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Numerical modelling of lockbolted lap connections for aluminium alloy plates

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ABSTRACT

A numerical modelling method for lockbolted lap connections of aluminium alloy plates is studied based on experimental tests. The mechanical properties and constitutive models of aluminium alloy 6061-T6 and stainless steel 304HC were intensively determined by tensile tests. A tensile test of a single-lockbolted lap connection was conducted to obtain its elongation and strain responses. A basic numerical model that considers the lockbolt preload, interface contact and friction, and gaps between the lockbolt and bolt hole was established to simulate the tensile behaviours of this connection. This model was updated to minimize errors between the numerical and test results, and values for the friction coefficients of different contact surfaces were suggested. To improve the accuracy and efficiency of the numerical analysis, an incompatible mode solid element was adopted in the numerical model. It was determined to be better than refining the mesh or accepting a higher-precision element type. This numerical modelling method was investigated by two types of double-lockbolted connections, and its validity was verified by comparing the displacement and strain responses between the numerical and test models. The modelling method suggested in this paper is applicable for simulating the mechanical behaviours of complicated joints with lockbolted lap connections.

1. Introduction

Single-layer lattice roof structures that consist of aluminium alloy I-beams have been widely used in long-span buildings owing to their low weight, corrosion resistance, and elegant appearance [1–3]. As welding significantly decreases the material's strength, aluminium alloy I-beams are generally connected using mechanical methods [4] such as the gusset joint shown in Fig. 1a. In this type of joint, two circular aluminium alloy plates cover the top and bottom flanges of all I-beams, and lockbolts (Fig. 1) are used to connect all top and bottom flanges to the cover plate. The lockbolts can be pretensioned and rapidly fastened by specific pneumatic tools (Fig. 2) to realize the high-efficiency installation of aluminium alloy single-layer lattice structures [5,6].

No specific method has been reported for the design of this type of lockbolted lap connection. For the gusset joint, in which the webs of the I-beams are unconnected, a very complex stress state actually exists in the lap connections between the cover plate and the flange because the shear forces in the I-beams have to be transferred through the lockbolts. In current design of aluminium alloy structures, the bearing capacity of gusset joint is normally obtained by loading test [7–10]. Obviously, it is inefficient and even impractical for a complicated aluminium lattice structure with a large number of gusset joints with a varying number, size, and arrangement of lockbolts. Numerical analysis becomes an important measure for investigating the mechanical behaviours and estimating the bearing capacities of gusset joints. However, many difficulties arise when numerically simulating lockbolted lap connections using finite element (FE) methods [11–14], including preload in lockbolts, friction and slippage between plates, the strain hardening [15] and the material nonlinearity of aluminium alloy and stainless steel typically employed for lockbolts. To reliably simulate gusset joints with different forms, an effective FE modelling method should be primarily developed for lockbolted lap connections.

The numerical modelling of lockbolted lap connections has been addressed by a few studies about gusset joints, and some simplifications were usually introduced. Zhao et al. [16] investigated the shear capacity of aluminium alloy honeycomb sandwich plates connected by high-strength, ordinary, or self-tapping bolts; however, the cover plates and bolts were all-steel material, and the preloads and gaps between the bolts and plates were all disregarded. Guo et al. [8] treated the connection as a rigid joint, regardless of its finite stiffness. The lockbolt was

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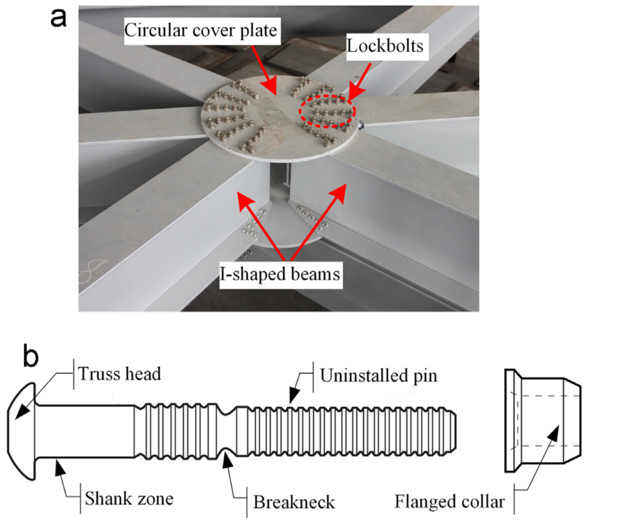

Fig. 1. Aluminium alloy gusset joint system: (a) Assembly details of typical aluminium alloy gusset joint, (b) Lockbolt used in the gusset joint.

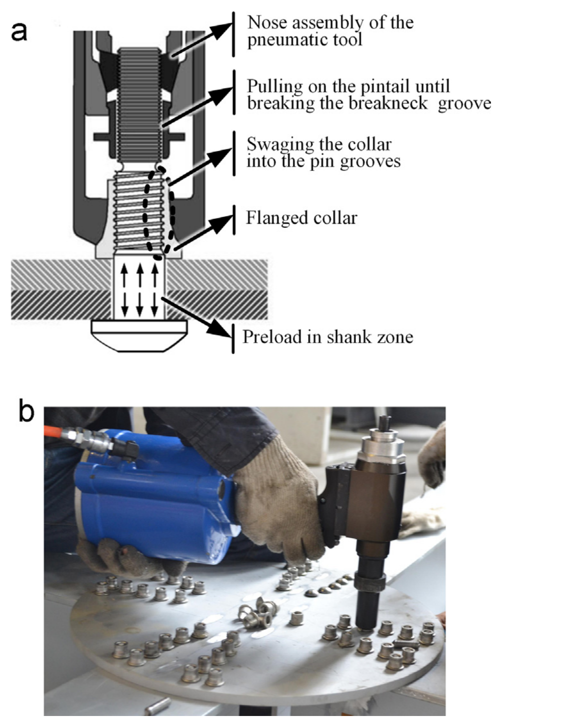

Fig. 2. Installation of lockbolts: (a) Installing process of lockbolt, (b) Pneumatic tool (QM1100).

