NEW APPROACH TO DESIGN OF CIRCULAR LINER PIPE TO RESIST EXTERNAL HYDROSTATIC PRESSURE

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Abstract

The basic mechanisms of restrained hydrostatic buckling of nominally close-fitting, circular sewer liners are described, and the origins and limitations of current design formulae based on simple linear ring bending theory are explained. A fully consistent, non-linear theory, already the subject of several years of academic research in Europe and North America, is introduced as the basis of a new, practical approach for dimensioning liner pipe. Simple design charts and algebraic formulae are developed to reflect the important influence on buckling pressure of liner ring compressive strains and separately identified "characteristic" (i.e. renovation technique related) and "system" (host pipe related) imperfections. It is shown how the incorporation of these features enables the new theory to be applied with much greater confidence to a wider range of renovation techniques and liner pipe dimension ratios, and to provide the basis for better targeted product characterisation and proving tests. The predictions of old and new design theories are compared, circumstances in which there is significant divergence identified, and the extent of experimental justification of the new approach in such cases briefly reviewed.

Introduction

Current methods for structural design of flexible gravity sewer liners, as typified in North America by the Appendix X1 to ASTM F1216 (1998), generally consider two external load cases for dimensioning of the liner pipe.

The first is sustained hydrostatic pressure due to groundwater acting in the annular space between the liner and the host pipe. Since gravity sewers in need of renovation invariably leak, this load case applies regardless of host pipe condition or whether the aim of the rehabilitation is primarily structural or simply to provide a barrier against internal corrosion or exfiltration. Even where the permanent groundwater table is below pipe invert level, the liner must generally be designed to resist a short-term hydrostatic head which could arise under storm conditions.

The second load case supposes that earth and traffic loads will in due course be transferred from the existing pipe-soil structure to the liner pipe. The likelihood of this occurring is assumed to be a function of the condition of the sewer at the time of lining. In the great majority of practical situations, however, little or no such load transfer ever takes place because the existing

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equilibrium of even quite badly deteriorated sewer structures is effectively and permanently locked in by the lining process. So far this fact is recognised explicitly only in the UK design procedure (WRc/WAA, 1994), and with few exceptions the treatment of the soil load transfer case in other national methods is irrationally over-conservative (Gumbel, 1998).

This excessive conservatism is particularly acute in the treatment of the so-called “fully deteriorated pipe condition” by ASTM F1216, for two reasons. First the soil load reaching the liner pipe is overestimated by treating the liner as though it had been directly buried, whereas the more correct analogy in circumstances where the existing structure may continue to deteriorate after renovation is with the situation of a tunnel lining (Schrock & Gumbel, 1997). Secondly, the formula used to describe liner response to transferred soil load, and hence calculate the required wall thickness, has been incorrectly modified from an already conservative theory for direct-bury applications in a way which entails further irrational safety factors (Gumbel, 1998).

Recognition of the unnecessary burden imposed on the North American industry by such conservative procedures led in April 1999 to the formation of a new Rehabilitation Design Task Group of the ASCE Pipeline Infrastructure (PINS) committee, of which the writer is a member. The goal of the group is to prepare an updated design procedure for circular sewer liners of all types, with particular emphasis on a more realistic assessment of the conditions for transfer of soil load, and of its true extent and impact in such cases. Since the adjustments made are expected to result in the hydrostatic load case proving the critical determinant of liner wall thickness or ring stiffness in most practical situations, it was considered a timely opportunity also to update the ASTM design procedure for the related limited state of restrained creep buckling.

It is in this context that a synthesis of recent research into mechanisms of restrained hydrostatic buckling, leading to a proposed new approach to the design of nominally circular, flexible sewer liners, is now offered.

Origins and shortcomings of the current ASTM formula for hydrostatic buckling

The formula for hydrostatic buckling of liner pipe still used in most rehabilitation design practice worldwide originated in the UK almost 20 years ago. It is based on the theoretical hydrostatic buckling pressure of a long, perfectly circular, unrestrained pipe given by

$$(P_{cr})_0 = 24\, E^* I / D^3 = 2\, E^* (t/D)^3 \qquad (1)$$

where
$E^* = E/(1 - \nu^2)$ = plane strain modulus
E = flexural modulus (short or long-term, as appropriate)
ν = Poisson’s ratio
I = second moment of area (= $t^3/12$ for solid-wall pipe of thickness t)
D = median diameter of pipe ring (= outside diameter minus thickness)

To account for the observed enhancement of this unrestrained buckling pressure in the first experiments in which cured-in-place pipe (CIPP) liners inserted in steel casings were subjected to external water pressure, Aggarwal and Cooper (1984) defined a simple empirical factor K applied to Eq.(1) for design purposes. As illustrated in Figure 1, they found that a value of enhancement factor K = 7 provided a close lower bound to the experimental data, and this was the figure subsequently adopted for CIPP in the first WRc Manual, and which found its way in due course into ASTM F1216.

