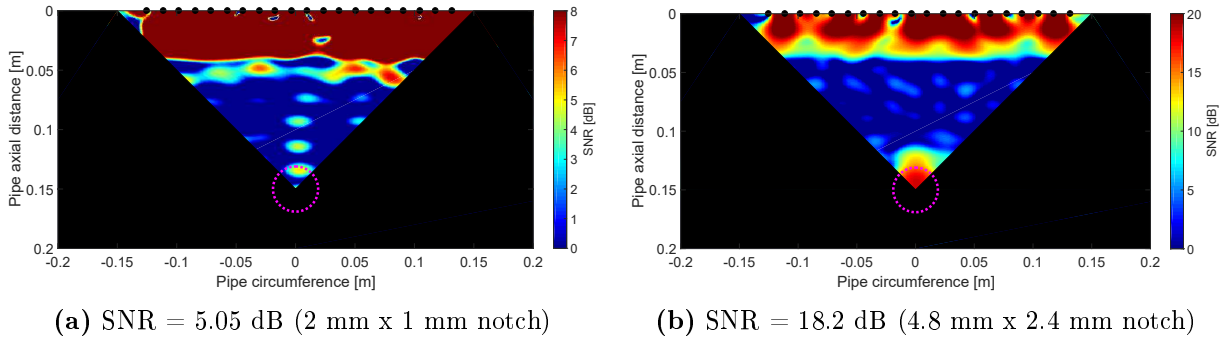


Figure 4.28: Images of the unrolled pipe with a notch of dimensions (a) 2 mm x 1 mm and (b) 4.8 mm x 2.4 mm (length x depth) constructed using the common source method (CSM), from a single measurement set of baseline subtracted residual signals measured using 20 receiving transducers (black dots) from the experiment. The image colour scale is displayed in decibels with a different scale used in both subplots. The position of the notch in the pipe is indicated by the centre of the magenta dashed circle with a diameter of the incident wavelength; the maximum SNR within this circle was calculated and presented in the subplot captions.

set of experimentally collected baseline subtracted residual signal during first and fifth stages of the experiment, which corresponds to the notch lengths and depths of 2 mm x 1 mm and 4.8 mm x 2.4 mm respectively. The black region in the images are masks that have been applied to discount regions outside the 90 degree arc centred on the location of interest; in the proposed scheme these would be interrogated with a different set of receivers. As expected, the figure shows that the larger notch size is visibly easier to detect than the smaller notch due to the higher signal-to-noise (SNR) ratio; the noise level can probably be reduced by taking the average of the CSM images formed using more than one set of experimentally collected baseline subtracted residual signals, but this has not yet been investigated. In summary, there is a potential to further improve the probability of detection of cracks using SH0 in a pipe by incorporating plane wave imaging algorithms, such as CSM, to address the issue with phase cancellations when using multiple signals collected from an array of transducers; future work could be carried out to investigate the performance of the imaging algorithm when all the transducers are individually addressable, enabling the scheme of Fig. 4.26 and also under more realistic circumstances such as for a tilted crack or for a crack near a weld.

4.4 Summary

This chapter investigated the performance of the fundamental shear horizontal (SH0) guided wave mode, at frequencies just below the higher order mode cut offs, for crack growth monitoring at critical locations. Baseline subtraction with temperature compensation was applied to experimental data generated by a ring of transducers on a 6-inch diameter pipe. It was found that the residual signals after baseline subtraction were normally distributed so the random fluctuations could be reduced by coherent averaging. This made it possible to reliably detect a 1 mm deep and 2 mm wide notch located one pipe diameter along the pipe from the transducer ring; this corresponds to 0.065% cross-section loss. These experimental results were also shown to be in good agreement with numerical and analytical findings predicted from the previous chapter.

The damage detection performance at different locations along the pipe was assessed by analysing receiver operating characteristic curves (ROC) generated by adding simulated defects to multiple experimental measurements without damage. At a fixed standoff distance, the damage detection performance increases with the square root of the number of signals averaged, and is also improved by averaging the signals received by transducers covering the main lobe of the reflection from the defect; plane wave imaging methods such as the common source method (CSM) can be used to address the issue with phase cancellations when using multiple signals collected from an array of transducers. Therefore, an SH0 mode monitoring system has great potential for crack monitoring in pipes and other applications because full circumferential coverage can be obtained using a modest number of transducers and the transduction system can be located within a pipe circumference away from the region of interest. This is likely to be attractive in, for example, monitoring crack development at welds which is explored in the next chapter.

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Chapter 5

Crack Monitoring Near Welds

5.1 Introduction

The previous chapter of this thesis demonstrated that a simulated crack can be detected in a featureless pipe, by using SH0 guided wave sensors consisting a ring of piezoelectric transducers in a monitoring configuration. This ring of sensors around the pipe circumference is used to transmit shear guided waves; summing up the signals from each sensor allows the calculation of the T(0,1) response. If the signal-to-coherent noise ratio from a standard guided wave sensor were to improve significantly, then this simulated crack could have been detected by a one-off inspection. In practice however, cracks can also be present in the heat-affected zone near welds [116] or other features, which makes detection of small defects from, for example one-off guided wave (GW) inspections, virtually impossible because the reflection from the crack is superimposed on the larger reflection from the weld or other feature. For example, the T(0,1) reflection amplitude ratio at 100 kHz for a notch with dimensions of 20 mm $\times$ 2 mm $\times$ 0.5 mm (circumferential length $\times$ depth $\times$ axial length) and a girth weld with dimensions of 2 mm $\times$ 12 mm (cap height $\times$ cap length) in an 8-inch schedule 40 pipe are 0.2% and 20% respectively [40]. Ideally, this change can be detected by employing a monitoring configuration and a baseline subtraction technique, but obtaining a stable baseline component can be challenging because it is strongly dependent on the environmental and operational conditions (EOC), which affect the performance of transducers and properties of the test specimen, including the

behaviour of the GW reflector [117]. Temperature has been shown to have a large effect on both the guided wave propagation speed and the transducer performance change [118], which will need to be compensated for. Therefore transducer stability and signal processing methods that compensate for EOC changes are crucial for reliable detection in monitoring.

Piezoelectric transducers that are adhesively bonded to a test specimen is an example of a contact-based method for guided wave monitoring in the industry. However, previous work has shown that the bonding adhesive, or other bonding agents, can degrade over a long period of time when exposed to cyclic thermal loading which results in baseline instability and drift [119]. In contrast to this method, electromagnetic acoustic transducers (EMATs) are generally regarded as contactless transducers as they do not require mechanical coupling to the surface structure. Although EMATs do not require the use of adhesives, they too are susceptible to transducer instability as recent work has shown that the contact between EMAT and a test specimen is also important; different contact materials can lead to different long-term stability behaviour [120]. Herdovics & Cegla recognised that compensation for changes in both transducer phase response and wave propagation speed are necessary for reliable low frequency (around 25 kHz) torsional guided wave monitoring in steel pipes, and developed an iterative-based compensation method that uses only the ultrasonic signal itself to minimise the effects of temperature [121]. The authors demonstrated that this method yields significant improvement over conventional scale-base transformation, but does not completely remove temperature dependent noise at features.

Overall, the biggest advantage of using EMATs is the ease of deployment and installation. In addition, EMATs could be beneficial in high temperature applications, where there is no need to maintain physical coupling to the specimen, in contrast to long term issues arising from solid coupling techniques for piezoelectric transducers [122]. Lorentz force-based EMATs, such as periodic permanent magnet (PPM) EMATs, can be used to generate shear waves in ferromagnetic, steel plate-like structures [123] and pipes [124]. PPM EMATs are made of an array of magnets having alternating magnetisation directions, that is normal to the sample surface and with straight wires that carry the driving current between the magnet array and the sample, as illustrated in Fig. 5.1 [125, 126].

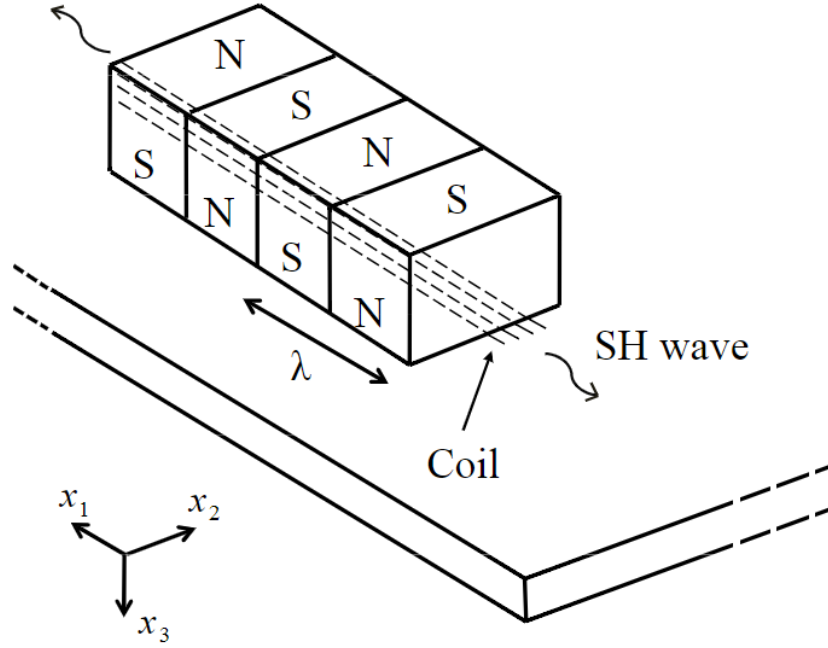


Figure 5.1: Schematic diagram of a periodic permanent magnet (PPM), with the wave propagation directions and the polarity of the magnets and coils shown [6].