simply considered by coupling the corresponding nodes at the edge of the bolt holes on the flange and cover plate [17]; however, neither the pretension nor the stiffness of the lockbolts were considered. Solid elements were adopted to model the lockbolts by Zhang et al. [18]; however, the contact and friction between the aluminium alloy plates, as well as between the stainless steel lockbolt and aluminium alloy plate, were disregarded. The diameter of the bolt hole was assumed to be equivalent to the diameter of the lockbolt by Lai et al. [19]; thus, the gap between them disappeared. The preloads in the lockbolts were not considered in this paper. Cho et al. [20] numerically estimated the ultimate shearing capacities of aluminium alloy plates that were connected by a single bolt; however, the bolt preloads and the gaps and friction between the bolt and the bolt hole were all disregarded.

In this paper, the numerical modelling method for the lockbolted lap connections of aluminium alloy plates is discussed based on experimental tests. The layout of the paper is as follows. The mechanical properties and constitutive models of aluminium alloy 6061-T6 and stainless steel 304HC, which are typically employed for aluminium alloy plates and lockbolts, respectively, were introduced in Section 2. The characteristic parameters in the constitutive models of these two materials were determined by tensile tests. The preload of a typical lockbolt was also tested. In Section 3, the tensile test was conducted for a single-lockbolted lap connection to obtain the elongation and strain responses. For this single-lockbolted lap connection, a basic numerical model, that considers the lockbolt preload, interface contact and friction, and gaps between lockbolt and bolt hole, was established in Section 4 by the finite element (FE) package ABAQUS. In Section 5, the validity of the basic FE model was investigated by comparing the numerical results with the experimental results. The friction coefficients of different contact surfaces in the connection were primarily adjusted to minimize errors. The influence of the adopted solid element type and its mesh density were also analysed with an emphasis on the computational efficiency and numerical accuracy. This numerical modelling method was investigated in Section 6 by two types of double-lockbolted connections. Its validity was verified by comparing the displacement and the strain responses between the numerical and the testing models. Some conclusions were formed in the end.

2. Material and preload testing

2.1. Mechanical parameters of materials

In China, I-beams and cover plates in single-layer aluminium lattice shells are typically made of the aluminium alloy 6061-T6, and lockbolts are fabricated with the stainless steel 304HC. Generally, the Ramberg-Osgood model [21] can be adopted to describe the strain-stress ($\sigma - \varepsilon$) relationship of these two types of materials as

$$\varepsilon = \frac{\sigma}{\varepsilon} + 0.002\left(\frac{\sigma}{\sigma_{0.2}}\right)^{n} \tag{1}$$

where E is the Young's modulus of the material; $\sigma_{0.2}$ is the nominal yield stress, which corresponds to a residual strain of 0.2%; and n is the strain-hardening coefficient that reflects the nonlinear degree of the strain-stress relationship. Steinhardt [22] suggested $n = \sigma_{0.2}/10$ for aluminium alloy. For stainless steel, the strain predicted by the Ramberg-Osgood model deviates from the test results when $\sigma > \sigma_{0.2}$. Thus, Eq. (1) was revised by Mirambell et al. [23] and Rasmussen et al. [24] as

$$\varepsilon = \begin{cases} \frac{\sigma}{E} + 0.002\left(\frac{\sigma}{\sigma_{0.2}}\right)^{n} & \sigma \le \sigma_{0.2} \\ \frac{(\sigma-\sigma_{0.2})}{E_{0.2}} + \varepsilon_{u}\left(\frac{\sigma-\sigma_{0.2}}{\sigma_{u}-\sigma_{0.2}}\right)^{m} + 0.002 & \sigma > \sigma_{0.2} \end{cases} \tag{2}$$

where σ_u is the ultimate tensile strength, $n = \ln 20/\ln(\sigma_{0.2}/\sigma_{0.01})$, $m = 1 + 3.5\sigma_{0.2}/\sigma_u$, $\varepsilon_u = 1 - \sigma_{0.2}/\sigma_u$, $E_{0.2} = E/(1 + 0.002nE/\sigma_{0.2})$, and $\sigma_{0.01}$ is the stress that corresponds to a residual strain of 0.01%.

Specimens of these two materials were manufactured for tensile tests according to the China mechanical testing standard [25]. Three equivalent specimens were prepared for each material. The sizes of these two types of specimens are shown in Fig. 3(a)-(d). Uniaxial tensile tests with displacement control were performed for these specimens at a constant speed of 0.02 mm/s. The mean values of the mechanical parameters defined in Eqs. (1) and (2), including E, $\sigma_{0.2}$, σ_u, and $\sigma_{0.01}$ only for 304HC, were calculated for these two materials according to the test results, as shown in Table 1. The strain-hardening coefficients n and m were determined and are included in Table 1.

The strain-stress curves obtained by the tensile tests are shown in Fig. 4(a)-(b). No distinct yield point elongations appear in the curves, which indicates the nonlinearity of these two materials. A simple elastic-perfectly plastic model is inapplicable to the numerical