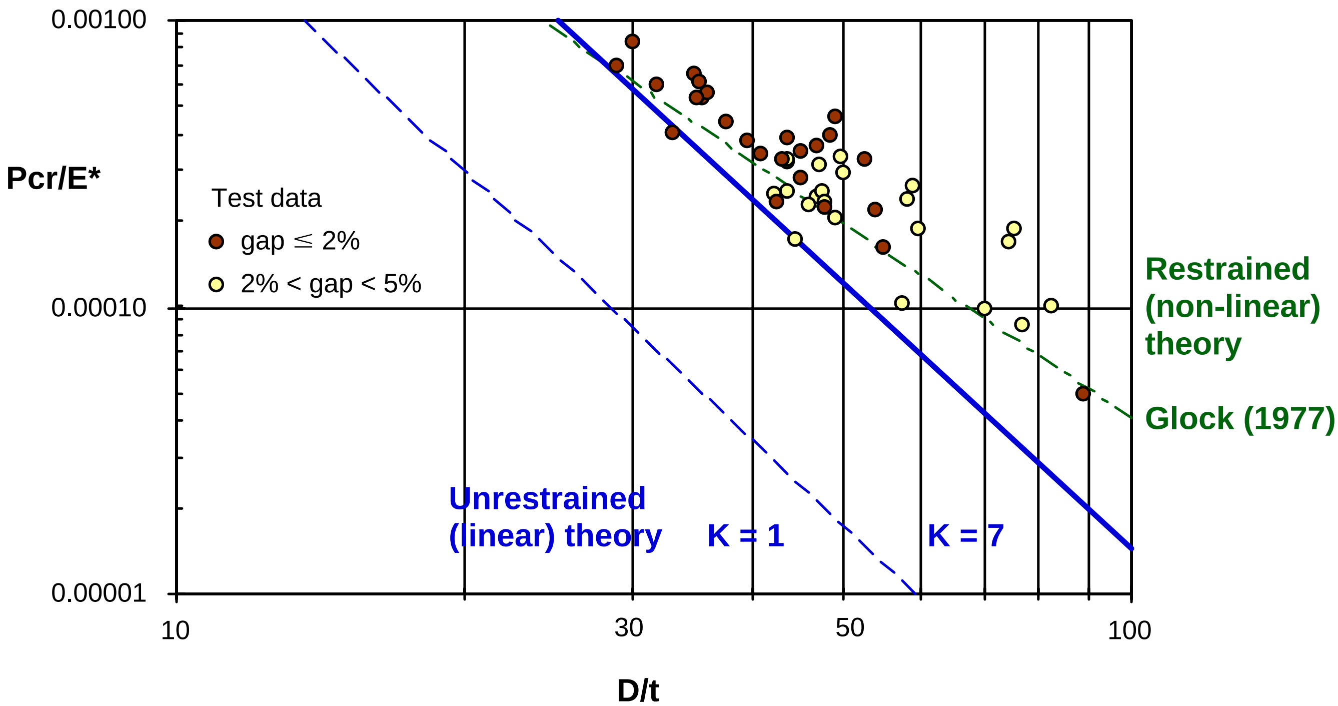
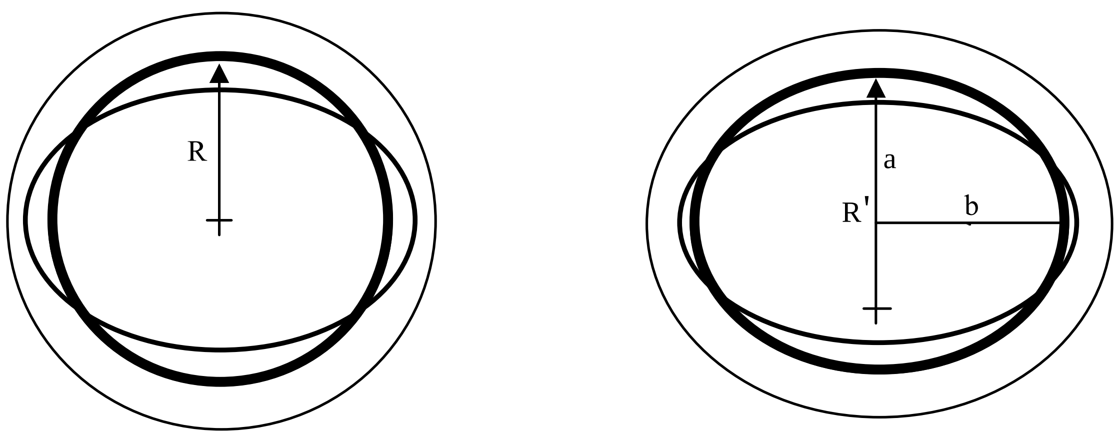


Figure 1 Aggarwal's 1984 hydrostatic buckling data compared with contemporary theories



$P_{cr} = \; K \,.\, C.\, (P_{cr})_0$

$R' = \dfrac{b^2}{a} = \dfrac{(1+q)^2}{(1-q)} R$

a) Close-fitting liner in circular pipe:
$C = 1$

b) Liner in pipe with elliptical ovality q:
$C = (R/R')^3$

Figure 2 Liner deformations implicit in current ASTM design formula based on linear theory of unrestrained buckling with enhancement factor K and ovality factor C

Based on Figure 1, this simple design procedure is clearly conservative, especially given that the experimental liner pipes were inserted into the steel test casing after cure. This resulted in much larger annular gaps (up to 5%) and hence less radial restraint than would occur in the field where such liners are truly "cured in place". But the approach is nevertheless essentially empirical, and so limited in validity to the particular CIPP product and range of dimension ratio D/t tested. The first of these points was borne out in a major US testing programme in which various different CIPP products were found to exhibit quite different experimental K-factors (Guice et al., 1994).

There is also at least one major theoretical inconsistency in the simple approach. As illustrated in Figure 2a, the linear buckling mode assumed involves liner deformations breaching the boundary conditions imposed by the restraining host pipe. Although there was an alternative, non-linear theory consistent with the boundary conditions available at the time that Aggarwal was analysing his test results (Glock, 1977), this was evidently missed, probably because it was published only in the German language literature. The unmodified Glock theory, also plotted on Figure 1, follows the trend of the data better and with a shallower slope, suggesting that the mean K-factor increases with increasing D/t. Neither theory however takes explicit account of initial gap imperfections or explains the large, apparently random scatter of Aggarwal's data.

The WRc and ASTM formulae did nevertheless from the outset take explicit account of the initial out-of-roundness or "ovality" due to vertical deformation of the sewer pipe prior to lining. The full ASTM formula for buckling pressure expressed in the notation of this paper (without safety factor) is:

$$P_{cr} = 2\ K.\ C.\ E^*\ (t/D)^3 \tag{2}$$

where C is a semi-intuitive "ovality factor" derived by substituting for radius R = D/2 in the buckling formula for unrestrained circular liner pipe the local maximum radius of curvature R' of an elliptically deformed host (see Figure 2b). It is only relatively recently that experimental data have become available to test the validity of this ovality factor, and the results published by Boot & Welch (1996) suggest that the ASTM C-factor, like the rest of the formula, is conservative (see Figure 3). But closer examination of these and other test data still being evaluated (Seeman et al., 2001), as well as broader theoretical considerations (Boot & Gumbel, 1998), cast doubt on the validity of attempting to isolate the impact of ovality as an independent factor on buckling pressure. The need for an integrated approach to predict the combined effect of ovality, gap and other imperfections provides a further imperative for updating the current theoretical model.

Mechanisms of restrained hydrostatic buckling

Figure 4 illustrates the steps leading to buckling failure of an encased circular liner pipe subject to external hydrostatic pressure, as observed in numerous laboratory experiments in the US and UK in recent years (e.g. Guice et al., 1994; Boot & Welch, 1996; Boot & Javadi, 1998a).