The work reported in this chapter extends on this idea by using a pair of EMATs in a pitch catch configuration for the purpose of crack monitoring. Firstly, the effects of temperature cycling on transducer stability are explored. The next section outlines the findings of simulating a crack near a weld. The conclusions are outlined in the final section.

5.2 Temperature-cycling of EMATs

This section investigates the stability of monitoring a weld using SH0 EMATs over several temperature cycles. Initially, the experimental setup is described, followed by the details on the temperature compensation method and finally, the system stability is assessed by evaluating how the baseline subtracted residual reflection ratio R changes with measurement number.

5.2.1 Experimental setup

The experimental setup used to collect guided wave signals during the temperature cycling study is shown in Fig. 5.2. It comprises a 1.2 m long, 6-inch nominal bore steel pipe (NPS6

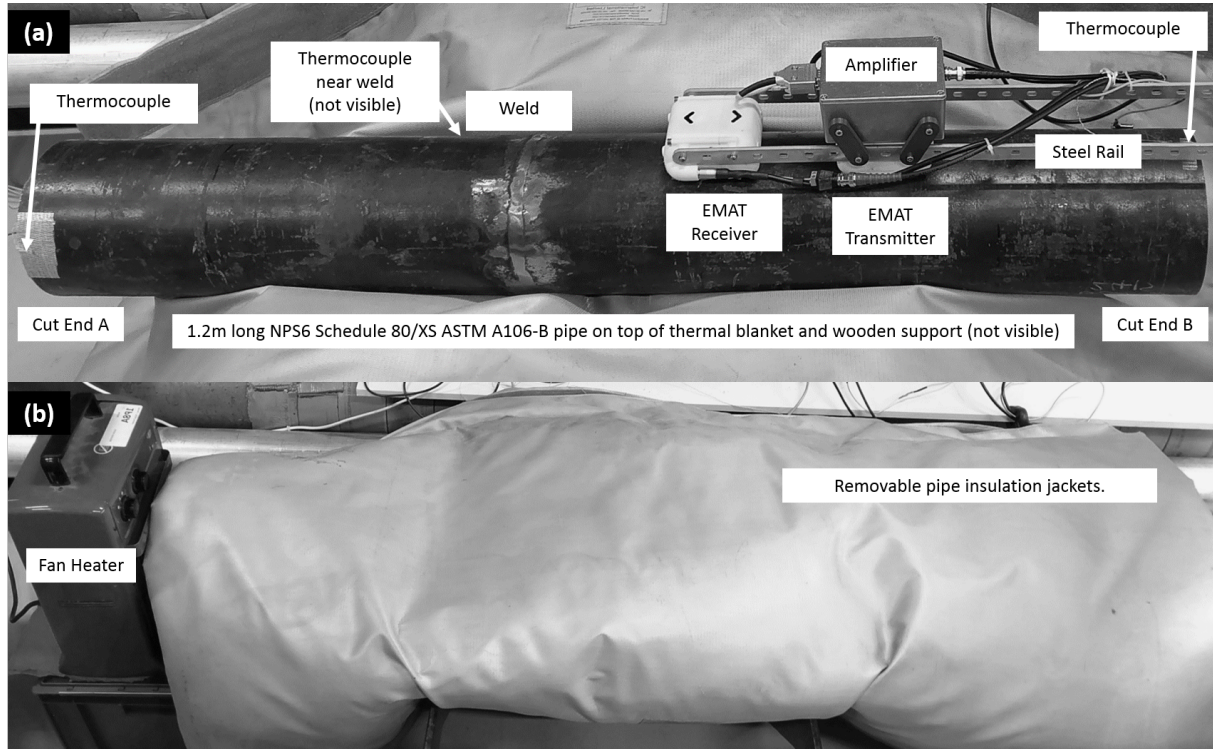


Figure 5.2: Photo of the experiment setup during temperature cycling program, (a) before and (b) after it was wrapped with pipe insulation jackets.

Schedule 80/XS ASTM A106-B) with an 11 mm thick wall and an outer diameter (OD) of 168.28 mm that is supported on steel V-blocks lined with plywood, which have soft surfaces to minimise guided wave reflections from the supports [102]. Figure 5.3 shows the Lorentz force based EMAT used in this study, which comprises a Neodymium-iron-boron magnets (N42 grade) magnet stack and current-carrying coil (not shown). A pair of this PPM EMATs was placed on the pipe and programmed to operate in a pitch-catch configuration; one transducer to transmit and the other to receive shear horizontal (SH) waves. Both EMATs were placed on the same side of the weld, which allowed the transmitted incident wave and the weld reflection to be measured and monitored. The transducers are circumferentially aligned and kept at a fixed distance from each other by a pair of custom-made steel rails. A schematic which describes the experimental layout in an unrolled pipe format is given in Fig. 5.4. A Handyscope HS5 waveform generator with

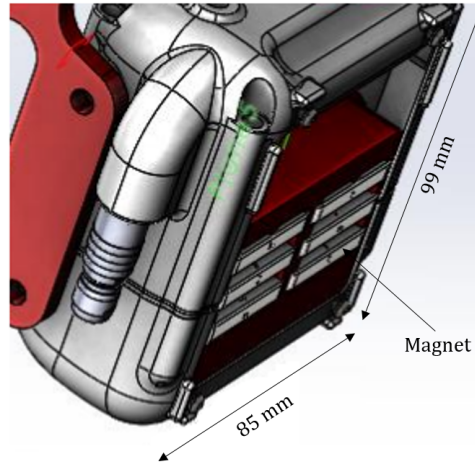


Figure 5.3: Rendered image of the PPM EMAT used in this study (current-carrying coil not shown). The transducer was designed and custom-built by Guided Ultrasonics Ltd.

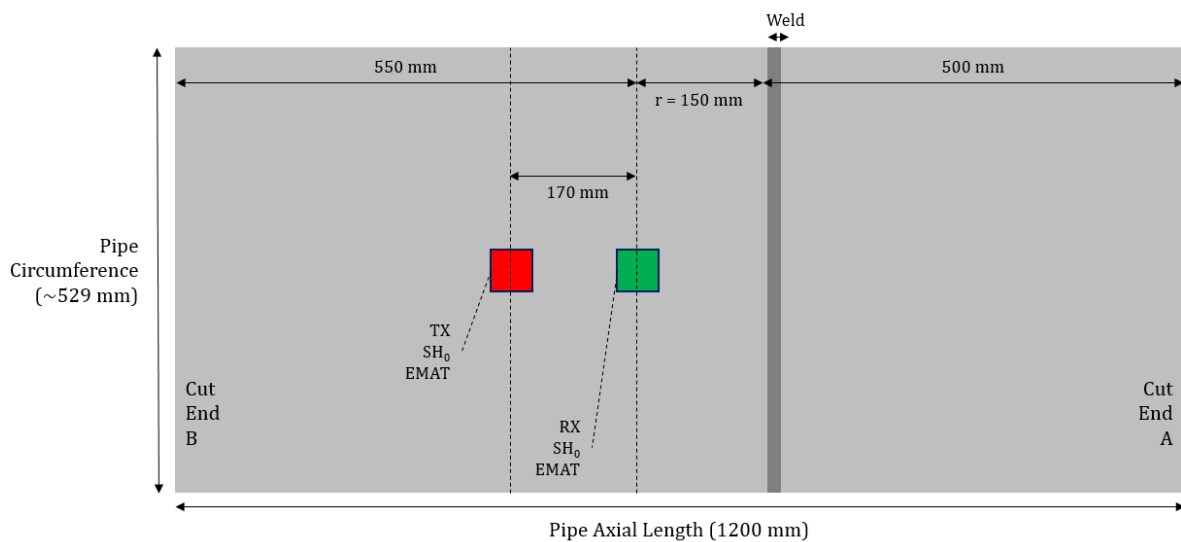


Figure 5.4: Unrolled pipe schematic of the experiment layout.

transmit-receive amplifiers was used to generate a 10-cycle Hann windowed wave with a 150 kHz centre frequency. The structure was exposed to 10 heating cycles over a period of 3 weeks, in order to mimic environmental changes. A commercially available 1 kW fan heater was used to heat up the pipe by channelling hot air through the middle of the pipe. In each heating cycle, the pipe was heated to 40°C and signals were acquired during the cooling stage at 2 minutes interval. The temperature measurements were recorded with 3 thermocouples located at the weld and each end of the pipe, as shown in Fig. 5.2.