If the unloaded liner is a tight fit to the host pipe, it can initially deform only by uniform hoop compression (Step 1). The resulting slack in the system allows the liner to lift away from the host pipe in either an asymmetric (one-lobe) or symmetric (two-lobe) mode (Step 2). As pressure is further increased the inward deformation of the lifted lobe or lobes is accompanied by a *shortening* of lobe length and increase in ring compressive strain. Eventual snap-through (Step 3) is in essence a form of geometric instability, in which the buckling pressure is associated with the critical lobe length at failure. The critical lobe length increases as a result of initial annular gap and both elastic and creep compressive strains contributing to further reduction in liner

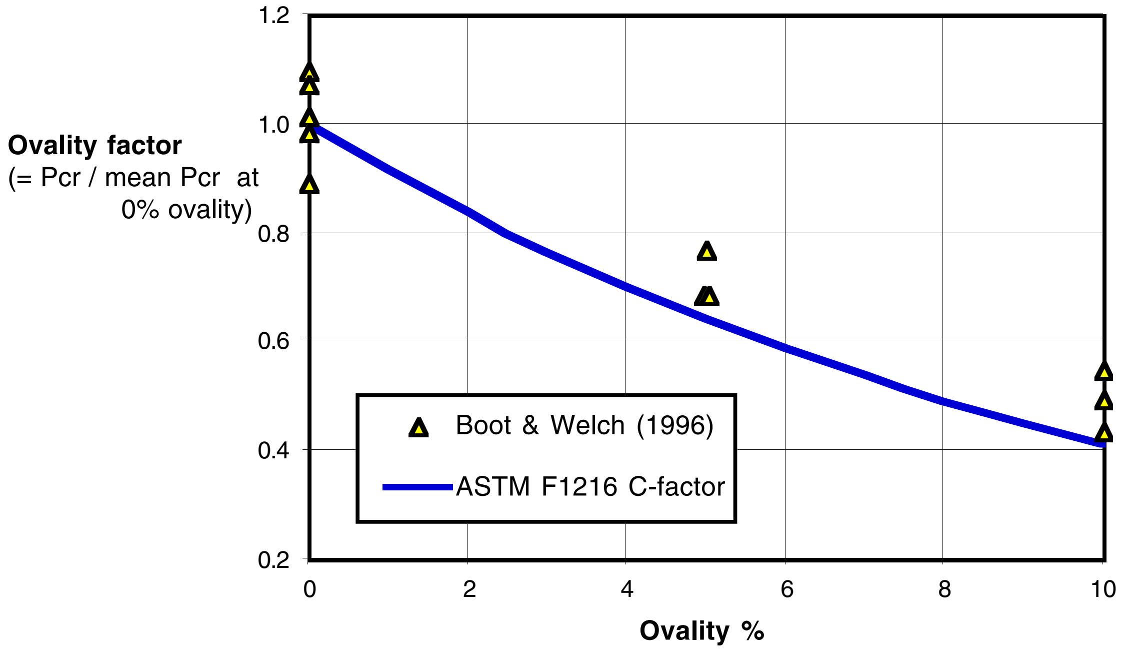
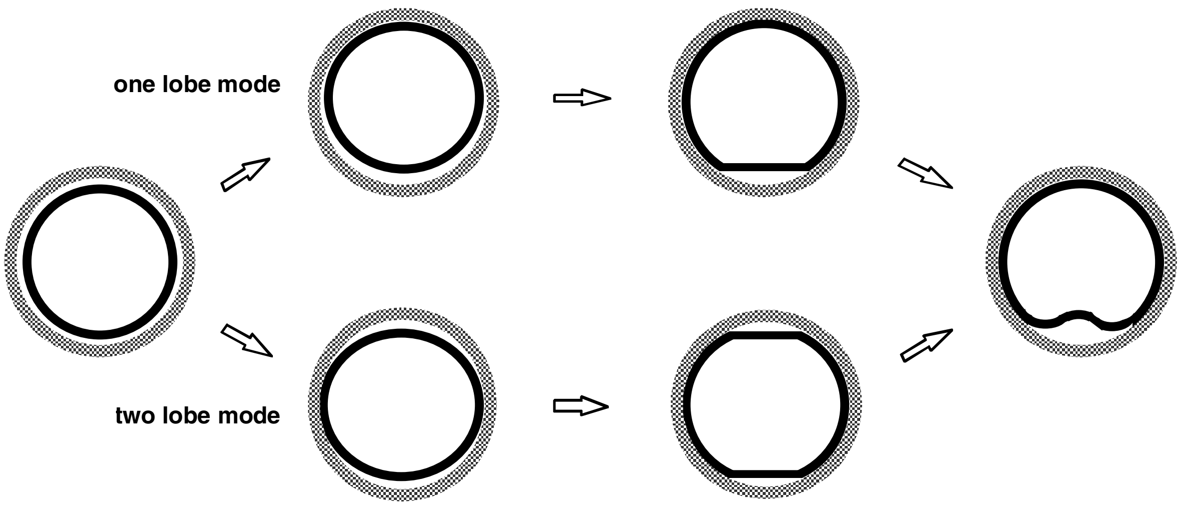


Figure 3 Experimental ovality factors for liner of D/t = 45 compared with ASTM C-factor



<u>Stage 1</u>: uniform ring compression and/or initial gap

<u>Stage 2</u>: lobe separation

<u>Stage 3</u>: snap-through at critical pressure

<u>Stage 4</u>: post-buckled shape

Figure 4 Steps in non-linear hydrostatic buckling of encased circular liner pipe (liner deformations fully consistent with boundary conditions)

perimeter under pressure. This controlling influence of hoop compressive strains is what distinguishes the buckling mode as essentially non-linear.

The pre-buckling deformations leading to instability are not generally discernible with the naked eye, and regardless of whether one or two lobes develop, snap-through can only occur at a single point because the associated release of strain energy instantly stabilises any other incipient points of failure. The resulting manifestation of post-buckling deformation (Step 4) as apparently single lobe should not therefore be confused with the buckling mode itself.

The original non-linear theory developed by Glock (1977), as plotted in Figure 1, assumed a single lobe mode associated with initially perfect circular geometry. However as soon as some initial gap is introduced, the liner pipe will tend to deform elliptically while taking up the slack as if unrestrained (Figure 2a), and then be locked into the symmetrical two-lobe mode up to failure. For nominally close-fitting liners, this tendency has been confirmed in the great majority of cases where pre-buckling deformation measurements have been made. This is contrary to the expectation expressed by Moore (1998) that a relatively loose-fitting liner tested horizontally and pressurised by water would tend to float and create an asymmetrical initial gap favouring the one-lobe mode. In the additional presence of even modest initial ovality (Figure 2b), for example the typical 2% tolerance on diameter associated with manufacture of new concrete and clay pipe, the liner will be even more strongly predisposed to follow a symmetrical deformation mode. Two-lobe deformation has indeed been observed in 100% of appropriately monitored buckling experiments in horizontally aligned, ovalised casings to date (Seeman et al., 2001).

In view of their important influence on the qualitative as well as quantitative buckling response of restrained liner pipe, it is clear that any fully consistent design theory must take explicit account of imperfections.

Development and testing of a fully consistent design theory

Boot (1998) extended Glock's closed-form theory to take account of initial gap imperfections and also to reflect the observed two-lobe buckling mode. The generalised buckling equation he derived, when converted to plane strain conditions for long pipe, can be expressed in convenient dimensionless form simply as:

$$P_{cr} / E^* \quad = \quad c\,(D/t)^{\,m} \quad = \quad c\,(t/D)^{-m} \qquad (3)$$

where *both* the coefficient c *and* the power m of D/t are functions of imperfections in the system.

For zero imperfection m = -2.2, and the coefficient c has value of 1.003 for one-lobe buckling (corresponding to Glock's published solution) and 1.323 for two-lobe buckling. As the mean gap/radius ratio w_{02}/R increases, both m and c increase, and at very large gaps approach 2-lobe values of -3 and 2 respectively, corresponding to the unrestrained buckling formula of Eq.(1).

These basic predictions of the Boot-Glock theory are plotted in Figure 5 and compared with experimental data from a variety of sources covering a wide range of D/t values from 30 to 100. The top two theoretical lines correspond to the "perfect" two and one-lobe solutions respectively, separated by a constant factor of 1.32 independent of D/t, and hence parallel on the log-log plot. The next two lines down show how introduction of symmetrical gap imperfections $w_{02}/R = 1\%$, and then 2%, not only reduces the 2-lobe buckling pressure but also progressively changes the mean slope of the curve closer to that of the unrestrained theory.

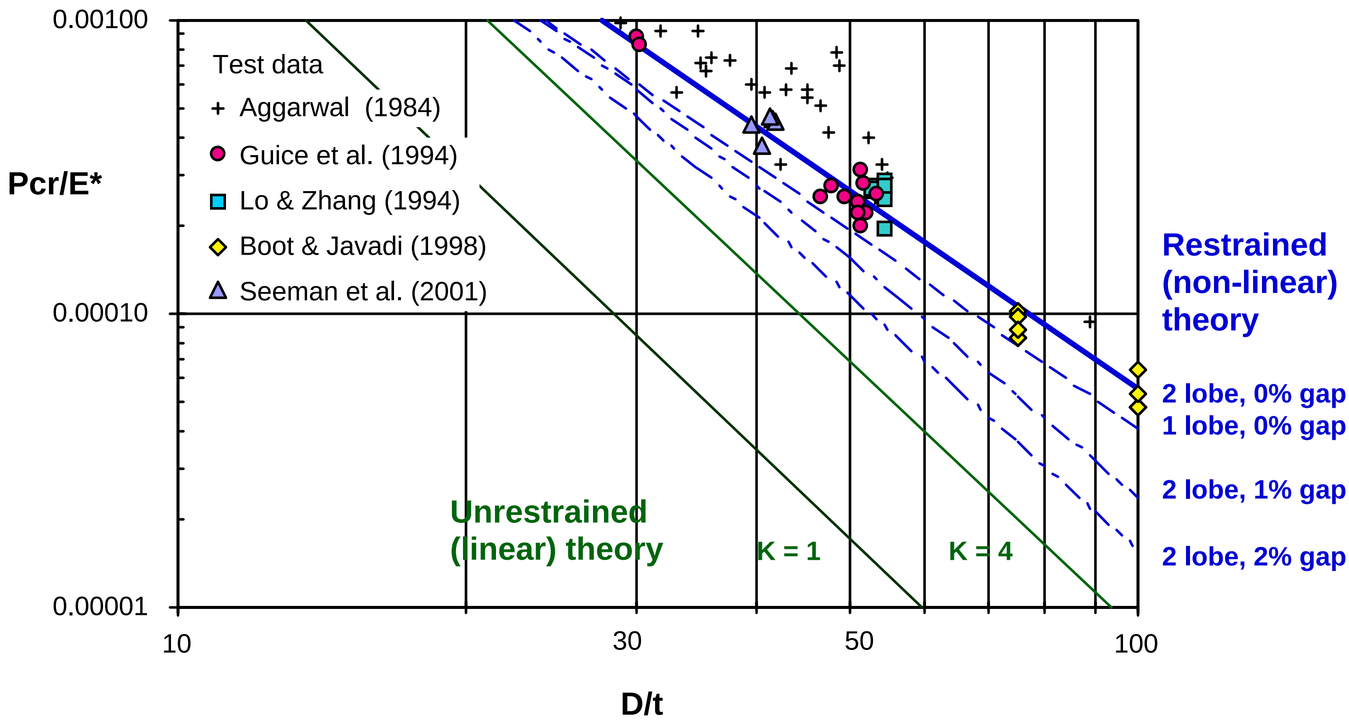


Figure 5 Various gap-corrected hydrostatic buckling data compared with current theories for perfectly close-fitting, perfectly circular liners

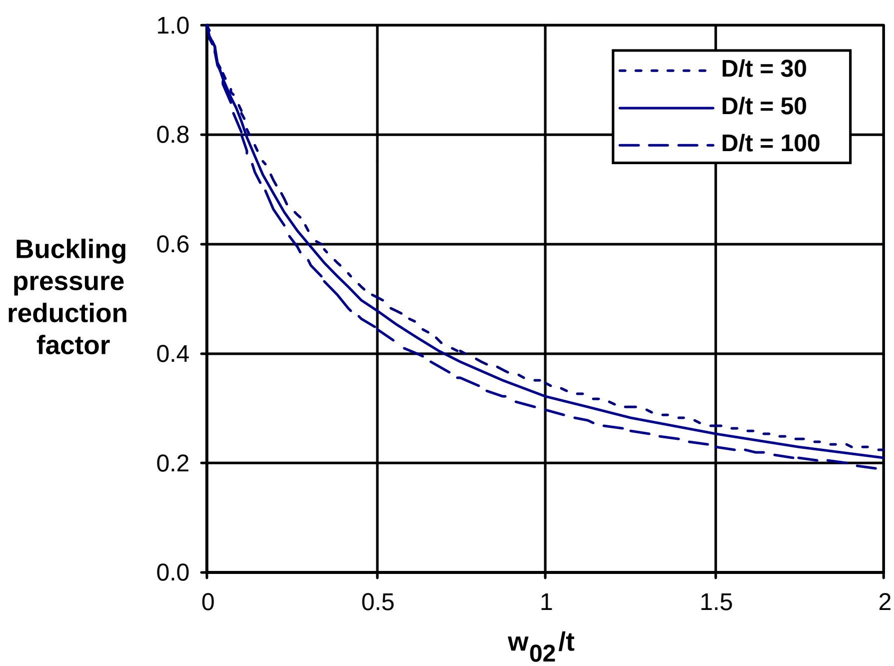


Figure 6 Reduction factors on two-lobe buckling pressure due to symmetrical gap imperfection expressed as a function of gap/thickness ratio

Although considered by Boot to be near enough straight, the lines for non-zero gap in fact curve downwards slightly, implying some variation of m with D/t. As previously pointed out by the writer (Gumbel, 1993) this apparent increase in sensitivity to relative gap w_{02}/R (or in the case of one-lobe buckling w_{01}/D) as dimension ratio D/t increases can be attributed almost entirely to the relationship of gap size to liner thickness[2]. Figure 6 shows that the theoretical reduction factor on buckling pressure due to gap alone hardly varies with D/t if expressed as a function of w_{02}/t. This linkage to thickness can best be understood in terms of the geometry of the buckle lobe as snap-through is approached (Step 3 in Figure 4). Although the increase in critical lobe length due to initial slack in the system is a function of gap expressed as an imperfection on diameter or radius, the associated reduction in snap-through pressure arises from the increase in slenderness or length/thickness ratio of the buckle lobe.