5.2.2 Guided wave signals

An example of an experimentally acquired guided wave signal after application of a band-pass filter, is shown in Fig. 5.5. The incident SH0 wave and the SH0 reflections from

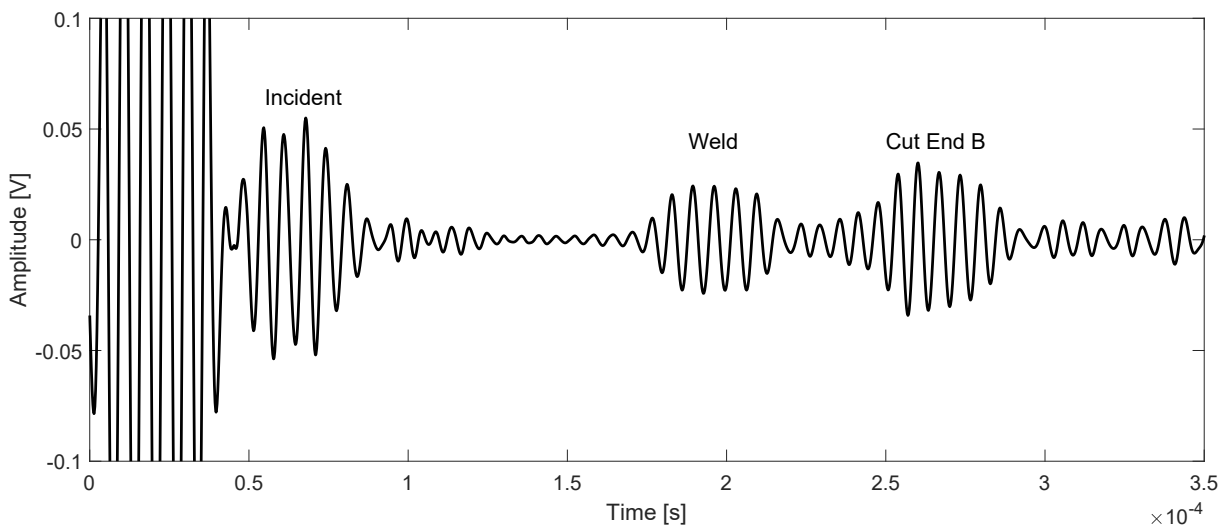


Figure 5.5: Ultrasonic signals acquired on a pipe after application of a band pass filter.

the weld and Cut End B from Fig. 5.2, are labelled in Fig. 5.5. It is worth noting that that reflection from the girth weld from this experimental setup is significantly higher ($R = 65\%$) than typically obtained from conventional guided wave inspection of pipelines (around 20%). This is because this girth weld was poorly prepared and manufactured by a non-coded welder; destructive testing was performed on the pipe specimen after the experiments, where it was discovered that the weld penetration was only about 3 mm of the 11 mm wall thickness which manifested itself as a large guided wave reflector. The larger than usual weld reflection makes it a more difficult case than with a good weld, as

defect detection involves measuring a small change in a very large reflection; this further emphasises the advantages of monitoring over one-off inspection, which is affected by poor signal-to-coherent noise ratio.

5.2.3 Temperature compensation

In the previous chapter, we used the combination of the baseline subtraction technique, trending the maximum residual within the location of interest in the time domain and averaging this maximum residual trend across multiple measurement numbers in order to detect the defects. However, it has been shown that EMATs that have not been designed and built specifically for long term monitoring, can have drift effects that effectively causes the baseline component of the signal to vary with time [120]. Consequently, this will cause errors to be present when calculating the baseline subtracted residual after a significant period of time has passed. Herdovics and Cegla [121] developed an iterative method to process signals from their EMAT array which involves estimating the stretch factor and phase shift corrections that minimises the residual error. The estimated parameters were calculated by using the incident wave signal as a reference, which could be measured in their array setup which was in a pitch-catch configuration. The authors showed that their compensation method reduced the coherent noise in their residual signals by up to 20 dB, in comparison with wave-speed compensation methods only. Recently, Mariani et. al [127] developed the location specific temperature compensation (LSTC) method, which works by compensating for temperature induced noise at a specific location in the time domain signal by developing a location specific calibration function. Generally, the LSTC method comprises both calibration and monitoring stages. The calibration stage involves generating a calibration curve that relates an environmental or operational parameter, for example the pipe temperature, to the amplitude of a signal at a specific location. In the case of a guided wave monitoring system, the data for this curve are obtained from guided wave baseline measurements of a structure with a known state across the temperature range of interest. In the monitoring stage, the residual signal amplitude at a specific location is computed for each measurement number of the monitoring dataset. Then, the amplitude value predicted by the curve computed for that location and temperature

is subtracted from the residual signal amplitude. This yields a so-called temperature compensated residual signal, which can be analysed to detect if there has been any damage at that specific location.

Since the EMAT configuration allowed measurements of the incident wave or initially transmitted signal, we have selected the incident wave as a reference signal for compensation of the behaviour of the weld signal with measurement number. Alternatively, the reflection signal from Cut End B in the opposite direction from the weld, as shown and labelled in Fig. 5.2 and Fig. 5.5, can be used as a reference signal. However, this is not practical in the field because in-service pipelines usually do not have a cut end. Figures 5.6(a-b) show one example of the same guided wave signals collected from the receiver EMAT shown in Fig. 5.4 where the incident and weld signals have been windowed respectively across a fixed time interval; the Fourier transforms for these signals are then computed. The magnitude and phase of the centre frequency component of the Fourier transforms are plotted against the measurement number in Fig. 5.6(c-d) and Fig. 5.6(e-f) respectively; the same plot of pipe temperature against measurement number is given in both Fig. 5.6(g-h) for convenience in the observation of temperature dependence behaviour. Evidently, Fig. 5.6(c-f) show a strong temperature dependence of both centre frequency magnitude and phase angle of the incident wave and weld reflection signals. From a temperature change of 20°C to 40°C, the change in phase of the centre frequency component for both the incident wave and weld reflection signals are approximately 23° and 43° respectively due to changes in phase velocity with temperature; the weld signal exhibits a larger phase change because of a higher propagation distance, which indicates a change in the velocity of the wave in the pipe. From this point onwards, we are interested in how the magnitude at the centre frequency of the weld signal behaves across the measurement numbers. The LSTC method has been adapted and used in this study, specifically by building calibration curves that relate pipe temperature to the weld magnitude data, by firstly estimating a 2nd order polynomial curve of best fit for the magnitude against temperature data collected during the first temperature cycle, which lies between measurement number 184 to 526, as shown in Fig. 5.7(a). This magnitude-temperature curve of best fit is used to temperature compensate the magnitude of the weld (magenta line in Fig. 5.8), which yields the blue line in Fig. 5.8(a). This process was then repeated for the