To facilitate comparison with the theory, all the experimental buckling data plotted on Figure 5 have first been corrected to perfect close-fit conditions by dividing the measured buckling pressure in each case by the reduction factor derived via Figure 6 from the mean measured gap imperfection and liner thickness. All the data plotted are for CIPP liners either cast directly into circular casings, or subsequently inserted with $w_{02}/R \leq 2\%$. The only exception is the set of points from Guice et al.(1994) plotted at D/t of around 30, which refer to a folded PVC product.

Apart from the Aggarwal data, which due to deficiencies in the early test rig design discussed by Moore (1998) exhibit exceptional scatter skewed to the high side, the gap-corrected test results plotted in Figure 5 clearly follow the slope predicted by the non-linear Boot-Glock theory. Furthermore the data are centred on the 2-lobe theoretical line, with a scatter band everywhere within 20%, at least on the low side. Given the high imperfection sensitivity of non-linear buckling at near-to-perfect circular geometry, as implied by the steep initial slope of the reduction factor curves in Figure 6, this provides strong validation of the 2-lobe theory as a basis for practical design of the CIPP and folded PVC products tested.

It is important that readers are not tempted to conclude, as some researchers have done, that it is appropriate to design such liners assuming a one-lobe buckling mode merely because the theoretical one-lobe curve in Figure 5 provides a convenient, if somewhat wide, lower bound to the experimental data. This would be irrational for two reasons:

i) although a small proportion of liners in near perfect circular casings have exhibited one-lobe buckling in laboratory tests, for field conditions it is normally necessary to design for at least 2% ovality imperfection, which invariably produces two-lobe buckling;

ii) the loss of consistency between theory and observed buckling modes could lead to errors in quantifying relevant imperfections and predicting their effects.

This does not rule out one-lobe buckling modes being relevant to other lining techniques with different characteristic imperfections. The great advantage of a consistent theory over the current empirical approach is its ability to reliably predict the behaviour of structures of types and in dimension ranges beyond testing experience. Much of the advantage of introducing non-linear buckling theory is however lost if, as in the latest German recommendations (ATV, 2000), a liner design methodology is built unquestioningly on the original one-lobe Glock theory.

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[2] See Boot (1998) for notation: for the same relative size of liner to host pipe, $w_{01} = 2\, w_{02}$.

Classification and combination of imperfections

Most of the literature agrees on the need in principle to consider three distinct types of geometric imperfection of liner pipes. In addition to the *gap* and *ovality* imperfections already discussed, a "wavy" imperfection of the liner pipe wall in the shape of an incipient buckle lobe can, in theory, significantly diminish external pressure resistance (El Sawy & Moore, 1997). The common description of such wavy intrusions as "local" imperfections is however potentially misleading in the present context, because in order to have an effect on the cross-sectional response of a restrained liner pipe they need to be continuous for a few diameters along the pipe axis. Any truly localised imperfection of the existing sewer pipe wall that is transferred to a close-fitting liner, e.g. a displaced brick or broken pipe segment, will in practice produce a three-dimensional stiffening effect. The preferred term used here therefore is *longitudinal* imperfection.

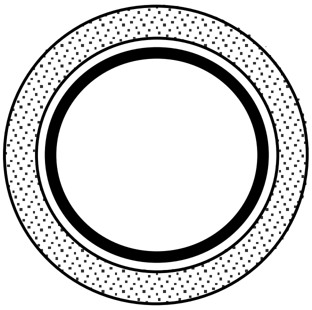
Typically in the stability analysis of structures a wavy "eigenvalue" imperfection is the most critical that can be imposed, but the theoretical impact of a longitudinal imperfection of height Δ in restrained liner pipe proves to be slightly less severe than that of gap. Moore (1998) has shown that, as in the case of gap imperfections illustrated in Figure 6, the buckling pressure reduction is a function primarily of Δ/t, and insensitive to D/t. But whereas a gap equal to the liner thickness will reduce the buckling pressure by just over one half, at $\Delta/t = 1$ the pressure is not quite halved. These observations only apply, however, when considering reductions from the theoretical buckling pressure of an otherwise perfectly circular and close-fitting liner. As soon as different types of imperfection are combined, as invariably happens in practice, the high imperfection sensitivity of the perfect structure, represented by the steep initial slope of the plots in Figure 6, is progressively diminished.

For the analysis of combined imperfection effects it is helpful to define and distinguish between the *characteristic imperfections* associated with a particular renovation technique, and the *system imperfections* due to irregularities in the host pipe being lined (Boot et al., 2001). Examples of these two categories of imperfection are illustrated in Figure 7.

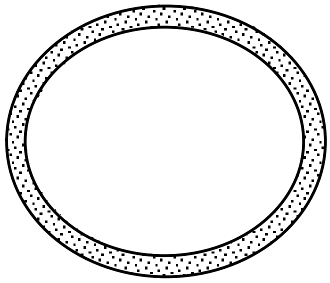
Looking down column a) of Figure 7, the most important *characteristic* imperfection of CIPP liners, as well as the close-fit PVC product analysed in Figure 5, is shrinkage gap. In grouted slip-lined systems, this gap may be largely (although, due to heat of hydration of the grout, not entirely) eliminated, but is replaced by a characteristic ovality due to manufacturing tolerances, bending during insertion and/or deformation under grout pressure.

The example of characteristic longitudinal imperfection illustrated is the incipient buckle along the original fold line of certain close-fit thermoplastic pipes. This is in fact more of a material imperfection than a purely geometric one; it is due largely to residual stresses in the liner arising from the processes of folding and reversion. The resulting impact on buckle location and critical pressure in folded PE pipes has been reported by Glanert (1992) and Alberding & Falter (1997). Other folded pipe materials and processes are not necessarily affected: for example the reduction factor on buckling pressure of the folded PVC product tested by Guice et al.(1994) and analysed in Figure 5 is fully accounted for by the measured gap imperfection.

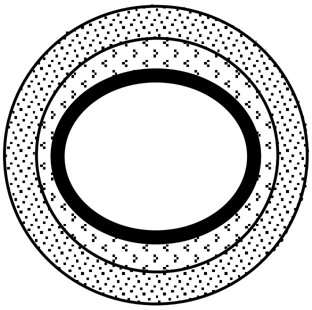
Other potential characteristic imperfections, not illustrated, are localised variations in material properties or thickness around the circumference of the liner. These have been analysed by Boot (1998) but found to have only very minor effect compared with gap or ovality. For design purposes the key is to identify the dominant imperfections of each material and process.



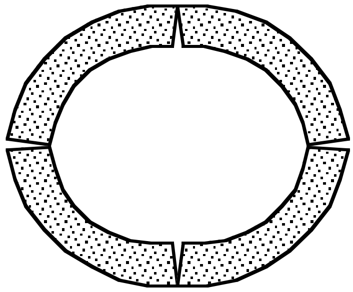
Gap (e.g. due to thermal shrinkage of liner)



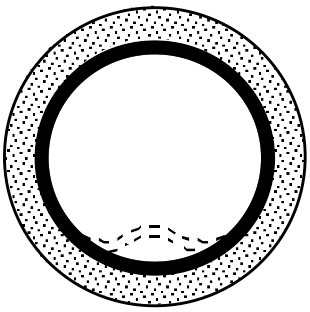
Ovality — elliptical (deformed flexible pipe)



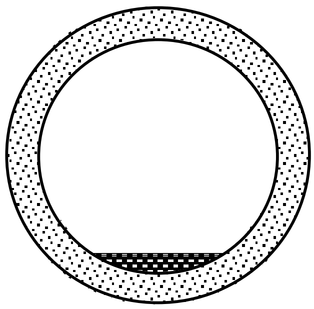
Ovality (e.g. deformation of grouted slip-lined pipe)



Ovality — 4-hinge (deformed rigid pipe)



Longitudinal (e.g. original fold line of close-fit pipe)



Longitudinal (e.g. flat invert due to residual sediment)

a) CHARACTERISTIC

b) SYSTEM

Figure 7 Examples of characteristic (renovation technique) and system (host pipe) imperfections affecting liner buckling resistance

Moving to column b) of Figure 7, the dominant *system* imperfection of deteriorated sewers affecting close-fitting liners of all types is ovality. This is not however commonly the elliptical ovality associated with deformation of flexible pipes: in cracked rigid pipes the typical "4-hinge" deformation creates a more severe imperfection for the same relative reduction in vertical diameter.

True longitudinal system imperfections are relatively uncommon: a layer of sediment encrusted in the pipe invert is one possible example, but usually removed by cleaning. Provided a conservative value of ovality is assumed, it should not normally be necessary to include any default value of longitudinal system imperfection for design.

System-related gap imperfections (not illustrated) may also arise where a circular slip-lined or close-fit pipe is limited in diameter by constrictions such as locally high ovality or offset joints. Such annular gaps would however normally be grouted.

For each particular renovation technique and host pipe to be lined it is necessary to consider the combined effect on buckling pressure of all the relevant imperfections. Due to the progressive reduction in imperfection sensitivity as total imperfection increases, the application of separate gap, ovality and waviness reduction factors, as advocated by the new German design guide (ATV, 2000), is unduly conservative. The real challenge for practical implementation of the new theory is to find a way to present it in a transparent and easily applied format, which nevertheless preserves the principle of consistency.

Practical applications of the new design approach

The basic design concept proposed is to develop buckling pressure or safe water head charts for each renovation technique which already incorporate its characteristic imperfections, but allow variation of the relevant system imperfections according to the condition of the sewer to be lined. The most appropriate form of chart may vary according to the type of renovation technique and/or liner material: three examples are considered here.

Cured-in-place pipes of homogeneous wall construction

Figure 8 shows a typical design chart appropriate for CIPP. P_{cr} /E* versus D/t curves are presented for different system ovalities which also incorporate a characteristic gap imperfection, in this case 0.5%. Since a closed-form solution for computing the combined effects of gap and ovality has yet to be developed (Boot et al, 2001), the curves were based on the results of finite element analysis by Toropova & Boot (1998). The "4-hinge" form of ovality appropriate to rigid pipe renovation (Figure 7b) has been assumed. For this paper only, the corresponding curves implied by ASTM F1216 are included for comparison.

It may be observed that the combined "2% ovality + 0.5% gap" curve is higher and of shallower mean slope than the pure 2-lobe, 2% gap curve plotted in Figure 5. This is because the effect on buckling pressure of a given percentage ovality on its own is quantitatively much less than that of the same amount of gap, and also qualitatively different. As already illustrated by Figure 3 in contrast to Figure 6, the pressure reduction due to ovality develops more gradually than that of gap, without the steep initial drop indicative of high imperfection sensitivity.

To appreciate the difference in outcomes using the new and old design curves in Figure 8, it should be noted that for CIPP the usual procedure is to determine the wall thickness t required to safely resist a given value of groundwater pressure. Since pressure is entered on the y-axis, and

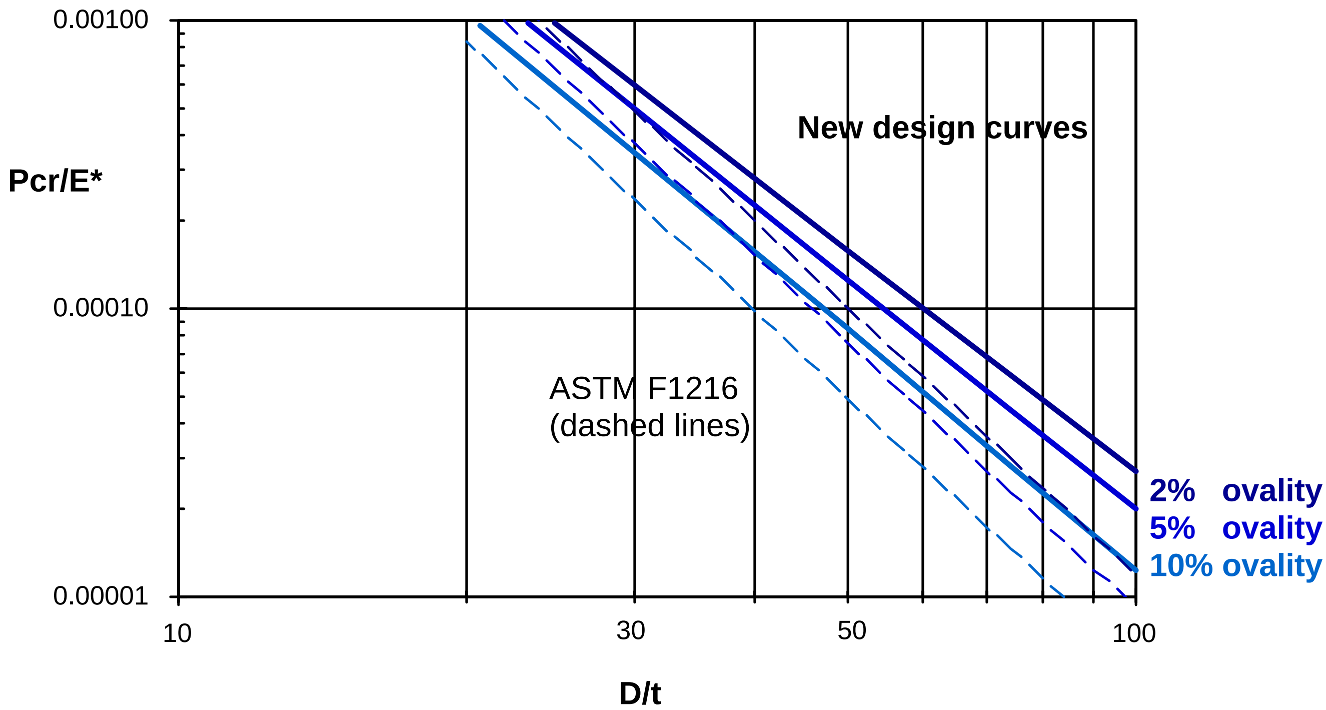


Figure 8 Typical form of new design chart for CIPP incorporating a characteristic gap imperfection (here 0.5%)