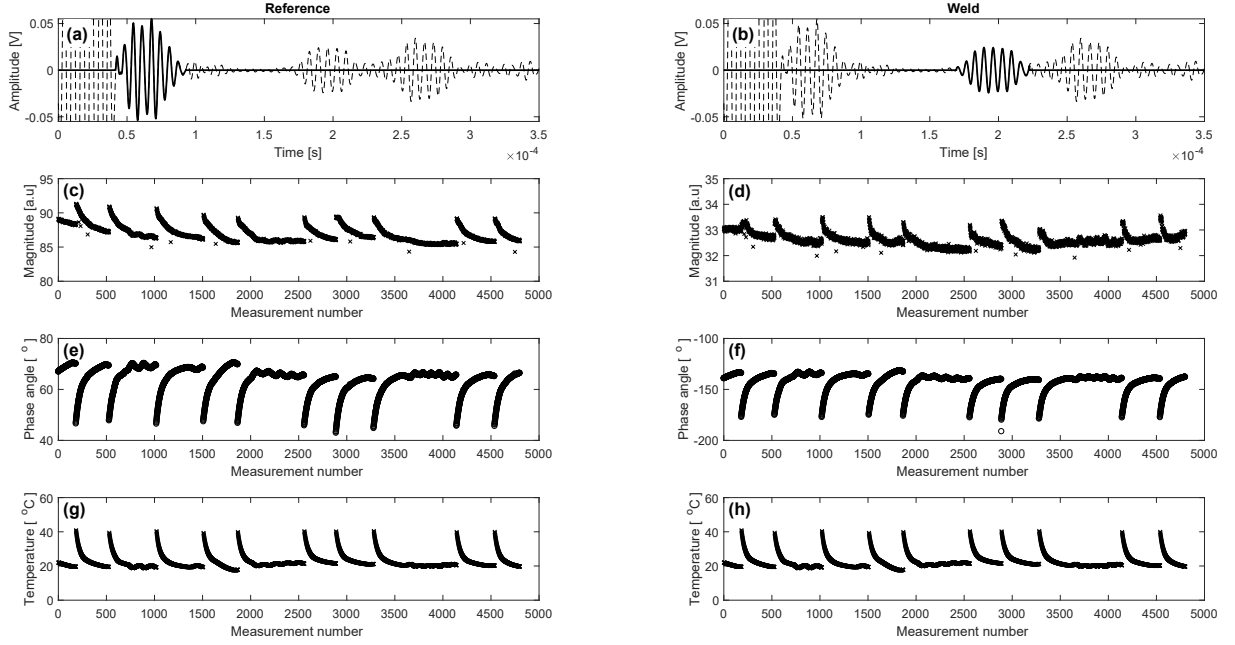


Figure 5.6: A single guided wave time trace (dashed line) and the windowed signal (solid line) for the (a) incident wave and (b) weld reflection. Fast Fourier transformation was used to calculate the centre frequency (c-d) magnitudes and (e-f) phase angle for the incident wave and weld reflection respectively. The pipe temperature (g-h) is shown to be a significant factor that affects both magnitude and phase.

temperature compensation of the magnitude of the incident wave, as shown in Fig. 5.8(b). An additional calibration curve is introduced to relate the incident wave amplitude to the weld reflection amplitude since the generated incident wave amplitude in this experiment was not constant across all measurement numbers. This was performed by plotting the temperature-compensated magnitudes of the weld and incident wave against each other, as shown in Fig. 5.7(c), and estimating a 2nd order polynomial curve of best fit. This weld-incident temperature-compensated magnitude curve of best fit is then used to obtain the temperature-compensated and normalised weld magnitude at the centre frequency, as shown by the black line in Fig. 5.8(a). Finally, the baseline subtracted residual amplitude ratio R plot can be created by subtracting the compensated magnitude of the weld from its first value and expressing it as a percentage of the cut end magnitude, which we assume is a 100% guided wave signal reflector, as shown in Fig. 5.9. Fig. 5.10 summarises the workflow used to calculate the compensated values of R . Fig. 5.9 shows that the effects of temperature on R has largely been compensated for and the data is relatively stable up to the fifth temperature cycle; the value of R displays variations beyond this

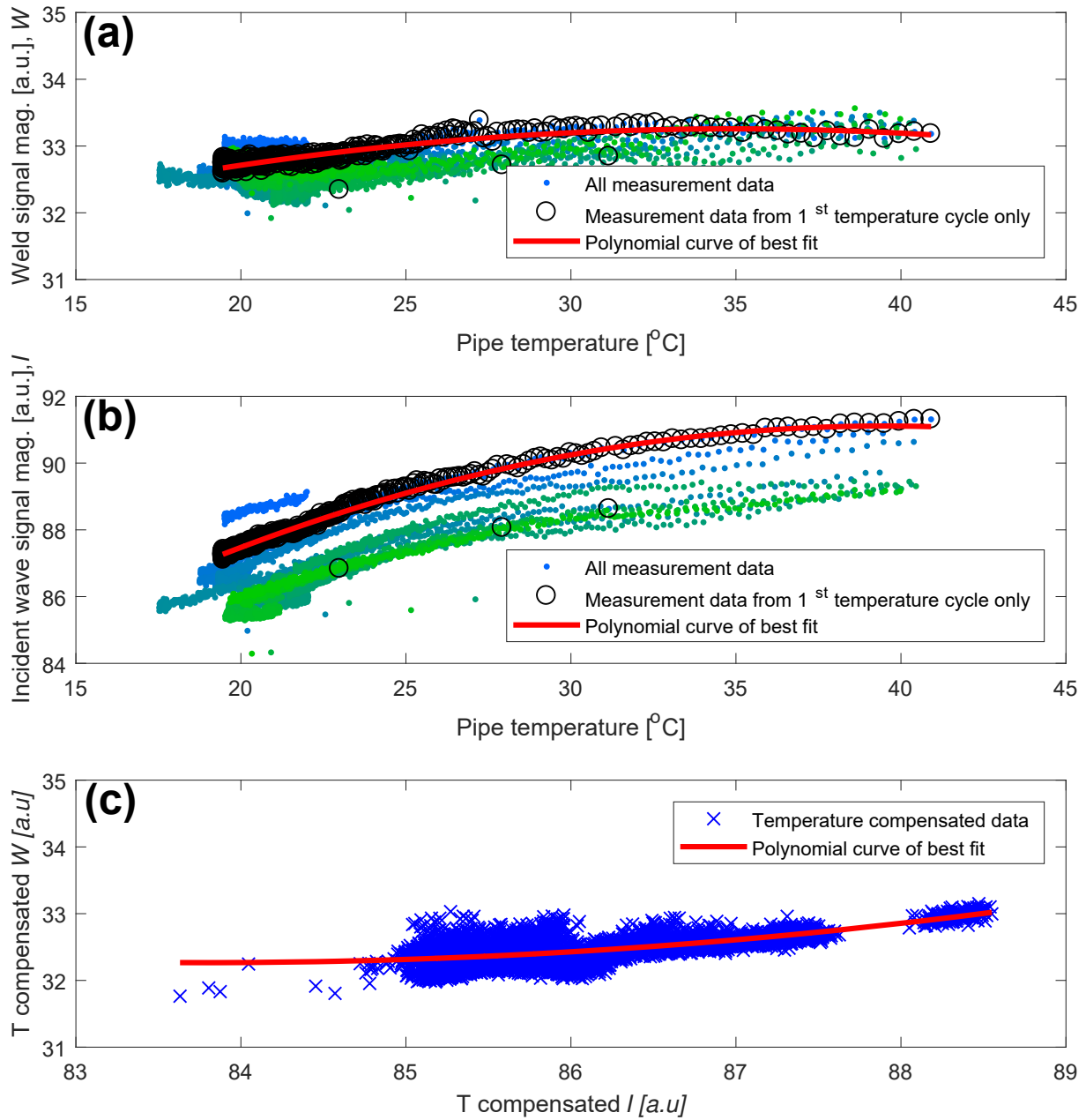


Figure 5.7: Plot of the raw magnitude at the centre frequency of the (a) weld signal and (b) incident wave signal against pipe temperature. The colour of the dot markers changes from blue to green to indicate measurements sorted in chronological order. In both (a) and (b), temperature calibration curves of best fit were estimated using the data from the first temperature cycle (measurement number 184 to 526); there are 3 anomalous data points (black circles). (c) Plot of temperature-compensated magnitudes at the centre frequency for the weld signal against the incident wave with an estimated curve of best fit (red line).