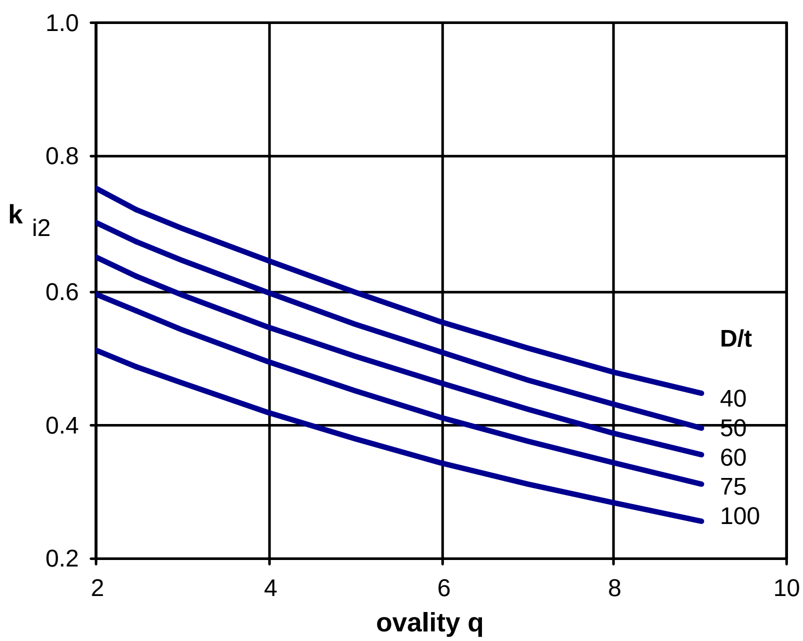


Figure 9 Combined imperfection factor on 2-lobe buckling pressure of perfect circular liner as a function of system ovality, for characteristic gap imperfection of 0.5%

thickness read via the value of D/t from the x-axis, it is the horizontal rather than vertical separation of the two sets of curves which is of practical significance. A simple example serves to illustrate the point.

For a 1200mm diameter sewer with 5% ovality under 6 metres sustained head of groundwater, the thickness of liner with long-term modulus E* = 1200 MPa required to provide a factor of safety N = 2 against buckling is determined as follows. The groundwater pressure P = 0.06 MPa, so P_{cr} = N.P = 0.12 MPa, and P_{cr} /E* = 0.0001. Entering this value on the y-axis, the intercept with the new design curve for q = 5% yields D/t = 60, while the corresponding ASTM F1216 curve gives D/t = 50. Since D is defined as mean diameter of the liner (i.e. measured to the centroidal axis of the wall), the corresponding values of standard dimension ratio SDR (= outside diameter/thickness) are 61 and 51 respectively. The required thickness by the new method is then 1200/61 = 19.7 mm, compared with 1200/51 = 23.5 mm by the old, a saving of 16%. For more lightly loaded liners, and/or stiffer materials, resulting in lower values of P_{cr} /E*, the potential savings increase as the new and old design curves increasingly diverge.

Recognising that accurate reading of log-log plots is not always as easy as in the above example, it is proposed in due course to derive simple curve-fitting formulae to enable programming of such a chart into a spreadsheet. The graphical presentation nevertheless provides a helpful overview of liner performance and the non-dimensional grouping of all plotted parameters allows any consistent set of units for each group to be used. Further specialisation of the chart, i.e. beyond incorporation of a characteristic gap imperfection, would however be straightforward if desired. For example, by also building in a safety factor and the relevant characteristic modulus E* of the product in question, the y-axis could be adapted to allow direct entry of the design water head in metres or feet, and the x-axis to read directly in SDR rather than D/t.

Close-fit thermoplastic pipes

For deformed/reformed liner pipes, charts similar to Figure 8 could be produced, incorporating characteristic longitudinal and gap imperfections as appropriate to each material and process. If, as is usual, the product is extruded and supplied in a discrete range of SDR's, an alternative graphical presentation is suggested. This would be based on a form of Eq.(3) written as:

$$P_{cr} /E^* \quad = \quad k_i .c_o(D/t)^{-2.2} \tag{4}$$

where c_o is the value of coefficient c for zero imperfection, and k_i is the overall reduction factor on buckling pressure. Eq.(4) is similar to the formula based on the original Glock analysis already advocated elsewhere (El-Sawy & Moore, 1997; ATV, 2000), with the important difference that k_i here is a *single* factor incorporating the *combined* effects of *all* imperfections. The fact, already demonstrated, that the power m of D/t in Eq.(3) is not constant but is itself a function of imperfections means that k_i also varies with some non-zero power of D/t.

Figure 9, developed using the same numerical data used to construct Figure 8, illustrates the principle of how Eq.(4) would be applied in practice. The factor k_{i2} plotted refers to 2-lobe behaviour and is intended for use in Eq.(4) in conjunction with a value of $c_o = c_{o2} = 1.323$. The need to interpolate between the plotted curves for the required output parameter D/t makes this form of chart relatively impractical for design of CIPP, but if reproduced for thermoplastic pipes at relevant fixed values of SDR it would be quite suitable. For some close-fit pipe materials and processes, for example heat-reverted polyethylene, the SDR range would be lower, and typical gap imperfection due to thermal shrinkage higher at around 2% (Alberding & Falter, 1997), so the detail of the design chart would look quite different from Figure 9, although qualitatively similar. Any material-related longitudinal imperfection of folded pipe (Figure 7a) would in practice be indistinguishable from that of an additional characteristic gap, and could be incorporated accordingly. If measurements of pre-buckling deformations established that the overall characteristic imperfection made the one-lobe rather than two-lobe mode critical, design should also for consistency be based on the one-lobe factor k_{i1} (which in theory differs slightly from k_{i2}) applied to Eq.(4) with $c_o = c_{o1} \approx 1$.

Profiled and structured wall liner pipes

A major advantage of the new theory is that it takes explicit account of the contribution of axial (hoop compression) as well as flexural strains on the stability of the liner pipe ring. For liner pipes of profiled or non-homogeneous wall construction the generalised form of Eq.(3) is:

$$P_{cr}/E^* = \lambda . c . \left[D\sqrt{\frac{A}{12I}} \right]^m \qquad (5)$$

$$\text{where} \qquad \lambda = \sqrt{\frac{A^3}{12I}} \qquad (6)$$

A and I are respectively the area and second moment of area of the wall cross-section, and the "median" pipe diameter D is now measured at the neutral axis rather than at mid-thickness.