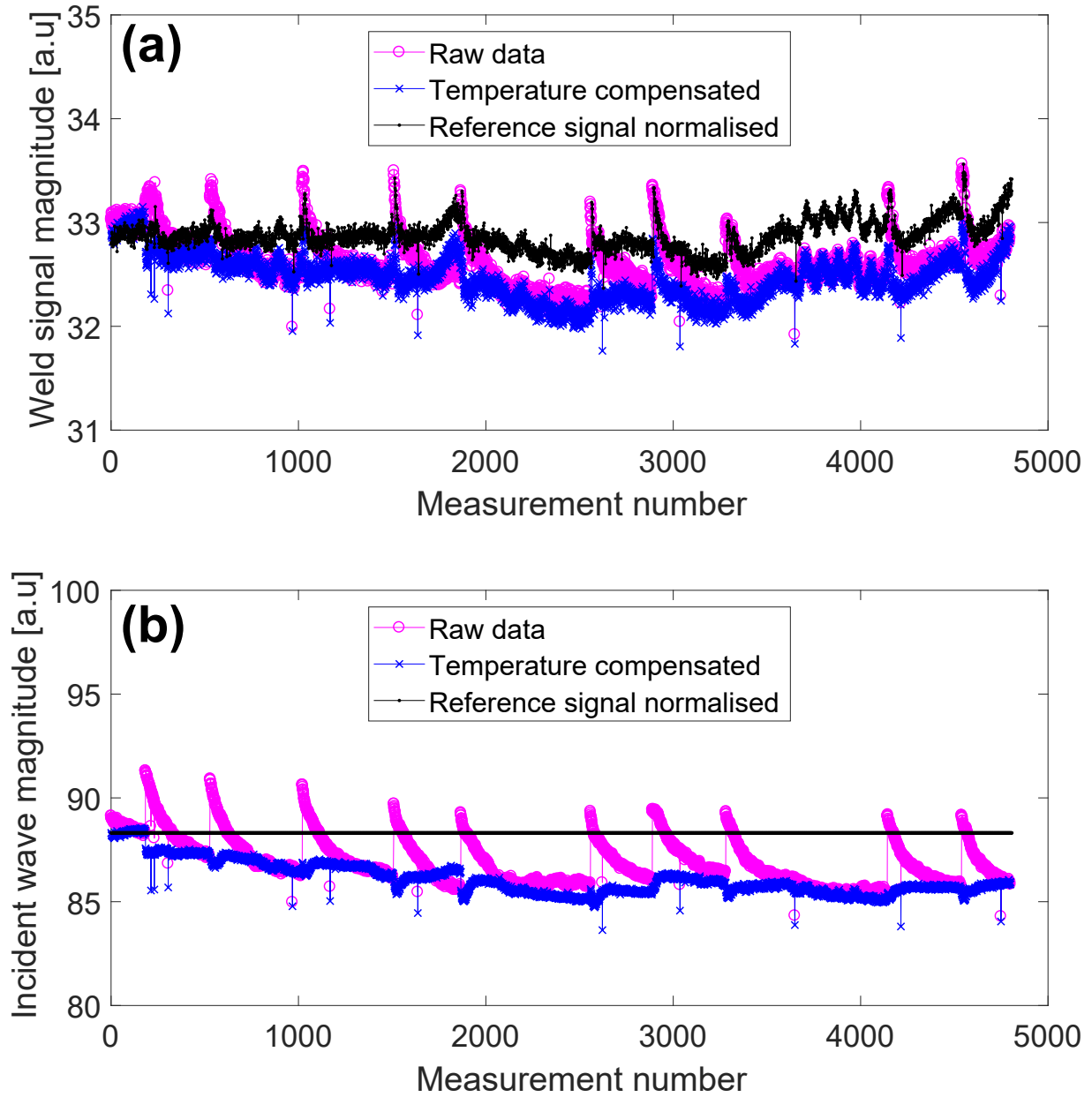


Figure 5.8: The centre frequency magnitude plot against measurement number for the (a) weld signal and (b) incident wave or reference signal during the temperature cycling study. The raw, temperature compensated and reference signal normalised magnitudes are described by the magenta, blue and black lines respectively.

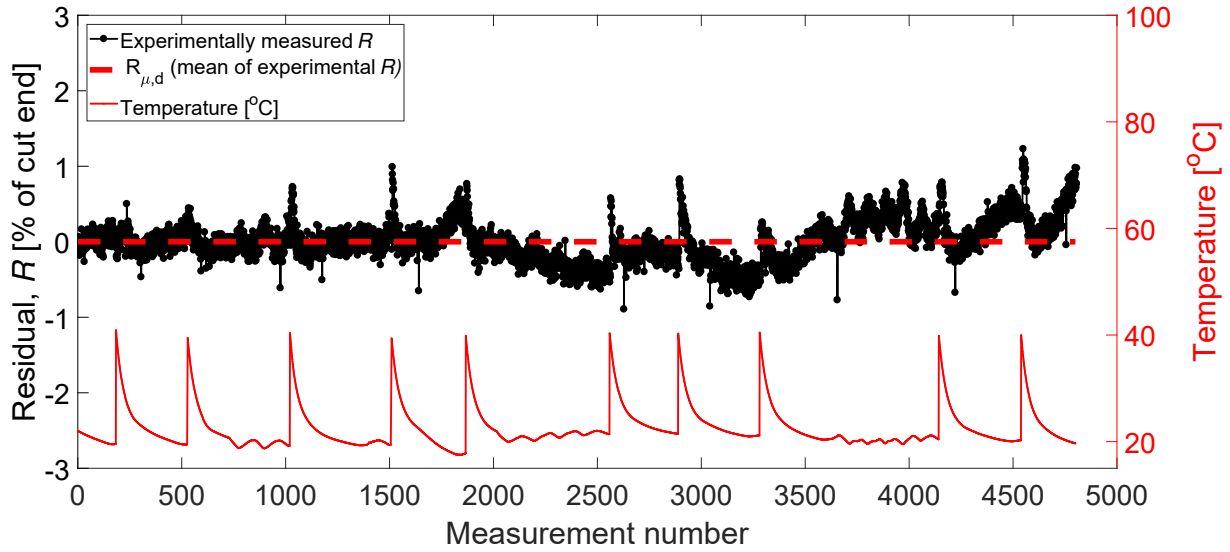


Figure 5.9: The change in magnitude of the weld signal at the centre frequency, expressed in baseline subtracted residual reflection ratio R , against measurement number during the temperature cycling study against measurement number.

measurement number. The variation of R is caused by uncompensated changes to the behaviour of the EMAT system, which manifest as drift in the magnitudes of the weld and incident wave signals as shown in Fig. 5.7(a-b). This drift effect was also observed in a previous study with EMATs that excited guided wave via the Lorentz force mechanism, with drifts of up to 5% in reflection ratio over 3 months, despite compensation of the signal propagation speed, amplitude and phase [121]. The variation is thought to be caused by an opposite force generated on the transducer, due to the coil current, which influences the displacement of the coil on the pipe surface [121]. The authors showed that the vibration could be isolated by installing a silicone interlayer between the pipe and the EMAT, which decoupled the transducer from the pipe; an experiment over several heating cycles showed significant improvement in signal stability compared to a setup without the silicone interlayer [121]. However, the EMAT system available during the time of study did not have this option present.

The distribution of the values of R across all the measurement samples was investigated by applying the Lilliefors test for normality [105], where the yielded test statistic value was found to be less than the respective critical value required for a normal distribution to be assumed at a significance level of 5%; the histogram of this distribution can be found in Fig. 5.11(a). Similarly, both the distributions of R up till and beyond the

(1) The incident wave and weld signals within the measured guided wave signal are time-windowed and their Fourier transforms are computed.

(2) Plot the magnitudes of the Fourier transforms at the centre frequency against pipe temperature. A curve of best fit is estimated across the first temperature cycle (red curve).

(3) Using the temperature calibration curves, plot the magnitudes of weld signal against magnitudes of incident wave signal at a reference temperature of 20°C and estimate a curve of best fit.

(4) Using the incident wave calibration curve, calculate the magnitudes of the weld signal at a reference temperature of 20°C. Then, we subtract these by the magnitude of the weld signal at the first measurement number to yield a baseline subtracted residual R . Finally, we plot R against measurement numbers, t .

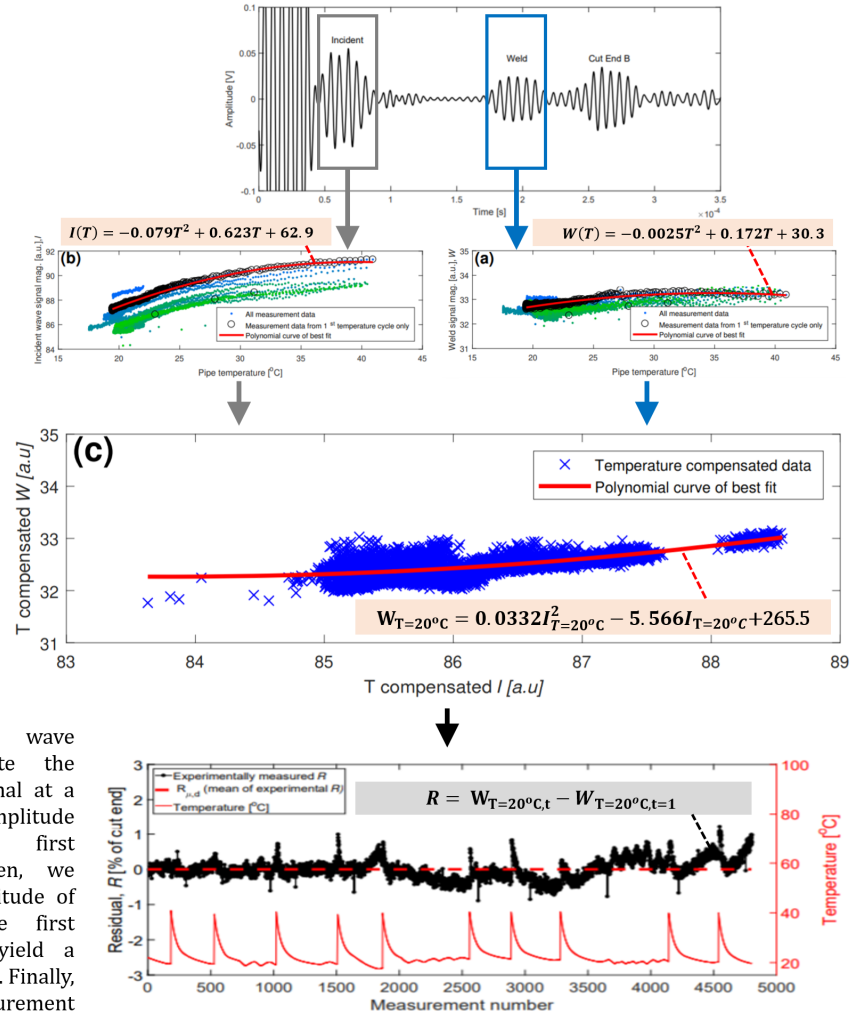


Figure 5.10: Workflow used in this chapter to calculate the baseline subtracted residual at the weld. Step (1) identifies the incident and weld reflection signal within the ultrasonic data. Step (2) shows the temperature calibration curves for the incident and weld signals. Step (3) is the incident wave calibration curve for the weld reflection. Step (4) is the subtracted residual amplitude at different measurement numbers.