Thus a chart of the form of Figure 8 may readily be adapted for profiled wall pipes simply by re-labelling the axes as follows:

y-axis: P_{cr}/E^* replaced by $P_{cr}/\lambda E^*$

x-axis: D/t replaced by $D.\sqrt{\frac{A}{12I}}$

Note that the simple substitution of thickness t by $\sqrt{12I/A}$, as referenced quite widely in the literature on linear buckling theory, is NOT sufficient. If this is done with the non-linear theory, as suggested by the new German ATV guide, it leads to the completely erroneous result that a profiled wall liner pipe has a higher buckling pressure than a solid wall pipe of the same flexural ring stiffness EI/D^3. In fact the complete opposite is true. This can be appreciated by the fact that a profiled wall pipe has relatively low cross-sectional area and hence axial compression stiffness, and that for perfectly close-fitting liners it is the compression stiffness EA rather than the flexural stiffness EI which dominates buckling response.

Correct interpretation of either the Glock (1977) or Boot (1998) analysis yields the additional factor λ applicable to the buckling pressure. Furthermore, the following expression can be derived for the buckling pressure of a profiled wall pipe relative to that of a solid wall pipe of the same flexural ring stiffness:

$$\frac{(P_{cr})_{\text{profiled wall}}}{(P_{cr})_{\text{solid wall}}} = \lambda^{[1-(m/3)]} \qquad (7)$$

where λ is given by Eq.(6).

At zero gap, m = 2.2, and for the range of I and A values of standard wall profiles listed in ASTM 1741(1996), Eq.(7) yields factors on buckling pressure in the range 0.65 – 0.85. With increasing gap, these factors increase as m increases, eventually approaching 1 as the unrestrained condition, for which m = 3 and the buckling mode is purely linear and flexural (see Figure 2a), is reached.

The same considerations apply to structured wall pipes for which the equivalent values of A and I/A assuming a uniform E value for all elements of the structure may be derived by the method of transformed sections as used for reinforced concrete design. For both profiled and structured-wall pipes, however, it is important to note that creep buckling may not be the critical limit state for resistance to external hydrostatic pressure. Depending on the detailed wall section design, failure may occur in the form of local rib buckling in profiled wall liners, or by delamination or shear in structured wall pipes, before the theoretical buckling pressure is reached. By contrast, for homogeneous, solid-wall, circular liner pipes of flexible plastics, material failure is invariably less critical than buckling (Boot, 1998; Boot & Gumbel, 1998).

Implications for characterisation of renovation products

Key input parameters for the new theory, as for the old, are the values of short and long-term liner modulus. The fact that non-linear creep buckling primarily involves compression of the liner pipe wall, with flexure assuming dominance only when the total imperfection becomes large, has sparked much debate about whether the conventional 3-point flexural or parallel-plate ring stiffness tests are appropriate. Research on CIPP in this area continues (Boot et al., 2001), but hitherto the indications are that some form of flexural test yields lower bound, but not overly conservative, E values for design (Boot & Javadi, 1998b). Some form of long-term testing is always necessary. Even where design for a short-term storm event, of the order say of 1-3 days, is critical, short-term laboratory tests of typically 1-3 minutes duration may not provide a creep modulus on the relevant time scale. The difference for some materials, for example polyethylene, can be very significant (Alberding & Falter, 1997).

Just as important as the material stiffness is the need to characterise the imperfections of each individual renovation technique and material. This is new. Initial annular gaps can be estimated from the material thermal expansion properties, but to the extent that they are process dependent should also be backed up by measurements. In all cases, but especially where non-measurable imperfection effects such as residual stresses may be involved, there is no substitute for external pressure testing of lined pipe samples to fully characterise both material and geometry. In this regard significant advance has been made since the early days of testing using perfectly circular casings which produced a large amount of scatter. By testing in casings with 5% ovality (Boot et al., 2001), the scatter band can be appreciably reduced. This is apparent already from the data plotted in Figure 3, and has been confirmed by more extensive recent tests (Seeman et al., 2001).

Next steps in method development and implementation

As demonstrated in the first half of this paper, the proposed new design theory is both more consistent and better substantiated by available experimental evidence than existing formulae for restrained hydrostatic buckling of circular liners. This alone is in principle sufficient to justify immediate implementation of the new approach. Some further work is however needed to develop in detail the various design charts and other tools outlined in the second half of the paper for practical application across the full range of renovation techniques discussed.

The choice of how best to present the methodology in each case will depend on the extent of available measurement and test data, as well as regional preferences and practice. In considering a suitable overall approach for application in North America, it is suggested that the ASCE PINS committee should proceed, in close collaboration with associated international research and standardisation initiatives, as follows:

a) Decide on suitable groupings of renovation techniques for which common design tools can be developed. Ideally the classification of renovation technique families in the process of being adopted as a European standard (CEN, 2001) should be followed.

b) Establish minimum testing requirements for each technique family and liner material, and/or assign appropriately conservative default values to all relevant design characteristics, including characteristic imperfections, for which test data are not yet available.

c) Publish fully detailed design procedures for beta-testing by both user and producer sides of the industry, which should also provide a consistent and balanced framework for validation testing of all individual products covered.

Concluding remarks

1. Although it has served a useful purpose in helping to launch the renovation industry, the original WRc/ASTM hydrostatic buckling formula has been shown to be too conservative for current needs, and above all limited in predictive power by its lack of theoretical consistency.
2. The adoption of an extensively researched and fully consistent design theory, already well substantiated at least for cured-in-place pipe liners, is now advocated. The principles of the theory have been explained, and its adaptability to all other close-fitting and grouted lining systems, including those using profiled or structured wall pipes, has been demonstrated.
3. With the aid of the new theory, the influence of gap, ovality and longitudinal imperfections on restrained hydrostatic buckling pressure has been described, both qualitatively and quantitatively. A new classification of imperfections as either *characteristic* to a particular renovation technique, or arising from the host pipe *system* has been proposed.
4. Practical forms of design chart have been presented which already incorporate the characteristic imperfections of an individual renovation technique and relate the critical short or long-term water head to the liner wall thickness or ring stiffness according to the dimensions and system imperfections of the pipe to be lined. The derivation of alternative, but similarly transparent, spreadsheet design tools has been discussed.
5. The implications of the new theory for the characterisation of renovation products have been briefly reviewed. Related improvements in testing technique are being developed as part of ongoing research projects at universities in the UK and USA.
6. The next steps to be taken by the ASCE Pipeline Infrastructure (PINS) committee in considering how the proposed new methodology might be implemented in North America have been outlined.

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