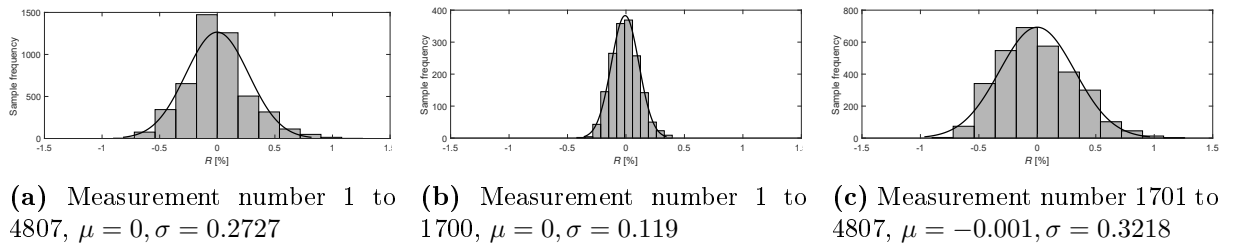


Figure 5.11: Histogram of experimentally measured baseline subtracted residual R for measurement numbers (a) 1 to 4807, (b) 1 to 1700 and (c) 1701 to 4807.

fifth temperature cycle are shown in Fig. 5.11(b-c) and were tested for normality using the same statistical test which concluded that both distributions can be assumed to be normally distributed. In summary, the measured values of R over 10 temperature cycles can be assumed to be normally distributed with a mean value of 0. However, there is evidently some effects occurring that are not fully compensated which led to the variations in R that increased the standard deviation of R beyond the fifth temperature cycle, thus reducing the defect detection performance of the monitoring system. It is predicted that adding a silicone layer between the EMAT coil and the pipe specimen could increase the stability of the EMAT system and eliminate low frequency variations, however this has not yet been investigated.

5.3 Crack growth near weld

This section investigates the performance of using SH0 for monitoring crack growth near a weld. Initially, the experimental setup is described, followed by the performance detection expressed as baseline subtracted residual reflection ratio R changes with measurement number.

5.3.1 Experimental setup

The experimental setup used to collect guided wave signals during the defect growth study is shown in Fig. 5.12. It comprises a 1.2 m long, 6-inch nominal bore steel pipe (NPS6 Schedule 80/XS ASTM A106-B) with an 11 mm thick wall and an outer diameter (OD) of 168.28 mm that is supported on steel V-blocks lined with plywood. A crack was simulated on the pipe at approximately 150 mm from the transducer, by progressively milling a notch at the same predefined location using a milling cutter tool with a diameter of 1 mm, that is equivalent to the axial length of the notch. Similar to the previous chapter, the experiment was setup on the bed of a computer numerical control (CNC) machine and secured in place with aluminium clamps lined with cardboard, so that the defect could be grown in stages whilst ensuring that the milling tool returned to the same location for each increment. Guided wave reflections from the supports are minimised by

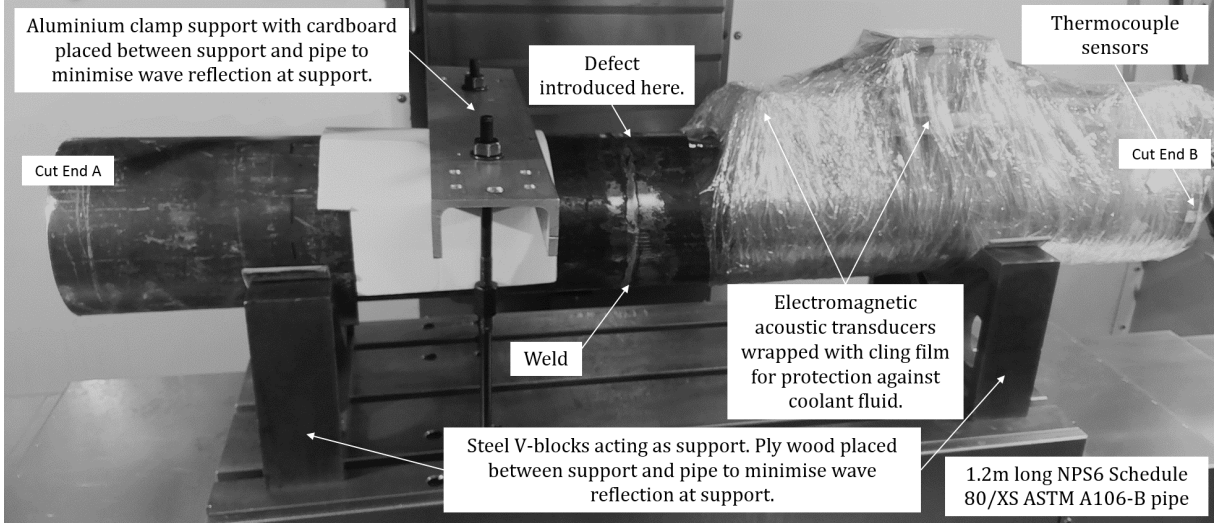


Figure 5.12: Photo of the experiment setup during the CNC machining program.

using the cardboard and plywood, which have soft surfaces [102]. The same transducers as described in the previous section were used. About 600 defect-free, baseline measurements were taken, along with temperature measurements of the pipe and the room. After this, the notch was grown progressively near the weld as shown in Fig. 5.13, the size at each step being given in Table 5.1; at each step, guided wave measurements were collected at 5 minute intervals over a minimum of two days, in order to check stability and to allow signal averaging. In contrast to the temperature cycling study, it was necessary to power down the transducers during the CNC machining steps, in order to mitigate the risk of the EMAT being damaged by the coolant fluid.

Table 5.1: Experiment Variables

Step	Interval	Defect Depth, d [mm]	Defect Length, c [mm]
0		0	0
1		0.5	1.0
2		1.0	2.0
3		2.0	4.0
4		3.0	6.0

5.3.2 Damage detection in weld

Using the procedure described in Section 5.2.3, the defect-free measurement data were used to derive a temperature calibration line of best fit, which was then use to tem-

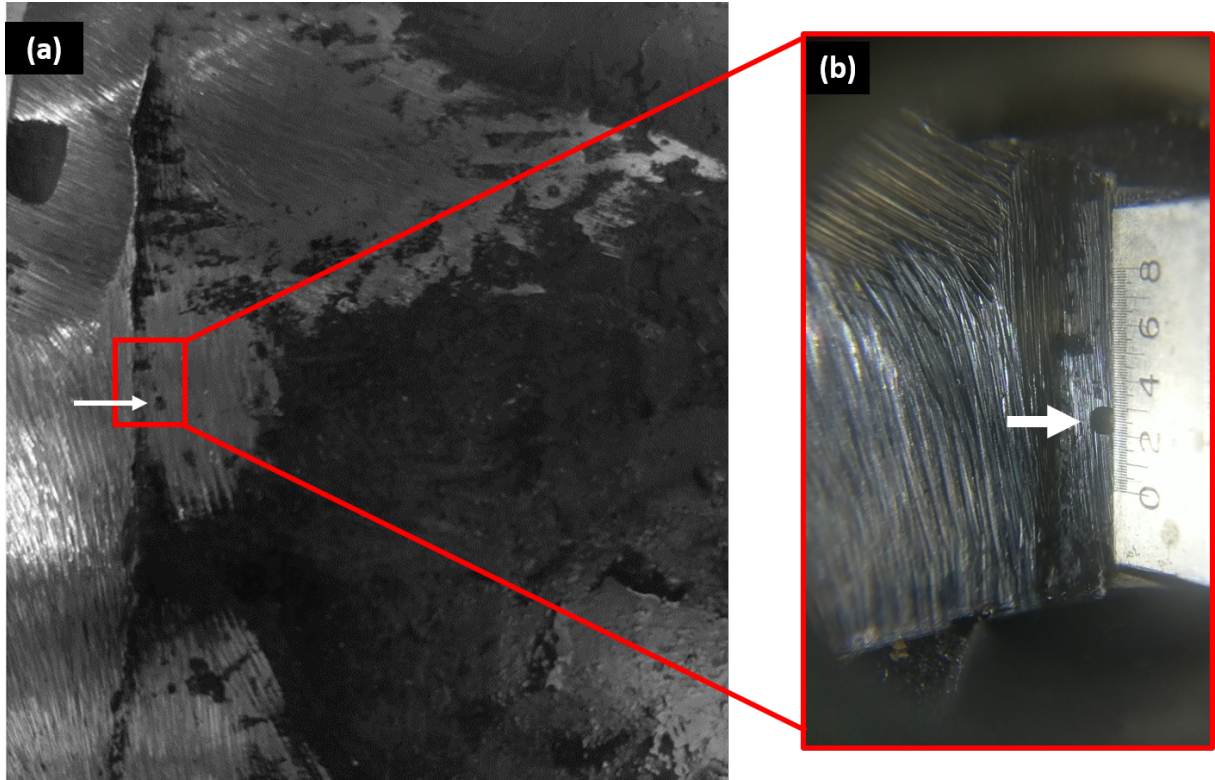


Figure 5.13: Photo taken using a (a) standard and (b) magnified lens, equipped with an 8 mm ruler, of the 0.5 mm (depth) $\times$ 1 mm (length) $\times$ 1 mm (width) machined notch in the heat affected zone of the weld.

perature compensate the magnitude of the weld and incident signals respectively. The temperature compensated weld magnitude was normalised with respect to the temperature compensated incident wave magnitude to yield the plot of R against measurement number as shown in Fig. 5.14. Approximately 1 hour worth of data was discarded at the beginning of each of the steps (dashed lines), to allow the system to stabilise after rebooting the power of the EMAT device (device was powered off during machining and powered on after that). Since it is reasonable to assume that the residual measurements are normally distributed, averaging the R measurements across the known defect windows was performed to improve the estimates of reflection ratio at each step size, which is denoted by $R_{\mu,d}$ and described by the red horizontal lines in Fig. 5.14. Statistical properties for the measured values of R at different stages of the simulated crack growth experiment are tabulated in Table 5.2. The results show that the growth of the defects can be detected, but the instability of the EMAT system decreases the effectiveness of the temperature compensation and hence decreasing the accuracy of estimating R by

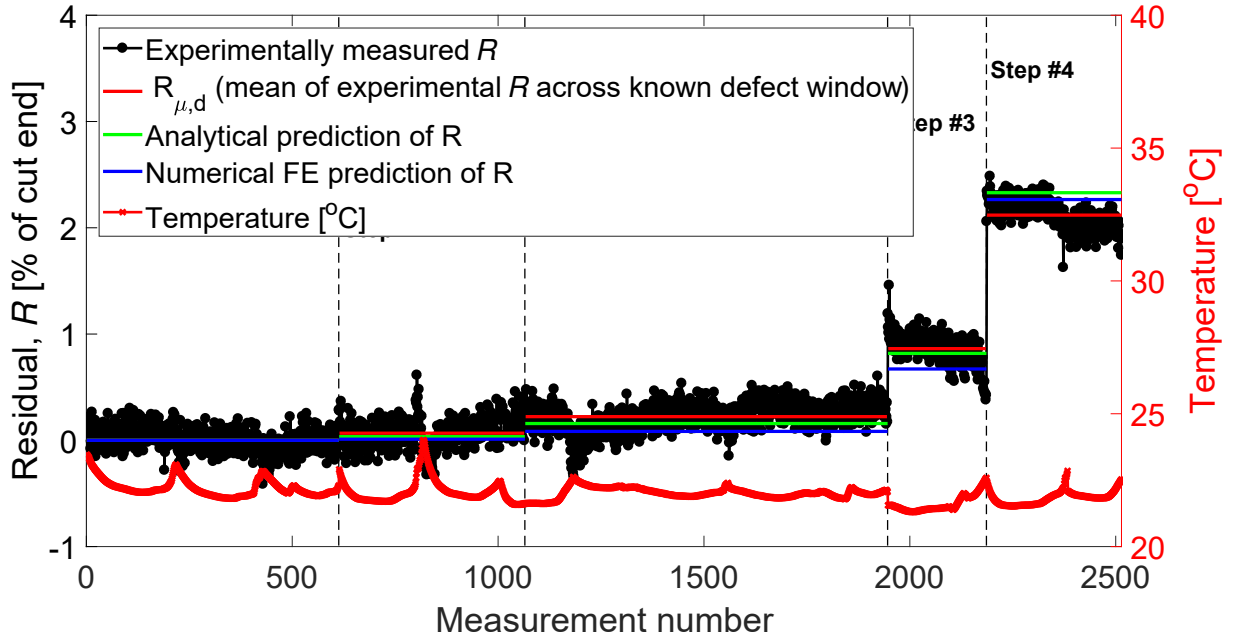


Figure 5.14: The change in magnitude of the weld signal at the centre frequency, expressed in R , against measurement number during the defect growth study. The mean of experimentally calculated R within each defect measurement window is given by the red horizontal line. The green and blue horizontal lines show the SH0 reflection ratio values from notches calculated analytically (Eq. 4.1) and using numerical simulations respectively.

averaging across the set of measurement numbers. The 2 mm and 3 mm deep notches can be more readily detected than the notches with 0.5 mm and 1 mm depths, since their mean to standard deviation ratios are significantly larger as calculated in Table 5.2. The estimated minimum of number of measurements required to achieve a probability of detection (POD) of 95% at a probability of false alarm (PFA) of 1% was calculated for each of the defect sizes and tabulated in Table 5.2. The results indicate that the existing setup can practically detect the 2 mm and 3 mm deep notches at a 95% $\text{POD}_{\text{PFA}=1\%}$ as the minimum measurement numbers are in the orders of hundreds and thousands. In contrast to this, the reliable detection of the 0.5 mm and 1 mm deep cracks poses much more difficulty because measurement numbers of the orders of 10^6 and 10^5 are required. The monitoring performance of this EMAT setup was found to be very similar to the performance of the ring of piezoelectric transducers with a single receiver, that was used in the previous chapter, as both systems would need measurements in the orders of 10^5 in order to detect a 1 mm $\times$ 2 mm crack with a 95% POD with a false alarm rate of 1%.

5. Crack Monitoring Near Welds

Table 5.2: Statistical properties of measured values of R at different stages of the notch growth experiment.

Parameters	Step 0	Step 1	Step 2	Step 3	Step 4
Crack depth, d [mm]	0	0.5	1	2	3
Sample size, n	613	452	881	240	328
Mean of sample distribution of measured R	0	0.0554	0.199	0.816	2.11
Analytical prediction of R	0	0.0105	0.0839	0.671	2.27
Numerical prediction of R	0	0.0343	0.158	0.818	2.33
Standard deviation of the sample distribution of R	0.108	0.140	0.133	0.147	0.139
Mean to standard deviation ratio	-	0.322	1.52	4.10	13.49
Minimum L for POD of 95% at 1% false alarm rate	-	7.94×10^6	1.33×10^5	2240	208

The EMATs used during the experiments can be approximated as a single point receiver, despite having a significantly large footprint, because of the curvature of the pipe. The SH0 monitoring performance of this setup can be improved by increasing the number of receiving transducers, but a stable transducer suitable for long-term monitoring needs to be incorporated for reliable crack growth monitoring at a weld or any feature which yields a large reflection signal.

5.4 Summary

This work explored the use of electromagnetic acoustic transducers (EMATs) for the purpose of SH0 crack monitoring at a weld using frequencies near the SH1 cut-off. The first part of this study evaluated the stability of the EMAT system by monitoring the guided wave weld reflection signal over 10 temperature cycles ranging from 20°C to 40°C. Similar to the recently developed location specific temperature compensation (LSTC), calibration curves were used to compensate for temperature effects and the variations in the transmitted incident wave amplitude, ultimately yielding a baseline subtracted residual reflection ratio R . The temperature cycling study showed that the effects of temperature have largely been compensated for and the data is relatively stable up to the fifth temperature cycle but the standard deviation of the distribution of R significantly increases beyond this, most likely due to uncompensated changes to the behaviour of the EMAT system, which manifest as drift in the magnitudes of the weld and incident wave signals. However, it was still reasonable to assume a normal distribution of R to a 5% significance level in our experiment which validates the use of averaging across the values of R to improve the accuracy of damage detection. The second part of this work

explored the capability of the SH0 system to monitor the growth of a simulated crack near a weld. After temperature compensation, the experimentally obtained reflection ratio R was shown to be in good agreement with both numerical and analytical values, with a simulated crack as small as 0.5 mm deep $\times$ 1 mm wide shown to be detected, but with a POD of 1.04% for a 1% false alarm rate. An SH0 transduction system designed for long term use and stability over multiple temperature cycles is essential to minimise the effect of transducer behaviour changes in the monitored data and improve the probability of critical crack detection near large features such as a weld.

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Chapter 6

Conclusions

6.1 Review of thesis

Fundamental elastic wave theory was summarised in Chapter 2, which covers the theory of shear horizontal guided wave propagation in both plates and pipes. The chapter also provides background information and details of the finite element (FE) models used to model the propagation and interaction of guided waves with a surface-breaking crack used throughout this study.

Chapter 3 begins with a brief overview of the relevant work conducted over the past few decades that covers scattering of elastic waves from cracks. Following this, the 2-dimensional (2D) analytical solutions to the scattering of the fundamental shear horizontal guided wave (SH₀) from both surface-breaking and subsurface crack were derived using a spring-interface model with quasi-static approximations; a quasi-2D FE model was used to validate this solution numerically. Next, the scattering solution for a 3D surface-breaking crack was derived using both dimensional analysis and quasi-static assumptions that was validated using a 3D FE model.

In Chapter 4, details of the experiment conducted to simulate and monitor crack growth in a steel pipe are outlined. The procedure used to detect and quantify the damage from the set of measured guided wave signals is demonstrated and the yielded results were compared to both analytical and numerical predictions from the previous chapter. Next, the performance of the monitoring setup used in the experiment was evaluated by using

synthetic data generated by combining FE data and guided wave measurements collected from the experiment. The monitoring performance was quantified using receiver operating characteristic curves, where the effects of a number of receiver transducers around the circumference, sampling length and transducer standoff distance were investigated. Furthermore, the common source method was investigated as a means to reduce the effects of phase cancellation when using signals from more than one receiver.

Chapter 5 sets out to investigate the effect of temperature cycling on the monitoring performance of electromagnetic acoustic transducers (EMATs), as well as the effect of the presence of a large reflector near the crack, which are factors that could reflect practical conditions in the field. The details of the temperature cycling experiment are outlined, followed by the damage detection and temperature compensation procedure. The chapter concludes with details of the experiment involving a simulated crack growth near a girth weld in a pipe.

6.2 Key contributions

Over the past 20 years, low frequency (25kHz) guided wave screening techniques have been used in pipeline inspection to detect corrosion because guided waves possess a very high inspection coverage per transducer and good sensitivity towards corrosion. When the sensors are permanently installed onto a pipeline, guided wave monitoring can be performed, which not only increases detection sensitivity and allows a good estimation of the corrosion growth rate, but also reduces inspection cost by greatly minimising inspection access costs. However, small but critical cracks can often be 1-2 orders of magnitude smaller in size than corrosion, which makes detection sensitivity an issue if conventional guided wave methods are to be used. As mentioned, guided wave is appealing because it offers a superior inspection coverage per transducer in comparison to higher frequency techniques such as phased array ultrasound. In addition to that, if there is no need to get accurate size measurements, then it becomes feasible to operate at low frequencies. Therefore, it is hypothesised that a short-range monitoring system that utilises the fundamental shear horizontal guided wave (SH₀) can be used to monitor crack growth in steel plate-like structures.

During the first part of this work, the SH0 scattering solutions for both 2D and 3D surface-breaking cracks were derived and validated numerically; the 3D solution was additionally validated by experiments. These analytical equations, in particular the 3D solution, are useful because they provide insight into how the scattered amplitude or reflection ratio changes as a function of the crack geometry, transducer standoff distance, waveguide thickness and inspection wave frequency. More specifically in 3D, it was shown that the reflection ratio is proportional to the cubic power of the effective crack size, frequency to the power of 1.5, the inverse square root of the waveguide thickness and the inverse square root of the transducer standoff distance from the crack. Consequently, these findings show that the reflection ratio of SH0 from a crack is not simply proportional to cross-sectional area, as commonly assumed during guided wave testing for corrosion. In addition, the shape of the crack was shown to be important, where 2:1 (crack length:crack depth) ratio was shown to yield the highest reflection ratio. Generally, cracks which are either narrow and deep or wide and shallow will yield smaller reflection ratios in comparison to a 2:1 sized crack. Furthermore, the derived solutions are intuitive and faster to solve for in comparison to numerical analysis and other more general types of analytical solutions; the caveat being the solution is only valid for low frequency assumptions.

The second part of the work showed that small, simulated cracks, as small as $2\text{ mm} \times 1\text{ mm}$ (crack length $\times$ depth) can be detected using a standard guided wave instrument, which consist a ring of piezoelectric transducers dry-coupled onto a pipe and programmed to transmit and receive shear guided waves. The challenge of this experiment is the low signal-to-noise ratio (SNR) despite operating in a monitoring configuration. One of the key findings is that the experimentally measured reflection ratio can be assumed to be normally distributed to a 5% significance level, which allowed a better estimation of the reflection ratio by taking the mean of the dataset. By combining FE data and the signals obtained from the experiment, a synthetic dataset could be created which served as parametric data to evaluate the monitoring performance of the system as a function of number of receivers around the pipe circumference, transducer standoff distance and monitoring sample length. Another key finding is that the guided wave system can be optimised by summing up the signals from several receiving transducers rather than using the signal from one or all of the receiving transducers, the latter being the conventional

method of measuring the $T(0,1)$ response during guided wave screening. The common source method is an example of a plane wave imaging method that can be used to reduce the effects of phase cancellations when using signals from multiple receivers. In the case of a pipe, it was concluded that the transducer standoff distance from the crack should not be more than 1 pipe circumference away because the scattered SH0 waves from the crack would have mode converted into pipe guided waves which will have a lower amplitude; a shorter standoff distance would yield better performance, but at the expense of inspection coverage per transducer. Furthermore, increasing the number of monitoring samples will generally increase the performance of the system, but the effectiveness of this action decreases with higher samples as this effect is analogous to the improvement of SNR by increasing the number of signal averages.

The final part of this work considered the feasibility of an SH0 monitoring system when there is temperature cycling within the structure and when the crack is located near a large reflector, such as a girth weld in a pipe. Both these factors are potential issues that a monitoring system is expected to overcome in field applications. In contrast to the previous experiment, electromagnetic acoustic transducers (EMATs) were used to generate and receive the shear guided waves because they are regarded as a type of non-contact transducer, which offers advantages in high temperature applications and ease of deployment. The temperature cycling experiment showed that monitoring techniques such as the recently developed location specific temperature compensation method can be applied to reduce the effects of EOC changes on the signals, however there were still uncompensated changes to the system which is believed to be caused by the transducers. The measured reflection ratios using the EMATs were also statistically tested for normality, which validated the assumption that they are normally distributed to a 5% significance level, which is in agreement with the findings from the previous experiment using a different transducer type. In the defect detection experiment, a simulated crack as small as $1 \text{ mm} \times 0.5 \text{ mm}$ (crack length $\times$ depth) was detected, but a 95% probability of detection with a false alarm rate of 1% can only be achieved with measurements in the order of 10^5 . The detection performance is believed to be able to improve if signals from more than one receiving transducers can be used, as shown from previous the experiment using an array of piezoelectric transducers. Ultimately, it is believed that an SH0 monitoring system

is feasible for long-term crack monitoring by designing an array of transducers that are stable over a long time duration.

6.3 Future work

One potential area of work for future consideration is the use of higher inspection wave frequencies that exceed the cut-off for higher order guided wave modes. Generally, a higher inspection frequency will yield a higher reflection ratio and hence increasing the detection sensitivity. The issues from doing so would involve isolating the mode of interest as a higher frequency regime would lead to the generation of multiple guided wave modes such as the SH1 and SH2. Another potential candidate for future consideration is to investigate the performance of the system in a more complex steel structure, such as at a pipe T-joint. This could potentially involve a more complicated array of transducers, rather than using equally spaced transducers around the pipe circumference.

The common source method is an example of an imaging method that was shown to offer improvement in measured reflection ratios as it reduces the effects of phase cancellations when using signals from more than one receivers. However, the limitations of the transducer configuration used during the experiment meant that the images were strictly only valid within the region of a 90 degree arc from the simulated crack. Future studies could investigate the benefits of measuring individually from all receivers around the pipe circumference and explore new imaging techniques that can optimise the detection of cracks. Finally, the long-term stability of an SH0-based crack monitoring system should be investigated over an extended period of time, using carefully designed and stable transducers in order to bring forward its technology readiness level.

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List of Publications

Written publications

- [P1] C. Chua, P. Cawley and P. Nagy. "Scattering of fundamental shear guided waves from a surface-breaking crack in plate-like structures", *IEEE Transactions on Ultrasonics, Ferroelectrics, and Frequency Control*, vol. 1, no. 1, pp. 1–1, 2019.
- [P2] C. Chua and P. Cawley. "Crack growth monitoring using fundamental shear horizontal guided waves", *Structural Health Monitoring*, vol. 1, no. 1, pp. 1–1, 2019.